

FIRE SAFETY

Science and Engineering

T. Z. HARMATHY
editor

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Foreword

This publication contains papers presented at the Symposium on Application of Fire Science to Fire Engineering, held in Denver, CO, on 26-27 June 1984. Sponsors of the event were ASTM Committee E-5 on Fire Standards and the Society of Fire Protection Engineers. The event was organized by Subcommittee E-5.32 on Research. T. Z. Harmathy of the National Research Council of Canada served as symposium chairman and as the editor of this publication.

The editor wishes to thank all those who contributed to the success of the symposium: members of Task Group No. 9 of ASTM Subcommittee E-5.32 and staff members of the Society of Fire Protection Engineers who helped in organizational matters; the authors of the papers with months or even years of hard work behind their presentations; ASTM staff who provided administrative support and monitored the reviewing of the papers; and, of course, the 72 referees who accepted the thankless but important task of reviewing the papers.

Related ASTM Publications

Fire Resistive Coatings, STP 826 (1983), 04-826000-31

Behavior of Polymeric Materials in Fire, STP 816 (1983), 04-816000-31

**Flammability and Sensitivity of Materials in Oxygen-Enriched Atmospheres,
STP 812 (1983), 04-812000-17**

Fire Risk Assessment, STP 762 (1982), 04-762000-31

Design of Buildings for Fire Safety, STP 685 (1979), 04-685000-31

Fire Standards and Safety, STP 614 (1976), 04-614000-31

A Note of Appreciation to Reviewers

The quality of the papers that appear in this publication reflects not only the obvious efforts of the authors but also the unheralded, though essential, work of the reviewers. On behalf of ASTM we acknowledge with appreciation their dedication to high professional standards and their sacrifice of time and effort.

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Introduction

The symposium on which this book was based was the eleventh in a series arranged by Committee E-5 on Fire Standards during the past 25 years. Of the previous ten symposia, four dealt with miscellaneous topics related to fire tests and product performance in fire tests. The other six were devoted to special topics, such as moisture in materials in relation to fire tests, restraint, smoke, ignition, heat release, noncombustibility, design of buildings for fire safety, fire risk assessment, and behavior of polymeric materials in fire.

The Denver symposium differed from the previous symposia in that it looked beyond the problem of testing and product performance. The papers were planned to present a rounded and comprehensive review of the status of fire science and technology.

Some may ask why ASTM should be interested in topics not specifically related to standards and the standards writing process. The answer is that ASTM is a society strongly committed to progress, and progress means searching for solutions not attainable by the application of standard performance tests.

Every test method reflects a level of understanding with respect to the product performance. Experience and the evolution of scientific knowledge are constantly at work to invalidate some existing test methods and to render others superfluous.

In spite of progress, standard fire tests will, for some time to come, yield much of the information needed in fire safety design. It is very important, therefore, that all those involved in the development of test standards be fully aware of the nature and limitations of these standards and be ready to alter or even discard them if science proves them to be inadequate. As fire science probes more deeply into the mechanism of fire phenomena, it comes to light that some of the existing fire test methods were built on precarious foundations. No wonder; they were designed to solve practical problems in an age when those problems were not fully understood.

Unfortunately, altering the test standards is not an easy task. There is usually stiff resistance to any change, partly by the users of the test results and partly by the industry. Having acquired familiarity with the interpretation of the results, the users often find it difficult to adjust to changes reflecting a new level of understanding. And some segments of the industry are also less than enthusiastic. Having tests conducted is a major investment for them in the interests of the marketability of their products, and there is always a chance that the suggested changes in the test standard may lead to a loss of

their market share. To minimize the burden that the changes might bring to the users and manufacturers, it is accepted generally that the updating of old test standards should not be so excessive as to invalidate the majority of available test results. Where major changes are required, the most painless route, it would seem, is to discard the old test procedure and either replace it with another less expensive procedure or allow the use of theoretical methods of performance assessment.

Fire science has come of age during the past 30 years. Although it cannot solve all fire safety problems, it can at least give guidance in the updating of old test standards and in the writing of new performance standards.

Task Group No. 2 of Subcommittee E-5.32 has undertaken the responsibility of scrutinizing all existing fire test standards in the light of available knowledge and making suggestions as to their improvement or replacement. Another task group, Task Group No. 7, has developed nine criteria for good performance tests, to be applied to new test methods. Among them are:

1. A test standard must address a well-defined component of the potential for harm.
2. Those tests that are expensive and time-consuming must be sufficiently fundamental, so that their principal features can be described analytically or by numerical follow-up techniques.
3. The set of prescribed test conditions must, even if in an idealized way, simulate those prevailing in real-world fires with overwhelming frequency. If no single set of test conditions can be regarded as overwhelmingly important, the product must be tested for a range of conditions.

Clearly, it is no longer possible to write performance standards without a thorough understanding of product behavior. Of course, the ultimate goal of fire science is to eliminate the need for performance tests, in other words to make it possible for the fire safety features of buildings to be designed on scientific considerations, supported by test data on basic material properties. The advantage of performance tests to yield early solutions without an insight into product behavior wears off with time as basic research catches up with developments. Inevitably, a stage will be reached when it will be more practical to derive solutions to all but a handful of problems directly from basic knowledge rather than from performance tests. Judging from the progress of fire science during the past 30 years, we have good reason to believe that that stage will be reached not too far in the future. The papers presented at the Denver symposium no doubt will contribute significantly to the preparation of the scientific foundations of fire protection engineering.

T. Z. Harmathy

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Harold E. Nelson¹

The Potential of Scientifically Based Fire Protection Engineering?

REFERENCE: Nelson, H. E., “The Potential of Scientifically Based Fire Protection Engineering?” *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 3–7.

ABSTRACT: Fire science advances of recent years are now creating a state of knowledge that permits the emergence of a scientifically based fire protection engineering technology. It is proposed that a clear overview of an effective engineering approach is apparent and to at least an initial degree supported by engineering capabilities. A conceptual model of the elements of an analytical method is provided along with a list of references that present procedures for calculating the impact of each element. Test methods need to provide the data required to execute the calculation procedure.

KEY WORDS: engineering, fire safety, fire protection engineering, mathematical methods, technology, technology transfer

I will discuss the inviting potential in fire protection engineering. I am particularly optimistic about the developing capabilities for competent engineering approaches to hazard and risk appraisals. The challenge lies in the need for test methods that analytically measure technically valid fire performance parameters useful in engineering analysis.

I believe that there is an emerging scientifically based fire protection engineering technology awaiting those who will make the effort to acquire it. I also believe that such a technology is critically needed to bridge the communication gap between the science and testing community and the applied world of building regulation, design, and operation.

Historically, at least until the past decade, fire research was largely empirical and much of fire testing was abstract and definable only in terms of the testing apparatus used. While empirical research has resulted in some major impacts on methods that have been applied to fire safety, these impacts have

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been sporadic. Conversely, many test procedures have become dominant forces unto themselves without a base in science.

Three or four decades ago this combination of empirical research, surrogate test values, and validation by experience was sufficient. Most buildings were inherently massive and highly compartmented. Wood and paper were the prime combustibles of concern. The rate of change in building technology was slow, and the cumulative history of how buildings reacted when exposed to fire or other stress was a reasonable prediction of future expectations. Our current system of consensus code and test standards arose in that atmosphere.

The code and its companion standard and test system was designed to address not just fire safety but rather the total scope of public health and safety. Wherever credible technology existed, it was incorporated. But when it was not available, committee consensus judgment was used. In the case of fire safety, technology input has been a minor influence; judgment has been the dominant force. The result is a rigid set of requirements and a regulatory system that has difficulty in accommodating to new materials, new designs, and new expectations on both cost and safety. Until recently it was not the practice of consensus bodies to record their objectives or expectations when setting requirements. Even today only a few bodies, such as ASTM, include any type of commentary as part of their official output. Usually the value and intent of code-type requirements are not apparent.

Virtually every code has an equivalency clause that permits alternative approaches provided equal performance can be achieved. It is, however, difficult to demonstrate the required equivalency when the factors which need to be considered are established by consensus. As a result, the code document rather than its original purpose frequently becomes the objective. Expertise becomes entombed in relating fixed requirements to building materials and systems. Sometimes the ability to test and measure a parameter rather than the importance of that parameter determines the requirement. Under this concept, innovation, rational design, and cost control have frequently been constrained and frustrated.

Over the past several decades, however, a relatively small but fortunately persistent group of research scientists and engineers have labored in laboratories and universities around the world. They have dedicated their efforts to determining the basic principles of unwanted fire, to measuring the variables involved, and (in recent years) to developing coordinated engineering approaches to predict the course of fire, the response of fire safety features, and the resulting impact on people, property, and productive missions. As a result, there is a progressively emerging fire protection engineering technology that can potentially be used to evaluate the fire safety performance of a building or structure that differs widely from the current prescriptions of the code. It will also provide an assessment of the impact of a code requirement as it applies to a specific building or set of circumstances.

With the current state of knowledge and data, it is now possible to make at

least a first order quantitative engineering evaluation of fire development and impact from the moment of ignition to the final determination of the results of the fire.

Such an engineering approach can be the basis for individual building analysis or for the appraisal of the generalized requirements for regulatory purposes. Also, by combining engineering technology with a probabilistic evaluation of the likelihood of events and conditions, significant advances can be made in the technology of fire risk analysis.

In order to assemble this emerging technology in a useful fashion, a conceptual model that partitions the problem in a manner responsive to the current available and emerging engineering capabilities is needed. Figure 1 is a diagram of such a model. The model is designed to treat fire as an energy-induced stress on the building and to measure the response of the building and its fire protection systems to that stress. The model also considers the analytical aspects of human response. From an engineering calculation standpoint, the key elements are those in the boxes labeled GROWTH & TRANSPORT, FIRE SAFETY SYSTEMS, and ACTIONS.

Usable analytical calculation methods now exist for each of the elements shown in these boxes. In a number of cases, several established engineering approaches are available. I have selected a series of established procedures

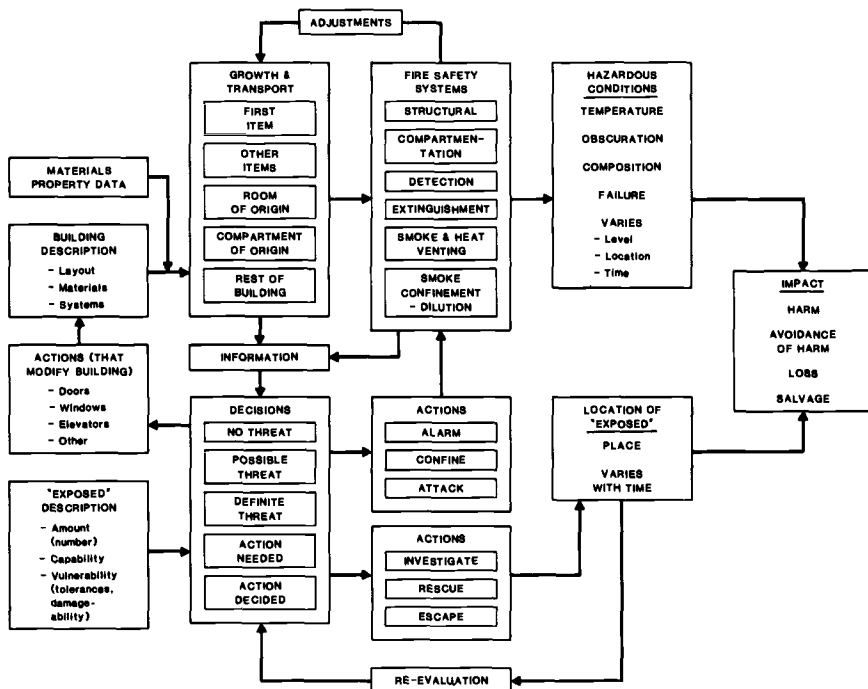


FIG. 1—Model of elements of an engineering approach.

that are expressed in terms of level of imposed energy stress, the response of the building and protection systems to that stress, and the resulting impact on environmental conditions in the building. Sources for these procedures are listed in the Bibliography. The listed references are not necessarily the most advanced engineering methodology but are rather those that are available in the open literature to engineers or scientists willing to expend the effort to obtain and use them.

As the new fire protection engineering technology emerges, it produces an increasing demand for data. The type of data needed are measurements of physical values that meet the demands of the calculation systems. The testing community must concentrate on the development of valid reproducible tests of engineering quantities and step away from tests that only rank order or provide measurements in terms of arbitrary values relatable only to the test device involved.

Meeting the dual challenge of improvement of the calculation methods and the provision of the supporting data is the key to moving fire safety from an indefinite to a definite technology.

Fire protection engineering technology is past the embryotic stage but is still a struggling child that continues to require support and encouragement. The development of the underlying science and the production of scientifically based data must continue to nurture this technology.

I am, however, firmly convinced that the maturity of fire protection engineering as a fully useful and credible technology will occur. The pace at which technology replaces subjective judgment is a function of the level of interest, demand, and support given by the fire protection engineering and related research and testing communities. Key to this technology development are the assembly of research into appropriate engineering forms, the production and cataloging of the essential data, and continued emphasis on proof testing and other verification programs.

Acquiring a truly scientifically based fire protection engineering capability will be essential to those who wish to continue in the field of fire protection engineering. Development of scientifically based analytical measurement methods is essential to this. Changes must take place. Past practices have required expenditures for fire safety beyond that appropriate for the level of safety provided.

Finally, I believe that the time of "burn to learn fire research," "test to rank" measurements, and unengineered arbitrary requirements has passed whether or not those of us in the field recognize that fact. We now have made critical advances so that all fire experiments large and small should be preceded by the best engineering predictions and the results used to verify and improve analytical methods.

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Fire Performance Standards for Buildings

REFERENCE: Marchant, E. W., "Fire Performance Standards for Buildings," *Fire Safety: Science and Engineering, ASTM STP 882*, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 8-20.

ABSTRACT: A building is described as a multifunctional agent of environmental change that has to achieve adequate and acceptable performance so that a safe and comfortable environment will result for any human activity. The 14 aspects of whole building performance are listed and examples of advantageous and disadvantageous interactions are offered. Fire safety performance interacts with all other aspects and is distinguished by not being part of the day-to-day environment. All fire safety problems cannot be solved by the application of conventional elements of fire safety technology, and, therefore, the performance approach to fire safety is important as more flexibility in the selection of solutions becomes possible. The identification of relative values of the components of fire safety is an important problem, and these values change according to building type. Performance profiles and levels of acceptability are discussed. Difficulties associated with the measurement of fire safety performance are discussed, and the selection of appropriate appraisal techniques is emphasized. The control of fire safety standards by governmental officials and private professionals is examined, and mention is made of the possible introduction of self-regulation for continuing fire safety standards in buildings.

KEY WORDS: building, performance, interactions, fire safety, testing, regulations, control

The production of an efficient building depends on the ability of the designer and his consultants to assess clearly the functional or performance requirements that the building will be expected to fulfill. The requirements should be capable of definition as they will reflect the activities to be carried out within the completed building. The building design team interprets and integrates the functional requirements into a building design that is likely to result in a pleasant, comfortable, and safe building.

The need for a building is established easily as very few human activities

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can be pursued outside a building, and a major reason is because the natural environment is not always an acceptable one in which work or leisure tasks can be pursued effectively; a building is definitely required if the general security of an activity is an important subfunction. A building therefore will displace part of the natural environment and replace it with an environment that is cooler or warmer, lighter or darker, safer or more dangerous than the environment that it replaces.

A building is therefore a multifunctional agent of environmental change and acts principally as a modifier of the natural environment. This concept has been discussed by Jokl [1], who regarded the environment in which a "subject" (man) exists as the "transfer field" that contained all the agents of environmental change, and who proposed that combinations of agent-flux and agent-intensity produce the environmental stress at a particular instant. Human reaction (the reaction of the subject) to the environmental stress was considered capable of depression as a syndrome—a general state which comprises any number of "symptoms," that is, the manifestation of a general human reaction to a specific stressful agent. For example, the change in physiological and psychological responses which the illumination levels in an environment change beyond the limits of perception at an instant in time would be regarded as a symptom of the change in an environmental agent. An overall expression of "well being" would describe a "syndrome," a combination of reactions that allows man (the central subject) to perceive an acceptable environment.

It can therefore be understood easily that if a building is to produce a satisfactory environment, an analysis must be carried out of the differences between the properties of the natural environment and the desired properties of the environment to be created within a building. Only with a knowledge of these differences and their consequences on "the subject" can the designer progress towards the creation of an acceptable and safe building.

As an aid to the classification of the components of the natural environment and to assess the degree of change needed to achieve an acceptable environment, Berköz [2] has proposed a design model for a natural climatization subsystem. The use of this model will help the designer to specify the optimum performance required of the building, as a sequence of barriers, to modify the natural climate so that comfort conditions are achieved. The model, an interactive network similar to those being developed for aspects of fire safety design, contains required performance profiles for the various sub-components of climate, and the quantitative performance requirement for each climatic subcomponent will vary according to its quantity in the natural environment and the acceptable range of quantitative values for that component in the building space. An example of a climatic subcomponent that affects fire safety directly is the natural wind [3,4]. The natural wind can negate all the advantages of a smoke vent system in simple buildings and can reduce the additional safety provided by mechanical smoke control to an unaccepta-

ble level. The air tightness of the external envelope, together with the leakages of barriers inside the building, will dictate the infiltration and exfiltration rates of air movement. This rate of movement is an important influence on smoke movement potential in a fire emergency but has day-to-day implications with respect to ventilation (that is, air changes in a building space) and the formation of surface and interstitial condensation on and in the constructional components [5]. The reduced thermal insulation, caused by the presence of interstitial condensation, may have an influence on the time to flash-over of a developing fire.

The multifunctional role of building components has been examined by Maggi et al [6], who offer a systematic method for the analysis of the performance required of all building elements with respect to their "ideal surface area," and who give an example of a matrix of building elements and expected performance that indicates clearly those building elements which should perform at the highest or the lowest possible levels. Examples of quantitative environmental requirements are given for acoustical hygrothermal and visual aspects of the internal space. The performance requirements for fire are limited to the thermal and chemical aspects of fire resistance.

The many developments [7,8] in understanding the complex problems of environmental performance and the control of the desired performance of the space(s) inside buildings indicate that the performance concept in the design of buildings is becoming increasingly important.

The need to pursue a performance approach to building design has been established for a long time [9,10], and fire safety performance is recognized as one of the major aspects of building performance. Progress towards the use of performance specifications for whole buildings is very slow because of the difficulties associated with gaining agreement on performance criteria and standards and on methods of test or assessment for performance attributes [7]. Some aspects of whole building performance, with special reference to fire safety, are discussed in the following sections.

Aspects of Whole Building Performance

The principal objective to considering the performance that a whole building needs to attain is "to assist in achieving a uniform approach among experts in building design and construction systems and for the international harmonization of building regulations." A definition of whole building performance may be: the simultaneous attainment of acceptable levels of performance in all aspects of in situ performance. Success of the performance approach is dictated by both the reality of the specification and verification standards of performance being expressed quantitatively and without reference to the specification of materials. The International Standards Organization [11] has defined 14 aspects of whole building performance: stability; fire

safety; safety in use; tightness; hygrothermal, atmospheric, acoustical, visual, tactile and anthropodynamic performance; hygiene; spatial suitability; durability; and economy.

Because of the interactive nature of the aspects of building performance, fire safety should be assured in an integrated performance design. Unfortunately, a malfunction of the fire safety components may not be perceived readily by the users—due to the intangible benefits of fire safety ordinarily—but this can be compensated for by monitoring devices, thereby maintaining the appropriate level of safety. The full value of the interactive fire safety performance is likely to be manifest only in real fire emergencies. The determination of the relative values for each aspect of performance has been identified as a significant problem in the development of performance specifications [7]. Assuming that the expected performance of all aspects can be achieved perfectly, the value of a perfect performance will be different for each building type as the relevance, or importance, of each aspect changes with building type. Figure 1 shows three notional performance profiles which describe the relationships of six aspects of performance for three different building types. Examining the notional values for hygiene, it is clear that the perfect relative value for the furniture store is very low compared with the other types. The expected relative values for fire safety are very high for the hospital ward because of the public anxiety that would be aroused if patients died in a fire; the much lower value given to the furniture store indicates a lack of public interest and a low life safety potential threat (within the space). The level provided indicates some valuable property (contents).

Assuming that sets of values could be agreed upon, the importance of achieving the target performance needs some assessment as no absolute value can be achieved precisely. Some concept of performance tolerance or the acceptable limits of performance needs to be developed. Such limits or banded performance profiles have been suggested by Blach [12]. Figure 1 indicates that the range of performance that may be acceptable varies with the relative importance of the aspect of performance and the tolerance of the user for deficient or enhanced performance.

This concept encompasses or ignores the fact that each of the 14 aspects must interact with all other functional aspects and the fact that that interaction is likely to enhance or degrade the performance of one aspect or another. Appropriate performance in any aspect will have an impact on the level of fire safety, and the attainment of ideal fire safety will influence the performance level of all other aspects of performance. The interactions of fire safety and the other aspects have been discussed [13], therefore only a few examples are presented here.

For example, the interaction of building tightness and the hygrothermal performance of a space will influence the chance of condensation, which may have a “second order” effect on fire growth. Another example is the interaction of the aspects of tightness and economy. Excessive infiltration and exfil-

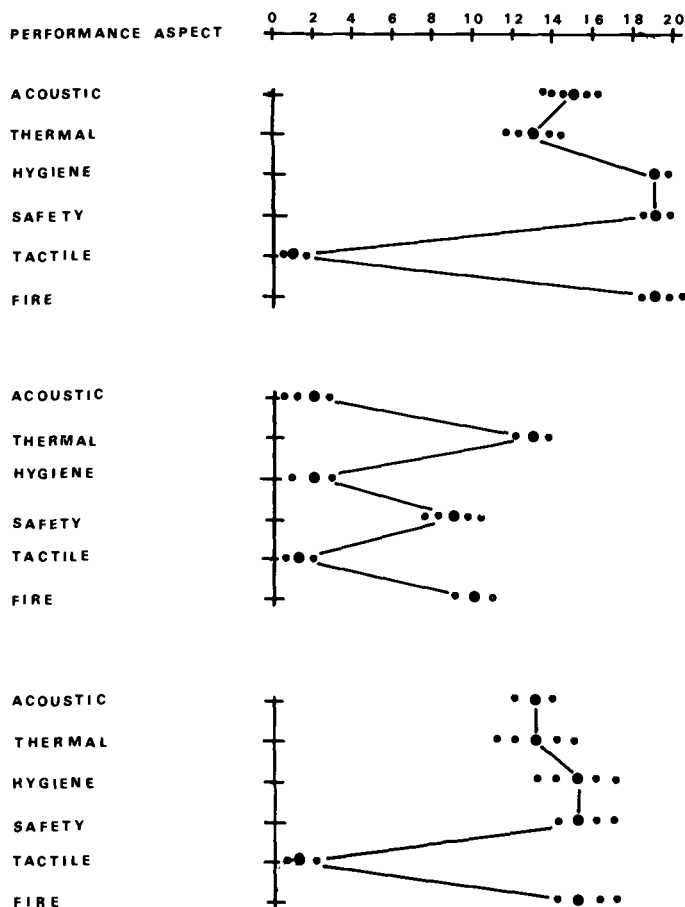


FIG. 1—Notional ideal performance profiles on relative performance scale (after Blach [12]).

tration of air could result in excessive costs for heating or cooling the building and could affect potential smoke movement patterns within buildings.

Fire safety performance interacts with all other aspects, and, therefore, the performance of the whole building will affect the level of fire safety provided. For example, the acoustic and anthropodynamic aspects need to be understood in the contexts of communication and escape path design, considerations that are essential for the achievement of adequate life safety.

The general concept of the importance of interactions was discussed some 40 years ago [14] in a discussion on the assessment of innovative design and constructional techniques for housing.

Performance and Components of Fire Safety

The definition of performance targets presents a range of problems as one expectation within the performance approach is that the performance of each aspect should be verifiable quantitatively. The various components of the comfort environment can be measured fairly readily because of the well documented relationships between user satisfaction and objective measures. The targets for fire safety are much more difficult to ascertain as the level of fire safety in a building is not an attribute that can be perceived readily. Only in the event of a real fire can the effectiveness of fire safety measures be verified directly. Because of the destructive nature of fire, such in situ verification cannot be a common feature of tests for the adequacy of building performance, although several well-documented test fires have enhanced the understanding of fire behavior. This type of study and a knowledge of national loss trends is some help in the assignment of relative values. Another approach is the analysis of codes of practice (fire codes) that have been applied to particular building types where the overall level of loss has been found to be acceptable. The analysis of fire codes identifies the relevant components of fire safety, but in the absence of definitive detailed statistics it is not possible to know which component is more, or less, important than another. A “temporary” solution to this problem is the use of a Delphi group (a set of experts in fire safety subjects—wise men) [15,16]. This approach has been used in the development of at least two successful schemes for fire safety evaluation, both of which included the development of values for perfect components [17,18]. In these evaluation schemes, the deficiencies in each component are assessed separately and then the results are integrated to give a measure of building fire safety—a static measure of the intangible! The values assigned to each component by the Delphi group depend on the fire safety objective(s) that the component is expected to fulfill, which may be property protection, life safety, or a combination of both subaspects of fire safety. The following list gives the proportionate percentage for each of the 20 components of fire safety [18] with respect to the triple goal of life safety, property protection, and mission continuity. A second set of values represents the proportionate values for life safety alone.

In the development of each of the evaluation schemes [17,18], there is no explicit recognition of the contribution of each component to fire safety during the various stages of fire development. A formal step to represent the dynamic nature of fire protection was presented in 1982 [19] in the form of a matrix between fire controllable and controlling factors and functions for fire safety objectives. The positive interactions were identified with the state of fire development sequence where the greatest impact was likely to be made.

The specification of particular components of fire safety can be made in performance terms as sufficient knowledge is now accumulated to enable the fire safety expert to give advice to the building designer on the selection of

No.	Component	Contribution to the Triple Goal, %	Contribution to Life Safety Alone, %
01	staff	9	7
02	patients and visitors	6	6
03	factors affecting smoke movement	7	7
04	protected areas	6	7
05	ducts, shafts, and cavities	4	3
06	hazard protection	7	5
07	interior finish	5	4
08	furnishings	6	2
09	access to protected areas	4	6
10	direct external egress	4	6
11	travel distance	5	7
12	staircases	5	7
13	corridors	5	7
14	lifts	3	4
15	communication systems	5	4
16	signs and fire notices	4	5
17	manual firefighting equipment	3	1
18	escape lighting	5	6
19	autosuppression	3	2
20	fire brigade	4	4

alternative “packages” of components that will all give the equivalent level of fire safety. A major problem remains—the verification of in situ performance.

Testing—Verification of Performance

The main objective of testing, or verification of performance, is to ensure that the completed building meets the objective performance criteria that were set after the performance requirements (or functional requirements) had been made. At present there is no universal acceptable and objective method of assessment by test of fire safety performance for whole buildings after completion. The best that can be achieved is the integration of the results of a mixture of assessments that have been carried out at different times and which have been applied to the subsystems, components, or subcomponents of the fire safety aspect of building performance. The assessment techniques range from full scale ad hoc tests through standard tests and simulation by computation to observation and judgement by experts. Each of these techniques is valid and needs to be used in appropriate circumstances [19]. The use of a variety of techniques will give results which can be classified in one of four categories:

1. Direct and predictive.
2. Indirect and predictive.

3. Direct and retrospective.
4. Indirect and retrospective.

In each category the results may be quantitative or qualitative. For example, the in situ testing of a smoke control system (a fire safety subsystem) using a real fire in a completed building [20] would yield results that are direct, retrospective, and quantitative. Other subsystems, such as escape route design and structural stability, can be assessed by computation or simulation and yield indirect, predictive, and quantitative results. Standard tests for the fire resistance of walls (components of the fire safety aspect) or pieces of structure (subcomponents such as beams and columns) would yield direct, predictive, and quantitative results that need to be combined with similar results for subcomponents, such as fire-resisting doors, before a prediction of potential barrier fire endurance can be made.

Wall linings can be submitted to a standard test for the surface spread of flame, but the results are somewhat inadequate for the prediction of fire behavior and a "corner test" has been developed [21] (E603-77), the results of which give a more realistic assessment of room fire behavior.

Tests on materials emphasize their flame spread potential, the smoke production potential, and heat release [21]. Acceptability of materials may be defined in standards (codes) and regulations as the attainment of specific scores achieved under test conditions. For materials, subcomponents, and components, the ASTM Fire Test Standards [21] lists 63 standards, 57 of which relate directly to building. The results of testing usually aid the selection of materials and components as a standard test is best used as a comparator, that is, it allows the object tested to be placed in a rank order of performance, as measured by the test.

It was mentioned previously that test results may be obtained or assessments made at different times, but generally they will be collated in the pre-construction phase of a building project so that the appropriate materials can be selected. The test results can be unique, that is, tests on innovative materials and components or taken from "catalogues" of test results, the designer selecting the material and mode of construction that has given the required performance in a previous test. Once all the test results, simulations, and computations have been carried out and the overall fire safety assessment of the design has been completed, the designer can be reasonably confident that the building that will result from the design will have a standard of fire safety acceptable to all the relevant code officials (building control officers and fire prevention officers).

During the construction process, minor or major modifications to the design may occur. Each variation must be assessed for its impact on fire safety before it is approved. Once the building is completed, some assessment needs to be carried out to ensure that all that was specified actually exists in reality in terms of location, quality, and performance. This area of assessment has

been examined by Alexander [22]. A situation is described where great confidence was placed in the contractor to ensure that necessary performance verification had been carried out. Although the intentions of the contract and the acceptance of responsibility by the contractor may be clear, Alexander comments that "without field testing or performance trials on completion" the attainment of an acceptable product relies heavily on the "professional builder." Naturally, the quality of the completed building is of great importance to the code official (and building control officer) as compliance with the approved design is necessary before the building can be released for occupation.

The immediate postcompletion stage, occupation, is of great interest to the fire authorities in the United Kingdom, especially for those building types which require a "fire certificate" before full occupation and operation. Assessment of fire safety provisions will be made but are usually limited to observation and overt performance (for example, to ensure that water actually flows from a hose reel). However, two components of life safety that can be measured nondestructively, alarm noise levels and the illumination level of emergency lighting systems, are being assessed quantitatively and compared with objective criteria giving a true measure of performance. For some building types in the United Kingdom, an emphasis is placed on the role of building management as part of their fire safety performance. This enables such factors as fire safety knowledge and awareness of staff and visitors to complement the fire threat control systems that are implicit in building performance.

Control of Performance

The principal control over building performance in the United Kingdom is the building regulations. Their major intention is to ensure adequate provision for health, safety, welfare, convenience, and energy conservation for a reasonable cost. In the United Kingdom, four sets of legislation operate and apply separately to Northern Ireland, Scotland, England, and Wales (excluding Inner London), and Inner London. The regulations that operate in Northern Ireland are similar to those for England and Wales, and building control in Inner London operates under a complex set of acts of Parliament, local regulations, and codes of practice.

The regulations for Scotland [23] contain long sections on "structural fire precautions" and "means of escape from fire and assistance to the fire brigade." Both parts contain a mixture of performance requirements and specific requirements. The former may specify periods of fire resistance but with no direct material specification, although schedules of "deemed-to-satisfy" constructional specifications are included in the volume. Any material can be used that is not "deemed-to-satisfy," providing test results or calculations verify the potential performance. Specific requirements are usually dimensional, for example, maximum sizes of compartments and travel distances.

Proposals are being considered at present to recast the regulations in terms of functional requirements and qualifying standards [24] within the statutory instrument as a means of simplifying the presentation of the regulations and emphasizing the opportunities for innovation.

Similar changes are more advanced in England and Wales, and examples of the functional requirements on fire topics that will become the legal requirement are as follows [25]:

“B2 Internal Fire Spread (Surfaces). In order to inhibit the spread of fire within the building, materials used as exposed finishes on walls and ceilings, and in cavities (a) shall be reasonably resistant to ignition; (b) shall be reasonably resistant to the spread of flame over their surface; and (c) shall not, if ignited, have an excessive rate of heat release” [25].

This functional requirement applies to elements of the building, namely, walls, ceilings, and cavities.

“B3 Internal Fire Spread (Structure). (1) The building shall be so constructed that, in the event of fire, its stability will be maintained for a reasonable period. (2) The building shall be subdivided into compartments where it is necessary, regard being had to its size and proposed use, to inhibit the spread of fire within the building. (3) Concealed spaces in the structure or the fabric of the building shall be enclosed and subdivided where it is necessary to inhibit the unseen spread of fire and smoke” [25].

Functional requirement B3 applies to the whole building.

These regulations are to be supported by nonstatutory “approved documents” that will contain relevant performance criteria and some approaches to the attainment of appropriate performance. These approaches may include: deemed-to-satisfy specifications, technical design guides, and references to methods of calculation so that solutions can be obtained from a consideration of basic principles. The *only* functional requirement not to be supported by a flexible approach to its fulfillment is that for means of escape!

*“B1 Means of Escape. (1) Means of escape from the building shall be provided, and such other work (excluding any provision of fire-fighting equipment) shall be carried out as may be necessary to ensure that those means of escape can be safely used at all material times. (2) The requirement in subparagraph (1) may be met only by the making of provision in accordance with the relevant requirements of the publication entitled *Mandatory Rules for Means of Escape in Case of Fire*, published by HMSO (1985 edition)”* [25].

The requirement for means of escape applies to whole buildings but will be of limited application, initially applying to blocks of flats (apartments), shops, offices, and dwellings of three or more stories.

Generally, a greater freedom for the designer should result from the current changes in building legislation.

In parallel, a change in the responsibility for compliance with the building regulations is expected. The Building Control Act 1984 [26] will allow private citizens with appropriate technical and professional qualifications to make application to become approved inspectors. That is, they would be able to approve building designs for compliance with the regulations, and they would be expected to certify that a building had been built in strict accordance with the approved plans—a private and professional alternative to the local authority building control system. The advantages to the building owner (the developer) may be a fast response and the availability of greater technical expertise, especially for complex, sophisticated buildings.

For his part in the new control system, the private-approved inspector will be expected to insure against his own negligent performance [27], and he will become liable for any contraventions of the building regulations, including the cost of remedying any defects that have caused the contraventions. As the regulations include the fire safety subsystems of means of escape and structural fire resistance, the approved inspector could be liable for injury or the death of a building occupant or any structural collapse during a fire if part of the subsystem for which he is responsible fails in its performance, although he will have no control over the contents of the building that are likely to be the initial cause of the consequences of ignition.

The professional responsibility for fire safety is likely to be further extended. At present the Fire Precautions Act [28] is administered wholly by local fire authorities on behalf of government and applies to a few selected building types. There is an intention to broaden the scope of the act to all buildings and to place the responsibility for achieving, and thereafter maintaining, a reasonable standard of fire safety on the person having control over a particular building. The broadening in scope could be supported by codes of practice supported by advice from the fire authority or from private sources (for example, architects). This intention is extending the principle of self-compliance. Effectively there will be no need for fire service involvement in any scheme for upgrading the standards of fire safety until the final inspection by the fire authority. Such a situation may bring into focus the need for a high caliber profession of fire engineers in the United Kingdom as exists in the United States.

Concluding Remarks

The movement towards (functional) requirements as a replacement to prescriptive specifications for the regulation of health and safety provision in buildings is almost complete in England and Wales. In some aspects of building performance, truly objective performance criteria will be lacking for the

time being, notably for means of escape. However, most aspects of performance will be capable of positive assessment at some stage in the construction sequence, although there are many problems to be solved before objective performance measurements can be made in a completed building [29]. Professional judgement and predictive tests, together with more full-scale ad hoc tests of fire behavior in buildings, should bring about a continuing growth of knowledge that will be applicable directly to the potential fire performance of individual buildings. Such knowledge will be essential for both the fire authority expert and the private professional adviser as the creative and innovative designs of buildings and their components continue to be generated. In some countries, the conventional classification of buildings by occupancy group is too broad an approach to allow a direct correlation between "fire risk" and building type. For specialist purposes, building types may need to be reordered into risk categories because of the wide variation in risk factors, such as fire load and occupant mobility, within the present occupancy classes.

The control of fire safety extends beyond law to logic and insurance. It is to be hoped that the approved inspectors (England and Wales) will be able to bring more logic into fire safety design as the knowledge base continues to expand so that their decisions are not too inhibited and not too conservative, even when faced with a continuing liability for negligence traded off against insurance.

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Fire Risk Assessment: Integrating Fire Testing and Fire Codes

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ABSTRACT: Fire risk assessment, using experience as a basis, has led to the development of fire test methods and building codes and standards. Fire risk is composed of the multiple elements of potential for harm, probability of occurrence, and probability for exposure. The integration of fire test methods and building codes and standards is part of a multiple-element response to fire risk.

KEY WORDS: fire risk assessment, fire tests, building codes, fire test laboratories, regulatory authorities, mathematical fire modeling

The concept of fire risk assessment is one which has received considerable attention in recent years. At the same time, there has been little agreement as to its definition. Yet, fire risk assessment processes are used by many involved in the development, reporting, and application of fire test data. Reviewing these processes can assist in achieving the objective of bringing the concept of fire risk assessment into sharper focus. Fire risk is composed of:

1. The potential for harm (or hazard).
2. The likelihood of occurrence.
3. The likelihood of exposure.

The objective of fire risk assessment has been to reduce the combined effect of these three elements to an acceptable level. Because fire risk is multifac-

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eted, the response mechanisms to fire risk are multifaceted. These response mechanisms are composed of:

1. *Fire tests*, which assist in assessing the potential for harm.
2. *Building codes and standards*, which assist in controlling the likelihood of occurrence and exposure.

Experience has been the predominating factor in shaping both the building codes and the standards used to regulate construction with respect to fire. Requests often are received by testing and certification organizations to undertake investigations of products or materials for which neither testing procedures nor code requirements or both previously have existed. One response is the development of a broad assessment of the use of the product and its field record, if the product has been in use, or its similarity to other products for which testing procedures and requirements have been developed, and a review of applicable or related installation codes and standards. This process has led to the development of many of the test procedures and requirements used in product certification programs. In some instances, this process has been applied within the framework of specific existing installation codes and standards and in other instances has contributed to changes in or the development of installation codes and standard requirements.

The Evolution of Fire Risk Assessment

In order to gain a better understanding of fire risk assessment, it is instructive to review both the evolution and current character of the fire risk assessment process.

In earlier times, the practice of risk assessment was exercised in a simple manner. Under the code of Hamurabi, if a structure failed in some fashion, the builder bore the consequences. Of course, in early societies in which the habitat usually consisted of stone, caves, or materials such as clay brick or adobe, fire was not likely of major concern. The evolution of society was accompanied by changes in the built environment and by new fire problems. In the Middle Ages, as communities grew in size, the widespread use of thatched roofing frequently resulted in the destruction of entire communities by fire. The assessment of fire risk in this circumstance led to the logical societal response of banning the use of thatch for roofing. As society became more sophisticated and complex, public buildings, factories, and mercantile centers were constructed. These structures led to other more complex fire problems. Improvements in construction and general practices advanced rapidly in the eighteenth and nineteenth centuries, and the problem of fire was generally narrowed from the danger of burning down entire communities to burning down districts or city blocks. The fire risk assessment response of society was to call for fire separation walls between buildings, which served to help restrict the problem of unwanted fire to individual buildings. The next logical

step was confinement of fire to individual compartments within buildings. Early in this century, attention turned to the contents of individual compartments, particularly where those compartments were relatively large or were likely to contain large numbers of people at any given time. The fire risk assessment process in this case identified the need to control the fire-spreading characteristics of materials used in buildings and to provide for other factors related to life safety, such as exiting requirements.

In each of these examples, fire risk assessment was applied. Banning thatch resolved the problem of conflagration; fire walls have helped to keep fires confined to a single building; compartmentization has helped to keep fire confined to a single room; sprinkler and detection have similarly had their effect. In each case, society learned by experience. However, as the magnitude of the problem in each succeeding step in this evolution has been reduced in scope (that is, from the destruction of towns to the destruction of city blocks to more localized events), the complexity of the remaining problem has increased in some approximate inverse proportion. The broadest problems have been the easiest to solve while the narrower remaining problems are the most challenging. Some of these remaining problems can be solved through reaction to experience, while others will require more analytical solution.

Codes as Fire Risk Assessment Mechanisms

As society learned from its experiences, its collective responses were codified. The codes which have evolved are a direct outgrowth of the assessment of fire risk in individual circumstances. For the most part, these codes express means for controlling risk by prescribing building details and performance requirements for building materials, for products, and for systems performance. The means for assessing the performance of the elements often take the form of standardized methods.

The general requirements applied to buildings through codes recognize the fact that various buildings are used in various ways. The codes do this by applying different requirements to different buildings based on these various uses. The size and number of doors, size of rooms, distance to exits, requirements for sprinklers, fire resistance requirements, and so forth all vary depending on the intended use of the building. Through such mechanisms, the codes serve in part to regulate those aspects of fire risk associated with likelihood of occurrence and likelihood of exposure. Of course, the enforcement of these codes also becomes a part of controlling fire risk. Whether or not they will be enforced becomes a part of fire risk assessment. A code requirement relative to the marking of exits in places of assembly is of no value if it is not enforced. Such factors introduce uncertainty into any fire risk assessment.

As accumulated experiences indicate circumstances which are not addressed by existing codes, the experience often results in changes to the codes,

which includes changes that may make a code more permissive, more restrictive, or, simply, more easily understood and applied.

Fire Tests as Fire Risk Assessment Mechanisms

Where building codes specify that certain performance characteristics are required for specific fire building elements, the specification usually refers to a particular fire test method. For the most part, these methods are based on experience.

In some cases, codes indicate that some characteristic behavior is required but do not specify how the characteristic behavior is to be measured, nor the level of performance to be achieved. Recent revisions to the National Electrical Code (NEC) NFPA 70 have specified that wiring installed in certain locations shall be of a type which is of low smoke producing and low flame spread characteristics. The NEC, however, specified neither the method to be used to make smoke and flame measurements for cable nor the specific level of performance expected. In this case, field experience was surveyed to assess what, if anything, could be learned as to anticipated performance, adverse or otherwise. Such information, it was thought, could assist in determining characteristics of concern, test conditions which might be appropriate, and related factors. Then, actual installations were surveyed in order to determine how the product was actually used. This led to the conducting of tests in simulated structures, which were intended to replicate the particular condition described in the code. Various ignition scenarios were used with various wire and cable of varying combustibility characteristics. Correlative, laboratory scale tests were conducted. From (tunnel furnace) observations made, the specific test method UL910 was developed and is now being successfully used to respond to the original NEC requirement.

Product Safety Testing Organizations as a Fire Risk Assessment Mechanism

Third party product testing and certification laboratories serve as an element of the fire risk assessment mechanism by performing fire tests and assisting in conveying the results of testing to the users of the codes. These same organizations, along with universities and other research institutions, also contribute to further development of both the codes and the test methods.

Frequently, certification laboratories find themselves in the circumstance of having to develop a test method. This generally occurs under one of three circumstances.

1. *No code or standards requirements.* A manufacturer submits a product to the lab for which no obvious test method or code requirements exist. This may involve a product for which the specific ultimate end use is not clearly identified. In such a case, it might be a construction material that could be

used in a variety of ways and consequently subject to a variety of potential exposures. It also might be a product whose functional use is readily apparent (such as furniture) but for which no tests or code requirements exist.

2. *Code or standards requirement exists.* A code requirement is written which dictates that products must meet certain requirements without specifying what those requirements are. An example might be the specification that furniture be fire resistant. Another example might be the specification that cables shall not be capable of propagating flame from floor to floor when installed in a vertical riser shaft.

3. *Adverse field experience exists.* Adverse field experience has occurred, and tests or other remedies are needed.

Developing Fire Risk Assessment Tests

Defining the Problem

When the use of a new product or a new circumstance requires the development of a fire test, several questions are typically asked. These questions may include:

1. What is the product?
2. How is the product used?
3. What are the likely exposures?
4. What has been the field experience?
5. If there have been adverse experiences, can the following be answered?
 - a. What were the exposure conditions?
 - b. What was the situation?
 - c. What were the results or developments?
 - d. Are these conditions typical?
6. Can field experience be replicated in the lab?
7. What are the product sensitivities? That is, are there certain conditions under which a product is relatively passive but which under a slight modification renders dramatic changes in performance?
8. How does the product contribute to fire circumstances? Can other elements of the scenario be altered?

Economics becomes a consideration. This would include the economics of a test as well as the economics of the potential elimination of a product by forcing the product out of the market or by forcing more expensive variations of the product to be required.

Finally, when a test method is conceptually developed, the questions must be asked whether authorities having jurisdiction can relate to the test method and test data. That is, will users of such information see a relationship between the test and the problem which they are intending to solve.

Developing Information

Several approaches are used in the development of test methods to respond to the need for fire risk assessment methodology. These approaches include:

1. *Experience simulation.* In circumstances where adverse field experience has occurred, this might include identifying specific field experiences and replicating those experiences in the laboratory. Observations made in the laboratory then can be used as a basis for formulating test methods. Where adverse field experience has not occurred, laboratory situations can be created which simulate the intended application or environment of the product. These simulated situations then can be challenged through the introduction of exposure fires and observations made. Again, experience gained through such simulations can be used to develop tests. An example is the development of the use of the 25-ft tunnel by Underwriters Laboratories, Inc. Another example is the use of a calorimeter by Factory Mutual Research Corp. for developing ratings for under deck flame spread for roof constructions. This also involved the intermediate step of simulation tests of adverse field experience.

2. *Related test methodology.* Tests may be applied that are applicable to other products which are similar or which may be used in a similar way. Development of a test method for an item penetrating a fire-resistive partition might appropriately be based on the requirements applied to windows, doors, or dampers.

3. *Comparative analysis.* Tests may be developed by making a determination of what functionally similar products are currently considered as being acceptable and then determining the relative performance of alternative materials under various exposure conditions of interest. Currently acceptable products form the baseline for proposed alternatives.

Integrating With the Codes

After information has been developed concerning the fire test method proposed for use as an element of fire risk assessment, the remaining step involves integrating the information with codes. The implementing step will involve various interests, including product manufacturers, testing and product certification organizations, and authorities of jurisdiction including building officials and insurance interests.

Regardless of the specific individuals involved or specific procedures followed, it is important to integrate the laboratory data with experience in implementing a fire test method as a part of the fire risk assessment and control process. It also should be recognized that the process cannot guarantee that the outcome or consensus will in all cases be perfectly correct as measured against future events. This is because the process is largely predicated on judgement reflecting individual experiences and individual opinions.

Thus, the practice of fire risk assessment can be seen quite clearly. Fire risk assessment is the process for determining and evaluating the risk of fire, the result of which can be used in codes and standards. A part of this process involves writing down specific provisions and prohibitions, and a part of it involves specifying expressions of anticipated permissible behaviors to be measured by test methods.

The Future

As noted, the response to adverse experiences has resulted in society being able to successively narrow the scope of adverse experiences. It was noted, in that connection, that as the individual problems narrowed in scope, their complexity increased. To illustrate, while questions of appropriate time temperature curves, pressure differentials, and so forth continue to be of interest, basically the matter of applying the concept of fire-resistive partitions between buildings or compartments within buildings is well established and is relatively simple. Likewise, the earlier problem of eliminating thatched roofing as a means of eliminating large-scale conflagrations in communities with thatched roofing is a relatively obvious problem with a straightforward solution. Conversely, the residual, unresolved fire protection problem in North America today, predominately concerns individuals who lose their lives one or two or three at a time in fires which often do not extend significantly beyond the area of origin in terms of property involvement. The current state of the art and research applied in the study of combustion products toxicology testifies to the complexity of this problem. Similarly, the role of furnishings or stored contents other than those which can be regulated by virtue of their being a fixed part of a structure represent a difficult challenge.

Here, experience, while helpful in identifying the presence of fire risk assessment problems, does not necessarily provide guidance for resolving the problems. The recent developments in mathematical modeling of the various aspects of fire and its impact provide an opportunity to replace experience alone with the systematic development of understanding of specific scenarios or environments of interest. As these are further developed, they should prove to be valuable in permitting judgement to be supplemented by systematic analysis.

Summary

The response of society to the risk posed by unwanted fire can be viewed as being the result of the application of fire risk assessment techniques. The nature of the assessment process has evolved along with the test methods used to support the assessment of hazards and the codes and standards which are

used to regulate exposure and reduce the probability of occurrences to an acceptable level.

The results of meaningful fire risk assessment are not brought about simply by identifying appropriate tests or levels of performance under specific test conditions; rather, fire risk assessment is an orderly process which requires the integration of building codes and fire tests as a means of responding to adverse experience.

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Standard Room Fire Test Development at the National Bureau of Standards

REFERENCE: Lee, B. T., "Standard Room Fire Test Development at the National Bureau of Standards," *Fire Safety: Science and Engineering, ASTM STP 882*, T. Z. Harthy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 29-44.

ABSTRACT: Research results with the proposed ASTM standard room fire test for interior finish materials are presented. The materials selected for the study were two untreated plywoods, a fire-retarded plywood, polystyrene, polyisocyanurate, and gypsum board. Three 900-s duration test scenarios were considered. Scenario A is a constant nominal 160-kW ignition source exposure. Scenario B achieves the same maximum exposure after three intervals of 30 s each in which the heat release rate is increased in equal steps of 40 kW. Scenario C evaluates a material over a 300-s exposure at a nominal 40 kW, with another 300-s exposure at a nominal 160 kW, followed by 300 s at zero exposure. This zero exposure allows the material to be screened for self-burning properties afterwards. The study demonstrated that all three scenarios could adequately differentiate material fire behavior, in terms of the maximum degree of fire buildup attained and the time to reach the maximum, for the materials selected. However, Scenario C would allow a more comprehensive evaluation of materials.

Thermal radiation incident on the floor and doorway air temperature were found to be the most consistent parameters for determining room fire buildup including room flash-over. Surface flame spread and rate of heat release are discussed for the room fires.

KEY WORDS: fire growth, flame spread, heat release, interior finish, room fire, fire test method

Evidence shows that room fire testing offers the only means for satisfactorily measuring the fire hazards of some synthetic foam materials [1,2,3]. In order for these foamed plastic materials to be accepted by the codes, they either have to be covered with a barrier layer equivalent to 12.7-mm-thick gypsum board or, if they are to be exposed, their fire safety must be demon-

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strated by a full-scale room fire test. In the model code of the International Conference of Building Officials (ICBO), a particular room fire test was specified for this purpose [4]. A task group was then set up to develop a modified version of this ICBO test which would be acceptable as an ASTM standard test method. A proposed method has been published in the grey pages of the 1982 ASTM Annual Book of Standards [5] for information purposes. In addition to requirements regarding the room and ignition exposure, the method specifies a hood outside the doorway to collect all of the exhaust gases in order to provide information on the rates of heat, smoke, and toxic gas production. Before the test method can be accepted as an ASTM standard, a suitable heat release rate scenario for the ignition source has to be agreed upon, and intra-laboratory and interlaboratory evaluations of repeatability and reproducibility must be conducted.

Eventually, room fire tests could be replaced with a mathematical model which could predict fire development for other room sizes and configurations and ignition conditions. Prerequisite to this approach is the need for an improved understanding of surface flame spread and its relation to the thermal environment in the room.

The objectives of the present project and of this paper are:

1. To evaluate the effects of three different heat release rate scenarios for the ignition source on the room fire behavior of a variety of interior finish materials having a broad range of fire properties.
2. To evaluate the various methods used for determining room flashover, an event representing a transition from a fire in which the flame spread normally can be confined to the room of fire origin to a fire which could readily involve the active burning of adjoining spaces and, eventually, of the entire structure.
3. To provide surface flame spread data from room fires as a function of the degree of fire development in the room.
4. To help evaluate the practicality of the operational procedure recommended in the proposed standard.

Experimental

Test Room and Exhaust Hood

The test room is constructed in accordance with the proposed ASTM method [5]. A hood to collect the effluent from the room is located above the door. This hood has horizontal dimensions of 3.7×4.9 m and discharges into a 1.2-m square duct. The ducting is comprised of an initial upward section, then a downward portion, and finally another upward section. Measurement of heat release rate and smoke are made in the downward flow portion. The collection system was calibrated for the rate of heat release measurement

using a diffusion burner, 0.305 m on each side, installed under the hood. For a rate of about 250 kW, weight loss of propane and volumetric measurement of propane with a rotameter were used to calibrate the hood. The accuracy of the rotameter was verified to within 3% using both wet and dry gas displacement meters. Both weight loss and volumetric readings indicated a calibration factor of about 0.70 for the hood measurement. For rates up to 4 MW, an orifice meter made to the American Gas Association and American Society of Mechanical Engineers (ASME) specifications and the building displacement gas meter were used with natural gas to calibrate the hood. A calibration factor of 0.70 also was found for the higher rates. Accordingly, the heat release rate values reported for the room fires have been adjusted by a factor of 0.70 to match the calibration data. The cause for this systematic behavior is being investigated.

The response time for the rate of heat release measurement is a composite of the transport time for the effluent to reach the gas sampling location, the transport time of the gas sampling system itself, and the response time of the individual analyzers as well as the response time of the electronic filters used on pressure transducers which monitor the Pitot-static tubes. Babrauskas [6] gives a discussion of various methods of correcting for the time delays before choosing a delay of 30 s as a reasonable approximation. Data on heat release as a function of time have been adjusted by subtracting 30 s to correct for the system response time.

The heat release rate from the room fire tests was measured using the oxygen consumption technique [7] which is based on the measurements of gas concentrations and mass flow in the exhaust duct. The hood also was used to quantify smoke from the room fires in terms of a critical cross section which is based on optical density and mass flow measurements in the hood duct [8].

Test Materials

In the assessment of a fire test method, it is desirable to use materials having a diverse range of fire properties. Six materials having significantly different flame spread behavior and heats of combustion were used in this study. These materials are indicated in Table 1. The 5.6-mm plywood was similar to that used in the room fire test at the University of California [9] and was used to assess the reproducibility between the two facilities for this material.

Test Procedure

The test specimen fully covered the back wall, the two side walls, and the ceiling. With the specimen in place, the interior dimensions were in conformance with the recommended standard room size of 2.44 ± 25 mm by 3.66 ± 25 mm by 2.44 ± 13 mm high. The specimen was backed with 13-mm gypsum board. For the foam plastics, the specimen was glued to the gypsum

TABLE 1—*Interior finish materials used in standard room fire test.*

Material ¹	Measured Thickness, mm	Density, kg/m ³
Polystyrene	50.8	30
Polyisocyanurate	50.8	33
Gypsum board	13.2	757
Plywood 1	5.6	586
Plywood 2	12.8	534
Fire-retarded plywood	13.1	545

board using 3M-2226 adhesive made by the 3M Corporation.² For the 5.6-mm plywood, the room construction replicated that used in the University of California test.

The relative humidity in the fire room was controlled with a humidifier to within 42 and 55% for at least 24 h prior to the test. The temperature of the laboratory was controlled so that the test room was maintained within the proposed test conditions of $21 \pm 3^\circ\text{C}$.

A 305 by 305 by 305-mm high propane gas diffusion flame burner in one back corner served as the ignition source. The flux levels on the back wall over the burner, at the 1.22 and 1.83-m heights above the floor and 0.15 m away from the corner, are given in Table 2 for the burner operating at the nominal 160 kW setting in the room lined with fire-exposed gypsum board. The flux levels in Table 2 can be used as a check on the reproducibility of the ignition source intensity when such tests are repeated at other facilities. The measurement of thermal flux can be affected by condensation of water vapor on the fluxmeter surface. The use of hot water, for example, above 50°C as coolant for the fluxmeter would alleviate this problem. The data in the table showed that flux levels were 5 to 8% higher with the coolant water at 70°C than those levels when 18°C water was used.

TABLE 2—*Average flux levels on back wall of room at 1.22-m and 1.83-m heights over burner.*

Water Temperature for Cooling Fluxmeter, $^\circ\text{C}$	1.22-m Height Flux Level, kW/m ²	1.83-m Height Flux Level, kW/m ²
18	59	52
70	62	56

NOTES—

1. Wall and ceiling finish were fire-exposed gypsum board.
2. In each run, the burner operated at a constant 160 kW for 300 s. Measurements were taken between 180 and 300 s.
3. Average values were based on four runs with 18°C water and four runs with 70°C water.

²The use of trade names does not constitute endorsement by the National Bureau of Standards.

Three ignition exposure scenarios, each producing a maximum nominal value of 160 kW net³ rate of heat release, were considered. This rate corresponded to a nominal propane flow rate of 1.84 L/s (actual rate of 2.06 L/s) at 20°C and 100 kPa. Scenario A was a constant 160 kW maintained for 900 s and was chosen to evaluate the effect of a severe sudden thermal insult on materials. Scenario B, proposed by Task Group 1 of ASTM E 5.13 in 1982, started at 0.25 of its maximum value, increased to 0.50 of its maximum at 30 s, to 0.75 of its maximum at 60 s, to its maximum in 90 s and was maintained at that level to 900 s. This scenario was chosen to evaluate the effect of having an increasingly severe fire exposure on materials. Scenario C started with 0.25 of its maximum value, maintained for 300 s, increased to the maximum for another 300 s, and the ignition source was then turned off for the final 300-s period. Scenario C was selected to evaluate the effect of a longer low fire exposure on materials, particularly char-forming materials such as wood, and to examine their subsequent behavior under a severe fire exposure. This scenario also allowed an evaluation of the self-sustained fire spread characteristics of materials.

Measurements described in the proposed room fire test method [5] were recorded continuously. Time-lapse photography and continuous video coverage of the burner flame and adjacent walls and ceiling were taken to allow mapping of the surface flame spread as a function of time. Prior to room flashover, the degree of fire buildup was measured by the maximum air temperatures reached near the ceiling and near the top of the doorway and by the thermal flux incident on the floor. For the determination of room flashover, five criteria were used. These criteria were based on the times of occurrence for:

1. Flameover, defined here as the emergence of flames from the doorway (t_F).
2. The ignition of crumpled newspaper on the floor (t_{FO}),
3. The attainment of a heat flux of 20 kW/m² on the floor (t_{Floor}),
4. The attainment of 600°C average air temperature near the ceiling (t_I).
5. The attainment of 600°C average air temperature near the top of the doorway (t_D).

Room Fire Tests

Fifteen tests were performed and are outlined in Tables 3 and 4 along with their ambient test conditions. Except for the untreated plywoods, each material was subjected to all three ignition exposure scenarios. The 5.6-mm plywood was used only to check on the reproducibility between tests conducted at the National Bureau of Standards and at the University of California [9] using

³Recalibration indicated that all of the ignition exposures were 12% higher than the nominal values.

TABLE 3—Summary of standard room fire test results.

Test No.	Material	Exposure	Flameover, t_F	Newspaper Ignition ^a , t_{FO}	Floor Flux ^b , 20 kW/m ² , t_{Floor}	Occurrence Time for Flashover Indicators in Seconds		
						Interior Air Temperature 600°C, T_I , t_I	Doorway Air Temperature 600°C, Average ^c , T_{D20} and T_{D2}	t_{D2}
1B	gypsum board	A	none	none	(4.5 kW/m ² , 740 s)	none	none	none
2	gypsum board	B	none	none	(3.5 kW/m ² , 870 s)	none	none	none
3B	gypsum board	C	none	none	(3.2 kW/m ² , 600 s)	none	none	none
15	fire-retardant treated plywood	A	861	^d B, 861 F	847	163	851	229
4	fire-retardant treated plywood	A	none	^e B, none F	(13.8 kW/m ² , 900 s)	186	300	218
6	fire-retardant treated plywood	B	none	483 B, 568 F	(18.5 kW/m ² , 510 s)	431	469	509
5	fire-retardant treated plywood	C	none	528 B, ^e F	(16.1 kW/m ² , 580 s)	387	528	424
7	Plywood 2	C	193	206 B, 209 F	195	169	200	215
8	Plywood 1	B	134	143 B, 165 F	140	101	137	141
10	polystyrene	A	48	39 B, 40 F	/	47	50	48
11	polystyrene	B	83	80 B, 82 F	71	74	83	84
12	polystyrene	C	110	107 B, 109 F	101	105	112	106
9	polyisocyanurate	A	14	15 B, 16 F	19	23	28	22
13	polyisocyanurate	B	50	51 B, 52 F	42	45	52	46
14	polyisocyanurate	C	312	314 B, 315 F	315	312	315	313

^aBack and front newspaper flashover indicators denoted by B and F, respectively.^bMaximum flux and its time of occurrence given in parenthesis.^cAverage interior temperature T_I based on eight 0.51-mm thermocouples located 0.10 m down from ceiling. T_{D20} based on the average of two 0.51-mm thermocouples located 0.10 m down from top of doorway. T_{D2} based on 0.05-mm thermocouple at same location.^dBack indicator ignited prematurely by falling embers.^eNewspaper discolored due to heating.^fResolution inadequate.

TABLE 4—Summary of standard room fire test results.

Test No.	Material	Exposure	Rate of Heat Release at		Peak Rate of Heat Release and Time		Peak T_I and Time		Peak T_{D20}		Ambient Conditions	
			t_{Floor}	MW	MW	s	°C	s	°C	s	Temperature	Relative Humidity
1B	gypsum board	A	none	0.3	70	400	60	325	780	19	67	45
2	gypsum board	B	none	0.2	840	395	120	305	880	21	70	42
3B	gypsum board	C	none	0.2	560	405	350	320	360	19	66	52
15	fire-retardant treated plywood	A	2.1	6.2	860	865	890	810	900	23	73	51
4	fire-retardant treated plywood	A	none	0.5	260	715	260	600	300	23	73	42
6	fire-retardant treated plywood	B	none	0.6	480	745	487	640	530	21	70	47
5	fire-retardant treated plywood	C	none	0.5	480	720	468	610	530	22	71	48
7	Plywood 2	C	1.7	6.7	260	930	340	895	360	21	70	51
8	Plywood 1	B	1.9	8.5	260	850	312	860	180	21	70	55
10	polystyrene	A	^a	9.4	100	1050	75	930	60	21	70	48
11	polystyrene	B	4.2	4.2	70	1015	86	800	90	22	71	50
12	polystyrene	C	3.1	3.5	110	970	120	940	120	22	71	48
9	polyisocyanurate	A	2.2	5.2	50	1200	80	1245	100	21	70	54
13	polyisocyanurate	B	2.9	4.3	50	1065	60	1040	60	22	72	55
14	polyisocyanurate	C	3.2	4.1	310	1095	320	1020	320	24	75	47

^a Resolution inadequate.

ignition Scenario B. For the 12.8-mm plywood, only Scenario C was considered. This exposure was expected to produce the most charring of the three ignition scenarios and thus could best be used to show the subsequent behavior of charred plywood when subjected to the maximum exposure of 160 kW.

Results and Discussion

Effect of Heat Release Rate Scenarios on Fire Development

One would expect that as the ignition exposure increases in severity from Scenarios C to A, the peak values of heat release rate, air temperatures, and thermal radiation also would increase with decreasing times of occurrence. An examination of Tables 3 and 4 shows that this was generally the case if one were to ignore small variations in the data and even large variations in the data once flashover has occurred. Post flashover conditions become more a function of the burning behavior of the room lining material than of the ignition source.

The different ignition scenarios gave erratic results for the fire-retardant treated plywood. This was partly due to the difficulty in the conditioning of the material over a reasonable period of time and possibly partly due to a difference in composition or a nonuniformity in fire-retardant treatments between different panels of the material.

Test 8 with the 5.6-mm plywood was the same as Test C-213 conducted at the University of California [9]. However, Test 8 resulted in a more rapid fire buildup than C-213, mainly because the ignition exposure in Test 8 was found to be 12% higher than that used in Test C-213. Both tests used an AD-type plywood obtained separately from neighborhood lumber yards. A comparison of results from the two tests is given in Table 5. Differences between the two plywoods also may have accounted for some of the difference in fire buildup times between Test C-213 and Test 8. It should be noted that the difference was also within the experimental repeatability expected between similar runs.

TABLE 5—Comparison of data between National Bureau of Standards (NBS) and University of California (UC) for room fire tests of plywood.

Test	Time of Occurrence				Rate of Heat Release at Flashover Based on t_{Floor} , $\dot{Q}$ (MW)
	Flameover, t_F (s)	Ignition of Newspaper, t_{FO} (s)	20 kW/m ² Flux at Floor, t_{Floor} (s)	600°C Avg Interior Air Temperature, t_I (s)	
C-213 (UC)	170	210	205	115	2.1
8 (NBS)	134	165	140	101	1.9

Comparison of Various Methods for Determining Flashover

Flashover is defined here as the event in which the thermal radiation level becomes high enough to ignite light combustible materials, such as newspaper, in the lower part of the room. This corresponds to thermal radiation levels on the floor of about 20 kW/m^2 . The five criteria discussed in the Test Procedure section for determining the occurrence of room flashover were applied to the room fire tests performed under this study. The results are shown in Table 3 under headings of t_F , t_{FO} , t_{Floor} , t_I , and t_D . All five criteria are consistent with each other for severe room fires. For moderately severe fires, one or more criteria might not be satisfied.

The application of each of these criteria has its own problems. Flameover may not always occur for the situation where there is sufficient thermal radiation to ignite combustible items in the lower part of the room. Accidental ignition of the newspaper indicators could occur. Materials separating from the ceiling and falling over the flux meters, thereby either obscuring or transferring additional heat to the fluxmeters, are also possibilities. Local heating and flame contact can occur at some of the thermocouples near the ceiling, resulting in readings that are higher than average with consequent premature times for flashover. For example, in Tests 7, 8 and 15, the times for t_I were much too soon compared with the times for t_F , t_{FO} , t_{Floor} , and T_D . The hot air inside the room usually becomes well mixed by the time it is exhausted through the doorway. Consequently, the peak doorway air temperature may be a more reliable indicator of the fire buildup than is the interior air temperature.

As there are many problems that can arise in determining flashover, it is necessary to have more than one reliable method for monitoring flashover. Analysis of Table 3 indicates that the most reliable flashover indicators are the incident flux on the floor, the newspaper indicator, and the doorway temperature. For determining the degree of fire buildup short of flashover, obviously the newspaper indicator is not useful. Thus, for most applications, the floor flux and the doorway temperature are the most suitable.

Flame Spread Patterns

Figure 1 shows the flame spread patterns at selected times for Test 7, but these patterns are typical for all of the room fire tests except for their times of occurrence. Only the flame spread along those portions of the back wall and ceiling, which could be viewed through the doorway, was actually observed. The remaining portions of each profile shown had to be estimated based on past experience with room fire testing of interior finish materials. This is part of an effort to better understand surface flame spread and its relation to the thermal environment in the room and will be discussed further for another publication.

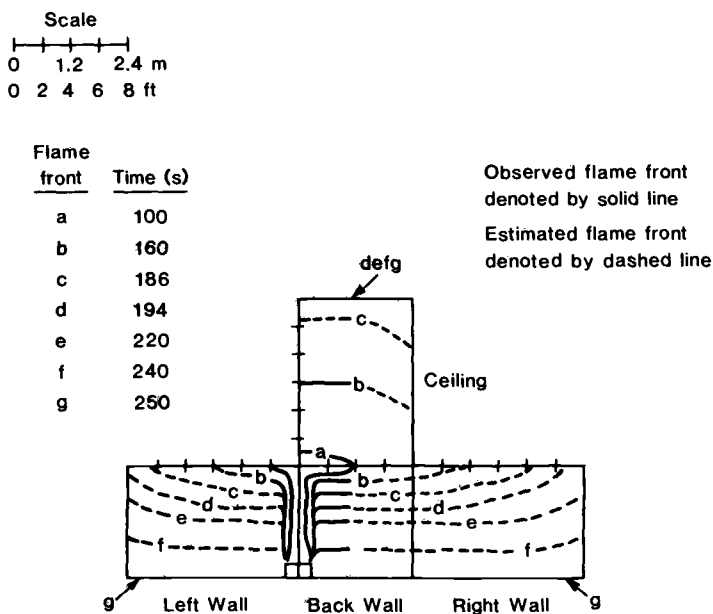


FIG. 1—Flame spread profiles for Test 7 (Plywood 2, Exposure C).

Heat Release Measurements

The rate of heat release histories for Tests 7 to 15 are shown in Figs. 2 to 4. For the gypsum board tests, these rates were basically the same as those for the propane burner. With measurement accuracies of ± 50 kW, the rate histories for Tests 1B to 3B for gypsum board would not be meaningful and thus were not included. For the fire-retardant treated plywood, the peak rates in Tests 4 to 6 were an order of magnitude lower than that for Test 15. Consequently, the rate of heat release history is given only for Test 15. In every test, the fire was not extinguished until the peak fire development had passed. Peak rates and the rates occurring at the time t_{Floor} , when 20 kW/m^2 was measured at the floor, are presented in Table 4. For the plywood materials, the rates at time t_{Floor} ranged from 1.7 to 1.9 MW. This was consistent with a value of 2.1 MW found for plywood tested at the University of California [9].

Integrating the rates of heat release shown in Figs. 2 to 4 over time gave the total heat produced in each fire test. In Table 6, this total heat was compared with that calculated from the weight loss of the test material multiplied by the net heat of combustion and by the combustion efficiency for the material. The net heat is equal to the gross heat minus the heat of vaporization of water.

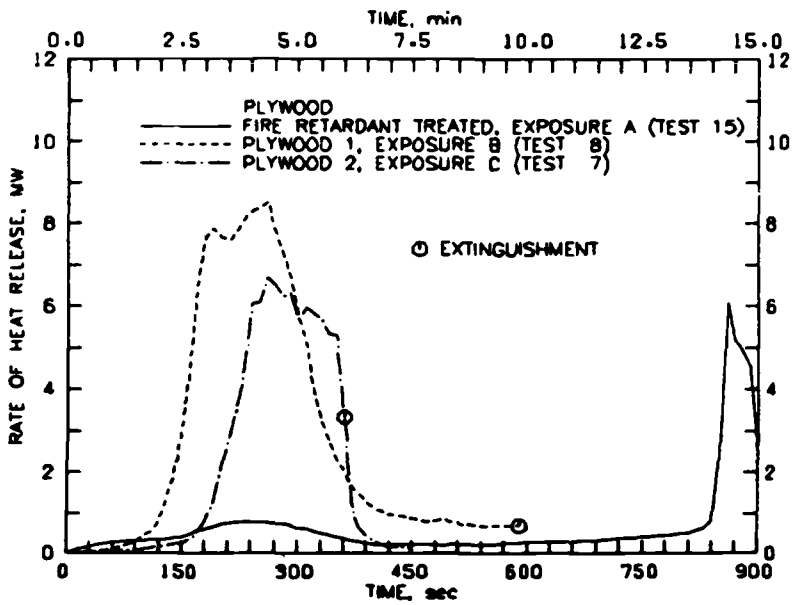


FIG. 2—Rate of heat release history for plywood.

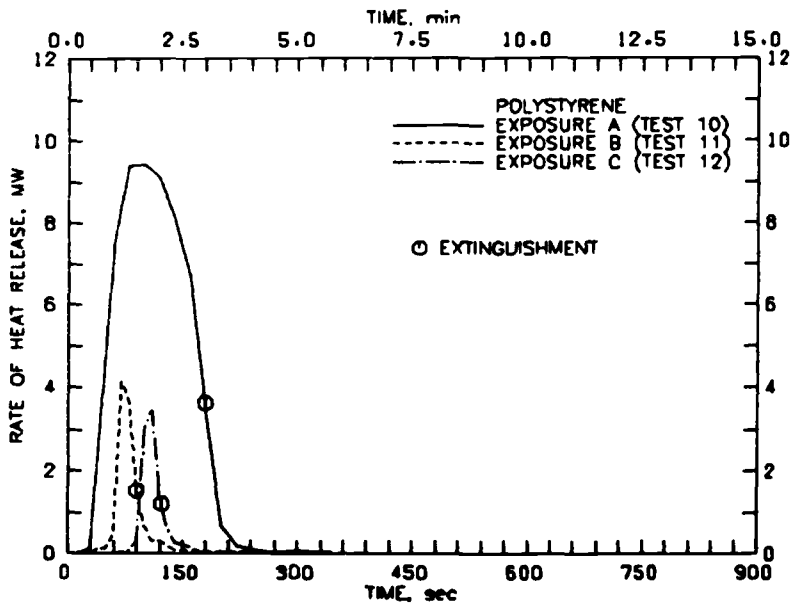


FIG. 3—Rate of heat release history for polystyrene.

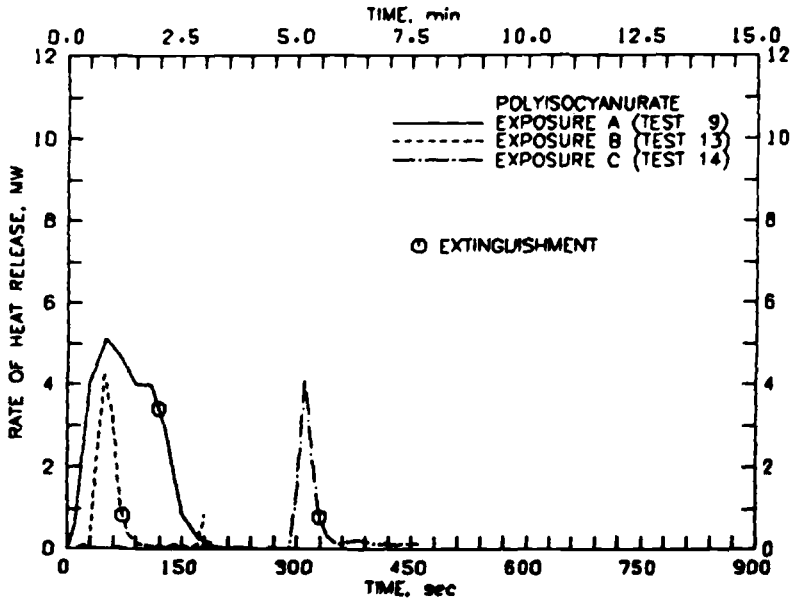


FIG. 4—Rate of heat release history for polyisocyanurate.

Recommended Changes in Test Procedure

In the test series, C.P. grade propane was used in lieu of commercial grade to avoid fractionation problems. Any additional cost and inconvenience were minor as one 45-kg bottle would last for about twelve tests, and the increased expense per test was insignificant compared with the material and labor involved. The present proposed standard room fire test method called for thermocouples, mounted on supports, to be located 100 mm down from the center of the ceiling and from the center of each of the four ceiling quadrants and from the ceiling directly over the center of the ignition burner. The method cautioned against attachments to the specimens. However, for the 15 tests conducted in this study, 6.4-mm holes were drilled through the ceiling at these positions for the thermocouples and then resealed with gypsum spackling compound. No adverse effects on the fire development due to these penetrations in the ceiling was observed. The proposed test method suggested either photographic coverage or video taping to record the fire spread in the room. Both methods were used in this study. When time-lapse coverage such as with 35-mm color slides was used, the flame spread and even the ignition of the newspaper flashover indicators could not be determined in some instances due to obscuration from the smoke and glare of the fire. Continuous coverage made such determinations much easier. Consequently, it is recommended that C.P. grade propane, thermocouple penetration of the ceiling, and video

TABLE 6—Comparison of total heat release from room tests with values calculated from mass loss.

Test No.	Material	Original Weight, kg	Weight Loss, kg	Combustion Efficiency	Calculated Net Heat Release, Q_N (MJ)	Measured Net Heat Release, Q_s (MJ)
15	fire-retardant treated plywood	233.1	69.2	<1.0	<1050	620
7	Plywood 2	222.6	65.1	1.0	980	890
8	Plywood 1	107.1	107.1	1.0	1620	1490
10	polystyrene	49.3	49.3	0.59	1110	1010
11	polystyrene	49.3	11.9	0.59	270	130
12	polystyrene	49.3	5.9	0.59	130	100
9	polyisocyanurate	55.4	25.3	0.53	350	500
13	polyisocyanurate	55.4	8.3	0.53	110	130
14	polyisocyanurate	55.4	8.4	0.53	120	110

NOTES—

1. Lining material in Tests 1B, 2, 3B, 4, 5, and 6 did not burn well. Consequently, measurements of total heat and mass loss would not be accurate and were not included.
2. Net heat of combustion of 15.1 MJ/kg for treated and untreated plywood.
3. Net heat of combustion of 38 MJ/kg for Polystyrene Foam GM 47 in Ref 10 used for polystyrene.
4. Net heat of combustion of 26 MJ/kg for Polyisocyanurate Foam 29 in Ref 10 used for polyisocyanurate.
5. Combustion efficiencies for polystyrene and polyisocyanurate from Ref 11.
6. Q_N = weight loss $\times$ net heat of combustion $\times$ combustion efficiency.
7. Q_s = integrated value of room heat release rate history.

tape or more complete time-lapse photographic coverage be adopted in any new test procedure.

Concluding Remarks

1. The study demonstrated that all three exposure conditions could adequately differentiate material fire behavior for the materials evaluated when the test material covers the walls and ceiling of the room. Consequently, each condition could be used to indicate the fire safety level for room interior finish materials. However, ignition Scenario C has advantages over Scenarios A and B in that materials can be evaluated and rated over a reasonable length of time of 300 s at a low exposure of about 40 kW/m², 300 s at a high exposure of about 160 kW/m², and then over another 300-s period for self-burning without enhancement from the burner source. Scenario A cannot evaluate interior finish materials at low exposures nor self-burning properties of materials. Scenario B included four successive exposure levels, but the period of change from lowest to highest exposures lasted only 90 s. This may not be adequate time to evaluate some materials at the lower exposures. Furthermore, no evaluation of self-burning was included.

2. The three exposure conditions will be evaluated further with tests using the same materials lining only the walls and gypsum board on the ceiling. Ignition Scenario A also will be evaluated for these materials lining just the ceiling, with gypsum board lining the walls.

3. In determining the degree of fire buildup, including room flashover, measurements of the incident flux on the floor and of the air temperature near the top of the doorway are recommended. Flameover time, newspaper indicators, and interior air temperatures either cannot determine the degree of fire buildup or are less reliable.

4. Flame spread in the fire room is difficult to follow with time-lapse photography in intervals of 15 to 30 s. Shorter intervals of 1 or 2 s or continuous video coverage is recommended during the rapid fire growth period.

5. In this study, there was a reasonable agreement between the total heat release measured by oxygen consumption and the heat calculated from the mass loss of the materials in the fire.

6. It is recommended that the proposed standard room fire test method be amended to adopt the use of C.P. grade propane in lieu of commercial grade to avoid fractionation in the burner fuel and to allow thermocouple penetration of the ceiling for measurement of air temperature near the ceiling.

Acknowledgements

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DISCUSSION

*R. B. Williamson*¹ (*written discussion*)—An evaluation of Table 5, which compares the results of plywood tests conducted both by the University of California Fire Research Laboratory and by the National Bureau of Standards Center for Fire Research, indicates that the time of occurrence of all parameters is shorter for the experiment conducted at the National Bureau of Standards than at the University of California Fire Research Laboratory. I believe the cause of this may be linked to a higher burner ignition source rate of heat release in the National Bureau of Standards experiments than in our experiments at the University of California Fire Research Laboratory. Our measurements of rate of heat release during burner calibration experiments gave very good correlation between propane gas flow rate to the burner versus net energy release rates measured by oxygen consumption.

This paper indicates that their rate of heat release measurement derived from oxygen consumption required a correction factor of 0.704 to match the predicted rate of heat release from propane gas flow measurements.

It may be quite possible that their error is not in the measurement of heat release by oxygen consumption but in their measurement of propane gas input to the ignition source burner.

B. T. Lee (*closure*)—A recalibration of the flowmeter used for metering the propane to the burner indicated that the heat release rate values for the burner were 12% higher than originally believed. This would have contributed to the differences between the plywood tests conducted at the National

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Bureau of Standards (NBS) and the University of California (UC). However, the calibration of the hood at NBS for rate of heat release measurement was accomplished using several different techniques, one of which was the measurement of weight loss of propane in the 250 kW range as was done in the UC calibration. The calibration factor for the NBS hood was about 0.70 in every case. Differences in calibration factors between the NBS and UC hoods are due to differences in the construction of the hood ducting where the measurements of flow and oxygen levels are made. UC has a straight vertical duct. NBS has a complex duct with four short 90 degree bends, complicating flow measurements. A more complete description of our hood system and an elaboration of the various means used to calibrate our hood have been incorporated into the section entitled Test Room and Exhaust Hood.

Combustible Linings and Room Fire Growth—A First Analysis

REFERENCE: Magnusson, S. E. and Sundström, B., “Combustible Linings and Room Fire Growth—A First Analysis,” *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 45–69.

ABSTRACT: A computational procedure has been developed to correlate a full-scale room fire test process and results from the proposed International Organization for Standardization (ISO) small-scale laboratory tests. The analysis assumes that the combustible lining material covers ceiling and walls. Derived material characteristics and test room time lag factors are used as input data to an uncomplicated mathematical expression, essentially describing the full-scale test fire process as a concurrent flame spread phenomenon. Undetermined parameters in the model have been derived using linear regression analysis and results from seven full-scale room tests. In these room tests, the tested materials were of such thickness that no burning through occurred. Using results from the regression analysis, a simulation of the fire growth behavior can be carried out, including the adjustments needed due to transportation time lags and instrument response time. The predictive capability was thought acceptable, but further experimental confirmation and sensitivity studies are needed to assess the inherent variability. Qualitatively the regression equation predicted the same relative ranking of materials as the room test.

For thin surface finish materials on a noncombustible base, it was possible to derive a radically simple expression to be used as indication of the risk of flashover. Again, the final usefulness only can be evaluated on the basis of further experimental evidence.

In summary, it is thought that a first step has been taken in the efforts to use results from small-scale tests to rationally predict full-scale fire growth (for one specified scenario) and rank materials.

KEY WORDS: combustible lining material, small-scale test, full-scale test, correlation study, mathematical models, design models

Nomenclature

A Area

a $h^2/k\rho c$

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B	Mass transfer number
c	Specific heat
h	Heat transfer coefficient (total)
k	Thermal conductivity
L	Heat of vaporization
$\dot{m}''$	Mass loss rate
$\dot{Q}''$	Rate of heat release per unit area ($\dot{Q}''$ without subscript refers to rate of heat release from small-scale test)
$\dot{q}''$	Rate of heat transfer per unit area
r_{ox}	Mass oxygen/fuel stoichiometric ratio
T	Temperature
Y_o	Mass fraction of (oxygen) O_2
α	Undetermined parameter in Eq 22
α'	$\ln \alpha$
β	Undetermined parameter in Eq 22
ϵ	Absorptivity of surface
θ	Temperature rise
λ	Rate of heat release curve decay coefficient
ρ	Density
χ	Degree of complete combustion
ΔH	Heat of combustion

Subscript

a	Ambient
av	Average
b	Burning
c	Convective
e	Externally impressed
f	Flame
p	Pyrolysis of ceiling
rt	Room test
s	Surface
uf	Unburnt fuel reaching ceiling

The compartment fire growth and fire spread has been a subject of extensive investigations, experimental and theoretical, during the last decade. For the case when the burning item is an isolated or single object such as a bed, an upholstered chair, etc, research has on the whole been successful and given a quantitative understanding of the physical phenomena involved. Room fire growth on combustible linings remains a virtually unexplored area, despite the fact that this has been a problem of concern to the legislators and authorities since the advent of building fire safety regulations. As a consequence,

there still exists no internationally accepted basis for a functional classification of surface finishes.

The work in this area can be divided into three interacting lines of development. The first concerns the generation of improved small-scale test methods. From an international, and perhaps especially European, point of view the work carried out within the International Organization for Standardization (ISO) is of importance as it opens the way for the replacement of the multitude of national test standards with internationally agreed ones. The second line of development relates to the development of a full-scale room test procedure, initiated within ASTM and taken up by ISO. The third trend concerns the evolution of mathematical modelling, both of the fire process in the small-scale laboratory apparatuses and the full-scale fire growth.

These three lines give the structure of the Swedish research project that is the subject of this paper.

Regarding the extensive work carried out within ISO in order to generate tests for measuring "reaction to fire" of building materials, Refs 1 and 2 give a general review of the historical background, the philosophy of hazard assessment by testing methods, and the possible use of specific tests to derive material flammability characteristics for use in fire environment modelling.

Objective and Extent of the Project

The objectives of the project were threefold: to utilize the proposed ISO tests to derive basic flammability characteristics which could rationally be used as classification criteria; to generate a full-scale fire test standard; and, finally, to mathematically correlate small-scale test data and the full-scale fire process. For this purpose, 13 materials were tested in eight small-scale tests in the full-scale test room and in a one-third scale version of this room. Table 1 lists the materials and Table 2 the test methods. The project is part of a larger one, described in Ref 3, carried out jointly by the Swedish National Testing Institute and Lund Institute of Technology.

Full-Scale Test

The full-scale test room [4], built at the Swedish National Testing Institute, is a lightweight concrete construction with dimensions and instrumentation according to the proposed ASTM standard room fire test [5]. Rate of heat release is measured by the oxygen consumption method. The test room has been extensively calibrated with results indicating that the total inaccuracy of the system is within 25 kW or $\pm 10\%$ of the measured value. In the full-scale test series, the material was mounted on the ceiling and all walls except the doorway wall. The material was ignited with a 0.17-m² propane sand burner located in one corner of the test room. During the first 10 min of an experiment, the gas burner heat output was kept at 100 kW, which produced a

TABLE 1—*Tested materials.*

No.	Type	Thickness, mm	Weight
1	Insulating fiberboard	13	250 kg m ⁻³
2	Medium density fiberboard	12	600 kg m ⁻³
3	Particleboard	10	750 kg m ⁻³
4	Gypsum plasterboard	13	700 kg m ⁻³
5	Polyvinyl chloride (PVC) wall covering on gypsum plasterboard	0.7	240 g m ⁻²
6	Paper wall covering on gypsum plasterboard	0.6	200 g m ⁻²
7	Textile wall covering on gypsum plasterboard	0.7	370 g m ⁻²
8	Textile wall covering on mineral wool	50 ^a	100 ^a kg m ⁻³
9	Melamine-faced particleboard	1.2	810 ^b kg m ⁻³
10	Expanded polystyrene	50	20 kg m ⁻³
11	Rigid polyurethane foam	30	30 kg m ⁻³
12	Wood panel, spruce	11	530 kg m ⁻³
13	Paper wall covering on particleboard	see Material No. 6	

NOTE—The wall coverings were glued on the substrates (similar types as Materials 2–4) according to the manufacturers' instructions. The expanded polystyrene was glued on 10-mm-thick calcium silicate board.

^a Refers to mineral wool.

^b Density of entire product.

TABLE 2—*Range of material tests used in the investigation.*

No.	Tested Method Designation	Reference
1	ISO ignitability	11
2	ISO surface spread of flame	1
3	Surface spread of flame	10
4	RHR apparatus	15
5	Sensenig apparatus	16,17
6	Cone calorimeter	24
7	Swedish box test	
8	NBS smoke density chamber	
9	1/3-model-scale room	
10	Full-scale room	5,4

NOTE—IMO = International Maritime Organization; OSU = Ohio State University; STFI = Swedish Institute for Wood Research; RHR = Rate of Heat Release.

flame that reached the ceiling. If flashover did not occur, the heat output was then increased to 300 kW and the experiment was discontinued after another 10 min.

Figure 1 gives the heat release curve for Material 3.

Methods of Analysis

When analyzing the full-scale data and the correlation to bench tests results, several methodologies are possible. These methodologies include a

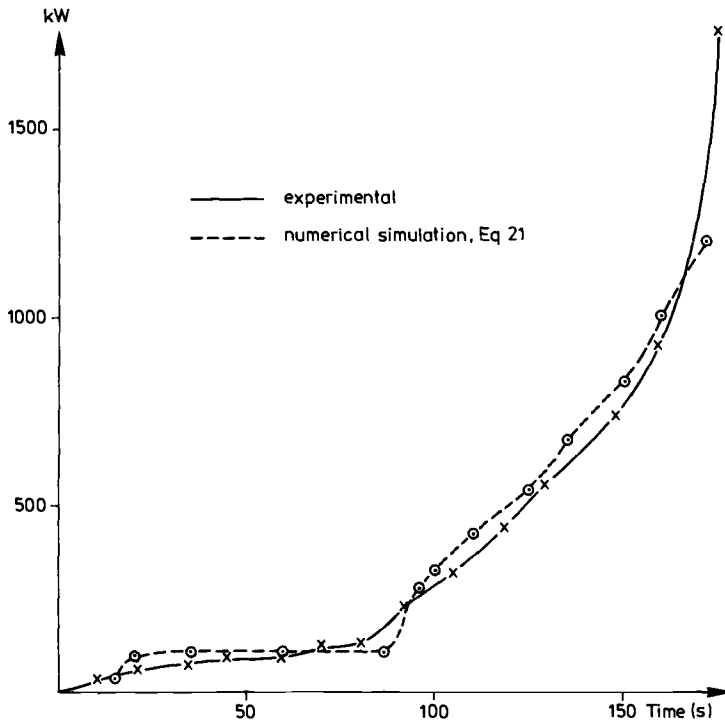


FIG. 1—Full-scale energy release rate, experimental (full line) and numerical simulation (broken line), Eq 21.

statistical factor analysis, a statistical modelling of the full-scale test process using regression analysis, deterministic modelling using the zone-volume approach, the field equation methodology, scale modelling, etc. We will in this preliminary report concentrate on the second method, leaving other approaches to future papers.

The different approaches reflect the degree of knowledge we have of the physical processes. The factor analysis method is of the "black box" type; typical discrete events in the fire growth (time to 500 kW, time to flashover, etc.) are described as a function of the independent variables such as ignition source output and $k\rho c$ of lining material. Nothing is assumed known of the fire growth phenomenon. The second method assumes that the basic structure of the model is understood and that unknown parameters in the model can be estimated by regression analysis. Deterministic zone modelling requires a more detailed knowledge of component processes, etc.

The reason for concentrating the initial efforts on statistical regression analysis is that this procedure promises to be the fastest way of obtaining a

design equation or design procedure; albeit they may be approximate, empirical, and temporary ones.

Basic Regression Model

The scenario we are considering is the following one: The walls and the ceiling of the test room are lined with the material. The ignition source in the corner ignites the wall corner material and spreads upward on an area approximately equal to 0.7 m^2 , equal to the width of burner (0.17 m) times the distance from the burner to the ceiling. The flame from the 100-kW ignition source has such size that upward spread and involvement of the wall corner area can be considered as nearly instantaneous, which considerably simplifies the analysis. The resulting ceiling jet or flame spreads along the intersections between the walls and ceiling and along the ceiling area in the mode of concurrent flame propagation. The model is primarily intended to describe this phase of the fire growth. After a certain time delay, flames begin to spread vertically downward from the ceiling and horizontally from the burning corner plume area. The mode of the flame spread is here “creeping” or opposite the air flow direction.

Our model will be based on the concepts presented in Refs 6–8. It is demonstrated in these references that for the ceiling flame spread and combustion:

1. Flame area A_f is linearly increasing with the total rate of heat production, that is, with the ceiling pyrolysis area A_p

$$A_f = c_1 A_p \quad (1a)$$

2. Subsequent pyrolysis areas A_p are proportional to the initial pyrolyzing area $A_{p,o}$

$$A_p \sim A_{p,o} \quad (1b)$$

3. The rate of spread of A_p is inversely proportional to the time δt necessary to increase the surface temperature at the flame tip from an initial temperature T_a to a pyrolysis temperature T_p .

From the results just enumerated, it follows that A_p may be considered as a driving force in a process where the rate of increase of A_p is proportional to the quantity A_p itself

$$\dot{A}_p \approx \frac{A_f - A_p}{\delta t} = \frac{c_1 - 1}{\delta t} A_p \quad (2)$$

with δt as just defined; that is, A_p is exponentially increasing with time. For the quantity δt and a thermally thick material, the following expression is given by elementary heat conduction theory

$$\delta t \sim k\rho c(T_{ig} - T_a)^2 / \dot{q}''^2 \quad (3a)$$

where

T_{ig} = the ignition temperature,

T_a = the ambient temperature,

$k\rho c$ = the product of thermal conductivity k and thermal capacity ρc , and

$\dot{q}''$ = effective net heat flux from flame to ceiling.

$\dot{q}''$ can be formally written as

$$\dot{q}'' = h(T_f - T_a) \quad (4)$$

where

h = total heat transfer coefficient, and

T_f = flame temperature,

and consequently

$$\delta t \sim k\rho c(T_{ig} - T_a)^2 / [h(T_f - T_a)]^2 \quad (3b)$$

The nondimensionalized time

$$h^2 t / k\rho c \equiv at \quad (5)$$

is thus an important scaling parameter.

From Eqs 2 and 3b

$$\dot{A}_p \sim aA_p \quad (6)$$

It follows that in a regression model of ceiling flame spread and combustion, pyrolysis area A_p can be generally written as

$$A_p = f(e^{at}) \quad (7)$$

One possibility is to write

$$A_p = \alpha(e^{at} - 1)^\beta \quad (8)$$

where α and β are coefficients to be determined statistically. Equation 8 will be the basic equation for the regression analysis.

The experimental variable from the room test used to validate the model is rate of energy release, $\dot{Q}_{ri}$. If we define $\dot{Q}_{ce}$ as that part of $\dot{Q}_{ri}$ that derives from ceiling combustion, $\dot{Q}_{ce}$ is written

$$\dot{Q}_{ce} = A_p \dot{Q}_{av}'' \quad (9)$$

where $\dot{Q}_{av}''$ is a suitable time and space averaged measure of material rate of heat release per unit area.

The next sections will be devoted to find values of $k\rho c$ and $\dot{Q}_{av}''$ to use in Eqs 8 and 9.

Basic Flammability Parameters

Various lists of these parameters have been published. An example is the analysis in Ref 9, which concludes with the following enumeration:

1. Thermal inertia $\sqrt{k\rho c}$.
2. "Creeping" flame spread coefficient C .
3. Minimum radiant flux for ignition $\dot{q}_{o,ig}''$.
4. Minimum radiant flux for flame spread $\dot{q}_{o,s}''$.
5. Heat of reaction ΔH .
6. Stoichiometric oxygen to fuel ratio r_{ox} .
7. Heat of vaporization L .

The first four are parameters needed in the analysis of room fire ignition and flame spread, while the last three, as will be discussed later on, are applicable to a description of rate of energy release. Because of the full-scale room test scenario and choice of regression model, we will concentrate our discussion on the results obtained from the ignitability and rate of heat release apparatuses (Methods 1,4,5 in Table 2). As mentioned, the full-scale experiment design assumes that the lining material covers walls *and* ceilings. Obviously, in real life situations a common practice is to put lining only on the walls. For this case, the modelling will have to be based on parameters derived also from the "creeping" surface spread of flame test methods analyzed in Ref 10 (Parameters C , $\dot{q}_{o,ig}''$, and $\dot{q}_{o,s}''$ in the just-mentioned list).

Ignitability Test and Derivation of $k\rho c$

The test—ISO TC92 TR5657, Fire Tests, Reaction to Fire, Ignitability of Building Products—is described in Ref 11.

The test is performed by exposing horizontal specimens to constant irradiance $\dot{q}_e''$ at specified levels ranging from 10 to 50 kW/m². The irradiance is provided by an electrically heated radiator cone positioned above the speci-

men. The cone has a hole at the top to avoid trapping any combustible gases. A pilot flame is applied once every 4 s to a position 10 mm above the surface of the specimen to ignite any volatile gases.

The specimen is placed on a silicate baseboard, and a counterweight system ensures that the specimen is kept in position and receives constant heat flux during the test.

Five replicate tests were performed on each irradiance level, 10, 20, 30, 40, and 50 kW/m².

Although not included in the normal test procedure, thermocouples were attached to the sample surface and between the sample and the baseboard, recording the temperature rise at these locations.

The main quantitative information obtainable from the ISO ignitability test is a set of values of t_{ig} —ignition time—for a set of exposure radiation levels $\dot{q}_e''$. The ignition time values can be regarded as relative hazard indices in themselves. Methods of deriving the indices are suggested in Refs 11 and 12. Data also can be extrapolated to yield useful quantities such as minimum level of impressed flux to cause ignition. Finally, and with the supplementary temperature measurements described earlier, test output can be used to derive important material and process parameters such as thermal conductivity k and thermal capacity ρc of the tested specimen. A prerequisite is a model of the test process.

A substantial effort was made to investigate available approaches to derive $k\rho c$. These approaches included various forms of analytical modelling and use of nonlinear regression methods and are described in some detail in Ref 13. In the end it was found that the most practical way of calculating $k\rho c$ was to use an approximate integral solution derived in Ref 14, giving the surface temperature rise θ_s from the relation

$$t = U \left[\frac{\theta_s^2}{\dot{q}_e''} + \frac{\tau}{\gamma^{3/2}} \ln \frac{(2s\theta_s + \tau - \sqrt{\gamma})(\tau + \sqrt{\gamma})}{(2s\theta_s + \tau + \sqrt{\gamma})(\tau - \sqrt{\gamma})} - \frac{\theta_s(\tau + 2s\theta_s)}{\gamma\dot{q}_e''} \right] \quad (10)$$

where

$$U = \frac{2}{3} \frac{k\rho c}{\epsilon^2} \quad (11a)$$

$$\tau = -(h_c/\epsilon + 4\sigma T_a^3) \quad (11b)$$

$$s = -\frac{25}{3} \sigma T_a^2 \quad (11c)$$

$$\gamma = (\tau^2 - 4\dot{q}_e''s) \quad (11d)$$

and

$$\dot{q}_\epsilon'' = \dot{q}_e'' / \epsilon = \dot{q}_e'' + \tau \theta_s + s \theta_s^2 \quad (11e)$$

$\dot{q}_\epsilon''$ is the impressed flux and ϵ the absorptivity of surface.

Equation 10 demonstrates the invariate or scaling properties of the factor $t/k\rho c$. The Eqs 10-11 constituted the basis for experimental determination of $k\rho c$.

It was found that the calculated $k\rho c$ will vary with time (which strictly speaking invalidates the theory behind Eqs 10-11) but not excessively. Typical examples are given in Fig. 2. The average values of $k\rho c$ used in subsequent calculations are given by Table 3.

Rate of Heat Release (RHR) Measurements and Characteristics

The 13 materials have been tested in three different RHR apparatuses: the Ohio State University apparatus [15], an open configuration [17] based on a design originally developed by National Bureau of Standards (NBS) [16], and the cone calorimeter [18]. The analysis made in this report will be based on the measurements reported in Ref 17.

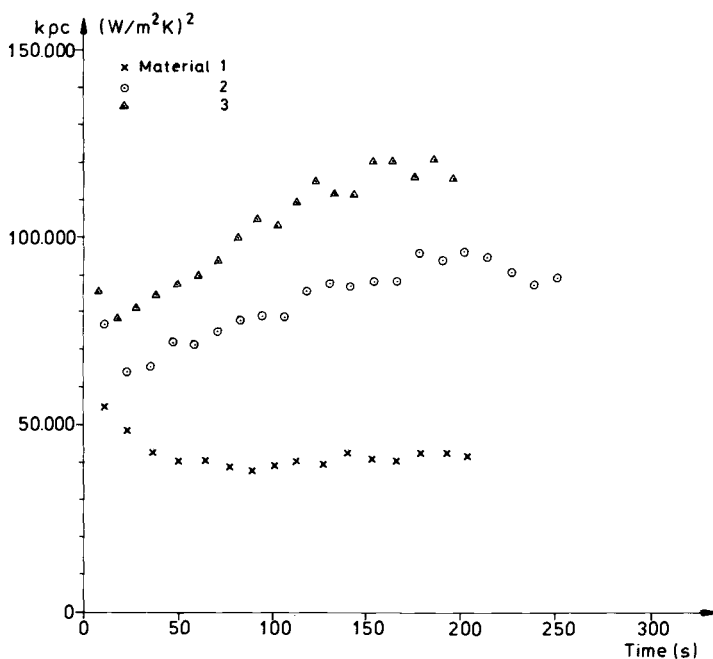


FIG. 2— $k\rho c$ values obtained from Eq 10 for three materials.

TABLE 3—Derived material properties.

Mat No.	$k\rho c \times 10^{-3}$ (W/m ² K) ²	$\dot{Q}_{\max}''$, kW/m ²	λ , s ⁻¹
1	41	139.8	0.0070
2	80	162.4	0.0027
3	110	199.8	0.0049
4	100	27.7	0.0150
5	75	107.5	0.0293
6	100	105.3	0.0208
7	80	222.0	0.0278
8	4.3	246.2	0.0382
9	105	40.9	-0.0032
10	...	...	...
11	4.0	130.6	0.0217
12	85	149.7	0.0086
13	110	164.1	0.0035

The equipment consists of a vertical sample holder and an electrical radiation panel placed under an open hood.

The samples were tested at 5, 3, and 2 W/cm² and some easily ignitable materials also at 1 W/cm². Three examples of test output are given in Figs. 3a, 3b, and 3c for Materials 3, 7, and 8 (compare Table 1).

Formally, the mass loss rate $\dot{m}_b''$ from a burning surface may be written

$$\dot{m}_b'' = \frac{1}{L} \dot{q}_{\text{net}}'' \quad (12)$$

where $\dot{q}_{\text{net}}''$ is the difference between heat flux to the fuel surface (externally applied flux, flux from flames) and heat loss terms (radiative and convective loss from the surface, heat conducted into the bulk of the fuel, cooling agents applied). L may be defined as the heat required to produce volatiles. If we accept Eq 12 in a formal sense, then $\dot{Q}''$ is given by

$$\dot{Q}'' = \chi \Delta H \dot{m}_b'' = \chi \frac{\Delta H}{L} \dot{q}_{\text{net}}'' \quad (13)$$

where χ stands for degree of combustion completeness. Obviously, the quantity $\Delta H/L$ is a parameter of major importance. It is also a component in the mass transfer number B , which plays a fundamental role in the theory of convection-controlled combustion. An engineering approximation to B is given by the expression

$$B = \frac{\Delta H}{L} r_{\text{ox}} Y_{\text{o,a}} \quad (14)$$

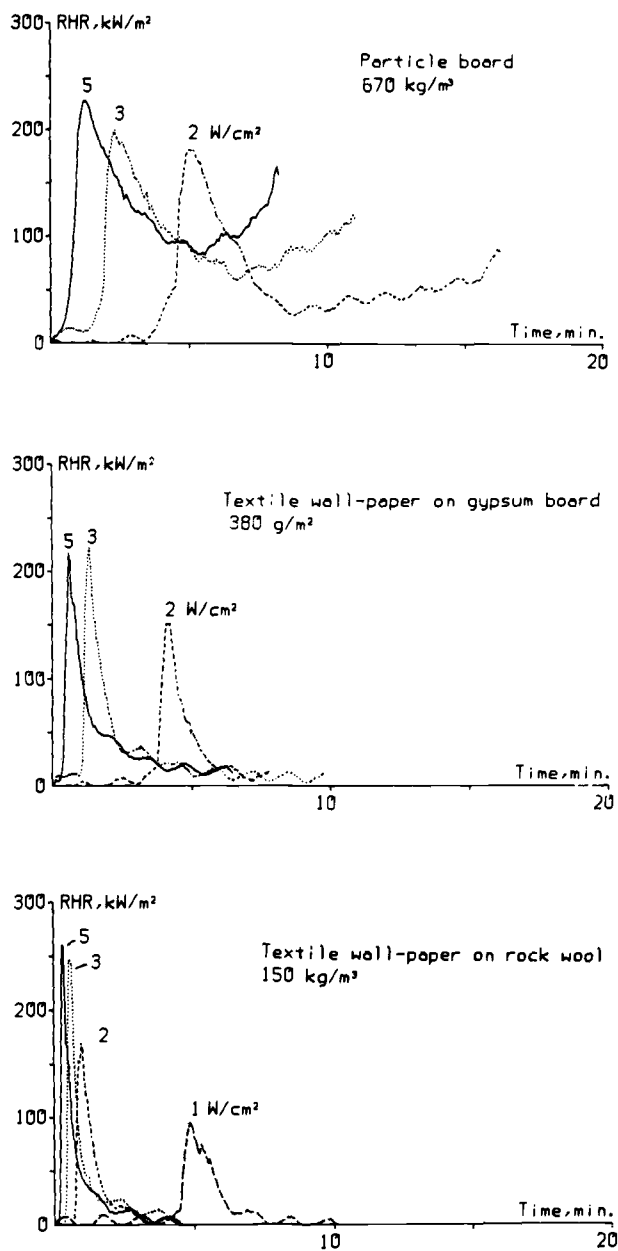


FIG. 3—Experimental RHR curves for Materials 3, 7, and 8.

where

r_{ox} = mass oxygen to fuel stoichiometric ratio, and
 $Y_{o,a}$ = ambient mass fraction of oxygen.

As described in Ref 13, the initial attempt was to try to derive the parameters ΔH , L , and r_{ox} from the RHR curves exemplified by Fig. 3. Unfortunately, it turned out that transient effects (the bulk heating term) made calculation of L unreliable. The longer heating-up period before ignition at lower flux levels in some cases neutralizes the effect of higher levels of external flux on rate of mass loss. There was also a considerable scatter in the computed values of ΔH and r_{ox} . As a consequence, it was decided to describe and make use of the RHR characteristics of the involved material directly, using a mathematical approximation of the curves shown in Fig. 3, primarily the curves valid for an external flux equal to 30 kW/m². In the full-scale experiments, heat fluxes to the lining material will vary considerably with time and location. A study of available literature indicated that an average value of 30 kW/m² might be more representative than 50 kW/m², but this has not been substantiated.

The experimental curves were idealized in the following way (Fig. 4). After exposure at $t = 0$, the sample will gradually heat up and reach pyrolysis temperature T_p at $t = t_p$. The mass loss rate during this period is neglected. At $t = t_p$, pyrolysis and rate of heat release jump to a maximum value $\dot{q}_{\max}''$ and start to decay. Asymptotically, the rate of pyrolysis is proportional to $(t - t_p)^{-1/2}$ [19]. For the initial transient period, which is of main interest for room fire growth process, it was found suitable to write

$$\dot{Q}''(t) = \dot{Q}_{\max}'' e^{-\lambda(t-t_p)} \quad (15a)$$

or, alternatively

$$\dot{Q}''(t) = \dot{Q}_o'' + (\dot{Q}_{\max}'' - \dot{Q}_o'') e^{-\lambda(t-t_p)} \quad (15b)$$

Here we will exclusively use Eq 15a. The $\dot{Q}_{\max}''$ values were taken directly from measurement and can be found together with the corresponding regression values of λ in Table 3. Figure 5 shows experimental curves and approximations according to Eq 15a for Materials 3, 7, and 8.

For the products investigated in this project, with the exception of Materials 9 and 10, the expression (Eq 15a) seemed phenomenologically correct. Thermoplastics like polystyrene, which melt and drip away before and after ignition, behave in such a way that modelling seems a remote possibility. Indeed, the room test was at least partly motivated by the incapability of small-scale tests to rationally evaluate the flammability characteristics of these materials. For Material 9, which is a composite, the general appearance of the small-scale test RHR curves varies with level of impressed flux, with the 30-

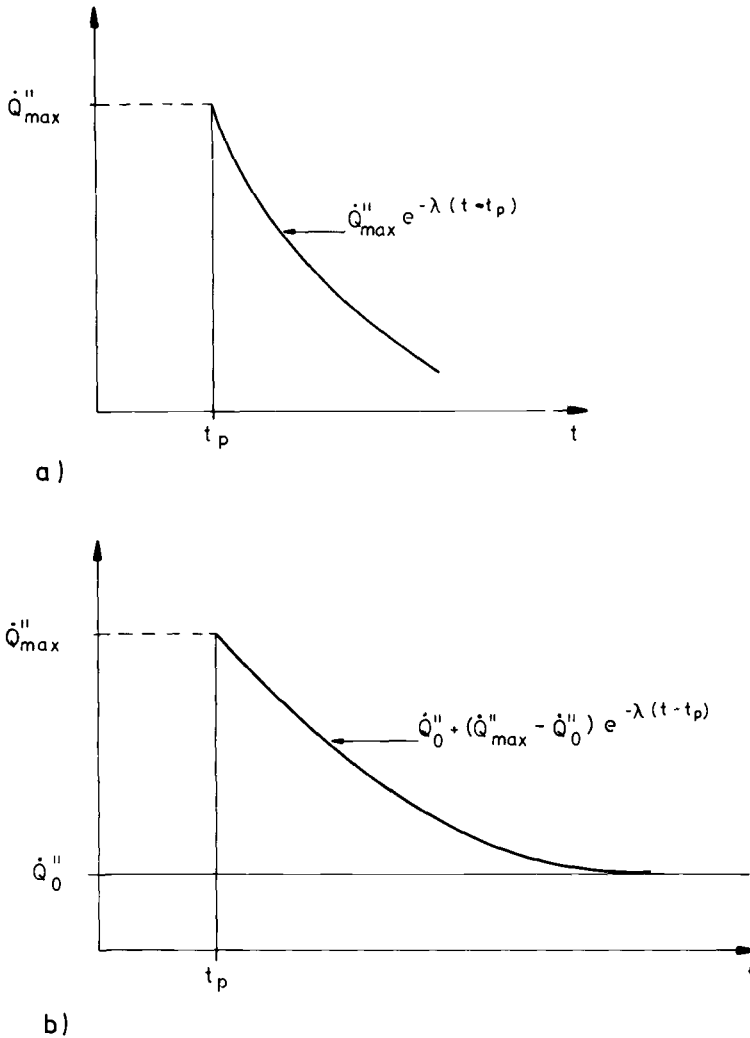


FIG. 4—Principle for analytical approximation of experimental RHR curves.

kW/m² level producing a RHR curve increasing in time and the 50-kW/m² level a decreasing curve. The remaining products may be characterized as char-forming cellulosic materials, in some cases with a thin surface cover.

Regression Analysis

Regression Equations

The derived $k\rho c$ values and RHR characteristics are to be inserted into the regression equations 8 and 9. We still have to determine the proper form of

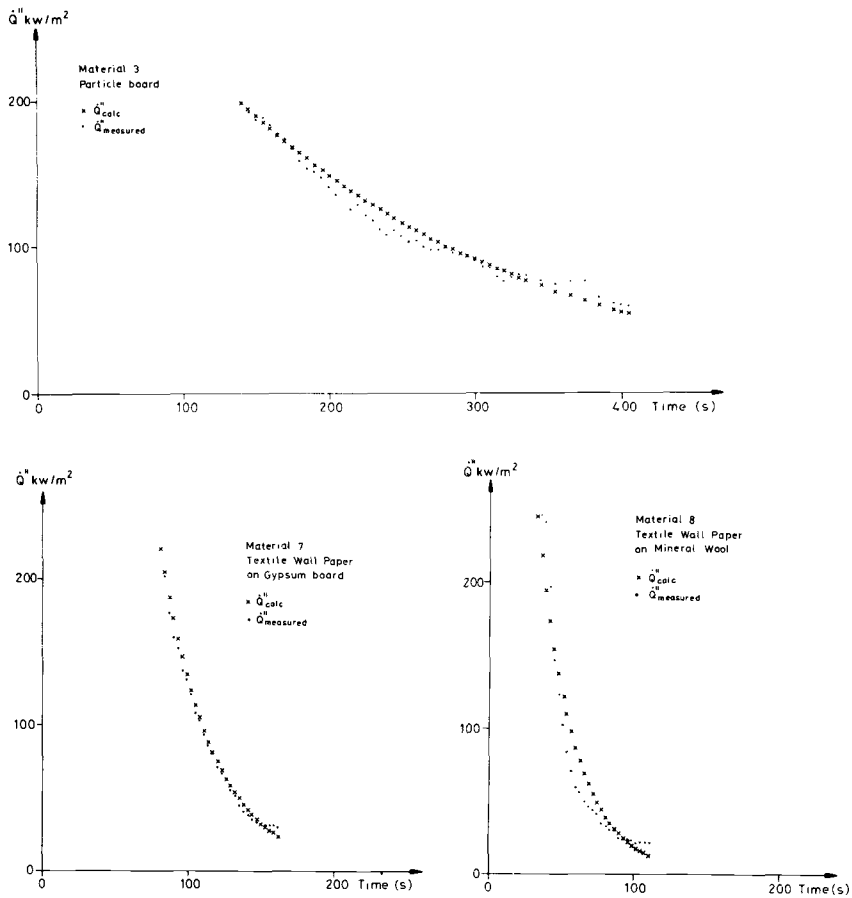


FIG. 5—Experimental RHR curves and the corresponding analytical approximation, Eq 15a.

$\dot{Q}_{av}''$. The simplest way of doing this would be to use a point estimate; that is, take $\dot{Q}_{av}'' = \dot{Q}''(t_{av})$ with t_{av} equal to a fixed fraction of elapsed time t , say $0.5t$ or $0.75t$. The rationale would be that $\dot{Q}_{ce}$ essentially is the product of two exponentials, where one, A_p , for most materials varies much faster than the other, $\dot{Q}_{av}''$, so that the time variation of the latter would be of little influence. Equation 9 would then be rewritten

$$\dot{Q}_{ce} = \alpha(e^{at} - 1)^\beta \dot{Q}''(t_{av}) \quad (16)$$

Another method would be to describe the interaction of flame spread and rate of heat release by a superposition, Duhamel-type integral.³

³Wickstrom, U., internal working memorandum, Swedish National Testing Institute, Borås, Sweden, Dec. 1983.

If we apply this method to our model and data, we obtain a simple expression for the integral (Fig. 6). We look at the situation at time $t_b = n\Delta t$ with pyrolyzing ceiling area $= A_p$. We have

$$\dot{Q}'' \sim e^{-\lambda t} \quad (17a)$$

$$A_p \sim e^{at} \quad \text{with} \quad a = h^2/k\rho c \quad (17b)$$

$$\Delta A_p \sim ae^{at} \Delta t \quad (17c)$$

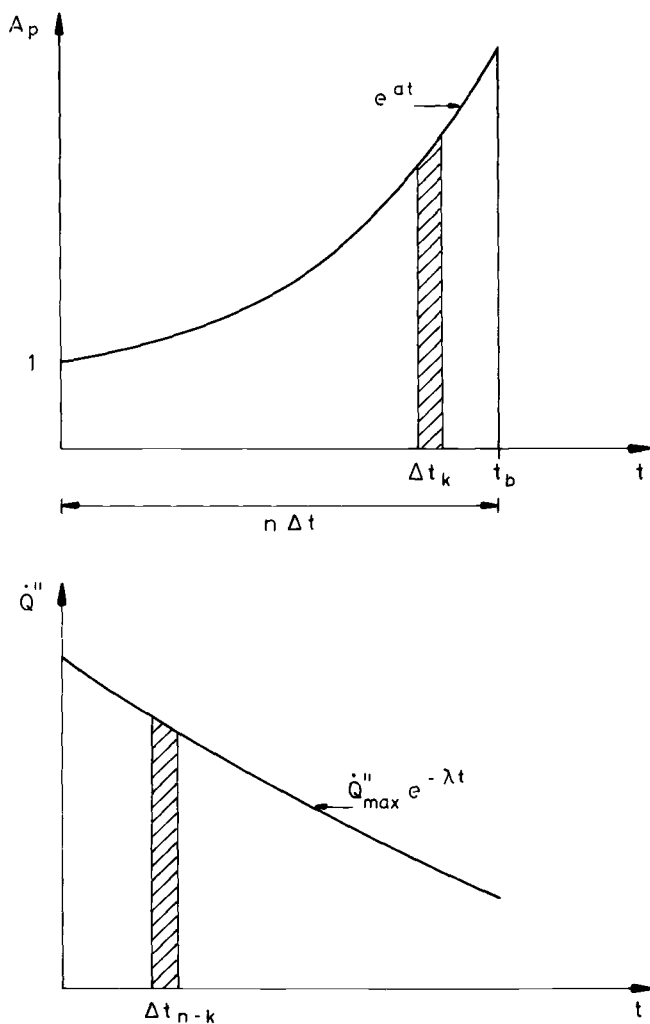


FIG. 6—Principle behind superposition integral in Eq 19.

For the infinitesimal area $\Delta A_{p,k}$ at time step k

$$\dot{Q}'' = \dot{Q}''(t_b - k\Delta t) = \dot{Q}_{\max}'' e^{-\lambda(t_b - k\Delta t)} \quad (18)$$

and, in summation form, for the total pyrolyzing area A_p

$$\dot{Q}_{ce}(t = t_b) \sim \sum_{k=1}^n a e^{-\lambda(t_b - k\Delta t)} e^{ak\Delta t} \quad (19a)$$

or, in integral form,

$$\dot{Q}_{ce} \sim \int_0^{t_b} a e^{a\tau} \dot{Q}_{\max}'' e^{-\lambda(t_b - \tau)} d\tau = \dot{Q}_{\max}'' \frac{a}{a + \lambda} (e^{at_b} - e^{-\lambda b}) \quad (19b)$$

Equation 18 is a direct physical interpretation of the so-called Duhamel's formula expressing the response of a system to a general driving function $A_p(t)$ in terms of the experimentally accessible response $[\dot{Q}''(t)$ from Eq 15] of the system to a unit step function [20]. The methodology can be expanded to treat the case where the step response $\dot{Q}''(t)$ is a function of impressed flux instead of being linked to the constant value 30 kW/m². It has previously been used to correct heat release measurements from various small- and large-scale test apparatuses for the effect of inherent time delays in the measurement system [21-23].

Time Lag Compensation

When comparing calculated and measured energy release curves, time lags deriving from room-filling time, wall ignition time, transport time, and oxygen measuring instrument response time must be considered. The response of the measurement system to a unit step load may be written as [24]

$$U(t) = 1 - e^{-\gamma(t-t_d)} \quad (20)$$

if

$$t \geq t_d$$

$$U(t) = 0 \quad \text{for } t < t_d$$

where

γ = characteristic of oxygen-measuring apparatus (the reciprocal of the "time constant"), and

t_d = delay or transport time of the combustion gases from burning zone to O₂ measuring cell.

If the true energy release rate from the full-scale test is written $\dot{Q}_{rs}$, the measured response $\dot{Q}_{mes}$ is given by the superposition integral

$$\dot{Q}_{mes} = \int_0^t U'(\tau) \dot{Q}_{rs}(t - \tau) d\tau \quad (21)$$

for $\tau \geq t_d$

Evaluation of the integral requires estimates of γ and t_d . For the type of oxygen analyzer involved, γ might be expected to be $\approx 0.25 \text{ s}^{-1}$ [24]. Experiments with furniture [4] where instantaneous mass loss rate can be compared with $\dot{Q}_{mes}$ indicate a transport time $\approx 15 \text{ s}$. Formulas for mass flow in plumes show a room-filling time, that is, the time it takes for the combustion gases to start flowing out of the doorway, to be in the order of 5 s for a 100 kW burner. Thus, t_d may be approximately 20 s. The form of the unit step load response means that parameters α and β in Eqs 16 and 19b cannot be determined by linear regression analysis. To avoid using nonlinear regression and the inherent computational difficulties, the following starting or calibration procedure was employed: $\dot{Q}_{rt}$ will be the sum of three terms: energy release from gas burner $\dot{Q}_{bu}$, from corner wall area $\dot{Q}_{co}$, and from ceiling material $\dot{Q}_{ce}$.

The sum of the first two parts is called $\dot{Q}_{start}$. If the burner is turned on at $t = 0$, a certain time t_{start} will elapse before the instruments register $\dot{Q}_{start}$. According to Eq 1b, another starting quantity is the initial pyrolyzing ceiling area, $A_{p,o}$, which is proportional to the initial ceiling flame area. This in turn is taken to be proportional to the heat energy stored in the unburnt fuel reaching the ceiling, $\dot{Q}_{uf}$. Regression Eqs 16 and 19b are then rewritten as, respectively,

$$\frac{\dot{Q}_{rt} - \dot{Q}_{start}}{\dot{Q}_{uf}} = \alpha(e^{at} - 1)^{\beta} \cdot \dot{Q}''(t_{av}) \quad (22a)$$

or

$$\frac{\dot{Q}_{rt} - \dot{Q}_{start}}{\dot{Q}_{uf}} = \alpha(e^{at} - e^{-\lambda t})^{\beta} \cdot \dot{Q}_{max}'' \cdot \frac{a}{a + \lambda} \quad (22b)$$

It should be observed that time t in Eq (22) will be measured from $t = t_{start}$. For most materials, the main component of t_{start} will be time to ignition of the wall corner lining material. In order to arrive at a practicable expression for t_{start} , a study was made of the correlation between t_{start} and the ignitability of the materials. The result was Fig. 7. It can be seen that with one exception, Material 13, t_{start} is closely approximated by the equation

$$t_{start} = t_{ig} + 5 \quad (\text{s}) \quad (23)$$

with t_{ig} measured at the 30 kW level.

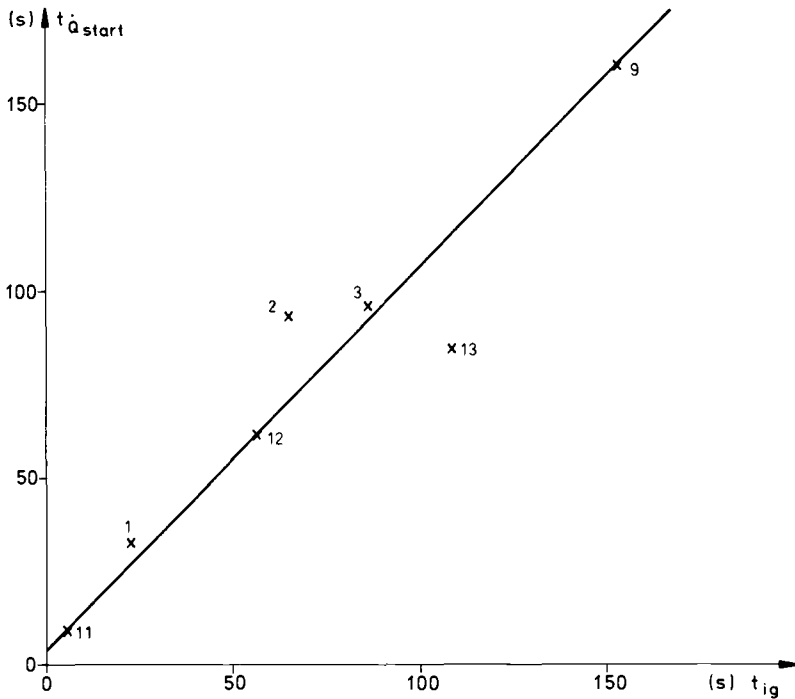


FIG. 7—Relation between response time of room test RHR-measurement system and specimen ignitability time at 30 kW/m^2 .

It remains to determine $\dot{Q}_{uf} = \dot{Q}_{start}$ — wall flame combustion. A study of air entrainment into the wall plume showed that approximately $30 \text{ g O}_2/\text{s}$, corresponding to a heat release of 400 kW , is entrained into the wall flame. It is reasonable to assume that actual combustion before the plume hits the ceiling may be in the area of 60 to 150 kW . As the computations will be insensitive to choice of $\dot{Q}_{uf}$, mainly changing the value of the parameter α with a constant, it was decided to arbitrarily put

$$\dot{Q}_{uf} = \dot{Q}_{start} - 60 \quad (24)$$

Grouping of Materials

Looking at the full-scale tests, it was decided to divide the materials into two groups: one for which the depth of combustible material was thick enough so that no burning through would occur during time up to flashover (Materials 1–3, 9, 11–13), the other for which a thin combustible surface finish covered an incombustible baseboard (Materials 4–8). Material 10, polystyrene, was left outside the analysis.

Results of Parameter Estimation

If the logarithm is taken of both sides of Eq 22, the relations are transformed into first-order linear models where the parameters α' ($= \ln \alpha$) and β can be determined by linear regression. With $\dot{Q}_t$ describing the output from the full-scale tests, this procedure was followed for the seven materials 1-3, 9, 11-13, using a standard library regression program. Table 4 gives the value of α' and β plus the correlation coefficient r for the seven materials, using Eqs 22a and 22b as a base. When using Eq 22a, the value of t_{av} must be selected. Computations with $t_{av} = t/2$ and $t/4$ gave very similar results. In Table 4, $t_{av} = t/2$. The value of t_{start} was in each individual case taken directly from the full-scale measurements, *not* from Fig. 7. The value of the heat transfer coefficient h from ceiling flame or jet to lining material must be specified. As an initial approximation, a constant average value of 40 W/m² K was considered reasonable.

As can be observed, the regression model points to negligible difference between Eqs 22a and 22b. Consequently, for the remaining part of this paper we will concentrate further computations on Eq 22a. It should be remembered, however, that in Table 4 heat release rate is linked to a single curve for each material. A more correct model taking into account that impressed flux is variable during the full-scale experiments may indicate the need for choosing rate of heat release characteristics in a more detailed way.

The output of a general least square computer program usually includes standard error of the undetermined coefficients, standard error of the dependent variable, an analysis of the variance table, and the value of the (multiple) correlation coefficient r . A closer study of the computed residuals and confidence bounds have not been carried out as yet, but the values of correlation coefficient r in Table 4 indicate that the models of Eq 22 are reasonable. At the same time, there is a variation in α' and especially β between the materials which could invalidate use of Eq 22 in a design model. For this equation to be generally applicable for design purposes, variations in α' and β must have a limited influence on the final results. The next section will deal with this question.

TABLE 4—Results from regression analysis.

Material	Eq 22a			Eq 22b		
	α'	β	r	α'	β	r
1	-4.69	1.77	0.994	-4.78	1.78	0.994
2	-4.13	1.22	0.993	-4.16	1.22	0.993
3	-4.34	1.23	0.983	-4.40	1.23	0.982
9	-5.33	0.79	0.980	-5.52	0.75	0.973
11	-5.14	1.06	0.947	-5.14	1.04	0.944
12	-4.39	0.97	0.962	-4.42	0.93	0.958
13	-3.89	1.02	0.993	-3.91	1.01	0.993

Accuracy of Prediction Model

For the model according to Eq 22a, the average values and coefficients of variation for α' and β are

$$\bar{\alpha}' = -4.58 \quad (25a)$$

$$\sigma_{\alpha'}/\bar{\alpha}' = 0.13 \quad (25b)$$

$$\bar{\beta} = 1.15 \quad (26a)$$

$$\sigma_{\beta}/\bar{\beta} = 0.276 \quad (26b)$$

Seen in the light of the scatter inherent even in standard small-scale fire tests, the variability does not seem excessive. To find out its practical importance, the full-scale tests were recalculated on the basis of the values of α' and β given by expression 25a and 26a, that is, the average values. Furthermore, t_{start} was determined by Eq 24. Evidently this procedure is something of a circle proof that had to be accepted for lack of independent experimental data. The results can be seen in Figs. 8a to 8g. In substance, the agreement indicated the general validity of the derived parameters α and β . There are two essential deviations: Material 9, which behaved erratically in the small-scale tests [17], and Material 13. For the latter the difference is due to the fact that material takes much longer to ignite in the small-scale test than in the full-scale one (see Fig. 7). A recalculation using the correct ignition time for the room experiment showed a considerably better agreement (Fig. 8g).

Calculation of Complete Process

Knowing the parameters α and β and having estimates of the transport delay term t_d and oxygen instrument characteristic γ in Eq 20, the superposition integral Eq 21 can now be evaluated. An example is given in Fig. 1 for Material 3. For reasons of computational simplicity, the parameter β was taken as equal to 1.0. Figure 1 illustrates the practical applicability of the approach described in this paper.

Thin Surface Finishes on Noncombustible Baseboard

Five materials, Nos. 4-8, can be regarded as thin combustible surface finishes on noncombustible baseboard. In only one case, Material 8, was there a flashover during the initial period of 10 min with burner output equal to 100 kW. If we rewrite Eq 22a and replace t_{av} with t we get

$$\dot{Q}_{rt} = \dot{Q}_{\text{start}} + \dot{Q}_{uf} \cdot \alpha \cdot (e^{at} - 1)^{\beta} \cdot \dot{Q}_{\text{max}}'' e^{-\lambda t} \quad (22')$$

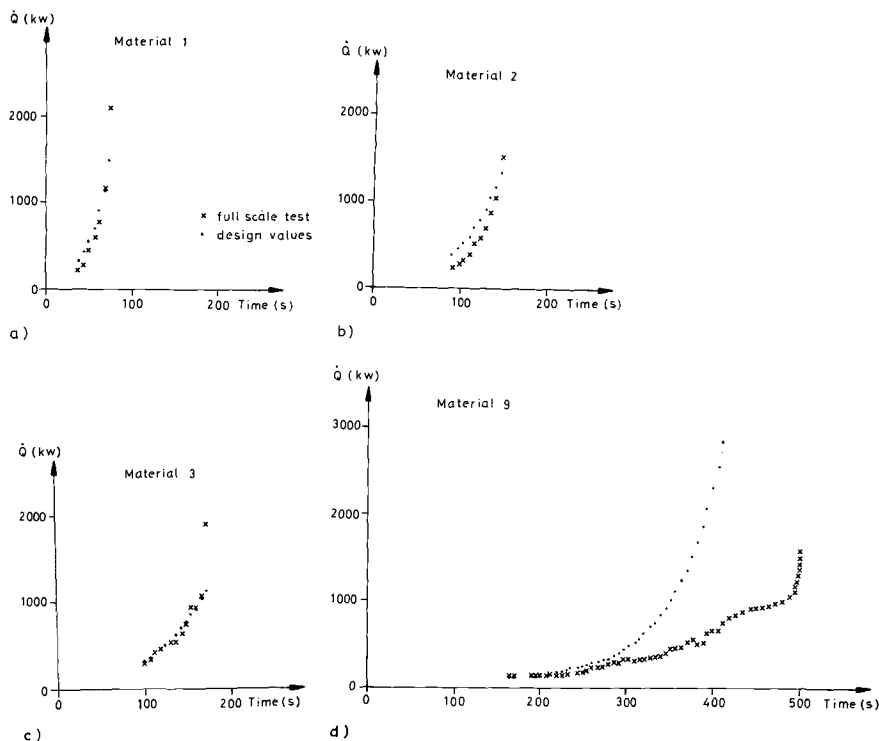


FIG. 8a to FIG. 8g—Experimental RHR curves from room test (x) and design theory curves (·). The curve with $\circ$ in Fig. 8g denotes calculated values where response time equals experimental value.

This expression will be bounded for all t only if

$$\beta a < \lambda \quad (27)$$

A look at Table 3 reveals that the condition is approximately fulfilled for Materials 4–7 but decidedly not for Material 8. The ratio $\beta a/\lambda$ is approximately 1.0, 0.72, 0.77, 0.72, and 9.7 for the five materials.

Equation 27 expresses the unbalance between the two competing processes: flame growth and decay in heat release rate. It should prove comparatively simple to incorporate the effects of partial burning through into Eq 21.⁴

It was demonstrated in Ref 13 that for thin materials a numerical simulation can be carried out, demonstrating the process when flame spreads over part of the ceiling, then recedes without causing flashover. The results were sensitive to choice of input data, and the procedure requires further exploration.

⁴Mitler, H., personal communication, June 1984.

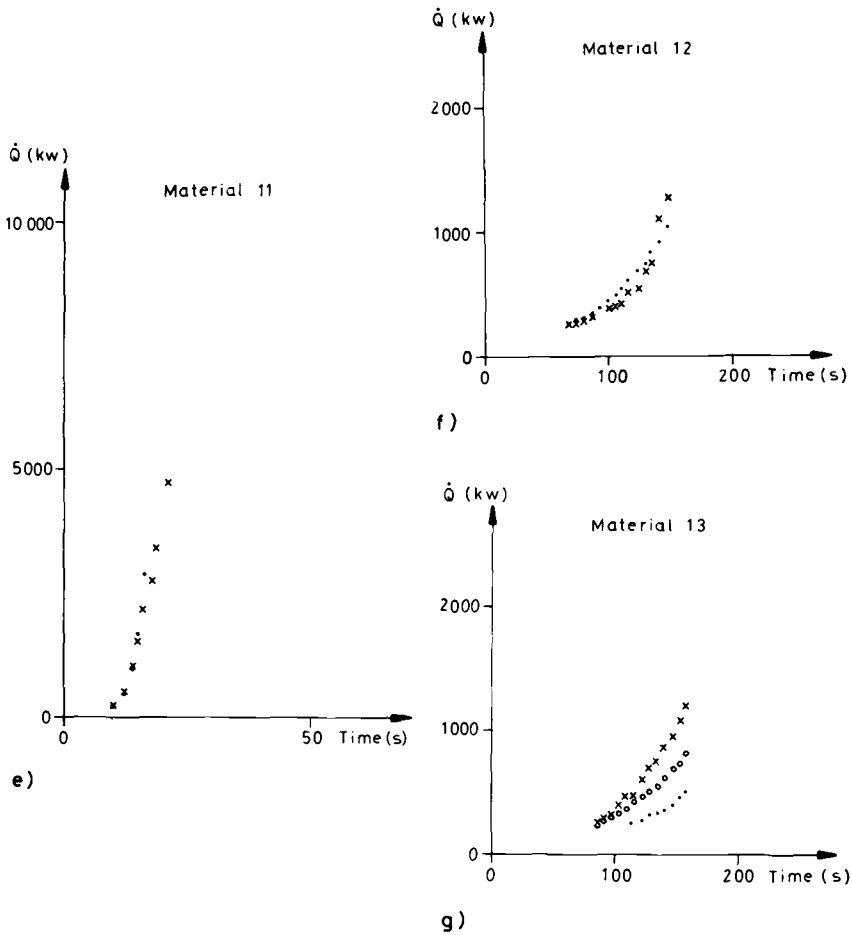


FIG. 8a to FIG. 8g—Continued from previous page.

Remarks on the Results

No sensitivity testing has so far been carried out with respect to the different assumptions and procedures just enumerated. It is likely that changes can be introduced into the analysis, leading to an improved consistency. As an example, putting the heat transfer coefficient equal to a constant is an oversimplification. It should be possible to describe h as a function of $\dot{Q}_r(t)$ and in this way lessen the tendency of Eq 22a to underestimate $\dot{Q}_r(t)$ when the test process is approaching flashover. Also, the effect of replacing the factor $h^2t/k\rho c$ with

TABLE 5—Relative ranking of materials:
Time, *s*, to reach 1 MW in room test.

Material	Experiment	Design Equation
1	66	65
2	140	134
3	165	166
9	499	351
11	14	14
12	139	146
13	148	~ 205

$$\frac{h^2 t}{k \rho c} \frac{(T_f - T_a)^2}{(T_{ig} - T_a)^2}, \quad (3b)$$

should be investigated.⁴

Finally, it should be pointed out that even if the predictive capability of the analysis in a quantitative sense may still be limited, it can be used qualitatively to rank materials. If we rank the seven materials with respect to the time it takes for the fire room test to reach 1 MW, the relative ranking is coincident in experiment and design theory (Table 5).

In summary, it is thought that a first step has been taken in the efforts to use results from small-scale flammability tests to rationally predict full-scale fire growth (for *one* specified scenario) and rank materials.

Further efforts should be directed firstly towards other fire scenarios, secondly towards code calibration studies. The sensitivity studies needed to derive confidence regions and safety factors can be based on the first order, second-moment statistical methods used in a number of engineering areas, for example, structural fire endurance [24].

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Future Directions for Modeling the Spread of Fire, Smoke, and Toxic Gases

REFERENCE: Jones, W. W., "Future Directions for Modeling the Spread of Fire, Smoke, and Toxic Gases," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 70-96.

ABSTRACT: In recent years there has been considerable interest in the growth of fires and the subsequent movement of smoke and toxic gases in multicompartiment structures. This activity is motivated by the need to understand and predict the environmental conditions which occur as a fire develops and spreads. Much of the attention has focused on the development of numerical models which are able to make reasonably accurate predictions from the onset of ignition. Most of the effort has centered on the control volume or zone model approach. While this approach loses some of the detailed analysis available from a fully differential model, such as the velocity profile in a vent, much is gained by the reduction in the number of equations to be solved for each compartment.

As with other types of models, the basic equations to be solved include conservation of mass and energy. These equations are usually recast into predictive equations for temperature, pressure, and volume (layer thickness). An almost universal approximation is that the transient pressure term (dP/dt) is not a significant process in energy transfer. One reason for this approximation is the assumption that an algebraic equation is easier and faster to solve than a differential equation.

We have rederived the zone model equations for the usual two-layer model, retaining the transient pressure term. This allows us to recast the equations in a simpler form which is symmetric for the "upper" and "lower" layers.

We have found this form of the equations to be computationally faster. Also, the transient nature of the equations can be seen in the stability of these equations as well as in the stability of physical processes such as flame puffing and flashover.

We have constructed a multicompartiment model using a framework discussed in the paper. Testing this model has been done using experimental results discussed in the paper. These comparisons, plus current calculations and timings for the various models, are presented.

KEY WORDS: fire models, smoke transport, zone models, toxic hazard

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Nomenclature

- A** Area, m^2 ; A_u, A_l are the upper and lower compartment surface areas in contact with the upper and lower gas layer (Fig. 1), respectively. A_d is the interface area between the upper and lower layers. In the section entitled Source Terms: Door Jets, A is used as a variable in the flow equations to indicate air.
- B** Width of a vent, m
- C, C'** Flow coefficient ≈ 0.6 to 0.7 for both smoke and air
- c** Specific heat— c_p, c_v (J/kg/ K)
- $\dot{E}$** Energy release rate, J/s
- F_{ij}** View factor—relative area of i as seen by j (dimensionless)
- g** Acceleration of gravity, 9.8 m/s^2
- H** Height (m); H_u, H_l are the upper and lower limits of a vent (Fig. 1)
- h** Enthalpy, J/kg/ K
- h_c** Heat of combustion—theoretical, J/kg
- i, j** Compartment indices
- L** Mean beam length, m, equivalent opaque sphere
- m** Mass, kg
- $\dot{m}$** Mass flow, kg/s:
 $\dot{m}_v$ = rate of release of volatiles
 $\dot{m}_e$ = entrained into a plume
 $\dot{m}_f$ = fuel burning rate = $\chi_e \dot{m}_v$
 $\dot{m}_{ij}$ = mass entering room i from room j
 $\dot{m}_p$ = flow rate in plume ($\dot{m}_p = \dot{m}_v + \dot{m}_e$)
- N** Height of the neutral plane, m
- P** Pressure, Pa:
 $\bar{P} \rightarrow P$ = floor reference pressure
 P_e = Eq 19
 P_a = outside ambient pressure
- $\dot{Q}$** Rate heat is added or lost, J/s:
 $\dot{Q}_u, \dot{Q}_l$ = upper, lower zones, respectively
 $\dot{Q}_f$ = fire ($h_c \dot{m}_v$)
 $\dot{Q}_o$ = objects
 $\dot{Q}_R$ = radiation
 $\dot{Q}_c$ = convection by walls
 $\dot{Q}_g$ = radiation added to upper gas layer
 $\dot{Q}_k$ = radiation from surface k
 $\dot{Q}_p$ = combustion energy lost by formation of volatiles
- R** Gas constant for specific mixture
- S** Smoke—section entitled Source Terms: Door Jets
- t** Time, s
- T** Temperature, K:
 T_a = ambient
 T_c = external wall

- T_u = upper wall
 T_l = lower wall
 T_R = reference temperature for enthalpy flow
 T_g = upper zone temperature
 T_v = pyrolysis temperature
 V Volume, m^3
 Z Layer thickness, m — Z_i = interface height in compartment, i
 α Absorption coefficient of upper gas layer (m^{-1}), thermal diffusivity (m^2/s)
 γ Ratio of specific heat, c_p/c_v
 ϵ Emissivity (dimensionless):
 ϵ_i = surface i
 ϵ_g = upper gas layer
 ϵ_u = upper compartment surface
 ϵ_l = lower compartment surface
 ρ_{ui} = density of the lower layer in compartment i
 ρ Mass density, kg/m^3
 κ Thermal conductivity, $\text{J}/\text{m}/\text{s}/\text{K}$
 δ_{ij} Kronecker delta = 0 $i \neq j$
 = 1 $i = j$

Subscripts

In general u and l indicate upper and lower gas layer, respectively. For area and emissivity variables, reference is to the compartment itself.

In recent years considerable research has been done regarding the spread of fire and smoke from a room of fire origin to connected compartments. The work is motivated by a need to understand and predict the environmental conditions which occur as a fire develops and spreads. Much of the attention has focused on the development of numerical models which are able to make a reasonably accurate assessment of the environment from ignition. The motivation is twofold. Being able to correlate laboratory scale experiments with full-scale tests is desirable from a cost standpoint. More important, however, from a life-safety and operational standpoint, the ability to make accurate predictions of the spread of fire, smoke, and toxic gases opens up many possibilities for combating these problems, as well as for taking effective preventive measures. The ability to prevent the hazards from developing becomes especially important as new and exotic materials become available.

This paper describes a model which allows one to predict the evolution of a fire in a room and the subsequent transport of the smoke and toxic gases which evolve from this fire. The numerical implementation improves on previous work (for a review see Jones [1]), in particular by retaining the conservation laws in their full differential form and solving them as a set of coupled ordinary differential equations (ODEs). Such a formulation takes advantage

of the effort which has gone into solving such systems of equations. The result is a numerical scheme which is considerably faster and much more "rugged" than previous models.

The model is assumed to be in a world of uniform temperature, T_a , and reference pressure, P_a , with the outside of a wall at T_e , which may not be the same as the ambient. The discussion is broken down into the basic structure and fundamental assumptions which go into the model, followed by a derivation of the predictive equations, a discussion of the source terms, the numerical implementation, and some calculations and comparisons with experimental data. The notation is given in the Nomenclature. The numerical implementation of the model is modular and straightforward. It is designed to be transportable.

Structure of the Model

The primary element of the model is a compartment. The primary interest lies in the composition of the gas layers in each of these compartments. As such, the model is structured around fluid transport phenomena. In this context, the predictive equations for the gas layers in each compartment result from conservation of mass, momentum, and energy together with an equation-of-state for each compartment. The actual physical phenomena which drive the transport are then couched as source terms. Such a formulation allows the greatest flexibility in adding, modifying, or deleting terms which are appropriate to the problem being solved.

Each compartment is subdivided into two "control volumes." These control volumes will be of sufficient size that we will require only a few to describe any system of interest. The choice is based on the premise that the details which occur within such a volume do not concern us (at present), but their mutual interaction does. Each of these control volumes is called a *zone*. The rationale for such a choice arises from the experimental observation that when a fire spreads, the gas layers in the compartments actually stratify into two distinct zones. This is a compromise between a network model and a finite difference model. The former is computationally fast but yields no information on the internal structure, and the latter is too computationally intensive. The two zones are referred to as "upper" and "lower," respectively. The basic equations describe the mass, momentum, and energy transfer from zone to zone in a fire-driven environment. A schematic is shown in Fig. 1.

In considering dynamic systems, it is necessary to solve a problem self-consistently. If this is not done, then some of the dynamics may be obscured or even lost. In particular, discussion of movement of the zone interfaces must be consistent.

The conservation equations for mass and energy can be written in the form

$$\frac{dm}{dt} = \sum_i \dot{m}_i \quad (1)$$

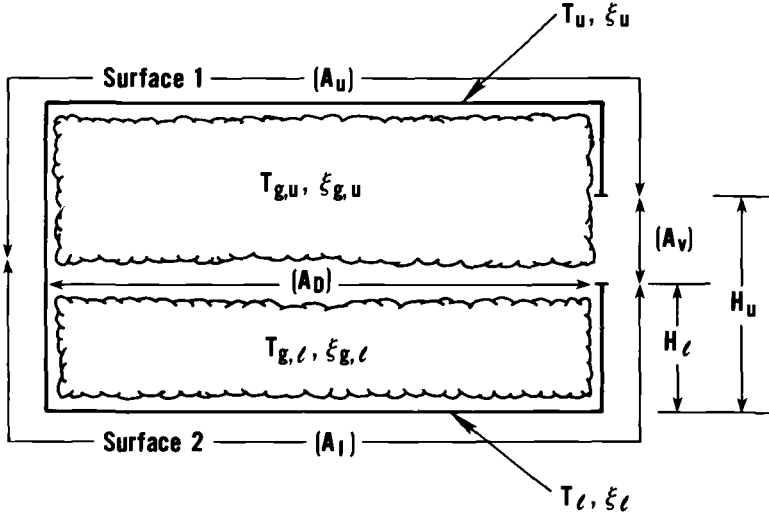


FIG. 1—A schematic of one compartment in the zone model concept. Two layers (u, l) and a single vent are shown.

for mass and from the first law of thermodynamics we have

$$\frac{d}{dt}(\epsilon V) + p \frac{dV}{dt} = \dot{Q} + \dot{h} \quad (2)$$

together with an equation of state

$$P = \rho RT \quad (3)$$

which closes the set of equations with the definitions

$$\begin{aligned} \epsilon &= c_v \rho (T - T_R) \\ \dot{h} &= c_p \sum_i \dot{m}_{i,in} (T - T_R) - c_p \sum_i \dot{m}_{i,on} (T - T_R) + \sum_i \dot{h}_{i,o} (T_R) \\ \dot{Q} &= \dot{Q}_f + \dot{Q}_R + \dot{Q}_c = \text{net energy input} \end{aligned}$$

The term $h_{i,o}(T_R)$ is relative to the temperature from which this mass parcel, $\dot{m}dt$, came. It includes enthalpy of formation. The equation of state for an ideal gas is usually used for closure of the system. More correctly, it should be written

$$P = P(\rho, T, \{i\}), \quad \text{where } \{i\} \equiv \text{set of species} \quad (4)$$

especially for applications to fire problems which are not ideal gas problems. However, for the case of an ideal gas, the derivations and discussion are simplified, and generalizations can be discussed later. The sign convention is that positive fluxes on the right-hand side of an equation will increase the quantity being calculated on the left-hand side; that is, transfer into a volume is indicated by a positive flux on the right-hand side.

The general form of the model is to divide each compartment into two zones: an upper zone which contains a hot layer, and a lower layer which is relatively cool. There may exist one or more fires and plumes in each compartment, and they can usually be considered to be part of the upper zone. Mass and energy transfer between the zones is provided by the plumes, mixing at the vents, radiation between layers, and flow along the walls. In general a plume, once created, simply transfers mass and energy from one zone to another. Another set of equations could be written for the plume, but as long as it is in quasi-steady equilibrium, considering it to be part of the upper zone is sufficient. Another way of looking at the plume is to consider it so small in mass, energy content, and volume that it can be ignored except as a transfer mechanism. For some problems, however, the plume must be considered a separate zone along with the concomitant conservation equations. An example would be when the rise time of the plume is of interest or when the actual size and composition of the plume is important.

Since the conservation equations are written in terms of the volumes of these entities, a relationship between the height and the volume is needed, such as

$$V_u = \int_{Z_l}^{H_R} ZA(Z)dz \quad (5)$$

to calculate the layer depths. This removes the usual restriction that the compartments be rectangular parallelepipeds and allows calculations for circular cross sections (aircraft) and trapezoids (atria).

The radiation transport scheme used is fairly simple and derives from the work of Siegal and Howell [2]. The view factors which are used in the calculation of solid angles are concomitant with surfaces which are planes or discs. The relationship is shown schematically in Fig. 2. The simplification used here and discussed in more detail by Jones [1] is retained, but the actual areas are used, and the exact view factors are used wherever possible, Kusada [3]. For example, in calculating the area of the upper wall, allowance is made for any vents which exist by subtracting their area from the total wall area. In addition, ceiling, upper wall, lower wall, and floor are all treated separately. This is necessitated by the possible use of different materials for each surface, which may change the radiative properties as well as the different types of convective flow that occur over each of these surfaces.

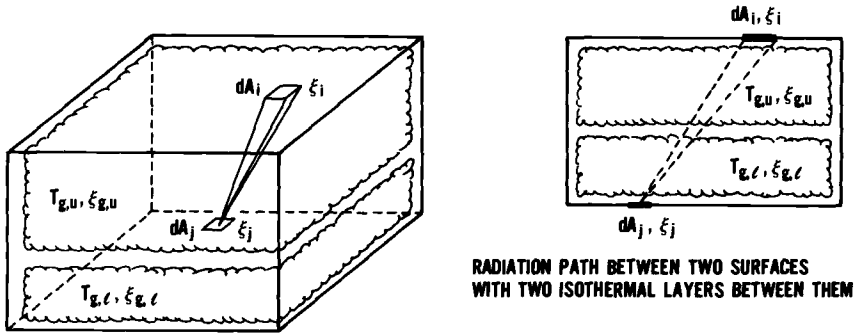


FIG. 2—Radiation transport scheme. Shown is the view factor calculation of surface i as seen by surface j , transmitted through both layers.

Actual closure of the model is obtained by assuming that the size of the compartment is fixed so that

$$V = V_u + V_l \quad (6)$$

and that there is a single reference pressure

$$P_u = P_l \quad (7)$$

at the boundary (interface) of the zones.

The set of equations which is necessary to describe fully such a physical system can be reduced by considering the physical impact of some of the terms. In particular, the pressure should be a general function of position, $P(X, Y, Z)$, in the compartment, which would require us to include a differential equation for the conservation of momentum explicitly. A general form for the perturbed pressure might be

$$\bar{P}(x, y, t) = P(t) - \int_0^Z \rho(t) dz + \delta P(X, Y, Z, t) \quad (8)$$

where P is a reference pressure and is usually the pressure at the base of the compartment. In the spirit of the “control volume” formulation

$$\delta P \rightarrow 0 \text{ as a function of position}$$

This implies that acoustic waves are filtered out and that internal momentum need not be calculated. The hydrostatic term is small in absolute value in comparison with the reference pressure, so it is not necessary to carry this calculation through the equation of state. Finally, the time-dependent por-

tion of the hydrostatic term deals with the movement of the interface and thus can be ignored if we limit ourselves to problems where the momentum associated with the discontinuity (alternatively, the velocity) is not significant. Dropping the momentum equation for internal waves increases the computational time step a great deal since we are not limited by the Courant time step criterion (time step a grid size/speed of sound). This prohibits us from considering problems such as deflagration waves and explosions. With these considerations in mind, we will assume

$$P_u = P_l = P \quad (9)$$

The pressure in Eq 9 is the reference pressure at the floor. This simplification is carried through the conservation equations and greatly simplifies the resulting predictive equations. However, it is necessary to retain the hydrostatic term for the flow field calculations at vents. As we are considering only small changes in the absolute pressure, differences of these terms will be comparable to the hydrostatic pressure change (Eq 8).

For example, for Eq 22

$$\delta P \sim 1 - 1000 \text{ Pa}$$

whereas

$$\int_{\text{Reference}}^{\text{orifice}} (\rho_1 - \rho_2 T_2) dz \sim 100 \text{ Pa}$$

So the flow can be dominated by the hydrostatic term, whereas the $\delta P/P < 1\%$.

Derivation of the Conservation Equations

A zone model describes a physical situation in terms of integrals of extensive physical quantities. Thus we deal with total mass rather than mass density, total energy rather than energy density, but temperature is used as before (an intensive quantity). The integrals are volume integrals whose boundary surfaces enclose the Euclidean space of interest. The space with which we are concerned usually is a compartment with zones including a hot upper gas, a cool (relatively) lower layer, objects, plumes, and fires. The connections occur at the boundary of these zones. Examples of possible connections are the vents connecting compartments, the radiation from a fire to the compartment walls, etc. With this basis we can mold the conservation equations into a form which describes a fire in terms of the quantities that are appropriate to the control volume approach, that are intuitively understandable, and that lend themselves to measurement.

The conservation equations for each compartment of a two-layer model are

$$\frac{dm_u}{dt} = \sum_i \dot{m}_{u,i} \rightarrow \dot{m}_u \quad (10)$$

$$\frac{dm_l}{dt} = \sum_i \dot{m}_{l,i} \rightarrow \dot{m}_l \quad (11)$$

$$\frac{d}{dt} [c_v m_u (T_u - T_R)] - V_u \frac{dP_u}{dt} = \dot{Q}_u + \dot{h}_u \quad (12)$$

$$\frac{d}{dt} [c_v m_l (T_l - T_R)] - V_l \frac{dP_l}{dt} = \dot{Q}_l + \dot{h}_l \quad (13)$$

In writing these equations, we will make two assumptions. First, that the fire will not feed mass directly into the lower layer. Second, we will write the upper layer equation as if there is only one fire. If more than one fire exists, then a sum over such sources is necessary and, if none are present, then this term vanishes. The source term $\dot{Q}$ includes all energy transfers due to radiation and convection, and $\dot{h}$ includes the enthalpy flow. We can rewrite the energy conservation equations as predictive equations for temperature, in which case they become

$$m_u c_p \frac{dT_u}{dt} - V_u \frac{dP_u}{dt} = \dot{Q}_u + \dot{h}_u - c_p T_u \frac{dm_u}{dt} + c_v T_R \frac{dm_u}{dt} \quad (14)$$

$$\rightarrow \dot{E}_u$$

and

$$m_l c_p \frac{dT_l}{dt} - V_l \frac{dP_l}{dt} = \dot{Q}_l + \dot{h}_l - c_p T_l \frac{dm_l}{dt} + c_v T_R \frac{dm_l}{dt} \quad (15)$$

$$\rightarrow \dot{E}_l$$

The right hand side of Eqs 14,15 can be rewritten in a simpler form. The reference temperature, T_R , is chosen arbitrarily. If the only phase change occurs in pyrolysis of the fuel, then we can set the reference pressure to zero and include the pyrolysis energy as a sink term in the energy release rate. As $T_R \rightarrow 0$ we obtain

$$\begin{aligned} \dot{E}_u = & \dot{Q}_f(u) + \dot{Q}_R(u) + \dot{Q}_c(u) - Q_p \dot{m}_f + c_p \dot{m}_p (T_v - T_u) \\ & + c_p \sum_i \dot{m}_{i,u} (T_i - T_u) \end{aligned} \quad (16)$$

and

$$\dot{E}_l = \dot{Q}_f(l) + \dot{Q}_R(l) + \dot{Q}_c(l) + c_p \sum_i \dot{m}_{i,l}(T_i - T_l) \quad (17)$$

The equations and assumptions necessary to close this set are

$$P = \rho RT \quad (18)$$

$$\rho_u RT_u = \rho_l RT_l \quad (19)$$

with

$$m = \rho V \quad (20)$$

and

$$V = V_u + V_l = \text{constant} \quad (21)$$

The energy source terms $\dot{E}_l, \dot{E}_u$ are shown schematically in Eqs 16,17 but can be much more complex and depend upon the configuration. Combining Eqns 10,11 and 14 to 17, together with the closure relations, we obtain

$$\frac{dP}{dt} = \frac{\dot{s}}{(\beta - 1)V} \quad (22)$$

$$\frac{dT_u}{dt} = \frac{1}{\beta} \left(\frac{T_u}{PV_u} \right) \left(\dot{E}_u + \frac{V_u}{(\beta - 1)V} \dot{s} \right) \quad (23)$$

$$\frac{dT_l}{dt} = \frac{1}{\beta} \left(\frac{T_l}{PV_l} \right) \left(\dot{E}_l + \frac{V_l}{(\beta - 1)V} \dot{s} \right) \quad (24)$$

$$\frac{dV_u}{dt} = \frac{1}{P\beta} \left(c_p \dot{m}_u T_u + \dot{E}_u - \frac{V_u}{V} \dot{s} \right) \quad (25)$$

where

$$\dot{s} = c_p \dot{m}_u T_u + c_p \dot{m}_l T_l + \dot{E}_u + \dot{E}_l \quad (26)$$

and

$$\beta = c_p/R = \gamma/(\gamma - 1) \quad (27)$$

It should be pointed out that the equations are written in this form for the sake of clarity and simplicity. In a numerical implementation there are better ways to express the source terms which minimize the problem of the small difference of large numbers.

Source Terms

Equations 22–25 are written so that physical phenomena which affect the environment are source terms and appear on the right-hand side. Sources which appear directly are:

1. Radiation between the gas layers and walls, fires, and other objects.
2. Convection heating.
3. Flow in plumes.
4. Flow in vent jets.
5. Mixing at vents.

Phenomena which are included but do not show up explicitly are:

1. Radiation between objects.
2. Conduction through walls and objects.

Source Terms: Radiation

In order to calculate the radiation absorbed in a zone, a heat balance must be done which includes all objects which radiate to the zone. Clearly, in order for this calculation to be done in a time commensurate with the other sources, some approximations are necessary.

The terms which contribute heat to an absorbing layer are the same (in form) for all layers. Essentially we assume that all zones in these models are similar, so we can discuss them in terms of a general layer contribution.

Radiation can leave a layer by going to another layer, by going to the walls, by exiting through a vent, by heating up an object, or by changing the pyrolysis rate of the fuel source. Similarly, a layer can be heated by absorption of radiation from these surfaces and objects as well as from the fire itself. The formalism which we will employ for the geometry is that used by Siegel and Howell [2] and is shown in Fig. 2. The radiative transfer can be done with a great deal of generality; however, as with most models we assume that zones and surfaces radiate and absorb like a grey body with some constant emissivity ($\epsilon < 1$).

A further assumption consonant with the stratified zone assumption is that emission and absorption are constant throughout a gas layer. In application to a growing fire, a further assumption is made that the lower layer is mostly diathermous. Although not a necessary assumption, this reduces the computation time for this term by 50%. For smoke propagation some distance from the fire(s), such an assumption will not be valid, but the temperature will be so low that radiation will not be the dominant mechanism for heat loss. Flames, plumes, fires, objects, and bounding surfaces have some average shape from which the view factors can be calculated. The walls of compartments are usually flat and rectangular. The gas layers are spheres with an equivalent radius of

$$L = 4V/A \quad (28)$$

and an effective emissivity of

$$\epsilon_g = 1 - \exp(-\alpha/L) \quad (29)$$

The terms which contribute to heating of a layer are:

1. Fires and plumes.
2. Walls.
3. Other layers.
4. Vents (radiation from other compartments).
5. Nonburning objects.

The radiation balance of Items 2-5 can be dealt with using the following notation:

- F_{jk} = Geometrical view factor of surface j by surface k
 σ = Stephen-Boltzman constant = $5.67 \times 10^{-8} \text{ W/m}^2 \text{ K}^4$
 α = Absorption coefficient of the upper gas layer, m^{-1}
 L = Mean beam length of the equivalent sphere, m
 = Defined in Eq 28
 $\epsilon_{u,l}$ = Emissivity of the upper/lower walls
 ϵ_g = Emissivity of the upper gas layer

Using the formalism of Siegal and Howell [2], we have

$$D = [1 - (1 - \epsilon_u)(1 - \epsilon_g)F_{uu}][1 - (1 - \epsilon_l)F_{ll}] - [(1 - \epsilon_u)(1 - \epsilon_l)(1 - \epsilon_g)^2 F_{ul}F_{lu}] \quad (30a)$$

$$\begin{aligned} \Pi_u = & \{ [1 - (1 - \epsilon_g)F_{uu}][1 - (1 - \epsilon_l)F_{ll}] \\ & - [(1 - \epsilon_l)(1 - \epsilon_g)^2 F_{ul}F_{lu}] \} \sigma T_{uw}^4 \\ & - (1 - \epsilon_g)F_{ul}\epsilon_l \sigma T_{lw}^4 - \{ 1 + (1 - \epsilon_l)[(1 - \epsilon_g)F_{ul}F_{lu} - F_{ll}] \} \epsilon_g \sigma T_g^4 \end{aligned} \quad (30b)$$

$$\begin{aligned} \Pi_l = & \{ [1 - (1 - \epsilon_u)(1 - \epsilon_g)F_{uu}](1 - F_{ll}) - (1 - \epsilon_u)(1 - \epsilon_g)^2 F_{ul}F_{lu} \} \sigma T_{lw}^4 \\ & - (1 - \epsilon_g)F_{lu}\epsilon_u \sigma T_{uw}^4 \\ & - \{ [1 - (1 - \epsilon_u)(1 - \epsilon_g)F_{uu}]F_{lu} + (1 - \epsilon_u)(1 - \epsilon_g)F_{lu} \} \epsilon_g \sigma T_g^4 \end{aligned} \quad (30c)$$

Finally,

$$\begin{aligned} \dot{Q}(\text{upper}) &= A_u \epsilon_u \Pi_u / D \\ \dot{Q}(\text{lower}) &= A_l \epsilon_l \Pi_l / D \end{aligned}$$

To this must be added the energy radiated by the fire. A heat transfer balance with the fire is not necessary simply because the amount of heat radiated by the fire is usually much greater than that absorbed by the fire. In order to investigate flashover, however, this calculation must be generalized to include the fire and lower layer absorption in the radiative heat balance equation rather than relying on the postulate that superposition of the terms is sufficient.

A simple example of the results can be given for the case

$$\epsilon_u = \epsilon_l = 1$$

for which we have

$$\begin{aligned} \dot{Q}_g = & -\sigma[\epsilon_g T_u^4 A + (1 - \epsilon_g) T_{uw}^4 (A_u + A_{uv}) \\ & - T_{uw}^4 A_u - \epsilon_g T_{lw}^4 A_d - T_a^4 A_{uv}] + F_f \dot{Q}_f \end{aligned} \quad (31)$$

where

A_d = area of the upper/lower layer discontinuity,

A_{uv} = area of vents which the gas layer "sees,"

A_u = area of the upper wall (including ceiling),

F_f = fraction of the fire which radiates times its view factor for the gas layer, and

$A = A_u + A_d$.

Source Terms: Convective Heating

Convection is the mechanism by which the gas layers lose (or gain) energy to wall or other objects. Conduction is a process which is intimately associated with convection, but as it does not show up directly as a term for heat gain or loss, it will be discussed here.

Convection heat flow is energy transfer across a thin boundary layer. The thickness of this layer is determined by the relative temperature between the gas zone and the wall or object surface Schlichting [4] and Turner [5]. We can write the heat flux term as

$$\dot{Q}_c = h_c (T_g - T_w) A_w \quad (32)$$

where the transfer coefficient can be written as

$$h_c = \frac{k}{l} C_v (Gr \cdot Pr)^{1/3}$$

The terms are:

A_w = area of wall(s) in contact with the zone,

Gr = Grashof number = $gl^3|T_g - T_w|/\nu^2 T_g$,

Pr = Prandtl number ≈ 0.7 ,

k = thermal conductivity of the gas = $6.5 \times 10^{-8} \left(\frac{T_g + T_w}{2} \right)^{4/5}$,

l = length scale $\approx \sqrt{A_w}$,

C_v = coefficient which depends on orientation, Turner [5],

N_u = Nusselt number, and

$\nu = 7.18 \times 10^{-10} \left(\frac{T_g + T_w}{2} \right)^{7/4}$

For the cases of interest

Orientation	Coefficient	Condition
Vertical	0.130	$Gr \times Pr > 10^8$
Horizontal	0.210	$T_g > T_w$
Horizontal	0.012	$T_g < T_w$

The coefficients for horizontal surfaces apply to a slab over the zone. For the inverse of this situation the coefficients should be reversed.

Source Terms: Plumes

A fire generates a plume which transports mass and energy from the fire into the upper layer. In addition, the plume entrains mass from the lower layer and transports it into the upper layer. The former generally increases the upper zone internal energy, whereas the latter will have a cooling effect. For a fire which is consuming mass at a rate $\dot{m}_f$, heat addition will be

$$\dot{Q} \cong \chi_c \dot{m}_f H_c$$

Some fraction, χ_R , will exit the fire as radiation, and the remainder will be left to drive the plume. We can empirically divide the heat transfer into actual combustion and simple gasification. The former, denoted by χ_c , is the relative fraction of pyrolysate which participates in the combustion. Also, once combustion occurs, a fraction of the energy leaves the fire as radiation ($\chi_R \dot{Q}$) and convective energy ($[1 - \chi_R] \dot{Q}$). The former is a function of such external effects as radiation, for example, other fires and vitiation. The latter efficiencies relate to sootiness and Froude number. The mass flow in a plume comes from a correlation of experimental data given by McCaffrey [6]. This correlation divides the flame/plume into three regions:

flaming

$$\dot{m}_p = 0.011 \dot{Q}(Z/\dot{Q}^{2/5})^{0.566} \quad Z/\dot{Q}^{2/5} < 0.08$$

intermittent

$$\dot{m}_p = 0.026 \dot{Q}(Z/\dot{Q}^{2/5})^{0.909} \quad 0.08 \leq Z/\dot{Q}^{2/5} < 0.20 \quad (33)$$

plume

$$\dot{m}_p = 0.124 \dot{Q}(Z/\dot{Q}^{2/5})^{1.895} \quad 0.20 \leq Z/\dot{Q}^{2/5}$$

Entrainment in the intermittent region agrees with the work of Cetegen et al [7] but yields greater entrainment in the other two regimes. This difference is particularly important for the initial fire as the upper layer is far removed from the fire. In this formulation, the total mass flow in the plume is given by the just-cited correlation, and the fuel pyrolysis is related to it by

$$\dot{m}_p + \dot{m}_v = f(m_f \epsilon, \dot{Q})$$

as just cited.

Source Terms: Door Jets

Flow at vents is governed by the pressure difference across a vent. In the control volume approximation, the general momentum equation for the zones is not solved. Instead, the momentum transfer at the zone boundaries is included by using Bernoulli's solution for forced flow. This solution is augmented for restricted openings by using "flow coefficients." The modification deals with the problem of constriction of velocity streamlines at an orifice.

There are two cases which apply to these models. The first, and most usually thought of in fire problems, is for air or smoke which is driven from a compartment by buoyancy. The second type of flow is due to a piston effect which is particularly important in the early stages of a fire. Rather than depending on density difference between two gases, the flow is forced, for example, by volumetric expansion when combustion occurs.

The results used for this model are those of Tanaka [8] and will not be duplicated here. The notation used is:

S = Smoke,

A = Air,

ij = Flow from compartment i to j , and

P = Floor pressure (reference).

The order of the letters indicates the type of atmosphere from which the fluid is coming or to which it is going.

Two important changes have been made: first, the flow is generalized in terms of the actual density. There is no assumption of an ambient gas layer; second is the change required in calculating the neutral plane. We now have

$$N = [P_j - P_i + (\rho_{li} - \rho_{ui})gZ_i]/(\rho_{lj} - \rho_{ui}) \quad (34)$$

Two mixing phenomena which occur at vents are similar to entrainment by plumes. For the case when hot gas leaves a compartment and is driven by buoyancy into the upper layer of a second compartment, a door jet exists which is analogous to a normal plume. Mixing of this type occurs for flow

$$SA_{ij} > 0$$

and is discussed in detail by Cetegen et al [7] and Tanaka [9]. The other is much like an inverse plume and causes contamination of the lower layer as cold gas from a compartment flows through a hot layer in a second compartment and is driven by buoyancy (negative) into the lower layer. Quintiere et al [10] discuss this phenomena for the case of crib fires in a single room and deduce the relation

$$SA_{ul}(2)/AS_{12} = 0.5(T_{ij}/T_{uj})\left(\frac{N - Z_i}{N}\right)$$

This term is predicated on the Kelvin-Helmholz shear flow instability and requires shear flow between two separate fluids. The instability is enhanced if the fluids are of different density since the e -folding distance is proportional to $\delta\rho$. A schematic of this type of flow is shown in Fig. 3. As can be seen, mixing into the lower layer of a room occurs under the same conditions for which the "door-jet" mixing to the upper layer occurs.

Source Terms: Fire

Currently we can deal with two types of fires. The first is a specified fire. Then the mass pyrolysis rate is specified and the heat release rate becomes

$$\dot{Q}_f = h_c \dot{m}_f - c_p(T_u - T_f)\dot{m}_f - Q_p \dot{m}_f$$

whereas the mass loss rate, $\dot{m}_v$, is related to the pyrolysis rate by

$$\dot{m}_v - \dot{m}_f = (1 - \chi_f)\dot{m}_v$$

As the burning efficiency becomes 100%, all of the volatiles are burned, and nothing remains for sooting. The heat release goes into radiation and enthalpy flux

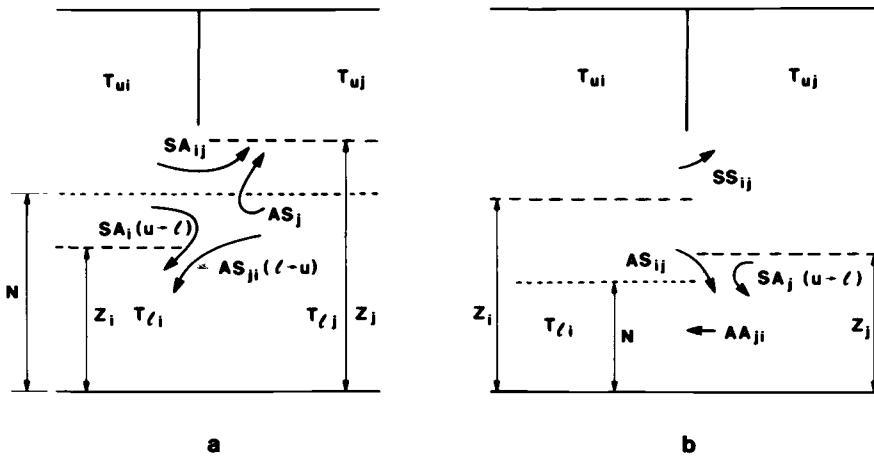


FIG. 3—Possible upper/lower mixing mechanisms which occur at vent jets.

$$\dot{Q}_R(\text{fire}) = \chi_R \dot{Q}_f$$

$$\dot{Q}_c(\text{fire}) = (1 - \chi_R) \dot{Q}_f$$

The term $\dot{Q}_c(\text{fire})$ then becomes the driving term in the plume flow equation (see Eq 33).

This approach is extended for a pool fire. A pool fire is basically the same except that it is driven self-consistently by reradiation from the compartment and the flame itself. From Rockett [11] we have

$$\dot{Q}_f = \dot{Q}_R(\text{ext}) + \dot{Q}_{\text{flame}} - \dot{Q}_{RR} + \dot{Q}_{\text{conv}} - \dot{Q}_{\text{cond}}$$

where

- $\dot{Q}_R$ = external radiation to the fuel source,
- $\dot{Q}_{\text{flame}}$ = radiation from the flame back to the fuel,
- $\dot{Q}_{RR}$ = fuel surface reradiation,
- $\dot{Q}_{\text{conv}}$ = enthalpy flux away from the fire, and
- $\dot{Q}_{\text{cond}}$ = conductive heat loss from the fuel to the surroundings.

Source Terms: Conduction

Conduction of heat through solids is not a source term in the sense mentioned earlier. That is, loss or gain of energy from solids occurs by convective heating, which in turn is influenced by subsequent gain or loss through the solids. However, as much of the net heat loss from a compartment occurs

through loss to the walls, as well as through heating of interior objects, the form of heat propagation in solids will be discussed here.

The equation which governs the best transfer in solids is

$$\frac{\partial T}{\partial t} = \frac{k}{\rho c} \nabla^2 T \quad (35)$$

and is a linear parabolic equation. As such, it must be solved by a different technique than is used for the ordinary differential equations which describe mass and enthalpy flux. In order to couple these systems in a reasonable way, we appeal to the principle of time splitting. Simply stated, we have two systems of equations which are decoupled as long as the time step used is short compared to the characteristic time scale for either set of equations. Wall temperatures change and the e -folding time for energy flux through a solid is characteristically on the order of minutes. By using a time step of no more than 0.1 s, the applicability of time splitting is assured.

Currently the model assumes two walls: the upper wall (and ceiling), which is in contact with the upper layer, and the lower wall (and floor), which is in contact with the lower layer. A refinement will be to separate the ceiling and upper wall, and the lower wall and floor. This will be useful since walls are generally constructed of materials whose thermal properties are different from the ceiling and floor. A further assumption is that conduction is one dimensional only. That is, the heat equation is

$$\frac{\partial T}{\partial t} = \frac{k}{\rho c} \frac{\partial^2 T}{\partial x^2} \quad (36)$$

and the solid behaves as an infinite slab in the other two space dimensions. A corollary to this is that the wall in contact with the gas layer changes temperature instantaneously as the layer interface moves up and down. Such a formulation is not entirely satisfactory as there is a finite equilibration time for the solid. An additional refinement will be to extend this equation to two dimensions to track the layer as it moves up and down. So far, the only zone model to attempt to include this effect is discussed by Jones [1] and Mitler and Emmons [12]. Even in this case the attempt was made only to include heat loss from the upper layer as it moves down and comes in contact with cool lower walls. However, the phenomenon is important, as is discussed by Quintiere et al [13], especially as the thermocline in the wall will influence the direction in which the wall boundary flow propagates.

Conduction through solids occurs in two places: the compartment walls and interior objects. The technique used is the same in both cases, although the boundary conditions on the equation may be different. Generally a slab is cut into N intermediate slices ($N + 1$ nodes). Then Eq 36 is solved for each

slice. The actual finite difference used is a time-centered, implicit scheme which is symmetric about the nodes. For the interior nodes we have

$$T'_i(1 + \eta) = \frac{\eta}{2} (T'_{i+1} + T'_{i-1}) + \left[T_i + \frac{\eta}{2} (T_{i+1} - 2T_i + T_{i-1}) \right] \quad (37a)$$

where

$$\eta = \frac{\Delta t}{\Delta x} \frac{k}{\rho c}$$

and for the edge nodes

$$T'_i \left(1 + \frac{\eta}{2} \right) = \frac{\eta}{2} \left(T'_{i+1} \pm \frac{\dot{Q}_c}{k} \right) + \left[T_i + \frac{\eta}{2} \left(T_{i+1} - T_i \pm \frac{\dot{Q}_c}{k} \right) \right] \quad (37b)$$

The temperature at the starting time at node "i" is T_i and at time $t + \delta t$ it is T'_i . The number of nodes is chosen to reduce the residual error to some reasonable value, say less than 1%. Use of $N > 20$ will improve precision with no concomitant increase in accuracy. This technique would allow one to use different constituents for each slab, although such is not done in the current implementation. Each time step requires both an initial condition and one boundary condition. We start with the internal temperatures in each case and the flux on the "hot" side. The usual scheme is to set the far side boundary condition to zero (which allows heat buildup in the interior) or to approximate the far side exterior as a constant temperature bath. Either technique is satisfactory unless this far side happens to be the interior boundary for another compartment.

Numerical Integration

The problem of the spread of fire, smoke, etc. has been formulated as a set of differential equations. These equations are derived from the conservation of mass and energy. As a result, most of the equations are nonlinear and first order ODEs. The exception to this rule is the heat conduction equation, which is a linear parabolic equation, in one or two dimensions. The former can be solved by using implicit predictor-corrector methods (Conte [14]) and the latter by successive overrelaxation (SOR) (Mitchell and Griffiths [15]).

In the numerical implementation, we have relied on the validity of a technique called time splitting. Simply stated, we have decoupled equations which have greatly differing relaxation times, that is

$$\frac{1}{n} \frac{dn}{dt} = L_n$$

where L_n varies by more than an order of magnitude for each process. Except for the driving program which invokes the hydrodynamics, species transport, and thermal conductivity, the various modules which incorporate the physical processes are exercised separately and interact as source terms. This splitting technique is standard, but the inherent assumptions should be checked when implementing a new numerical model. In addition, a check should be made at each time step to ensure that the relevant stability criterion (similar to a Courant condition for fluid flow) is not violated.

For both types of equations the solution at each time step is found by an implicit scheme. The implicit method allows us to implement the numerical solution as a time-centered algorithm. This ensures reversibility in the physical phenomenon, at least for nondiffusive systems. A test of this assertion is to exclude thermal conduction and integrate from an initial condition to some final time; subsequently, by changing the sign of the time step, we should be able to return to the starting position. A time reversal calculation is an important step in assuring ourselves that the integration scheme itself is not dissipative and thus will not relax to an incorrect final state. This is a real property in nondissipative physical systems and should be mirrored as closely as possible in a numerical model. When conductivity is included, such reversibility ceases to be strictly valid, of course.

An additional virtue of the time-centered scheme is avoiding the bifurcation which can occur in pure leapfrog schemes. The disadvantage is the one additional source calculation required at each time step. This time appears to be short, however, in comparison with the "corrector" phase of the implicit scheme.

The order of the integrations is as follows:

1. Estimate the values for pressure, etc. at $t_0 + \delta t$.
2. Find the source terms for Eqs 22-25 based on the time-centered values ($t_0 + 1/2 \delta t$).
3. Integrate Eqs 22-25 using the source terms defined at the time-centered positions.
4. Repeat Steps 2 and 3 until convergence is reached.
5. Integrate the conduction equation using the SOR technique.

The following illustration shows where each of these time-step points is in relation to Steps 1-5:

1. $t_0 \rightarrow t_0 + \delta t$ estimate values at $t_0 + \delta t$
2. $t_0 \rightarrow x \leftrightarrow t_0 + \delta t$ find sources at $t_0 + \delta t$
3. $t_0 \rightarrow t_0 + \delta t/2 \rightarrow t_0 + \delta t$ integrate from t_0 to $t_0 + \delta t/2$
4. $t_0 \rightarrow t_0 + \delta t$ repeat Steps 2 and 3
5. $t_0 \rightarrow t_0 + \delta t$ integrate conduction equation

Since each step is of at least second order accuracy, the overall scheme also will be second order accurate $[O(\delta t^3)]$. The relative error allowed at each time

step is $\sim 10^{-3}$. Thus the precision is greater than the precision of the computer being used (at least as long as these calculations are being done in single precision).

As for the integration scheme itself, it is derived from an Adam-Bashford backwards difference scheme (Conte [14]) of order $k = 1$. This yields a single step predictor and second order corrector, $O(\delta t^3)$. These equations become "stiff" if the individual source terms are large, which leads to a short time step, yet the total source function may be tightly coupled if the solution is being approached asymptotically. Another possibility is that the source terms for the various equations differ by more than an order of magnitude. In either case, the usual time step criterion would require a time step which is prohibitively short. It is possible to modify the Taylor expansion used in obtaining the predictor-corrector scheme to use the asymptotic nature of the equations to enhance the speed of the solver (Young and Boris [16]).

The general form of an equation is

$$\frac{dn}{dt} = q - ln \equiv f$$

Using the notation that $n(0)$, $q(0)$, $l(0)$, $f(0)$ are the initial values at time t_0 and $n(1)$, $q(1)$, $l(1)$, and $f(1)$ are the values at the new time $(t_0 + \delta t)$, we obtain first for the normal equations

$$\text{predictor } n(1) = n(0) + \frac{\delta t}{2} f(0) \quad (38a)$$

$$\text{corrector } n(1) = n(0) + \frac{\delta t}{2} [f(0) + f(1)] \quad (38b)$$

and for the stiff equations

$$\text{predictor } n(1) = n(0) + \frac{\delta t}{1 + \delta t l(0)} f(0) \quad (39a)$$

$$\text{corrector } n(1) = n(0) + \frac{2\delta t}{1 + \delta t(l(0) + l(1))} [f(0) + q(1) - l(1)n(0)] \quad (39b)$$

The corrector must be iterated until some specified error criterion is reached. If the specified error cannot be reached in a small number of iterations, say two or three, then the time step must be reduced. It turns out to be advantageous to half the time step for each instance that a reduction is re-

quired, and increase it by only 10% for each subinterval that the error criterion is satisfied.

Comparison with Experimental Data

The series of tests which serve as a data base for this analysis are based on a two-room fire scenario by Cooper et al [17] and an ongoing series of full-scale validation tests at the National Bureau of Standards. The former was a two-room configuration, consisting of a burn (or fire) room and a corridor. It is referred to as the "Nike Site" in later discussions. The latter is a three-room configuration with the additional room being a target room for testing high density occupancy, referred to as "Building 205." The geometry of each of these configurations is shown in Table 1.

Stability and Completeness

There are a number of phenomena which are either not included or need additional work. They are:

1. Wall effect—two dimensional, unsteady heat flow.
2. Separation of flow—vents.
3. Ceiling jet—transit time.

Comparisons between the model and experimental data for Table 1 are for fires of 100 kW. Figures 4 and 5 show the comparison for the Nike Site tests for the upper layer temperatures in the burn room and corridor, and the in-

TABLE 1—*The geometries of Nike Site and Building 205.*

	Burn	Vent	Corridor	Vent ^b	Target
NIKE SITE					
depth ^a	4.3	...	11.1 to 20.2	...	...
width	3.3	1.07	2.4	0.95	...
height	2.3	2.00	2.3	0.15	...
area	14.2	2.14	26.6 to 48.4	0.14	...
volume	32.2	...	61.2 to 112.3	...	...
BUILDING 205					
depth	2.3	...	12.2	...	2.3
width	2.3	1.0	2.4	1.0/1.0	2.3
height	2.2	1.9	2.4	1.9/2.0	2.2
area	5.5	1.9	29.7	1.9/2.0	5.5
volume	11.8	...	72.6	...	11.8

^aLengths are in meters, areas in square meters, and volumes in cubic meters.

^bV1/V2 in Building 205: V1 is a vent from corridor to target room, and V2 is a vent from corridor to the ambient.

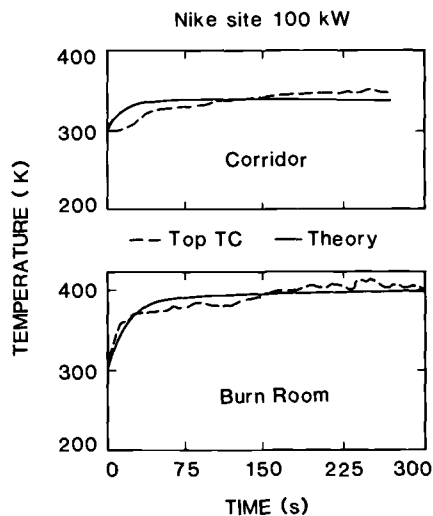


FIG. 4—Comparison of experimental and calculated temperature profiles for the upper layer in the burn room and corridor in the Nike Site experiments (Table 1).

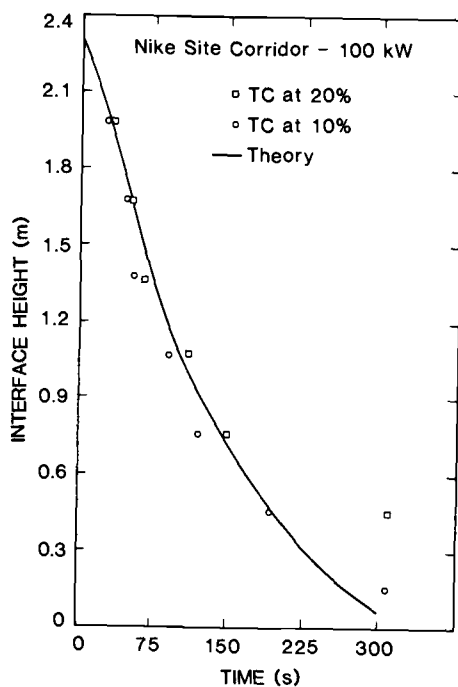


FIG. 5—Comparison of experimental and calculated interface height in the corridor of the "Nike Site" experiments (Table 1). See Quintiere et al [10] and Cooper et al [17] for an explanation of the criteria (10%, 20%) used to determine the experimental interface height.

terface height in the corridor. Figures 6 and 7 show a similar comparison to the current experiments in Building 205, a full-scale facility at NBS. It is clear from both that the plume entrainment is estimated very well, but that the door jet entrainment is underestimated. This has the effect of predicting too high a temperature and an upper layer depth which is too small in the corridor, whereas very good agreement is obtained in any compartment containing a primary plume. This underestimation occurs in the regions which McCaffrey [6] calls the flaming and far field regions. In the intermittent region, where the results of McCaffrey and Cetegen et al [7] agree, the entrainment rate appears to be correct.

Another factor which gives rise to disparity between theory and experiment

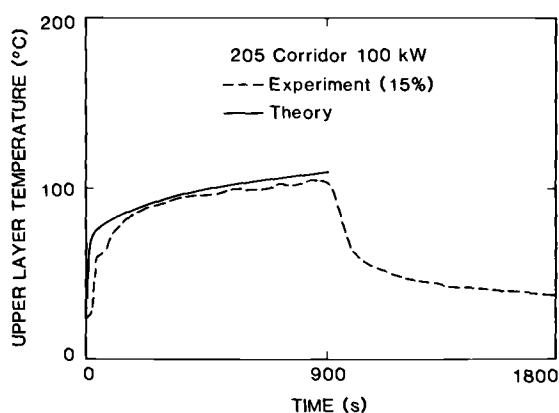


FIG. 6—Comparison of experimental and calculated upper layer temperature in the corridor of "B205" experiments (Table 1).

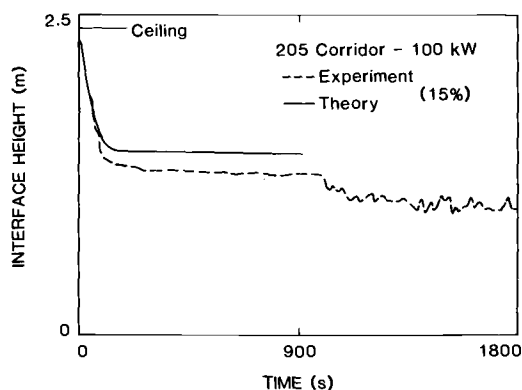


FIG. 7—Comparison of experimental and calculated interface height in the "B205" experiment (Table 1).

is the assumption, in the model, of known and uniform wall materials. In the experiments, walls consist of several materials in a composite such as calcium-silicate board over gypsum. Allowing for these factors, the agreement seems quite good. As research continues, these discrepancies will be resolved.

The first refers to the finite thermal inertia of the wall as the hot layer moves down (or possibly up). There will be a two-dimensional thermocline in the wall which differs from that in the compartment. Although this has only a small direct effect, it can lead to flow along the walls which can subsequently contribute to contamination of the lower layer. Such effects become particularly important as the smoke travels further from the fire source and temperature differentials become small.

The second problem is quite important for asymptotic predictions, especially near the room of fire origin. Currently we assume that hot gas mixes with hot gas or cold with cold gas as it traverses a vent. This was a reasonable approximation while the lower layer was assumed to be ambient. Now that we calculate the lower layer temperature, we find that the recirculating gas may have a temperature on the order of the lower layer of the room into which it enters. A particular example will demonstrate the effect. In a two-compartment calculation, Jones and Quintiere [18] found that the layer outside of the burn room is lower than that in the compartment of fire origin. As there is no source of heat in this second compartment and cooling occurs due to mixing, the upper layer in this adjacent space is cool relative to the upper layer in the fire room and even comparable in temperature with the lower layer. Currently we assume that all of the hot gas in the adjacent room, which flows into the fire room, will be deposited in the fire room upper layer. As can be seen in Fig. 3,² this assumption leads to discrepancies in prediction versus experiment. A better approach is to divide the incoming flow into two components: for flow into compartment i from compartment j , we have component into the upper layer =

$$\left(\frac{T_{uj} - T_{lj}}{T_{ui} - T_{li}} \right) \dot{m}_{j \rightarrow i}$$

with the remainder going into the lower layer. This occurs only after an interface discontinuity has been established.

The third problem will only be important for very long corridors (20 m) or very tall compartments or shafts. Also, it is only a transient phenomenon. For purposes of siting smoke detectors, for example, this transient can be crucial.

Conclusions

The fire and smoke transport model, as described in this paper, is quite detailed and complete as far as our current understanding of fire phenome-

²D. Evans, private communication, 1983.

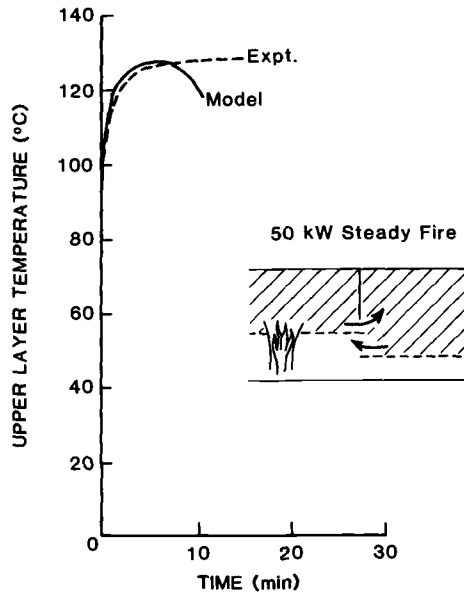


FIG. 8—An example of the upper layer separation which occurs in actual flows.

nology is concerned. It draws on a great deal of the research into fires which has occurred over the past ten years, pulling together much of the best work which has been done in the field. The numerical implementation is of particular interest because it is extremely durable. The problems discussed in the section Stability and Completeness need to be addressed if we are to carry this work further, such as to fires in high-rise hotels or on aircraft carriers.

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Scaling Correlations of Flashover Experiments

REFERENCE: Kanury, A. M., "Scaling Correlations of Flashover Experiments," *Fire Safety: Science and Engineering, ASTM STP 882*, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 97-121.

ABSTRACT: Preliminary scaling rules are synthesized to relate full-scale compartment fires with small-scale test fires; these rules then are applied to certain full- and quarter-scale model test data of the National Bureau of Standards, focusing on the issue of flashover as influenced by lintel height, initial burning item, and wall linings. The correlation attempt is only partially successful. Data of the tests in which flashover was deemed to have occurred within a finite time correlate in a reasonable fashion. However, test data in which flashover time is noted to be infinity do not obey the scaling hypothesis. The issue of whether or not flashover would occur under a set of prescribed conditions thus seems to be unsettled by this work. The similitude analysis presented here, however, appears to yield the rudiments of a framework within which a synthesis can be made of the experimental observations on the complex phenomenon of room-fire flashover.

KEY WORDS: flashover, scaling, room fires

Nomenclature

- A Area, also a constant
- B Steward's plume parameter
- b A time constant
- c Orifice coefficient
- C_p Specific heat
- d Density ratio
- f Flame-covered area to burner heat output ratio
- g Gravitational constant
- H Height
- h Heat transfer coefficient
- h_c Heat of combustion

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k	A mass flow rate constant
k_e	Plume entrainment constant
K	Thermal conductivity
L	Length
l	Length, depth of smoke layer, wall lining thickness
$\dot{m}$	Mass flow rate
$\dot{Q}$	Heat input rate, heat loss rate
q	Heat release rate
r	Stoichiometric air/fuel mass ratio
T	Temperature
t	Time
u	Velocity
V	Volume
W	Width
α	Thermal diffusivity
β	Wall combustibility parameter
γ	Soffit depth parameter
δ°	Conduction constant
ϵ	Emissivity, combustion efficiency
κ	Extinction coefficient
ρ	Density
Φ	Air flow rate parameter
ϕ	Fuel flow rate parameter
ψ	Opening aspect ratio
Ω	Wall/ceiling heat transfer parameter
ω	Steward's thermochemical parameter

Subscripts

a	Air
B	Burner
c	Ceiling
conv	Convective
d	Discontinuity
f	Floor
F	Fire-covered
g	Product gas
n	Neutral
o	Opening, initial
rad	Radiative
s	Lining solid
T	Total
v	Fuel supply
w	Wall lining
∞	Flame

Superscripts

- * Flashover

Background and Objective

Klein, Budnick, and O'Laughlin at the Center for Fire Research, National Bureau of Standards (NBS-CFR) have published a series of research reports [1-7] on the topic of mobile home fire studies with the objective of experimentally identifying changes in materials and designs which would improve the level of fire safety in mobile homes. Most of the tests reported were in a single-wide, full-scale unit, although some tests were also conducted in a quarter-scale unit. The initial fire source employed was a specified wood crib, upholstered chair, or gas burner. Different wall and ceiling linings were tested. The room geometry, location of the initial fire, and window (or door) geometry were among the other variables considered.

Based on the measurement of various temperatures, heat flux, gas concentrations, and smoke, a number of important conclusions were drawn from this test program. Most notably, a correspondence is shown between the various tests: when the hot gas temperature near the ceiling approaches and exceeds a critical value of about 500°C, the phenomenon known as flashover is imminent. This perhaps is due to development of sufficient thermal radiation from the hot gases and surfaces to result in the ignition of all combustible materials in the room. For a room of given geometry with specified wall linings and a stated window or door opening, a critical initial heat source strength is required to attain the critical ceiling temperature symptomatic of flashover. No attempts were made to develop the scaling relations in the subject series of experimental reports. Especially in serious need is a scaling explanation of the role played by the lintel height on the occurrence of and time to flashover. The data appear to hold a good deal more information that is applicable in a general way to a host of compartment situations which differ only slightly from the tested situations.

Extraction of this information would require nondimensional correlations of the temporal histories of the ceiling gas layer temperature, doorway out-flow temperature, velocity and stable chemical species, and heat fluxes at different locations of the room—all as dependent upon such prescribed variables as the compartment shape and dimensions; the doorway opening; the lintel depth; the nature and thickness of combustible linings; the kind, location, and strength of the initiating fire source; etc. These correlations would then indicate the circumstances surrounding the incipient flashover.

The overall objective of the research reported here is: to develop an understanding of the relationship and correspondence between the observation and measurements made on fire growth in compartments of different scales; to synthesize the scaling rules to deduce the full-scale compartment fire behavior from the results of small-scale tests; to apply these rules to full and quar-

ter-scale test data of NBS focusing on the issue of flashover as influenced by lintel (space between the top of the door and ceiling) height and wall linings.

Status of Scale Modeling of Room Fires

Theoretical studies of compartment fires may be placed for convenience into four different groups depending upon the objective use of the model and the fundamental detail sought. These groups are: the stochastic models, field equations models, zone models, and scale models [8].

Specifically, let us focus attention on the scale modeling of enclosure fire flashover testing. Although the phenomenon of flashover, in its various perceptions and definitions, was known for a long time before him, Waterman [9] appears to be among the first investigators to undertake a systematic study of it. Thomas [10] defined it in the light of ventilation and spread. Martin et al [11] developed a descriptive definition based on possible fire-state transitions. Hagglund, Jansson, and Onnermark [12,13] proposed a correlation of the burning rate versus ventilation to map out the range of conditions which would lead to flashover. Quintiere [14,15] and Martin and Wiersma [16] attempted to deduce this map theoretically, but only with partial success. The most recent advances in this direction have been made by Quintiere, McCaffrey, Den Braven, Bullen, and Thomas [17,18]. These advances lead one to believe that the flashover problem is amenable for scaling so that tests of different scales may be correlated together, and thus their results may be generalized enough to be read into when one seeks to draw conclusions as to the outcome of an untested situation.

In summary, it appears that a critical burning rate within the room exists to indicate whether or not flashover will occur and, if it does, the time to flashover. This critical burning rate is rather strongly dependent upon the vent opening size. The vent opening size thus appears to do one of two things: it regulates the outflow of hot combustion gases and hence the associated convective heat loss; and it regulates the thickness of hot gas layer near the ceiling of the compartment and hence the associated internal augmentation of heating due to radiation from the gas layer. Which of these two mechanisms were operative is not discerned by the experiments of Ref 12. It is possible that both mechanisms are relevant, although limiting cases might exist.

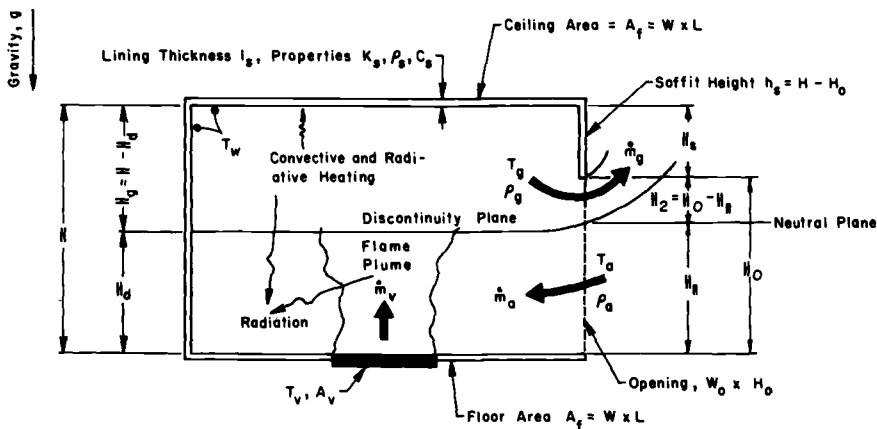
If the occurrence and arrival of flashover is independent of the vent configuration for the same ventilation factor of $A\sqrt{H}$, then the convective loss argument probably is valid. However, if the lintel height plays a dominant role in determining if and when the flashover would occur, then the radiative augmentation argument probably is valid. The experiments described by Friedman [19] further substantiate the significance of radiation and its augmentation within the compartment culminating in flashover. The balance between such various energy components in the compartment as the continuous generation in the flame reactions, conductive loss through the walls, convective

outflow through the vent, internal radiative exchange among the different parts of the room, etc. appears to hold the key for the event of flashover. In this balance, regimes of multiple solutions of fire behavior are expected; some of these regimes involve rapid growth of the room fire.

Room Fire Growth Process

Figure 1 shows a schematic of a growing fire in a room. As the initial fire burns under conditions of essentially unrestrained air supply, its hot gases and flames convect and radiate energy to the enclosure walls in a manner affected by the fire location/size and the room/ventilation details. Conductive heating of the linings will lead to pyrolysis, vaporization, or charring to produce combustible vapors which then convectively mix with air and ignite to enhance the flame volume. The conductively heated walls may emit radiation to make the room an energy augmentor. Accumulation of smoke near the ceiling, however, may soon attenuate the surface radiant heat exchange and introduce soot layer radiation into the subsequent heating process. All during this transient process, either the initial fire is continuously and acceleratively growing towards the state of a physicochemical catastrophe known as flashover or, if the initial fire is externally controlled to maintain a constant size/intensity such as could be done with a gas burner, the room wall linings are gradually heated to arrive at a critical thermochemical state at which the various surfaces tend to be simultaneously ignited.

The variables of influence identifiable by this description exhibit nonlinear



Width of the enclosure and opening normal to the plane of the paper = W and W_0 respectively

FIG. 1—Schematic of a fire enclosure.

interactions among the various time-dependent fire processes. Figure 2 shows an attempt to enumerate these interactions. The room geometry, for instance, affects the thermal radiative exchange of energy in the room as well as alters the convective flows of air and hot gases and the associated mixing and heating phenomena. As a result, the ignition or fire spread or both will be influenced by the room geometry.

Ventilation will obviously affect the air supply rate to the fire, convection and mixing, accumulation of smoke in the ceiling layer, and hence the overall thermal radiation process itself. Herein arises the important connection between the window geometry (which determines the flow of gases) and the lintel height (which determines the thickness of the hot ceiling gas layer). The energy balance responsible for continued fire-growth process (or the ignition process of various parts of the room) is thus modified by the ventilation characteristics.

The nature of the initial fire and its proximity to the lined walls shall result in convective and radiative heating of the adjacent surfaces, thus bringing them to the thermochemical state of ignition. In this entire picture of heat exchange, the wall linings play not only the active role of confining the gas flows and absorbing/reflecting/emitting thermal radiation but also a passive role of being heated by transient conduction progressively towards a thermochemical state of receiving the growing fire or of spontaneous ignition.

This qualitative discussion leads one to believe that the flashover problem entails two specific questions: (a) Is flashover possible under a given set of conditions of the room geometry, ventilation, initial fire, and wall lining? (b) If it is possible, how does the time to flashover depend upon these prescribed conditions? The answers to these questions require that the experimental fire growth data be represented on the basis of a dimensional analysis of the problem of room fire growth and the associated transient heating of the room. It is

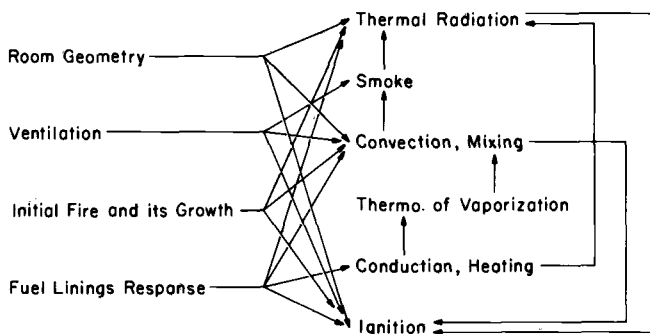


FIG. 2—Families of properties and concerned processes—some nonlinear interactions in the transience of room fire growth.

apparent that the variables expected to influence the growth of a room fire to flashover pertain to the following five groups:

1. Room geometry—volume, depth and height, lintel height, etc.
2. Ventilation—door/window location and dimensions.
3. Fuels—furnishings (not considered in this work), wall linings.
4. Initial fire—size, location, burning rate, etc.
5. Heating of walls and ceiling by conduction, convection, and radiation.

If the variables involved are thus identified, five independent dimensionless groups are expected to evolve pertaining to the five families of variables just listed. (For the convenience of this discussion, these parameters will be denoted by A , B , C , D , and E .) Typically then, one can represent the time-wise development of the enclosure fire as sketched in Fig. 3 in which one of the parameters (say, the initial fire parameter) is systematically altered while the other four parameters are kept fixed. A critical value of the altered parameter is expected to exist representing the occurrence of flashover. Beyond the critical value of the parameter, the time to flashover will depend on the magnitude of the parameter.

Once one deduces the nondimensional parameters and identifies the correct way of scaling the time and fire intensity coordinates, one can attempt to present the detailed experimental data in the format of Fig. 3 to note whether or not flashover would occur and, if it does, the time to flashover.

Scaling Rules

In this section, the scaling parameters are deduced from the laws of conservation of mass, energy, and momentum. Whereas mass and energy equations

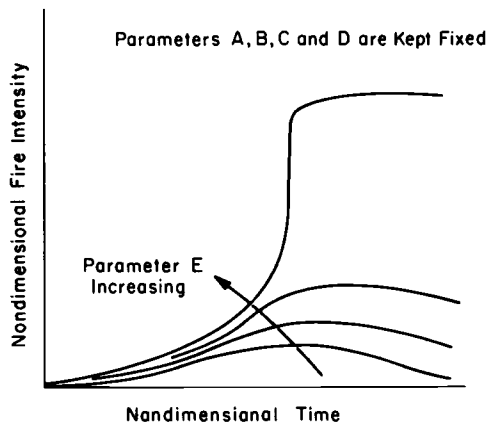


FIG. 3—Methodology of flashover.

dominate in this deduction, the momentum equation comes into play only indirectly in determining the various mass flows involved. The species equations are not considered here; concern for smoke, in estimation of thermal radiation, can be fulfilled by taking smoke as a passive variable and the smoke generation rate as a property of the fuel ensemble.

Note again that the following analysis is not meant for prediction of fire growth behavior but is to enable identification of the nondimensional parameters for use in correlating the experimental data.

Mass Conservation

With the aid of Fig. 1 and its notation, the mass conservation requires under quasi-steady state that the mass of air ($\dot{m}_a$) and fuel ($\dot{m}_v$) entering into the control volume around the room be equal to the mass of combustion products leaving the room ($\dot{m}_g$).

$$\dot{m}_g = \dot{m}_a + \dot{m}_v \quad (1)$$

While $\dot{m}_v$ will be determined by the initial fire burning characteristics, $\dot{m}_a$ and $\dot{m}_g$ mutually adjust to share the window opening for flow according to the well-known equations [20,21]

$$\dot{m}_a = (2/3) c W_o \rho_a [2g(1 - d)]^{1/2} (H_n - H_d)^{1/2} (H_n + H_d/2) \quad (2)$$

$$\dot{m}_g = (2/3) c W_o \rho_a [2gd(1 - d)]^{1/2} (H_o - H_n)^{3/2} \quad (3)$$

where

- H_o and W_o = the window height and width, respectively,
- ρ_a = the ambient air density,
- d = the ratio of product gas density to ρ_a ,
- g = gravitational acceleration,
- c = window orifice coefficient, and
- H_n and H_d = the height locations of the neutral and discontinuity planes, respectively.

The air induced into the room by the buoyant effect of the fire, according to Eq 2, is mixed with the fuel by the following relation first derived by Steward [22]

$$\dot{m}_a = \dot{m}_v \omega [1 + B H_d / H_o]^{1/2} \quad (4)$$

where the nondimensional properties ω and B respectively pertain to the thermochemistry of the flame and fluid mechanics of the flame plume. These are defined as follows:

$$\omega = (1 + h_c/rC_{pg}T_a)^{-1} \quad (5)$$

$$B = (4/5) k_e^{4/5} (1 - \omega) H_o [(5g\pi^2\rho_a^2)/(12\omega^3\dot{m}_v^2)]^{1/5} \quad (6)$$

where

h_c = enthalpy of combustion of the fuel,
 r = air/fuel mass ratio,
 C_{pg} = the specific heat capacity of the gases,
 T_a = ambient air temperature, and
 k_e = the fire plume entrainment constant.

Quintiere [15] solves Eqs 1-4 for the four unknowns $\dot{m}_a$, $\dot{m}_g$, H_n , and H_d for any given set of the properties: c , W_o , H_o , ρ_a , g , d , ω , k_e , and $\dot{m}_v$. A systematic elimination of $\dot{m}_g$, H_n , and H_d to obtain $\dot{m}_a$ from these equations leads to identification of certain important scaling parameters. Assume, following Emmons and Prahl [20], $c \approx 0.65$, $\rho_a \approx 1.18 \text{ kg/m}^3$, $g = 9.81 \text{ m/s}^2$, $d \approx 1/2$, and $\omega \approx 0.08$. Then $k \equiv (2/3)c\rho_a[2g(1-d)]^{1/2} \approx 1.6 \text{ kg/m}^{5/2}\text{s}$, a constant. With the definitions

$$\Phi \equiv \dot{m}_a/kW_oH_o^{3/2}; \quad \phi \equiv \dot{m}_v/kW_oH_o^{3/2}; \quad \text{and} \quad \psi \equiv W_o/H_o \quad (7)$$

the suggested elimination process leads to the following relation for fire in the middle of the room (that is, $k_e \approx 0.1$)

$$\begin{aligned} \Phi = [1 - 1.26(\Phi + \phi)^{2/3} - \{2.79(\Phi\psi)^{2/5} - 1.0155(\phi\psi)^{2/5}\}]^{1/2} \\ \times [1 - 1.26(\Phi + \phi)^{2/3} + \{1.395(\Phi\psi)^{2/5} - 0.5078(\phi\psi)^{2/5}\}] \end{aligned} \quad (8)$$

For fire by a wall ($k_e \approx 0.05$) or in the corner ($k_e \approx 0.025$), Eq 8 will hold but with constants 1.26, 2.79, . . . etc. different. More importantly, Eq 8 suggests that the air flow rate Φ is a function of the fuel flow rate ϕ and the window aspect ratio ψ for any given situation.

$$\Phi = \Phi(\phi, \psi) \quad (9)$$

Scale experiments in which ϕ and ψ are preserved are thus expected to exhibit a preserved Φ .

Energy Conservation

Whereas ϕ and ψ are thus identified as two scaling parameters, no account has been taken yet of the energy transfer processes. Such an account calls for energy conservation considerations. We will do this again by a simple mechanistic model whose solution is not as much of an interest for us as the scaling parameters suggested by it. The physics is deliberately maintained simple in

this exercise so that the parameters can be redefined later to capture any required sophistications.

The energy released due to (total or partial) combustion of the fuel mass supply $\dot{m}_v$ is used in part to raise the enthalpy of gas flow and to heat up the enclosure walls while the remainder is radiated out through the window. (In the preflashover stage of fire growth, all the combustion is presumed to be contained within the enclosure so that flames do not shoot out of the opening.) The first law then says

$$\begin{array}{rcl}
 \text{Rate of internal} & \text{Rate of energy} & \text{Enthalpy inflow rate} \\
 \text{energy change} & = \text{release due to} & + \text{due to air flow and} \\
 & \text{combustion} & \text{fuel input} \\
 \\
 - \text{Heat loss by} & \text{Enthalpy outflow} & \\
 - \text{radiation} & - \text{rate due to the} & - \text{Rate of heat loss} \\
 \text{through windows} & \text{exit flow of} & \text{to the walls} \\
 & \text{combustion gases} &
 \end{array}$$

The transience of the room fire growth problem is a result, mainly, of the time-dependency of the first and last terms of the right-hand side of this equation. The left-hand side term can be safely set equal to zero. The second, third, and fourth terms on the right-hand side are either quasi-steady or in some linear way related to the combustive energy release term.

The fuel mass flow rate $\dot{m}_v$ is generally small so that $\dot{m}_g \approx \dot{m}_a$. The excess outflow enthalpy rate thus is $\approx \dot{m}_g C_{pg}(T_g - T_a)$ where T_g is the hot exiting gas temperature. Thus, assuming a quasi-steady state,

$$\dot{m}_g C_{pg}(T_g - T_a) = \dot{Q}_{\text{generation due to combustion}} - [\dot{Q}_{\text{loss to walls}} + \dot{Q}_{\text{rad loss through window}}] \quad (10)$$

The radiative loss through the window is neglected. The generation term may be written as $\epsilon h_c \dot{m}_v$ where ϵ is the completeness (or efficiency) of combustion and h_c is enthalpy of combustion.

In his development of room fire growth model, Quintiere [15] views the room to be composed of three subsidiary control volumes, one representing the flame, the second around the hot gas layer in the upper part of the room, and the third representing the cooler air near the floor. A detailed account of energy exchange between these control volumes leads to prediction of whether or not flashover is possible. Here, however, summing up the conservation equations for all these control volumes, one obtains Eq 10. If the hot gas layer imparts heat to the ceiling and skirt of the walls lying above the soffit by combined convection and radiation and if the radiation from the ceiling surface and hot gas layer to the floor is deferred for a later consideration, Eq 10 may be simplified to

$$\epsilon \dot{m}_v h_c = \bar{h} \bar{A} (T_g - T_w) + \dot{m}_g C_{pg} (T_g - T_a) \quad (11)$$

where $\bar{h}$ is the combined convective and radiative heat transfer coefficient between the hot gases and the ceiling plus soffit skirt walls whose area is $\bar{A}$ and whose temperature is T_w . Both T_g and T_w are time-dependent; $\dot{m}_g$ is obtained from the mass conservation.

The energy received by the ceiling plus skirt walls, as represented by the first right-hand side term of Eq 11, is conducted into the walls. If these walls behave as thermally thick in the time prior to flashover, the wall energy balance is given by

$$\bar{h}\bar{A}(T_g - T_w) = K_w\bar{A}(T_w - T_o)/\delta^\circ(\alpha_w t)^{1/2} \quad (12)$$

where

T_o = the initial temperature of the walls ($T_o \approx T_a$),

δ° = conduction constant lying between 1 and 2,

K_w and α_w = the wall thermal conductivity and diffusivity, respectively, and

t = time.

Equations 11 and 12 can be now solved for $T_g(t)$ and $T_w(t)$ to obtain

$$\theta_w = \left[\frac{\phi}{\phi + \Phi} \right] \left\{ 1 + \left[1 + \frac{\Omega\gamma}{\phi + \Phi} \right] \frac{1}{\sqrt{\tau}} \right\}^{-1} \quad (13)$$

$$\theta_g = \left[\frac{\phi}{\phi + \Phi} \right] \left[1 + \frac{\Omega\gamma}{\phi + \Phi} \right]^{-1} \left\{ 1 + \frac{\Omega\gamma\theta_w}{\phi} \right\} \quad (14)$$

where

$$\theta_j \equiv \frac{C_{pg}(T_j - T_a)}{h_c}, j = g, w,$$

$$\Omega \equiv \frac{\bar{h}}{kC_{pg}H_o^{1/2}},$$

$$\gamma \equiv \bar{A}/W_oH_o, \text{ and}$$

$$\tau \equiv \bar{h}^2 t / K_w \rho_w C_{pw}.$$

We stress again that it is not the precise solutions (Eqs 13 and 14) that we are seeking but the nondimensional parameters suggested by them. Since Φ is destined to be a function of ϕ and ψ from mass conservation constraints, the temperature θ is thus realized to depend on ϕ , ψ , Ω , γ , and time τ .

Some Properties

The radiant heat transfer coefficient entering into the definition of $\bar{h}$ is defined as the radiant heat flux from the hot gas to the lining divided by the

temperature difference. Obviously, this is a strongly transient coefficient proportional to $\sigma(T_g^2 + T_w^2)(T_g + T_w)$. The gas layer emissivity itself is a changing quantity that may be estimated for ceiling heating to be between 0.5 and 1.0. The gas temperature quickly attains some value in the vicinity of 800 to 900 K. The surface temperature of the lining increases from 300 K to about 650 K, first quickly and then slowly. Thus, h_{rad} will probably vary from an initial value of about 10 W/m² K to a late value of about 100 W/m² K.

The area $\bar{A}$ is defined as the area of the ceiling itself (which is generally equal to the floor area) plus the area of the walls above the lintel level. The lining properties come into the definition of scaled time τ .

For a given burner power ϕ , window geometry ψ , lintel description γ , and ceiling heat transfer characteristic Ω , one expects the ceiling surface temperature θ_w and gas temperature θ_g in like experiments to be correlated as functions of τ . The energy stored within the lining material, to result in a possible fire growth even after the demise of the initial fire, also appears to be scaled by the present scheme.

Consideration of the enclosure, as to be comprised [15] of the upper ceiling layer zone which interacts with the lower floor zone predominantly by thermal radiant exchange, leads to an additional aspect. This aspect is concerned with the loss of energy by the hot gas to the floor by radiation. Certain existing scale model experiments include measurement of the radiant heat flux impinging on the floor as the fire develops. There are criteria suggested for flashover upon the basis of this flux.

The essence of radiant flux reaching from the upper zone to the floor involves two components: first, the surface radiation emitted by the hot ceiling and partially attenuated by the strongly participating ceiling layer gases; second, the radiation emitted by the ceiling layer gases. In both these components, the emissivity of the hot smokey ceiling layer gases is relevant. Assuming unit shape factor between the ceiling, smoke layer, and the floor, the radiant flux arriving at the floor can be estimated by

$$\dot{q}_{\text{rad, floor}}'' = \sigma\{\epsilon_g(T_g^4 - T_f^4) + \epsilon_w(1 - \epsilon_g)(T_w^4 - T_f^4)\} \quad (15)$$

where T_f stands for the floor lining temperature. An imbalance between this incident flux and convection due to cool fresh air flow results in transient heating (or cooling) of the floor lining material. Even when one ignores this zonal heating process, Eq 15 leads to an additional dimensionless parameter pertaining to the smoke layer emissivity. This parameter imposes an indirect influence on the overall heat transfer coefficient $\bar{h}$ and hence on Ω .

The simplest available relation for the emissivity is $\epsilon_g = 1 - \exp(-\kappa_g l_g)$. The extinction coefficient κ_g depends on the smokiness of the layer. This smokiness is possibly variant with time. The effective optical path length of the smoke layer l_g is clearly time-dependent. Its limiting maximum value is expected to depend on the lintel depth. Taking l_g to be approximately equal to

four times the layer volume divided by its surface area and noting the lintel depth to be the difference between the room height H and door height H_o , the maximum smoke layer optical thickness is of the order of

$$l_g \propto \frac{H}{2} \left\{ \frac{1 - H_o/H}{1 + (1 - H_o/H)(H/L + H/W)} \right\}$$

Since all the ratios in parentheses are geometrical, we are left with l_g/H to be a scaled factor so that $(\kappa_g H)$ will be a scale parameter. The radiation to floor can be accounted in the nondimensional factor

$$\bar{q}_r \equiv \dot{q}_{\text{rad, floor}}'' / kH_o^{1/2} h_c$$

where $\dot{q}_{\text{rad, floor}}''$ itself contains $\kappa_g H$ as a nondimensional parameter.

The basic scaling parameters thus appear to be in hand. Radiation terms, however, might need further attention.

Experiments, Parameter Estimation, Correlation, and Discussion

Experiments

Four NBS-CFR reports are considered to test the present scaling hypothesis for flashover. They are: NBSIR 78-1530 (Ref 4); NBSIR 79-1720 (Ref 6); NBSIR 78-1531 (Ref 5); and NBSIR 81-2333 (Ref 7). These reports pertain to the Mobile Home Fire Project at the NBS/CFR. Reports 1530 and 1720 deal with one set of experiments while Reports 1531 and 2333 deal with another set, as discussed in following paragraphs. Additionally, Report 2333 also describes a set of quarter-scale-model experiments. Thus, the four reports under consideration present three distinct sets of experiments.

Reports 1530 and 1720—Tests were conducted in the living room of a single-wide mobile home with two different furnishing loads. The geometry and fire location are shown in Fig. 4. Report 1530 involved sixteen tests, five of which engaged a medium-sized upholstered chair as the initial burning item while the rest (nine) engaged a wood crib. All doors and windows were kept shut during a test, but sufficient infiltration existed to permit the fire development. In Report 1720, a total of eight tests were reported in the same setting as in Report 1530. Two of these eight tests engaged a 35-kg upholstered chair as the initial burning item, while the rest engaged a medium-sized (16-kg) upholstered chair.

Prefinished lauan paneling 4-mm thick, the same paneling with either an intumescent coating or a vinyl cladding for fire retardation, and 8-mm gypsum wallboard (either with paper coating or spackling) constituted the wall lining materials tested. Wood fiberboard 13-mm thick and 8-mm gypsum board (papered or textured) were the ceiling materials employed.

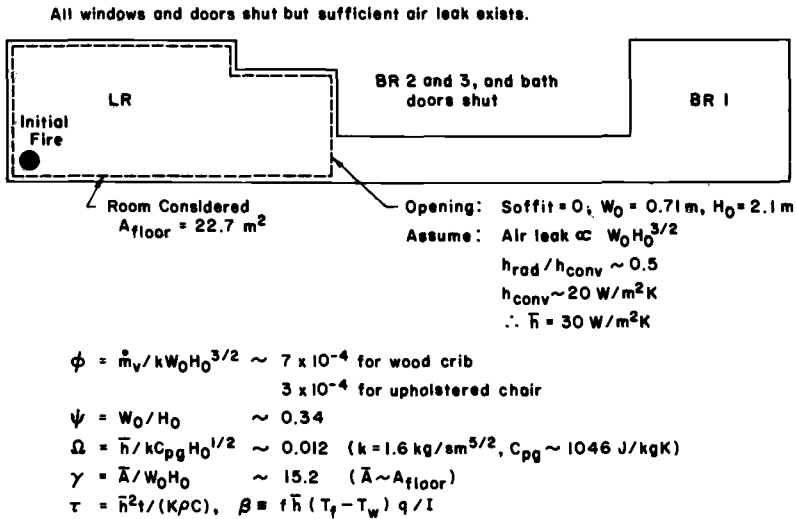


FIG. 4—Test setup for experiments of Reports 1530 and 1720.

Reports 1531 and 2333—Nine tests in Report 1531 were conducted in the master bedroom in the configuration shown in Fig. 5. An upholstered chair served as the initial fire source. Finished lauan paneling 4-mm and 6-mm thick, 6-mm lauan paneling treated with an intumescent coating, and gypsum board with and without a calcium silicate (CaSi) overlayer constituted the wall materials tested. Thirteen-millimetre fiberboard and 8 or 13-mm gypboard (once with CaSi overlayer) composed the ceilings tested. In one of the nine tests, a bed mattress was employed as the seat of the initial fire.

Report 2333 describes 13 fire tests in the same bedroom. Five of these tests, meant to serve as calibrators, involved gypsum board with CaSi overlay in both the walls and ceilings, two with a 16-kg upholstered chair and three with three different wood cribs as initial fire sources. None of these five tests resulted in flashover. In the remaining eight tests, a methane burner adjustable to three different intensities was used as the prompting fire. Gypsum board, either papered or CaSi-overlaid, and 4-mm prefinished lauan paneling were the wall materials tested. Thirteen-millimetre fiberboard and gypsum board with and without CaSi overlay were the ceiling materials.

2333 Quarter-Scale Tests—The just-described Report 2333 full-scale tests were simulated in a linearly reduced scale model of the master bedroom employing a gas burner initial fire and a variety of wall and ceiling materials.

Scaling Parameter Estimation for the Experiments

The Initial Fire Parameter ϕ —In Report 1530, wood cribs and a 16-kg upholstered chair were used as the initially burning items. Figures 6b and 8b

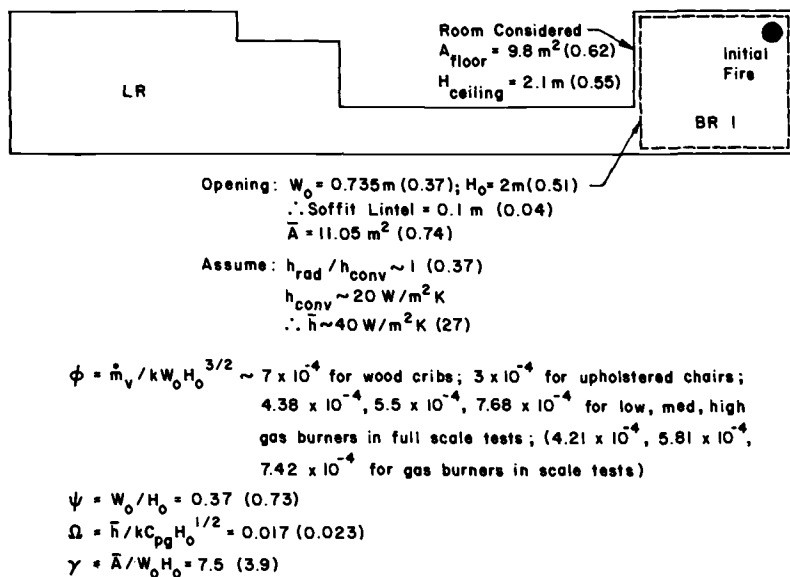


FIG. 5—Test setup for experiments of Reports 1531 and 2333.

of Ref 4 lead to estimating the mean burning rates of cribs and chairs to be respectively 2.5×10^{-3} and 1×10^{-3} kg/s. The given burning rate data were averaged by us over the first 5 min to obtain these figures. Once $\dot{m}_v$ is thus known, it can be converted to ϕ by definition through the opening dimensions. In Report 1720, there were two tests in which the upholstered chair was of mass 35 kg. Having no characterization data available for this heavier chair, it was assumed to have the same mass burning rate as the 16-kg chair.

In the full-scale tests reported in Report 2333, one 16-kg chair and three (different) wood crib tests were involved. For these four tests also, the just mentioned burning rates were employed. The remaining eight tests of Report 2333 used a gas-fueled burner as the initial fire. A table on page 51 of Ref 7 gives the three volumetric flow rates of methane to the burner, which are readily converted to mass flow rates for substitution in the definition of ϕ . One of the small-scale tests employed propane instead of methane as the fuel; account for this is taken by noting the variance in fuel molecular weight. In Report 1531, a 16-kg upholstered chair was used for which the same $\dot{m}_v$, as in Report 1530, is used. One of the tests in Report 1531 used a bed mattress as the seat of initial fire. Having no data specified for this situation, a burning rate of 1 g/s is assumed.

These values of $\dot{m}_v$ are also consistent with other available sources. Klein [3] gives the crib burning rate constant $\dot{m}_v/m_o$ to be $8.6 \times 10^{-4} \text{ s}^{-1}$. Quintiere [15] gives the ranges of maximum burning rates for various residential combustible sources. Among the reported: wood and upholstered chairs 5 to 60 g/s and beds 5 to 140 g/s are indicative of a broad range of variation.

Coupled with the window/door opening dimensions, these mass burning rate values give ϕ according to the definition

$$\phi \equiv \dot{m}_v / k W_o H_o^{3/2}$$

Window aspect parameter— $\psi \equiv W_o / H_o$ depicts whether the window is a narrow vertical slit ($\psi \ll 1$) or a thin horizontal slit ($\psi \gg 1$). Trivially estimated from the known opening dimensions, this parameter describes the relation between area of flow $W_o H_o$ and the possible velocity of flow which is a function of H_o .

Ceiling and lintel (soffit) skirt parameter γ —Defined as $\gamma \equiv \bar{A} / W_o H_o$, this parameter is of concern in two respects. It first delineates the area $\bar{A}$ of the ceiling and upper parts of the walls which come into convective contact with hot combustion gases before they flow out of the window. This is a crucial aspect, for when coupled with the buoyancy-induced flow velocity, the parameter may become a measure of the residence time of the hot gases during which they lose heat to the room. The second point of importance of γ pertains to the issue of thermal radiation. A large lintel, yielding a large γ , represents a relatively thicker layer of hot combustion gas which radiates energy to the ceiling as well as to the floor. In as much as thermal radiation is a process deemed crucial in flashover, γ becomes crucial as well.

Modified Stanton parameter Ω —The parameter $\Omega \equiv \bar{h} / k C_{pg} H_o^{1/2}$ with $\bar{h} \equiv h_{\text{convective}} + h_{\text{radiative}}$ is a Stanton number since $k H_o^{1/2}$ is a mass flux. Upon multiplying the numerator and the denominator by a characteristic temperature difference, it becomes immediately clear that Ω is a measure of heat transferred to the ceiling relative to the enthalpy in the flow.

Based on the measured heat flux dependency on surface temperature of a sample in a radiant panel test, Quintiere, Harkleroad, and Walton [23] deduce a value in the range of 10 to 20 W/m² K for the convective heat transfer coefficient. A correlation has been presented [14] by Quintiere for the convective heat transfer coefficient associated with the impingement flow of hot gases through a doorway into the corridor ceiling. For 2-m-high doorways with a density ratio ρ_g / ρ_a of 0.5, $h_{\text{convective}}$ can be as high as 40 W/m² K. In Ref 15 the same author surmises that the convective coefficient for flow within the room probably lies in the range 1 to 100 W/m² K, depending upon the fire condition and location in the room. An average approximate value of $h_{\text{conv}} = 20$ W/m² K is deemed appropriate from the view of Refs 24, 25, 26 also.

Quintiere et al [23] indicated that the radiant heat transfer coefficient h_{rad} in their radiant panel test may range from one to four times the convective coefficient. Considerable amount of literature exists on the radiation from smoke layers; however, we find it difficult to quantitatively deduce data on h_{rad} useful for the present purposes.

For tests in Reports 1530 and 1720, $h_{\text{rad}} = 0.5 h_{\text{conv}}$ is assumed. Due to the larger lintel in tests of Reports 2333 and 1531, a value of $h_{\text{rad}} = h_{\text{conv}}$ is as-

sumed. Based on the ratio of the expected optical depth, the radiant heat transfer coefficient for the 1/4-scale tests in Report 2333 is estimated to be 0.37 times the convective coefficient.

Having thus available the coefficients, the Stanton parameter now can be estimated for all the three sets of experiments.

Wall Lining Flammability β —It is obvious that a highly combustible wall is conducive to rapid development of the room fire. Our dimensional analysis and scaling rules have not explicitly considered this issue. The deficiency is attempted to be rectified as follows.

In Report 1530, a variety of wall and ceiling materials were assessed for their flammability from the best available tests for ease of ignition, surface flame spread, smoke generation, and heat release rate. From the results given in Table 2 of Ref 4, the time-wise-averaged heat release rate is known for four different values of externally imposed radiant flux in the heat release rate calorimeter. One expects that the characteristics of wall-lining involvement in the fire include the heat release rate as dependent upon the exposure flux. Additionally, the fuel lining thickness l and the heat of combustion h_c of the lining material of density ρ also appear relevant. Thus, if $\rho l h_c$ is the energy content of the lining material per unit area J/m^2 , and q is the average heat release rate (W/m^2) per unit lining area, a characteristic time constant associated with the lining combustibility may be obtained as $(\rho l h_c)/q$. Nondimensionalizing this time with the heat transfer coefficient h and the wall conductivity K , density ρ and specific heat C as $(h^2 \rho l h_c / K \rho C q)$, a measure of the wall contribution to the flame growth is obtainable. A small value of this measure indicates rapid energy release and possible rapid flashover. As meaningful in capturing the role played by the heat release rate on the flashover process as this parameter thus may be, it points to the wrong direction so far as the influences of heat transfer coefficient h and heat of combustion h_c are considered.

The dilemma only can be resolved by a scrupulous dimensional analysis in which the growth of fire on the wall is modeled. Not having this analysis available at present, we seek to adopt the following heuristic approach suggested by Parker.²

Suppose that the total initial fire heat output rate $\dot{Q}_T$ is of concern in the room fire growth. This is the sum of the gas burner heat output $\dot{Q}_B$ and wall-lining fire heat output $\dot{Q}_W$, that is, $\dot{Q}_T = \dot{Q}_B + \dot{Q}_W$. The lining fire heat output may be obtained from the area A_F of linings covered by the fire and the average heat release rate $\bar{q}$ as $\dot{Q}_W = A_F \bar{q}$. Parker's tests [27] on E-84 tunnel indicate that A_F is proportional to the burner heat output $\dot{Q}_B$, the proportionality constant being $f \equiv 0.02 \text{ m}^2/\text{kW}$, so that $A_F = f \dot{Q}_B$. Thus, $\dot{Q}_T = \dot{Q}_B + f \dot{Q}_B \bar{q} = \dot{Q}_B (1 + f \bar{q})$. Let $f \bar{q}$ be denoted by β so that $\dot{Q}_T = \dot{Q}_B (1 + \beta)$.

There are two different ways in which β can be evaluated. First, the heat

²Parker, W. J., personal communication, Feb. 1983.

release rate $\bar{q}$ may be viewed as the product of mass release rate and the heat of combustion of the released gases. To a first approximation, the mass release rate is the ratio of convective heat transfer rate from the flame to the lining and the effective latent heat of pyrolysis of the lining material. Thus,

$$\bar{q} \approx \frac{h(T_\infty - T_w)}{L_p} h_{cp}$$

where

- h = the heat transfer coefficient between the flames and the lining ($\text{W/m}^2 \text{K}$),
- $(T_\infty - T_w)$ = the flame to lining surface temperature difference (K),
- L_p = heat of pyrolysis (J/kg fuel pyrolyzates produced), and
- h_{cp} = the pyrolyzate heat of combustion (J/kg).

L_p is assumed here to be a constant. Then

$$\dot{Q}_T = \dot{Q}_B \{1 + fh(T_\infty - T_w)h_{cp}/L_p\}$$

Alternatively, the wall contribution may also be expressed as a function of the measured heat release rate.

$$\dot{Q}_T = \dot{Q}_B \{1 + fh(T_\infty - T_w)(q/I)\}$$

where I is the externally imposed radiant heat flux in the heat release rate calorimeter measurement of the release rate q .

The burner heat input rate is $\dot{Q}_B = \dot{m}_v h_{cb}$, where $\dot{m}_v$ is the gas input rate and h_{cb} is heat of combustion of the gas fuel. Thus, the prompting effects of the initial gas burner fire and the wall involvement are accountable by the parameter $\phi(1 + \beta)$, where ϕ is defined earlier and β is the wall combustibility parameter defined in the following two possible ways

$$\begin{aligned} \beta &\equiv fh(T_\infty - T_w)h_{cp}/L_p \\ &\equiv fh(T_\infty - T_w)q/I \end{aligned}$$

If the heat release rate measurements are available, the second of these definitions is probably preferred.

The combined effect of the initial fire burner and the wall lining expressed in the form $\phi_o(1 + \beta)$ is perhaps a statement not strong enough to capture the very active role played by the wall lining materials in bringing about the flash-over. To attain this active role in our correlation, it perhaps is desirable to replace $\phi_o(1 + \beta)$ by $\phi_o/(1 - \beta)$ so that as β arrives at unity, the prompting of the fire growth becomes quite strong and the resultant time to flashover quite

small. This speculation will be considered for a reasoned analysis in a later study.

In the present correlation, $\beta = fh(T_\infty - T_w)(q/I)$ is taken in conjunction with the prompting parameter $\phi_o(1 + \beta)$. The constant f is taken to be equal to $0.02 \text{ m}^2/\text{kW}$; heat transfer coefficient h is taken as $h \approx \bar{h} \equiv h_{\text{rad}} + h_{\text{conv}}$, values given in Figs. 4 and 5 for the different experiments. The difference between flame and lining surface temperatures ($T_\infty - T_w$) is taken to be 500 K, independent of the initial fire source and the geometry of the experiment. And, finally, the ratio of heat release rate to imposed radiant flux is taken as the average of the four exposure conditions reported in Ref 4, Table 2.

No account is now taken of the lining solid moisture content (available only in a few experimental runs) and of the atmospheric relative humidity. The moisture content is expected to exert a dual influence, first, to increase the latent heat of pyrolysis of the lining material and, second, to reduce the heat of combustion of the pyrolyzates. In a subsequent study we might find it necessary, and possible, to consider these effects of the moisture content.

Conductivity, Density, and Specific Heat Data—The thermal properties of all materials are adapted from Table 2 of McCaffrey, Quintiere, and Harkelroad [28]. Even for the thinnest of the most diffusive materials considered in our work, the diffusion time l^2/α is much larger than the generally observed flashover time. As a result, all the present linings behave as thermally thick bodies in the preflashover stages. A consequence of this is that the appropriate reference time is $(K\rho C)/\bar{h}^2$.

The flashover time t^* is nondimensionalized thus with $K\rho C$ of the walls and $K\rho C$ of the ceiling, and then an average $\bar{\tau}^*$ is taken.

$$\bar{\tau}^* \equiv \frac{\bar{h}^2 t^*}{2} \left\{ \frac{1}{(K\rho C)_{\text{walls}}} + \frac{1}{(K\rho C)_{\text{ceiling}}} \right\}$$

Correlation and Discussion

Having thus evaluated for each of the tests the window/door opening aspect parameter ψ , initial fire parameter ϕ_o , soffit parameter γ , heat transfer parameter Ω , and wall flammability parameter β , data from Refs 4–7 are considered for correlation. Our first attempts to coalesce the detailed temperature histories of various tests on a nondimensional basis in the pattern of Fig. 3 proved to be futile, mainly due to the numerous minor differences from test to test. A decision was then made to focus our effort on the time to flashover as dependent upon the major scaling variables just listed. All the other factors of minor variability between tests are unaccounted for.

Note from Figs. 4 and 5 that the geometry of the fire room and its connection to the surroundings are different for the sets of tests considered. Even the fire location relative to the major vent opening is different between these sets. Additionally, the living room fires of Fig. 4 do not involve a soffit lintel be-

tween the room of fire and the hallway. And further, the different full-scale tests involve different furnishing fuel loads.

Keeping these differences in mind, the data are nondimensionalized and are plotted in Fig. 6. The y-axis is the nondimensional flashover time $\bar{\tau}^*$ defined, computed, and averaged as described in the preceding section. The x-axis, $\phi_o(1 + \beta)$, a combination of the initial fire parameter and the wall flammability parameter, is chosen on the following basis. The susceptibility of the wall lining to contribute to the fire growth is expected to be a major factor in flashover. The growth of a fire generally is known to follow approximately an exponential pattern. In the context of the experiments being correlated in the present work, the simplest yet plausible view taken is that the initial fire source adjacent to a combustible wall represents a growing fire according to the arguments presented in the preceding section.

$$\phi = \phi_o(1 + \beta)$$

where ϕ_o represents the constant initial fire source strength and β represents the wall flammability. The size of the initial fire is of obvious significance in this x-axis parameter. Even more importantly, a small initiating fire with a

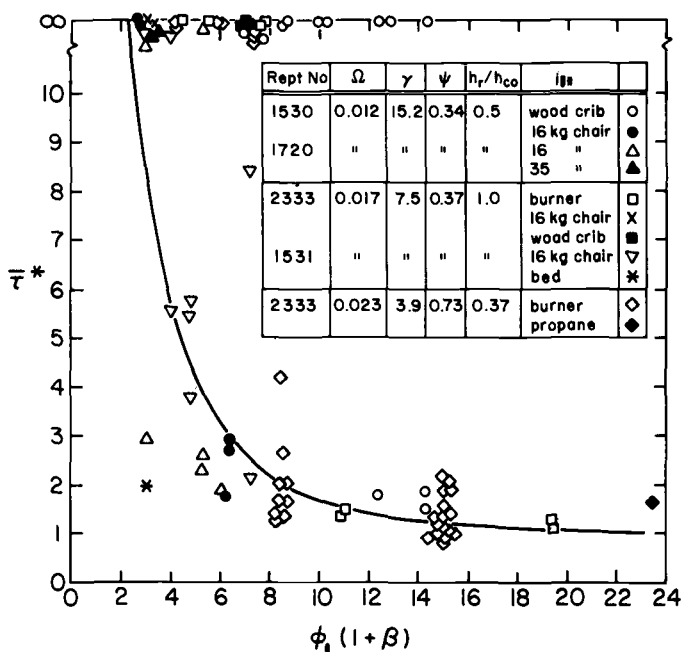


FIG. 6—Data correlation.

highly flammable wall appears to be accountable through this chosen simple form.

Figure 6 shows the correlation of all the just-mentioned tests. The estimated values of the parameters Ω , γ , ψ , ϕ_o , and β are shown in Figs. 4 and 5. The following observations are made:

1. Data of all the tests for which the flashover time is finite follow a pattern of decreased τ^* with increased $\phi_o(1 + \beta)$. This pattern is expected, for situations involving large initial fires and highly combustible walls culminate in an early flashover.

2. There appears to be indication that no flashover would occur if the initial fire is so small and the wall lining is so poorly combustible as to result in the quantity $\phi_o(1 + \beta)$ less than about 2.

3. However, data on those tests in which flashover is judged not to have occurred bear an unreasonable departure from the present hypothesis. Thirty-two out of the total 78 tests pertain to this predicament. Apparently there exists a process yet to be accounted to explain this anomaly. Most of these anomalous tests involved gypsum board linings, on the walls as well as ceiling, with or without spackle texturing, papered finish, or calcium silicate overcoat. Four of these tests, however, involved gypsum board at the ceiling only.

4. The range of variation in the window aspect ratio ψ , and in the ceiling heat transfer parameter Ω , between the two sets of full-scale tests and one set of quarter-scale tests is quite wide although not systematic. On the other hand, the range of variation in the soffit parameter is quite narrow. The scatter in the correlation does not now permit a clear-cut delineation of these parametric effects. The scatter could be partly due to the incompleteness of the correlation scheme, partly due to the method of interpretation of the flashover phenomenon as embodied in the time to flashover recorded in the source experiments, partly due to uncontrolled variances between tests which are otherwise deemed identical, partly due to departures in geometric (and sometimes dynamic) similarity between test groups, and lastly due to possible property variations. Recall that we have taken constant $(K\rho C)$ values. Also recall that: (a) the wall-lining moisture content is ignored; (b) the heat release rate of the lining materials is averaged over the expected incident radiant fluxes; (c) the convective and radiant heat transfer coefficients are merely estimated and taken to be the same for the flame to wall-lining heat transfer as well as hot gas layer to the ceiling; and (d) the wall combustibility parameter β as well as the functional form in which it enters into our correlation are based on heuristic arguments rather than on a reasoned hypothesis. Further study is required of these and other relevant issues of the room-fire scaling problem.

Consideration of further systematically conducted experiments with an improved hypothesis and with better property values appears to be in order.

5. Ambiguities related to moisture exist for further scrutiny. For example,

how does moisture affect the conductivity, density, and specific heat, in addition to the heats of combustion and pyrolysis?

6. The ambient air relative humidity is purported to have an influence on the fire development process. We do not now know how to account for this influence.

7. The role of furnishings in the flashover process is another issue not considered in the present correlation.

8. Notwithstanding these difficulties, it is indeed encouraging that there exists a reasonable trend of dependency of flashover time on the initial fire and the wall combustibility.

Conclusions and Recommendations

Based upon the partial progress made in this work, the following overall conclusions appear reasonable.

1. Scaling studies represent a step in the right direction to deduce the commonalities (and identify the differences) between different tests and their results. While basic research into the scientific nature of the component processes of a growing enclosure fire must continue towards the development of predictive theories, scaling studies will be of benefit on a relatively shorter time scale. This benefit lies in drawing systematic information from the test data and even in guiding the directions for the predictive theory development.

2. Based on the premise that flashover is a consequence of the transience involved in heating the solid boundaries rather than of the transience in the gas phase, the present model leads to the following scaling parameters which appear to be essential in systematically correlating the room-fire flashover test data:

(a) Initial fire characteristic	$\phi_o \equiv \dot{m}_v/kW_oH_o^{3/2}$
(b) Window geometry	$\psi \equiv W_o/H_o$
(c) Lintel (soffit) depth parameter	$\gamma \equiv \bar{A}/W_oH_o$
(d) Ceiling heat transfer parameter	$\Omega \equiv \bar{h}/kC_{pg}H_o^{1/2}$
(e) Wall-lining parameter	$\beta \equiv f\bar{h}(T_\infty - T_w)q/I$

The nondimensional flashover time τ^* is defined as $\bar{h}^2t^*/(K\rho C)$ with properties appropriately averaged between the wall linings and the ceiling. The overall heat transfer coefficient $\bar{h} = h_{\text{conv}} + h_{\text{rad}}$ requires an estimation of the ratio of radiative and convective coefficients. The wall combustibility constant f and average heat release rate q with external radiant exposure I are obtained from the test measurements and heat release rate calorimeter measurements of ASTM Method for Surface Burning Characteristics of Building Materials (E 84-84).

3. Whereas the tests in which the flashover time is measured to be finite appear to obey an expectable trend in correlation, those in which this time is

measured to be infinite require further attention. There appear at least two underlying reasons in this issue. First, the present scaling hypothesis is lacking in some as yet unidentified process. Second, a scrutiny is required of the experimental procedure in which the occurrence of flashover is indicated by the arrival of a critical radiant heat flux at the floor. Some other criteria might actually make these flashover times finite, which then can be checked against the expected correlation.

4. Rating the wall and ceiling lining materials for their combustibility in terms of a fire growth time constant appears to be a viable strategy to account for the contribution of linings to fire growth. The manner of incorporation of lining flammability employed in the present correlation requires further work.

5. Also begging for additional attention is the role played by the moisture content of the lining materials as it influences the combustibility. Even more direly lacking, and difficult to grapple with, is an understanding of the role of the ambient relative humidity in the fire growth process.

6. Better property values with the effect of moisture content are desirable.

7. Other test data in which geometric similarity is strictly maintained, and sufficient range of systematic variation in the soffit parameter is available, have to be considered for correlation by the present scheme. In this connection, improvement is required to estimate the radiation heat fluxes from the ceiling gas layer.

8. A more stringent characterization of the initial burning items also is desirable.

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Stochastic Modelling of Fire Growth

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ABSTRACT: This paper presents a framework for modelling the growth of fire in a building as a stochastic phenomenon. After discussing the probability distributions of duration of burning and damage, a state transition model involving four states is proposed for describing the progressive stage of fire development (and extinguishment) within a room. This model is based on data furnished by fire brigades in the United Kingdom for fires extinguished in each state during each time period since ignition. It is explained how, by applying "extreme value" technique to these data, approximate values can be obtained for a probability distribution of duration of burning in each state and for probabilities of transition from one state to another. Four materials ignited first in the bedroom of a dwelling are used as examples.

KEY WORDS: fire protection, fires, fire growth, modelling, probability distributions, stochastic processes, state transition model, transition probabilities, extreme value technique

Most of the mathematical models of fire growth available at present are deterministic in nature and generally relate to fire development within a compartment [1,2]. In these models, physiochemical and thermodynamic theories are formulated to explain the functional relationships between parameters describing fire growth and parameters influencing the growth. Data on the performance of structural elements and materials in experimental fires are utilized for estimating the parameters of deterministic models. However, the development of a real fire (as distinct from an experimental fire) is influenced by a diversity of materials, differences in their arrangement, and environmental changes such as in temperature, air pressure, wind velocity, and humidity. Some of these factors cannot be controlled, and hence the spread of a real fire needs to be treated as a random phenomenon. Stochastic models [3] recognize the random nature of fire growth and lead only to probabilistic

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assessments of the extent of spread and the effectiveness of fire protection measures.

This paper presents a method for estimating the behavior of a fire in probabilistic terms. After discussing the probability distributions of duration of burning and damage, a state transition model consistent with statistical data furnished by fire brigades in the United Kingdom [4] is proposed for describing the progressive stages of fire growth within a room. Using a dwelling as an example, it is shown how such a statistical approach can provide approximate values for a probability distribution of duration of burning in each state and for transition probabilities. The results for each type of room in a building can be used as inputs to depict fire spread between rooms on the same or different floors.

Probability Distributions

Consider the burning of a particular object as a random (stochastic) process [5]. If $Q(t)$ is the probability that the object is still burning at time t and $\mu(t)\delta t$ the probability of extinction of fire during the short period $(t, t + \delta t)$

$$Q(t + \delta t) = Q(t)[1 - \mu(t)\delta t] \quad (1)$$

Equation 1 is based on the fact that, if an object is burning at time $(t + \delta t)$, it should have been burning at time t and should not have become extinct during $(t, t + \delta t)$; this leads to the differential equation

$$Q'(t) = -Q(t) \cdot \mu(t) \quad (2)$$

where $Q'(t)$ is the derivative of $Q(t)$.

In a simple model the process may be assumed to be Poisson, so that $\mu(t)$ has a constant value μ . In this case, following Karlin [Ref 5, p. 14-15], the solution of Eq 2 is the exponential function

$$Q(t) = \exp(-\mu t) \quad (3)$$

The function $Q(t)$ also can be interpreted as the probability that the duration of burning is greater than t . Then $[1 - Q(t)]$ is the probability that the duration of burning is less than or equal to t . Hence the (cumulative) probability distribution function of duration of burning is

$$F(t) = 1 - \exp(-\mu t) \quad (4)$$

The value of μ for any object can be estimated by carrying out replicated extinction experiments with the object. If $\bar{t}$ is the mean (average) duration of burning

$$\mu = 1/\bar{t} \quad (5)$$

according to the properties of an exponential distribution. The value of μ will vary depending on whether it is a free-burning fire or a fire extinguished by fire fighting, for example, by fire brigade or sprinklers. Kida's [6] probabilistic analysis of extinction experiments implied exponential probability distribution for duration of burning.

Experiments on fire growth suggest that the volume of building affected by fire increases exponentially with duration of burning—see Labes [7] for example. As an approximation, it can be assumed that damage x in a fire is proportional to the volume destroyed. Hence $\log x$ will be proportional to t . It therefore may be deduced from Eq 3 that the probability of damage exceeding the value x is given by

$$\phi(x) = x^{-w} \quad (6)$$

where w is a constant.

Eq 6 is known as Pareto distribution. The use of this distribution for fire loss, originally proposed by Benckert and Sternberg [8], was later supported by Mandelbrot [9], who derived this result following a "random walk" model. This model implies a nearly equal balance between the fire propensity for growth and the fire fighting effort. This balance does not prevail during all stages of fire growth. Hence a Pareto distribution for x or equivalently an exponential distribution for $\log x$ is not a reasonable assumption, especially for fires which are fought. It may be more realistic to assume a normal distribution for $\log x$ or equivalently a log normal distribution for x . Log normal distribution has been recommended by many actuaries for modelling fire insurance claims (see Ramachandran [10]). In general, $\log x$ (or t) has a probability distribution of the "exponential type" [11]. This type, defined by Gumbel [12] with reference to limiting behavior of distributions at their tails, includes normal, log normal, gamma, and logistic as well as the exponential distribution.

Stochastic Model—Room

A fire in a room usually starts with the ignition of one of the objects. As fire spreads to other objects in the room, there is a chain of ignitions which could lead to flashover and fully developed fire conditions. There is, however, a chance that this chain could break at some stage for a variety of reasons with the fire becoming extinguished before spreading further. In the opinion of Williamson [13], many fires go out before igniting the wall or furniture or both. Statistics also support this theory (see, for example, Table 1 relating to dwelling fires). The duration of burning as shown in this table is the period from ignition to the time when fire is brought under control by a fire brigade.

Consider the burning of a particular object or set of objects in the chain.

TABLE 1—*Fires in bedrooms of dwellings, United Kingdom 1980—extent of spread.*

Duration of Burning, min	Bedding			Furniture			Electrical Insulation			Paper, Packaging		
	a	b	c	a	b	c	a	b	c	a	b	c
Up to 5	20	16	...	1	3	...	5	12	...	21	18	...
6-10	128	173	8	12	5	...	10	37	...	32	66	2
11-15	177	359	15	12	21	...	12	72	2	31	109	3
16-20	100	193	9	4	14	4	6	43	2	14	62	3
21-25	88	175	13	5	14	1	10	47	3	14	71	5
26-30	179	361	20	6	24	3	9	85	9	20	131	11
31-35	87	279	25	11	24	...	8	39	5	10	68	7
36-40	44	143	18	2	13	3	3	36	2	5	55	7
41-45	17	97	15	...	13	2	...	21	8	1	26	12
46-50	12	56	18	1	8	2	1	14	8	1	17	9
51-55	30	86	11	4	6	2	3	16	1	9	21	2
56-60	38	136	13	5	14	...	1	28	5	4	39	9
61-65	26	98	15	6	8	...	1	15	2	1	18	2
66-70	15	67	13	1	3	...	...	13	3	...	16	4
71-75	5	32	12	...	4	...	...	18	4	...	20	6
76-80	3	15	12	...	...	1	...	6	1	2	7	2
81-85	3	21	3	1	...	1	...	2	5	...	2	5
86-90	1	11	3	...	1	...	...	2	4	...	2	4
More than 90	17	53	29	...	5	2	3	7	9	5	7	10
Total	990	2371	252	71	180	21	72	513	73	170	755	103

NOTE:

a = Number of fires confined to the object first ignited.

b = Number of fires which spread beyond the object first ignited but which were confined to the room of fire origin.

c = Number of fires which spread beyond the room of fire origin.

The simple model described with reference to Eq 1 can be expanded to include a probability of spread. During any period $(t, t + \partial t)$, the fire can burn out or be put out by an extinguishing agent with a probability $\mu(t)$ or spread with a probability $\lambda(t)$ or burn without spreading with a probability $s(t)$. In this case

$$\mu(t) + \lambda(t) + s(t) = 1 \quad (7)$$

If these three "instantaneous probabilities" are assumed as constants, following the derivation of Eq 3, it can be shown that

$$Q_s(t) = \exp[-(\mu + \lambda)t] \quad (8)$$

where $Q_s(t)$ is the probability that the fire is burning at time t without spreading. The duration of burning of such a fire, therefore, follows an exponential distribution with mean duration given by $(\mu + \lambda)^{-1}$. In reality, however, the probabilities are functions of time.

The spread probability $\lambda(t)$, also known as "transition probability," depends on the state from which the transition is made. Berlin [14] used measurable criteria such as heat release rate and air temperature to define six realms (states) for residential occupancies: nonfire state, sustained burning, vigorous burning, interactive burning, remote burning, and full room involvement. Williamson [15] introduced a state transition model for analyzing and reporting the results of experiments performed under conditions resembling actual fire conditions.

Based on data available from fire brigades in the United Kingdom (Table 1), the following four states are suggested for a simple state transition model to describe the development of fire within a room:

1. Fire involving the object first ignited.
2. Spreading beyond the object first ignited involving other objects in the room.
3. Spreading beyond the room of fire origin involving objects outside this room.
4. Extinguishment or burning out of fire.

For any time period t , the development of fire can be represented schematically as in Fig. 1. Probability of spreading beyond the object first ignited, denoted by $\lambda_1(t)$, and probability of spreading beyond the room of fire origin, denoted by $\lambda_2(t)$, are transition probabilities involving fire growth. Probabilities $\mu_1(t)$, $\mu_2(t)$, and $\mu_3(t)$ relate to transitions to the fourth state involving fire

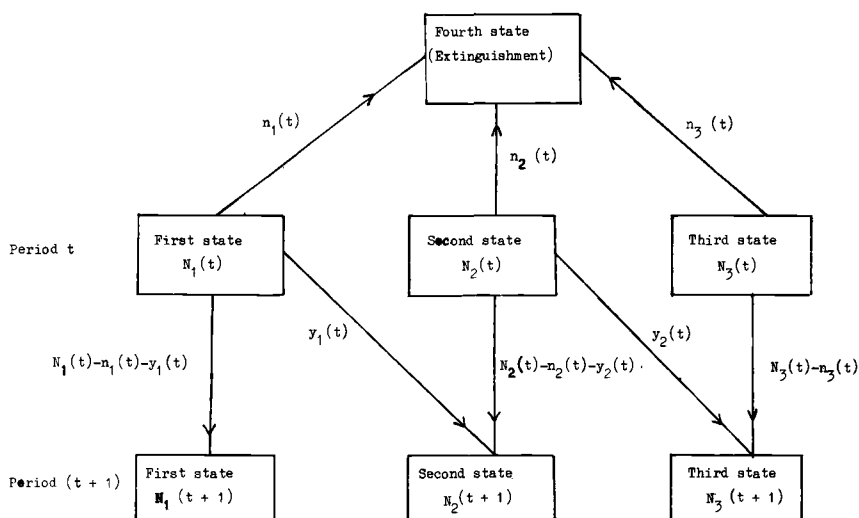


FIG. 1—State transition model—schematic representation.

extinguishment. The fourth state is known as an “absorbing state” since the fire process cannot leave this state after entering it.

In the absence of necessary data from experiments, the transition probabilities have to be evaluated from available statistics on real fires, such as those described in Table 1. The figures in this table, however, relate to fires which were extinguished during each time period since ignition. Estimates have to be obtained for the number of fires which were burning in a particular state (i) at the beginning of each period (t) [denoted by $N_{(i)}(t)$] and the number $y_i(t)$ which spread to the next state during the period. Then, for the t^{th} period we can evaluate

$$\lambda_i(t) = y_i(t)/N_i(t) \quad (9)$$

and

$$\mu_i(t) = n_i(t)/N_i(t) \quad (10)$$

where $n_i(t)$ was the number of fires extinguished.

From Eq 7

$$\mu_i(t) + \lambda_i(t) + s_i(t) = 1 \quad (11)$$

where $s_i(t)$ is the number remaining (burning) without spreading.

Construction of the following equations is self-explanatory.

$$N_1(t + 1) = N_1(t) - n_1(t) - y_1(t) \quad (12)$$

$$N_2(t + 1) = N_2(t) - n_2(t) - y_2(t) + y_1(t) \quad (13)$$

$$N_3(t + 1) = N_3(t) - n_3(t) + y_2(t) \quad (14)$$

Since spread beyond the third state is not being considered in this model for fire development in a room, $y_3(t)$ and $\lambda_3(t)$ have been taken as zero so that

$$s_3(t) = 1 - \mu_3(t) \quad (15)$$

At the beginning of the first period, all the N fires in the sample analyzed would have been in the first state. Hence

$$N_1(1) = N$$

$$N_2(1) = 0$$

$$N_3(1) = 0$$

In this preliminary investigation, the duration of burning has been classified according to periods each of 5 min to provide an adequate number of fires for each period and each state. An "incubation" or "latent" period of 5 min has been assumed for fire becoming capable of spreading beyond the object first ignited. Hence the second state is deemed to commence only in the second period with $n_2(1) = 0$, although a few fires have been extinguished in this state during the first period. Accordingly, it has been further assumed that $y_2(1) = 0$. Data also support the fact that $n_3(1) = 0$ and that the third state can commence only in the third period; it will take at least 10 min for a fire to spread beyond the room of origin. The assumptions mentioned above lead to the following further initial conditions

$$N_2(2) = y_1(1)$$

$$N_3(2) = 0$$

$$n_3(2) = 0$$

$$N_3(3) = y_2(2)$$

If data are available for r periods, $t = 1$ to r , the problem now is to estimate $\lambda_i(t)$ and $\mu_i(t)$, which require estimates of $y_i(t)$ and $N_i(t)$ satisfying Eqs 12 to 14 and the initial conditions. The values of $n_i(t)$ are those given in Table 1. While attempting a solution, another fact needs to be taken into consideration. Data suggest that, for each cumulative period, a certain minimum number of fires should have spread from one state to another. Consider, for example, figures in Table 1 for fires with bedding as material first ignited. During the period up to 10 min a total of 197 fires were extinguished in the second and third states. Hence, during the first period (up to 5 min) at least 197 fires should have spread beyond the first state. Using the same reasoning, during the period up to 10 min at least 571 fires should have spread beyond the first state; out of this number, at least 23 fires should have spread beyond the second state. If it is estimated that a certain number of fires, $\bar{y}_1(1)$ (> 197), has spread beyond the first state during the first 5 min, then a minimum number of $[571 - \bar{y}_1(1)]$ fires should have spread beyond the first state during the second 5-min period and so on. Using such minimum values, an approximate value for the expected number of fires spreading beyond the i^{th} state ($i = 1, 2$) during the t^{th} period can be estimated by applying extreme value technique [16] as explained in the following paragraph.

According to extreme value theory, if the number of fires spreading beyond the i^{th} state during the t^{th} period has an expected value $\bar{y}_i(t)$ and standard deviation σ_{it}

$$y_{im}(t) = \bar{y}_i(t) + \sigma_{it} \cdot \bar{\Theta}_m \quad (16)$$

where $y_{im}(t)$ is the minimum number that could have spread beyond the i^{th} state during the t^{th} period. The parameter $\bar{\Theta}_m$ is given by

$$\bar{\Theta}_m = B_m + (\bar{y}/A_m) \quad (17)$$

It is assumed that in a large sample (larger than the one used) the number spreading has a normal distribution and that the probability of this number being less than the minimum is 0.01. The parameters B_m and A_m are then given by

$$G(B_m) = 0.01 \quad (18)$$

$$A_m = g(B_m)/0.01 \quad (19)$$

where $g(u)$ and $G(u)$ are the density and distribution functions of the standard normal distribution with mean zero and variance unity. From tables of this distribution it may be seen that the values

$$B_m = -2.3263 \quad (20)$$

$$A_m = 2.6652 \quad (21)$$

satisfy Eqs 18 and 19.

The “reduced” variable y has the extreme value distribution applicable to the smallest value, with density function given by

$$\exp[y - \exp(y)]$$

The variable y has the expected value

$$\bar{y} = -0.5772 \quad (22)$$

and variance

$$\sigma_y^2 = 1.6449 \quad (23)$$

Also, according to extreme value theory, if $\sigma_{im}^2(t)$ is the variance of $y_{im}(t)$, from Eqs 16, 17, 21, and 22

$$\sigma_{it}^2 = \sigma_{im}^2(t) A_m^2 / \sigma_y^2 = \sigma_{im}^2(t) \cdot 4.3184 \quad (24)$$

Also, following Eq 9

$$\begin{aligned} \lambda_{im}(t) &= y_{im}(t)/N_i(t) \\ \sigma_{im}^2(t) &= N_i(t) \cdot \lambda_{im}(t)[1 - \lambda_{im}(t)] \end{aligned} \quad (25)$$

From Eqs 17, 20, 21, and 22

$$\bar{\Theta}_m = -2.5429 \quad (26)$$

$$\bar{y}_i(t) = y_{im}(t) + 5.2843 \sigma_{im}(t) \quad (27)$$

Using Eqs 25 and 27 and the minimum number of spreading fires $y_{im}(t)$ as discussed earlier, the expected number of fires $\bar{y}_i(t)$ spreading beyond the i^{th} state during the t^{th} period was estimated for the four leading materials first ignited in the bedroom. These results together with the known values of $n_i(t)$ provided estimates of $N_i(t)$ and $y_i(t)$. Table 2 is an example of the results obtained with fires starting in beds. Based on these figures, expected values of the probabilities $\mu_i(t)$ and $\lambda_i(t)$ were estimated as shown in Table 3. Similar results were obtained for the other three materials: furniture, electrical insulation, and paper. It may be recognized that in these calculations the probabilities have been regarded as functions of time and not constants as in Eq 8. It has been assumed that the probability of $\lambda_i(t)$ being less than the minimum $\lambda_{im}(t)$ is 0.01. The probability level of 1% has been used only for the purpose of illustrating the technique.

TABLE 2—Bedroom—bedding (number of fires).

Period, min	First State			Second State			Third State	
	a	b	c	a	b	c	a	b
1-5	3613	20	269	...	...	...	...	...
6-10	3324	128	389	269	189	47	...	...
11-15	2806	177	169	422	359	0	47	23
16-20	2460	100	192	232	193	0	24	9
21-25	2168	88	409	231	175	39	15	13
26-30	1671	179	289	426	361	14	41	20
31-35	1203	87	136	340	279	23	35	25
36-40	980	44	105	174	143	3	33	18
41-45	831	17	63	133	97	35	18	15
46-50	751	12	109	64	56	0	38	18
51-55	630	30	158	117	86	15	20	11
56-60	442	38	102	174	136	15	24	13
61-65	302	26	73	125	98	10	26	15
66-70	203	15	30	90	67	14	21	13
71-75	158	5	24	39	32	7	22	12
76-80	129	3	24	24	15	2	19	12
81-85	102	3	0	31	21	0	7	3
86-90	99	1	49	44	11	14	4	3
91-100	...	...	...	...	...	...	...	...

NOTE:

a = Number burning at the beginning of the period $[N_i(t)]$.

b = Number extinguished during the period $[n_i(t)]$.

c = Number spread during the period $[y_i(t)]$.

TABLE 3—*Bedroom—bedding (probabilities).*

Period (t), min	First State		Second State		Third State
	$\mu(t)$	$\lambda(t)$	$\mu(t)$	$\lambda(t)$	$\mu(t)$
1-5	0.0055	0.0745	...	...	...
6-10	0.0385	0.1170	0.7026	0.1747	...
11-15	0.0631	0.0602	0.8507	0	0.4894
16-20	0.0407	0.0780	0.8319	0	0.3750
21-25	0.0406	0.1887	0.7576	0.1688	0.8667
26-30	0.1071	0.1730	0.8474	0.0329	0.4878
31-35	0.0723	0.1131	0.8206	0.0676	0.7143
36-40	0.0449	0.1071	0.8218	0.0172	0.5455
41-45	0.0205	0.0758	0.7293	0.2632	0.8333
46-50	0.0160	0.1451	0.875	0	0.4737
51-55	0.0476	0.2508	0.7350	0.1282	0.5500
56-60	0.0860	0.2308	0.7816	0.0862	0.5417
61-65	0.0861	0.2417	0.7840	0.0800	0.5769
66-70	0.0739	0.1478	0.7444	0.1556	0.6190
71-75	0.0316	0.1519	0.8205	0.1795	0.5455
76-80	0.0233	0.1860	0.6250	0.0833	0.6316
81-85	0.0294	0	0.6774	0	0.4286
86-90	0.0101	0.4950	0.2500	0.3182	0.7500

The figures in Table 3 and those for the other three materials reveal a few basic facts, although they vary considerably with t and exhibit a somewhat random behavior. The values of $\lambda_1(t)$ are greater than $\mu_1(t)$ indicating that, when the fire is in the first state, its propensity for spreading is greater than the fire fighting effort or propensity for burning out. This situation is reversed in the second state with $\mu_2(t)$ greater than $\lambda_2(t)$ since fire would have spread to most of the objects in the room. If the fire is not controlled before it spreads beyond the second state (room), it will continue to burn, affecting other areas of the building. Hence the probability in the third state [$\mu_3(t)$] would be less than the probability of extinguishing it in the second state [$\mu_2(t)$]. As fire continues to burn in any state, producing more and more heat, its propensity for spreading increases. Hence $\lambda_i(t)$ will be increasing up to, say, 60 min, after which it will decrease with increasing t . A fire cannot spread after a certain stage if by that time it has not grown and generated sufficient heat. (The probabilities for periods beyond 60 min are based on a small number of observations.)

The values in Table 3 are instantaneous or conditional probabilities per periods of 5 min. Approximate values of the probabilities per minute are given by $\mu_i(t)/5$ and $\lambda_i(t)/5$ in cases involving single periods or by $\mu_i(t)/10$ and $\lambda_i(t)/10$ in cases involving two periods.

When interpreting the results, the following points should be taken into consideration. The time when a fire started is generally ascertained by the fire brigade by making inquiries and is estimated according to the following broad classification:

1. Discovered at ignition.
2. Discovered under 5 min after ignition.
3. Discovered between 5 and 30 min after ignition.
4. Discovered more than 30 min after ignition.

For purposes of the statistical analysis, average times of 2, 17, and 45 min were used for Classes 2, 3, and 4. Fire brigade definition of time of ignition could be different from the definition adopted for experiments on fire growth. In an experiment, efforts are made to achieve sustained burning as quickly as possible. In an accidental fire caused by, say, dropping a cigarette on a bed, the fire could be smoldering for some time and then suddenly burst into a flame. Hence the transition probabilities $\lambda_1(t)$ for real fires can be small during early periods as revealed by the figures in Table 3. Also, for the first 25 min, values of $\lambda_1(t)$ were estimated to be higher for fires starting in electrical insulation, paper and packaging than for fires starting in bed and furniture.

Consistent with Eqs 9 to 14 and following Aoki [17], the following two probabilities can be defined.

$q_i(t + 1)$ = probability that the fire will be burning in the i^{th} state during the $(t + 1)^{\text{th}}$ period.

$P_i(t + 1)$ = cumulative probability of fires which will have become extinguished in the i^{th} state up to the end of the $(t + 1)^{\text{th}}$ period.

The following equations are easily derived

$$q_1(t + 1) = q_1(t)s_1(t) = \sum_{r=1}^t s_1(r) \quad (28)$$

since $q_1(1) = 1$

$$P_1(t + 1) = \sum_{r=1}^{t+1} q_1(r)\mu_1(r) \quad (29)$$

$$q_2(t + 1) = q_2(t)s_2(t) + q_1(t)\lambda_1(t), \quad q_2(1) = 0 \quad (30)$$

$$P_2(t + 1) = \sum_{r=2}^{t+1} q_2(r)\mu_2(r) \quad (31)$$

$$q_3(t + 1) = q_3(t)s_3(t) + q_2(t)\lambda_2(t) \quad (32)$$

with $q_3(1) = 0$, $q_3(2) = 0$ and $\lambda_2(1) = 0$

$$P_3(t + 1) = \sum_{r=3}^{t+1} q_3(r)\mu_3(r) \quad (33)$$

We can also define cumulative probabilities of spread as follows:

$$\xi_1(t + 1) = \sum_{r=1}^{t+1} q_1(r)\lambda_1(r) \quad (34)$$

$$\xi_2(t + 1) = \sum_{r=2}^{t+1} q_2(r)\lambda_2(r) \quad (35)$$

For purposes of calculations, the equations mentioned above reduce to the following simple forms with N denoting the total number of observations in the sample

$$q_1(t + 1) = N_1(t + 1)/N \quad (36)$$

which gives

$$\begin{aligned} Nq_1(t + 1) &= N_1(t + 1) \\ &= Nq_1(t)s_1(t) \text{ (from Eq 28)} \\ &= N_1(t)[1 - \lambda_1(t) - \mu_1(t)] \\ &= N_1(t) - y_1(t) - n_1(t) \end{aligned} \quad (37)$$

using Eqs 9 and 10. Eq 37 is the same as Eq 12.

In the same way it may be seen that

$$q_2(t + 1) = N_2(t + 1)/N \quad (38)$$

so that

$$\begin{aligned} Nq_2(t + 1) &= N_2(t + 1) \\ &= N[q_2(t)s_2(t) + q_1(t)\lambda_1(t)] \\ &= N_2(t) - y_2(t) - n_2(t) + y_1(t) \end{aligned}$$

agreeing with Eq 13. It may also be verified that

$$q_3(t + 1) = N_3(t + 1)/N \quad (39)$$

is consistent with Eq 14. The following equations also can be easily derived.

$$P_1(t + 1) = \sum_{r=1}^{t+1} q_1(r)\mu_1(r) = \frac{1}{N} \sum_{r=1}^{t+1} n_1(r) \quad (40)$$

$$P_2(t+1) = \sum_{r=2}^{t+1} q_2(r) \mu_2(r) = \frac{1}{N} \sum_{r=2}^{t+1} n_2(r) \quad (41)$$

$$P_3(t+1) = \sum_{r=3}^{t+1} q_3(r) \mu_3(r) = \frac{1}{N} \sum_{r=3}^{t+1} n_3(r) \quad (42)$$

$$\zeta_1(t+1) = \sum_{r=1}^{t+1} q_1(r) \lambda_1(r) = \frac{1}{N} \sum_{r=1}^{t+1} y_1(r) \quad (43)$$

$$\zeta_2(t+1) = \sum_{r=2}^{t+1} q_2(r) \lambda_2(r) = \frac{1}{N} \sum_{r=2}^{t+1} y_2(r) \quad (44)$$

In Fig. 2, the values of $[-\log_e q_1(t)]$ for the four materials have been plotted against time (t) since ignition, using midpoints of the time periods. If the

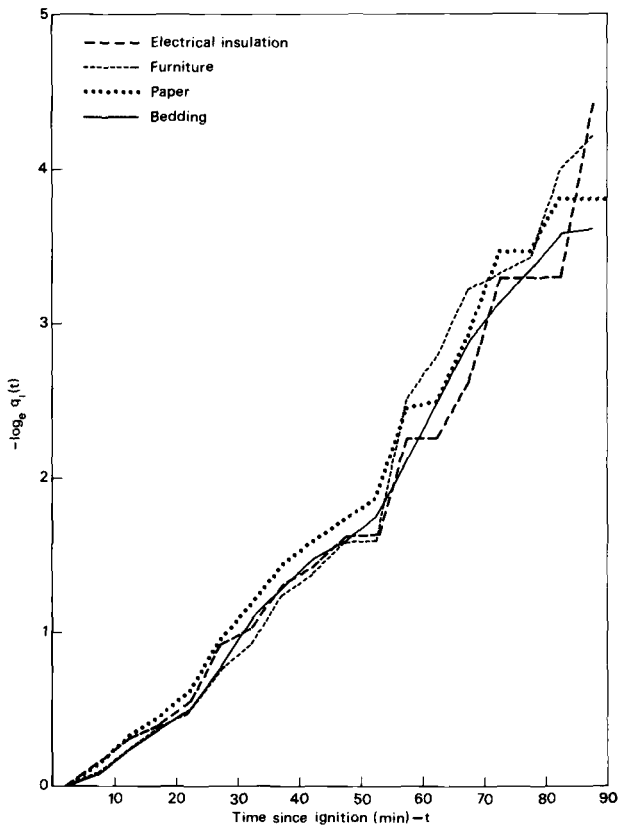


FIG. 2—Probability of burning in the first state.

random fluctuations are smooth, the relationship is almost a straight line in each case, indicating that

$$q_1(t) = \exp(-k_1 t) \quad (45)$$

approximately in the case of t taking continuous values. Equation 45 denotes the probability of burning in the first state at time t (since ignition) without spreading to the second state. This exponential distribution is identical to Eq 8 with parameter k_1 having a constant value equal to $(\lambda + \mu)$. Hence, for the first state, $[\lambda_1(t) + \mu_1(t)]$ can be regarded as a constant for all practical purposes. A regression estimate of the constant per minute is given by

$$-\Sigma \log_e q_1(t) / \Sigma t \quad (46)$$

An estimate of k_1 is also given by

$$\bar{\lambda}_1(t) + \bar{\mu}_1(t) \quad (47)$$

where $\bar{\lambda}_1(t)$ and $\bar{\mu}_1(t)$ are average values per minute over the burning period provided by figures such as those in Table 3. The values given by Eqs 46 and 47 are compared in Table 4 together with their reciprocals ($1/k_1$) denoting the average duration of burning.

The probabilities $q_2(t)$ and $q_3(t)$ of fire burning in the second and third states vary widely with t without revealing any trends. These states involve more than one material. Average values of $[\lambda_2(t) + \mu_2(t)]$ and $\mu_3(t)$ are given in Table 5. The reciprocals of these values would indicate approximately the average duration of burning if exponential probability distributions are assumed for these states. These durations apply from the time the fire starts burning in these states after spreading from the previous states. As assumed earlier, from the time of fire starting in the first state, a minimum time of 5 min lapses before fire spreads to the second state and a minimum time of 10 min before spreading to the third state.

TABLE 4—*Burning in the first state—average values of parameters.*

Parameters	Bedding	Furniture	Electrical Insulation	Paper, Packaging
Regression estimate, k_1 (per min)	0.0370	0.0391	0.0373	0.0399
Duration of burning (min)	27.0	25.6	26.8	25.1
Average values, $\bar{\lambda}_1(t) + \bar{\mu}_1(t)$ (per min)	0.0408	0.0373	0.0377	0.0444
Duration of burning (min)	24.5	26.8	26.5	22.5

TABLE 5—*Burning in the second and third states—average values of parameters.*

Parameters	Bedding	Furniture	Electrical Insulation	Paper, Packaging
Second state, $\bar{\lambda}_2(t) + \bar{\mu}_2(t)$ (per min)	0.1695	0.1110	0.1339	0.1609
Duration of burning (min)	5.9	9.0	7.5	6.2
Third state, $\bar{\mu}_3(t)$ (per min)	0.1179	0.0426	0.0739	0.0831
Duration of burning (min)	8.5	23.5	13.5	12.0

The figures in Tables 4 and 5 are based on straight (unweighted) average values of $[\lambda_i(t) + \mu_i(t)]$. A weighted average is given by

$$\begin{aligned} \frac{\sum q_i(t)[\lambda_i(t) + \mu_i(t)]}{\sum q_i(t)} &= \frac{\sum N_i(t)[\lambda_i(t) + \mu_i(t)]}{\sum N_i(t)} \\ &= \frac{\sum [y_i(t) + n_i(t)]}{\sum N_i(t)} \end{aligned} \quad (48)$$

For the first state, estimates provided by Eq 48 were somewhat higher than the corresponding figures in Table 4. For the other two states, the weighted averages were almost the same as the corresponding figures in Table 5. If a fire starting in, say, bed was extinguished within any time, this event could have happened when the fire was in one of the three states; the (cumulative) probability of this event for any state is given by

$$\sum_{r=1}^t n_i(r)/N$$

where, as defined earlier, $n_i(r)$ is the number of fires extinguished in the i^{th} state during the r^{th} period and N the total number extinguished in all the three states. The parameter N is also the total number of fires (observations) in the sample analyzed. The maximum value of the cumulative probability is the ratio (n_i/N) where n_i is the total number of fires extinguished in the i^{th} state. In Figs. 3 to 6, the cumulative probabilities of extinguishment have been plotted against time that has elapsed since the commencement of the fire with the ignition of the first object. Taking Fig. 3 as an example, the probability that the fire will be extinguished in the first state within 25 min is 0.142. The probability of being extinguished in the second state within 25 min is 0.254. For any time since ignition, the probability that the fire is extinguished in the second state within that time is higher than the corresponding probabilities for the first and third states.

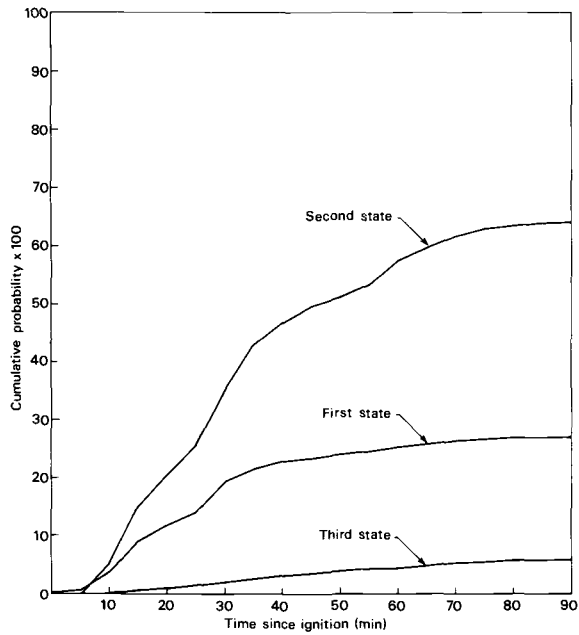


FIG. 3—Bedding—cumulative probability of extinguishment.

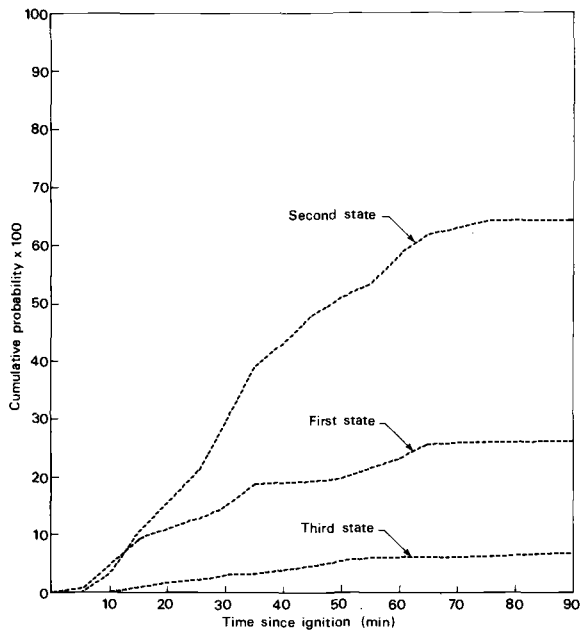


FIG. 4—Furniture—cumulative probability of extinguishment.

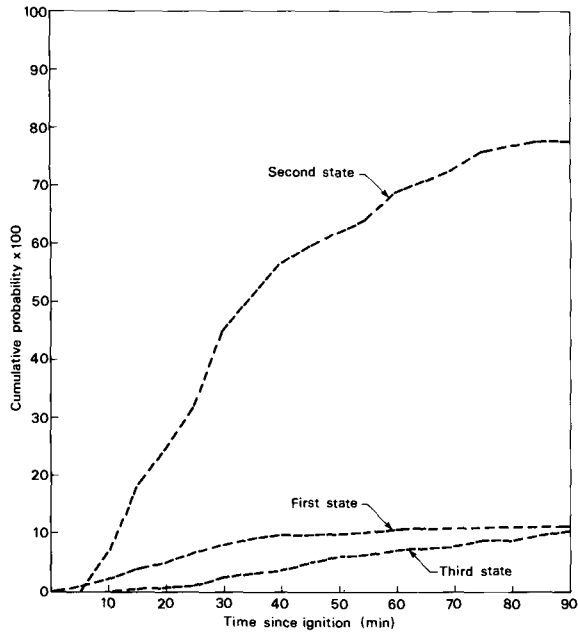


FIG. 5—*Electrical insulation—cumulative probability of extinguishment.*

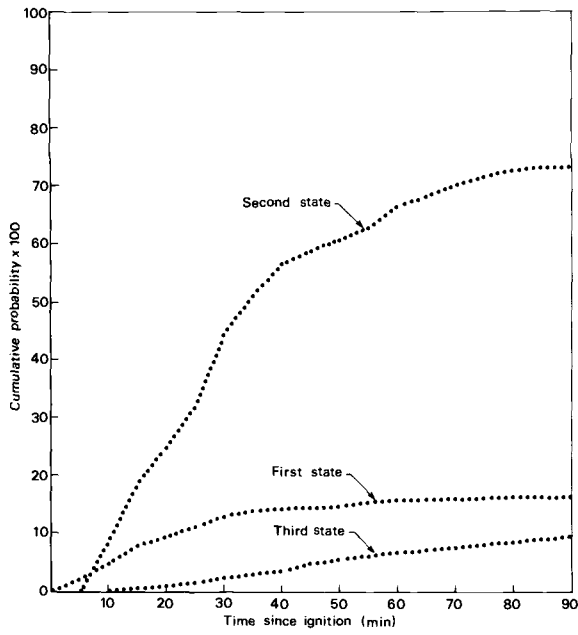


FIG. 6—*Paper, packaging—cumulative probability of extinguishment.*

We can define similarly the cumulative probability of spreading beyond the i^{th} state by

$$\sum_{r=1}^t y_i(r)/N$$

where $y_i(r)$ is the number of fires which spread from the i^{th} state (to the next state) during the r^{th} period. The maximum value of the cumulative probability of spread is the ratio (y_i/N) where y_i is the total number of fires which spread beyond the i^{th} state. The cumulative probabilities of spreading beyond the first and second states are shown in Figs. 7 to 10. Taking Fig. 7 as an example, the probability of fire spreading beyond the second state within 20 min since ignition is 0.013, which is much lower than the probability 0.282 of spreading beyond the first state within the same time. This difference in the spread probabilities of the two states increases considerably with the time that has lapsed since ignition.

In a sense Figs. 3 to 10 express unconditional cumulative probabilities for each state. The conditional cumulative probability of extinguishment given that the fire has been extinguished in the i^{th} state is estimated by

$$\sum_{r=1}^t n_i(r)/n_i$$

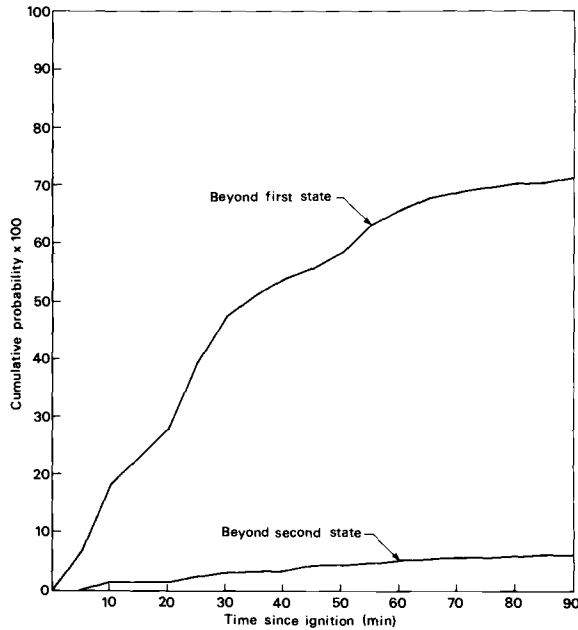


FIG. 7—Bedding—cumulative probability of spreading.

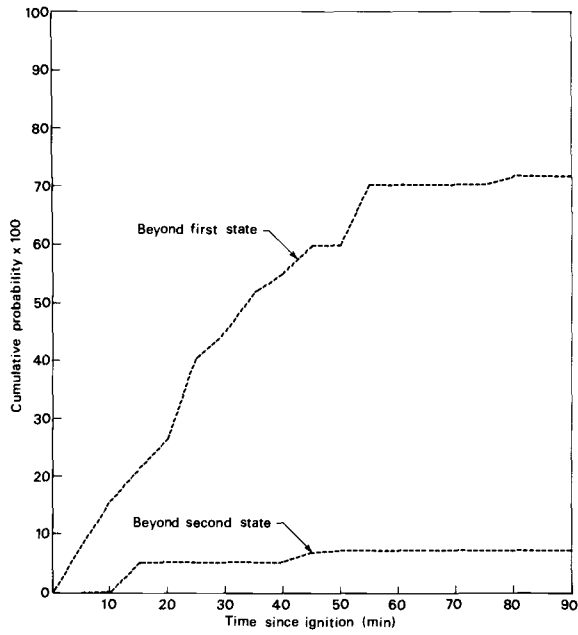


FIG. 8—Furniture—cumulative probability of spreading.

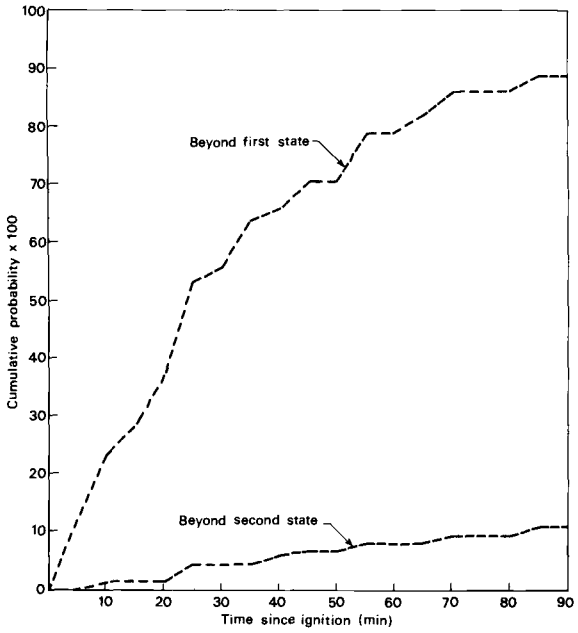


FIG. 9—Electrical insulation—cumulative probability of spreading.

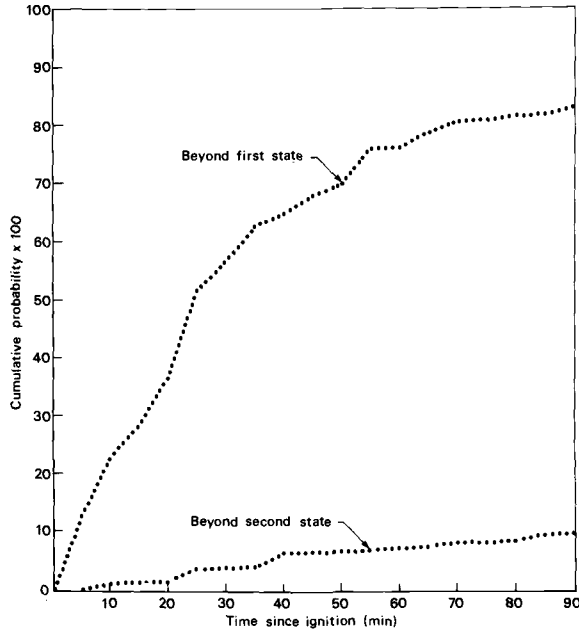


FIG. 10—Paper, packaging—cumulative probability of spreading.

Likewise, the conditional cumulative probability of spread given that the fire has spread beyond the i^{th} state is estimated by

$$\sum_{r=1}^t y_i(r)/y_i$$

The average time for extinguishment in the i^{th} state is given by

$$\bar{E}_i = \frac{\sum t q_i(t) \mu_i(t)}{\sum q_i(t) \mu_i(t)} = \frac{\sum t \cdot \frac{N_i(t)}{N} \frac{n_i(t)}{N_i(t)}}{\sum \frac{N_i(t)}{N} \frac{n_i(t)}{N_i(t)}} = \frac{\sum t n_i(t)}{\sum n_i(t)} \quad (49)$$

Where the summation extends over all time periods using the midpoints of the periods as values of t . $\bar{E}_i$ is the average time since the ignition of the first object in the first state. Results based on Eq 49 are given in Table 6. It should be noted that $\bar{E}_i$ is not the same as the average duration of burning without spreading (Tables 4 and 5). If a fire has stopped burning in a particular state, this does not imply that it has been extinguished; it could have spread to the next state.

TABLE 6—Average extinguishment time (MTS).

States	Bedding	Furniture	Electrical Insulation	Paper, Packaging
First	26.2	30.0	22.4	20.6
Second	32.2	34.4	32.0	30.6
Third	44.1	39.1	49.3	47.0

Following the derivation of Eq 49, the average time (since the start of the fire) for spreading beyond the i^{th} state is easily seen to be given by

$$\bar{S}_i = \frac{\sum_t tq_i(t)\lambda_i(t)}{\sum_t q_i(t)\lambda_i(t)} = \frac{\sum_t ty_i(t)}{\sum_t y_i(t)} \quad (50)$$

Results based on Eq 50 are given in Table 7. These figures also can be interpreted as the average time taken by the fire (since it started in the first state) to reach or arrive at the $(i + 1)^{\text{th}}$ state. In this context Aoki [17] uses the term "average arrival time."

If it is assumed that the extinguishment probabilities $\mu_i(t)$ and transition probabilities $\lambda_i(t)$ are constants, it may be seen from Eqs 49 and 50 that, in this particular case, the average extinguishment time and average spreading time are equal and are given by

$$\frac{\sum_t tq_i(t)}{\sum_t q_i(t)} = \frac{\sum_t tN_i(t)}{\sum_t N_i(t)} \quad (51)$$

Aoki [17] has derived results similar to Eq 51 using constant values for the transition and extinction probabilities and considering t as a continuous variable. Please see Table 1 of his paper where the average arrival time for phase i or average spreading time for phase $(i - 1)$ is the same as the average extinguishment time for phase $(i - 1)$.

Following Aoki, we can regard, to some extent, transition from the first state (or arrival at the second state) as corresponding to flashover. As ob-

TABLE 7—Average time for spreading beyond the state (MTS).

States	Bedding	Furniture	Electrical Insulation	Paper, Packaging
First	27.9	26.8	27.5	26.2
Second	38.9	28.9 ^a	43.8	42.5

^aBased on a small number of observations.

served by him, the average flashover times revealed by fire brigade statistics are greater than the flashover times obtained from experiments. It should be mentioned that experiments are carried out under controlled conditions, taking care that sustained burning commences as quickly as possible. But a real fire could be in a smoldering state for a considerable length of time. The probability of flashover occurring within any time for a fire starting in, say, a bed can be obtained from Fig. 7.

Stochastic Model—Building

Each room or corridor in a building has an independent probability (p_s) of fire spreading beyond its boundaries. This probability depends on the materials in a room and their spatial arrangement apart from ventilation and other factors. Two rooms, though adjoining (no distance), are separated by barriers such as walls, ceilings, and doors. Depending on the degree of fire resistance of the barriers, fire spread from one room to another would be delayed or even prevented in some cases, thus affecting the probability of spread. In this sense, the rooms in a building are connected by directed "bonds," each bond having an independent probability of blocking the passage of fire. This bond probability p_r and the probability p_s give rise to a situation which could perhaps be tackled by percolation theory, which has been considered for modelling fire spread from building to building [18].

With the probability p_s we can associate a parameter t_s to represent the time to spreading beyond the room. Instead of p_r we can use q_r ($= 1 - p_r$), the probability of not blocking to represent the probability of failure of the barrier element with t_r denoting the endurance of the barrier element as measured by a standard fire-resistance test. If fire resistance is not adequate, this will tend to increase q_r and decrease t_r .

Network theory has been considered by some authors regarding a building as a network and rooms as nodes; the links between these nodes represent possible paths for fire spread. In this connection, studies of Ling and Williamson are of special interest [19]. These authors include the element of time and probability for each link as well as the containment of fire by fire-resistive building elements. They have discussed this model in detail using as an example the floor plan of an actual high rise building transformed into a network. They have used the phenomenon "flashover" instead of the event of fire spreading beyond room. Elms and Buchanan have described the development of a computer-based technique for analyzing the spread of fire through a multicompartment building [20].

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Application of the Standard Fire Curve for Expressing Natural Fires for Design Purposes

REFERENCE: Wickström, U., "Application of the Standard Fire Curve for Expressing Natural Fires for Design Purposes," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 145-159.

ABSTRACT: A method suitable for design purposes is presented which allows postflashover compartment fires to be expressed in one single curve; time is then modified or scaled to take into consideration ventilation conditions and wall properties. Ventilation-controlled combustion, uniform temperature, and wall losses proportional to the thermal inertia of the wall material are assumed. Although the proposed method allows the designer to consider important parameters, it is still very approximative and must be used with care. Several assumptions are made, and many parameters are difficult to assign values in the general case. The method therefore should be looked upon as an improvement of the current technique of carrying out fire engineering design by simply employing a standard curve like ISO 834.

An analysis of the postflashover compartment fire, basically the same as has been presented elsewhere, is summarized. The parameters of ventilation and wall properties are grouped together in such a way that their influence on the temperature development may be expressed as a time factor. It is further shown that for certain parameter values the fire temperature response in the heating phase nearly coincides with the ISO 834 standard curve. Therefore, as this curve is well-established in fire engineering, it is suggested that it be employed with the here suggested method; the time scale, however, is modified with a compartment time factor depending on the properties of the fire compartment.

The assumed fire temperatures may be used to estimate the temperature in fire-exposed structures. It is demonstrated how very compact diagrams may be used for concrete structures, which allows the designer to take into account various fires and thermal material properties of the structure. The diagrams were obtained from finite-element computer calculations in which various fires and material properties were simulated. By then plotting the temperature response versus suitable parameter groups, it was possible to present the results in a very compact way. Corresponding diagrams have earlier been presented for insulated steel structures [1].

KEY WORDS: compartment fire, postflashover, temperature calculation, concrete

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Nomenclature

A	Opening area
a	Thermal diffusivity
A_t	Surrounding area
C_0, C_1, C_2	Constants
c	Specific heat capacity
h	Opening height
I	Rate of heat
k	Conductivity
q	Heat flux rate
T	Temperature
t	Time
t^*	Modified time
x	Depth
Y	$A\sqrt{h}/A_t/\sqrt{k\rho c}$

Greek Letters

α_1	Flow constant
α_2	Burning efficiency
Γ	Compartment time factor
γ	$\gamma^2 = \Gamma$
ϵ	Effective emissivity
η	Temperature rise ratio
Θ	Temperature rise $T - T_0$
ρ	Density
σ	Stefan-Boltzmann constant
τ_f	Time constant

Subscripts

A	Standard fire compartment
a	Thermal diffusivity
C	By combustion
c	Normal weight concrete
d	Duration
f	Fire
L	By convection
i	Thermal inertia
R	By radiation
W	Walls
x, y	Depth
O	Ambient, initial

Fire engineering design of structures and structural elements are in most cases characterized by a procedure based on a classification system and an associated standardized fire test, ISO 834, with fixed heating conditions. This test exposure simulates a fully developed fire of prescribed duration; in general, only the heating phase is considered. However, in many countries there is a growing interest in computing the fire exposure from real or natural conditions. Thus, for several years now the Swedish building code has allowed for an engineered design based on heat and mass balance analysis of fire compartments.

The fire compartment is taken to be a well-stirred reactor, with the following major assumptions:

1. The fire is ventilation-controlled.
2. The compartment is ventilated by natural convection.
3. The gas temperature is uniform in the fire compartment.
4. The fuel burns wholly inside the fire compartment.

All fires are, of course, not ventilation-controlled, and particularly in large spaces local fires may have a very severe impact on structural elements. Fires in smaller spaces severe enough to threaten structural elements tend, however, to be ventilation-controlled. Using this theory, the fire temperature in the heating phase is derived with a Laplace transform technique and compared with the ISO 834 standard time-temperature curve. It will be shown that ventilation and wall properties may be taken into account by altering the time scale, and consequently the development of fire temperatures under various conditions may be expressed in a single diagram. For a certain set of input parameters, this temperature diagram nearly coincides with the ISO 834 standard curve for the first several hours of a fire. It therefore is suggested that this well-established temperature relation also may be employed for fire engineering design when natural fires are assumed. The thermal response of concrete structures exposed to the derived fire temperatures then may be given in a very compact way. The thermal properties of the concrete structure and the fire compartment in general are different. The mathematical properties of the time-temperature function suggested here make it possible to plot surface temperature and interior temperature in single diagrams. Fire characteristics and thermal properties of the concrete are considered by modifying the time scales in suitable ways. Thus a simple method for assessing temperature in concrete with various thermal properties exposed to natural fires is obtained.

The work extends the earlier concept [1] of modified time to describe natural fires for insulated steel structures. In that paper, a general natural fire curve was defined based on the ventilation-controlled fire description of Magnusson and Thelandersson [2,3].

A different approach to rational fire engineering design has recently been introduced by Harmathy. In a series of papers (see, for example, Refs 4 and 5), the severity of fires is characterized by a single parameter named the normalized heat load, which is explicitly related to fire load, thermal inertia of com-

partment boundaries, and ventilation properties. The relation of this approach to standard furnace testing has been explored as well [6].

Temperature Analysis of Postflashover Fires

The theory of the fully developed or the postflashover compartment fire is summarized in this section. Such fires have been analyzed amply by many authors [2,3,7-9] and therefore will not be discussed in detail here.

The heat balance of a compartment fire as shown in Fig. 1 may be written as

$$I_C = I_L + I_W + I_R \quad (1)$$

where

I_C = the heat release rate by combustion,

I_L = the heat loss rate by convection out of the compartment openings,

I_W = the heat transfer to the compartment boundaries, and

I_R = the radiation rate out of the openings.

Air and combustion products flow in and out of the compartment driven by buoyancy at a rate of $\alpha_1 A \sqrt{h}$, where α_1 is a flow constant and $A \sqrt{h}$ the ventilation parameter; for a single opening compartment A and h are the opening area and height, respectively.

For a ventilation-limited fire, the combustion rate inside the compartment is proportional to the available oxygen in the incoming air

$$I_C = \alpha_1 \alpha_2 A \sqrt{h} \quad (2)$$

where

α_2 = a constant describing the burning efficiency and the combustion energy developed per unit mass of air.

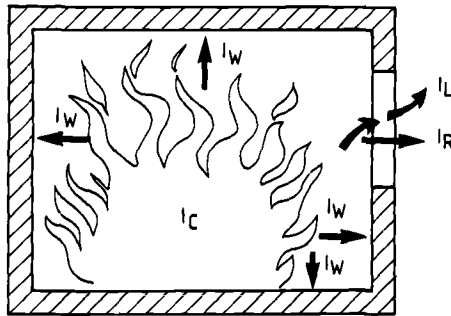


FIG. 1—Heat flow balance of a fire compartment.

The convection loss term is

$$I_L = c \alpha_1 A \sqrt{h} \Theta_f \quad (3)$$

where

c = the specific heat of air, and

Θ_f = the temperature rise of the fire above ambient conditions.

The wall loss rate is

$$I_W = A_t q_W \quad (4)$$

where

A_t = the area of the compartment boundaries, and

q_W = the heat flux rate.

For typical fire durations, the thermal thickness of most walls is so great that they may be considered semi-infinite. Thus, q_W is proportional to the thermal inertia $\sqrt{k\rho c}$ of the wall material. In this simple model constant effective values of the thermal inertia must be chosen which incorporate effects of material properties varying with temperature as well as effects of thin, exposed layers where the conditions on the backside influence the thermal response of the exposed surface. In most cases this influence is believed to be very small. The influence of the heat transfer resistance is small, and for the sake of simplicity it is ignored in this study, that is, the wall surface temperature and the fire temperature are assumed equal. The heat flux at the surface for a unit temperature rise is $\sqrt{k\rho c/\pi t}$, and q_W is obtained by convolution between this response function and the rate of temperature rise of the fire, Θ_f .

The radiation loss through the openings is

$$I_R = A q_R \quad (5)$$

where

q_r is approximated as σT_f^4 , where σ is the Stefan-Boltzmann constant and T_f the absolute fire temperature.

Inserting Eqs 2 through 5 into Eq 1, followed by rearranging the heat balance equation, yields

$$C_2 = C_1 \Theta_f(t) + \frac{1}{\sqrt{\pi}} \int_0^t \frac{1}{Y\sqrt{\xi}} \Theta_f(t - \xi) d\xi + \frac{\sigma T_f^4}{\sqrt{h}} \quad (6)$$

where

$$C_1 = c\alpha_1,$$

$$C_2 = \alpha_1\alpha_2, \text{ and}$$

$$Y = (A\sqrt{h}/A_t)/\sqrt{k\rho c}.$$

$\Theta'_f(t - \xi)$ is the time derivative of the fire temperature at the time $(t - \xi)$. The numerator, $A\sqrt{h}/A_t$, is recognized as the opening factor [3]. The concept of modified time, $t^* = \gamma^2 t$, is now introduced where the dimensionless fire compartment time factor $\Gamma = \gamma^2$ is defined by $\gamma = Y/Y_A$, where Y_A is a constant characterizing a fire compartment with standard ventilation and wall properties

$$Y_A = (0.04)/(1160) = 34.5 \times 10^{-6} \text{ m}^{5/2} \text{ K s}^{-1/2} \text{ W}^{-1} \quad (7)$$

After changing the time variables, Eq 6 yields

$$C_2 = C_1 \Theta_f(t^*) + \frac{1}{Y_A \sqrt{\pi}} \int_0^{t^*} \frac{\Theta'_f(t^* - \xi)}{\sqrt{\xi}} d\xi + \frac{\sigma T_f^4}{\sqrt{h}} \quad (8)$$

Since the height, h , of the compartment openings varies in general only within a small range and the radiation term is small, the influence of h on fire temperature is small and may be ignored. An examination of Eq 8 then reveals that the fire temperature is a universal function of modified time, t^* , only.

Thus when the time-temperature curves reported in Ref 2 were plotted versus modified time, a single curve was approximately obtained for the heating phase [1]; the curve was named the general natural fire curve.

Analytical Solution for the Ventilation-controlled Compartment Fire

If the radiation loss I_R is neglected and material properties are assumed constant, Eq 6 may be solved by employing Laplace theory and the temperature rise may be given in a closed analytical form² as

$$\Theta_f = (C_2/C_1) [1 - \exp(t/\tau_f) \operatorname{erfc} \sqrt{t/\tau_f}] \quad (9)$$

where

$$\tau_f = 1/(C_1 Y)^2.$$

Thus the fire temperature development may be expressed by a maximum temperature C_2/C_1 and a time constant τ_f . If the radiation losses are considered another parameter, $\sqrt{h}$ must in principle be introduced and an analytical solution may not be derived. The window height varies, however, within a very small range, and its influence may be included in the choice of temperature function. For $\tau_f = 0.33 \text{ h}$ and $C_2/C_1 = 1325^\circ\text{C}$ Eq 9 approximately coincides for several hours with the standard fire curve according to ISO 834, that is, $\Theta_f = 345 \log(480t + 1)$, where the time t is in hours. In Fig. 2, these two rela-

²Personal communication with Gören Sandberg, Lund Institute of Technology, Lund, Sweden.

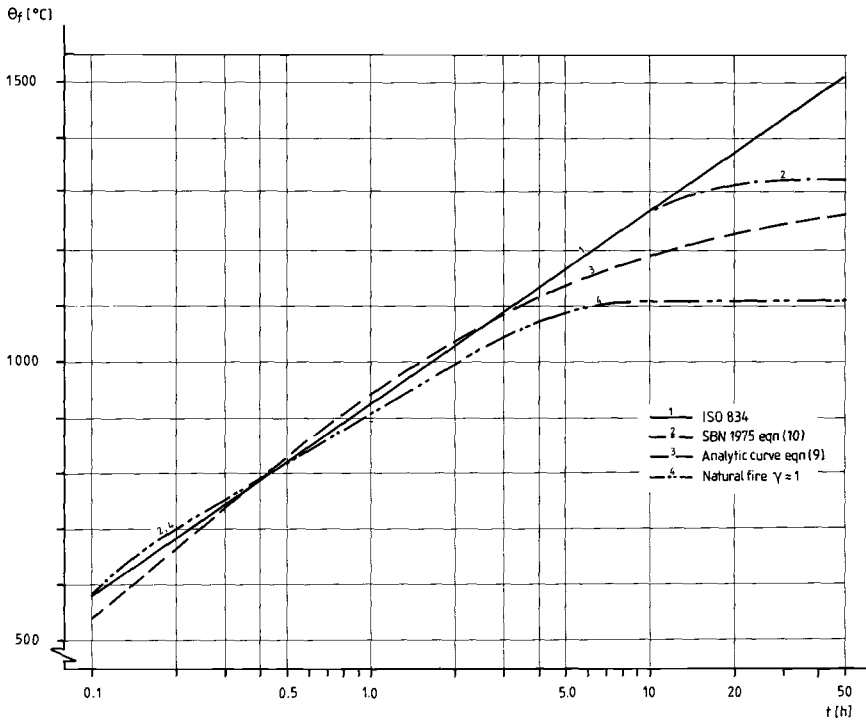


FIG. 2—Fire temperature curves.

tions are plotted as well as the general natural fire curve as defined in Ref 1 and an exponential approximation to the standard curve specified in the former Swedish building code, SBN 1975 [10]. During the first several hours, all four curves differ very little.

The Standard Fire Curve to Express Natural Fires

As the ISO curve is well-known, it would be advantageous if it could be used for design purposes together with the concept of modified time. It is, however, impossible as it goes to unrealistically high temperatures for fire durations that very well may occur in the modified time scale. Therefore, it is suggested that the exponential approximation as given in the former Swedish building code [10] may be employed, that is

$$\Theta_f = \sum_{i=0}^3 B_i \exp(-\beta_i t) \quad (10)$$

where B_i and β_i are given in Table 1.

TABLE 1—Constants in the exponential expression (Eq 10) of the ISO 834 standard curve [10].

j	0	1	2	3
$B(^{\circ}\text{C})$	1325	-430	-270	-625
$\beta (\text{h}^{-1})$	0	0.2	1.7	19

Temperature in Fire-Exposed Concrete Structures

In this section, a technique for estimating the temperature response of example concrete structures will be shown. The structure is exposed to a given fire; its thermal properties differ in the general case from those characterizing the compartment walls. The design curves given are obtained by plotting numerically calculated temperatures versus time scaled by suitable parameter groups. Any rigid mathematical derivations were not carried out.

In a first step, only semi-infinite, one-dimensional structures will be considered. The time variations of the ratios $\eta_W = \Theta_W/\Theta_f$ and $\eta_x = \Theta_x/\Theta_W$ will be shown, where Θ_f , Θ_W , and Θ_x are the temperature rises of the fire, the exposed surface, and a point inside the structure, respectively. Various fires characterized by the fire compartment time factor and various material properties of the exposed structure are taken into account.

A computer program, TASEF-2 [11,12], based on the finite-element method was employed to calculate the thermal response. The heat transfer rate per unit area by radiation from the fire to the structure is assumed equal to $\epsilon \sigma (T_f^4 - T_W^4)$, where ϵ is an effective emissivity, σ the Stefan-Boltzmann constant, and T_f and T_W the absolute temperatures of the fire and the surface, respectively. The convection to the walls is assumed equal to $h(T_f - T_W)$, where h is the heat transfer coefficient. In all calculations reported here, $\epsilon = 0.7$ and $h = 25 \text{ W m}^{-2}\text{K}^{-1}$.

The plots shown below are based on computer analyses of one-dimensional, semi-infinite structures where the assumed material properties, as given in Table 2, simulate normal-weight concrete and light-weight concrete, respectively.

TABLE 2—Assumed material properties of normal-weight concrete and light-weight concrete [13].

	Normal-Weight Concrete	Light-Weight Concrete
Density, ρ (kg m^{-3})	2400	500
Specific heat capacity, c ($\text{J kg}^{-1} \text{K}^{-1}$)	1000	1000
Thermal conductivity, k ($\text{W m}^{-1} \text{K}^{-1}$)	1.0 ^a	0.3
Thermal inertia, $\sqrt{(k\rho c)}$ ($\text{W m}^{-2} \text{s}^{-1/2}$)	1550	387
Thermal diffusivity, $a \times 10^9$ ($\text{m}^2 \text{s}^{-1}$)	417	600
($\text{m}^2 \text{h}^{-1}$)	0.116	0.167

^aVariations in thermal conductivity prescribed by Ref 13 are shown in Fig. 3.

The values are taken from the Swedish Building Code [13]. In some cases the thermal conductivity of concrete is assumed constant with the purpose of testing the theory and creating the graphs for material with constant material properties in general.

Figure 3 shows that for a given material the ratio $\eta_W = \Theta_W/\Theta_f$ may approximately be given as a function of γt only. Of η_W is plotted versus $t_W = (\gamma/\gamma_i) t$ as in Fig. 4, the material properties of the actual structure may be included in one diagram as well. γ_i is the ratio between the thermal inertia of the material of the actual structure and that of normal-weight concrete (see Table 2)

$$\gamma_i = \sqrt{(k\rho c)/\sqrt{(k\rho c)_c}} \quad (11)$$

The surface temperature rise may now be estimated as

$$\Theta_W = \eta_W \Theta_f \quad (12)$$

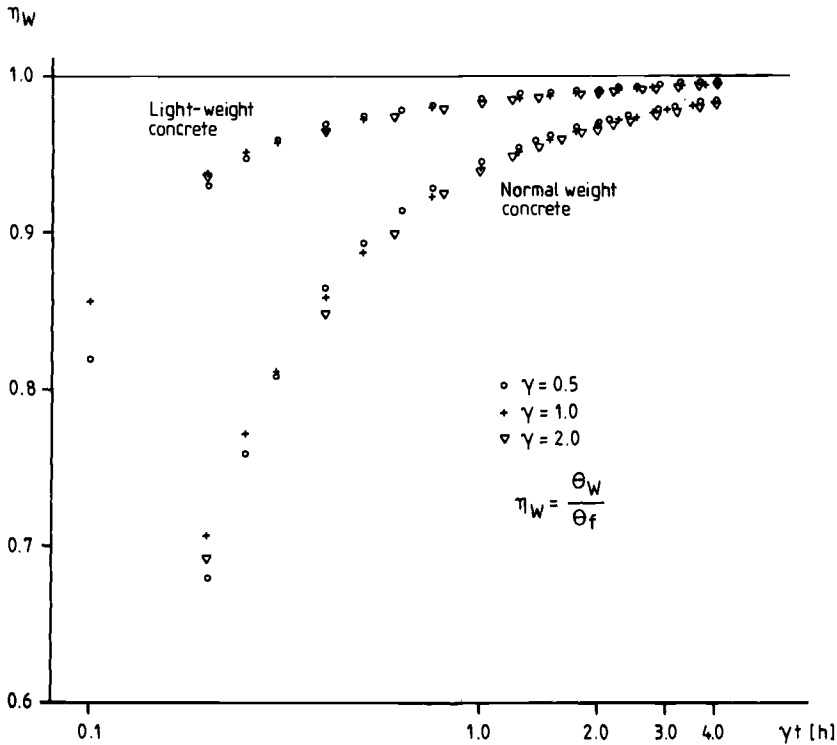


FIG. 3—The ratio η_W between the surface temperature rise Θ_W and the fire temperature rise Θ_f plotted versus γt . The thermal conductivity of normal weight concrete varied according to Fig. 3.

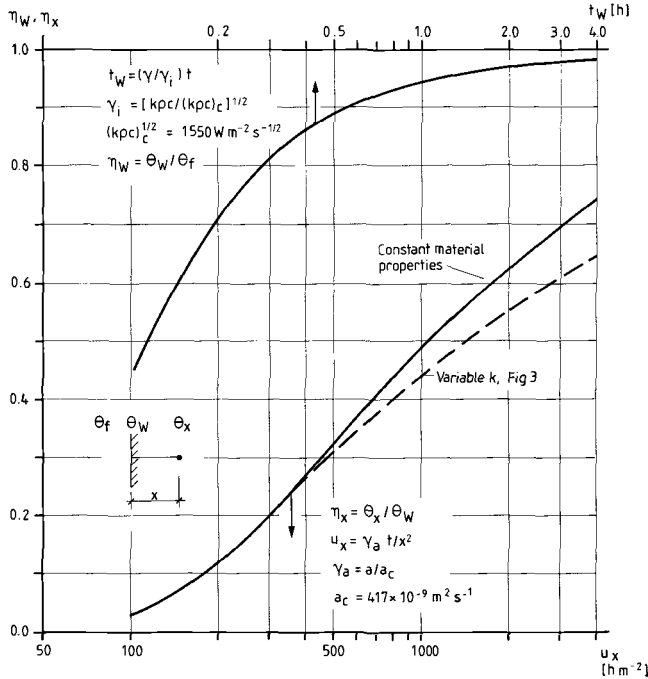


FIG. 4—Upper curve and upper horizontal axis: the ratio η_w versus $t_w = (\gamma/\gamma_i)t$. Lower curve and lower horizontal axis: the ratio η_x versus $\mu_x = \gamma_a t/x^2$.

The temperatures obtained with this approximation differ by less than a few percent from the values obtained with TASEF-2.

Inside a structure the temperature rise Θ_x at a distance x from the surface may also be obtained from Fig. 4 where the ratio $\eta_x = \Theta_x/\Theta_w$ is plotted versus $\mu_x = \gamma_a t/x^2$. The coefficient γ_a is the ratio between the thermal diffusivity of the actual structure and of normal-weight concrete (see Table 2)

$$\gamma_a = a/a_c \quad (13)$$

When assuming constant material properties, η_x lies within 1 or 2% from the full line. Many combinations were checked with x equals 0.04 m and 0.10 m and t up to four h. For concrete structures with a varying conductivity, according to Fig. 5, the results follow a different graph (dashed line in Fig. 4). Still, the accuracy is within a few percent. Thus, the temperature at an internal point may be calculated as

$$\Theta_x = \eta_x \Theta_w \quad (14)$$

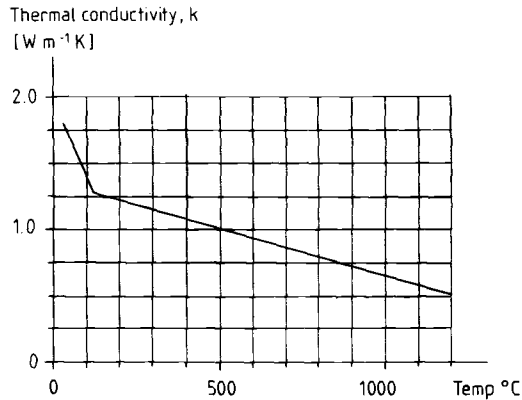


FIG. 5—Thermal conductivity of normal weight concrete as a function of temperature [13].

and by inserting Eq 12

$$\Theta_x = \eta_x \eta_W \Theta_f \quad (15)$$

Observe that the approximations as outlined here are particularly simple to use when analyzing normal weight concrete structures exposed to the standard fire curve, that is γ , γ_i , and γ_a are equal to unity.

Other Geometrical Configurations

So far only thermally semi-infinite, one-dimensional bodies have been studied; a wall is thermally semi-infinite if the second boundary has negligible influence on the temperature development in the area considered. This condition is satisfied when the penetration depth δ is less than the thickness of the wall plus the distance back to the point to be analyzed (see Fig. 6). The wall

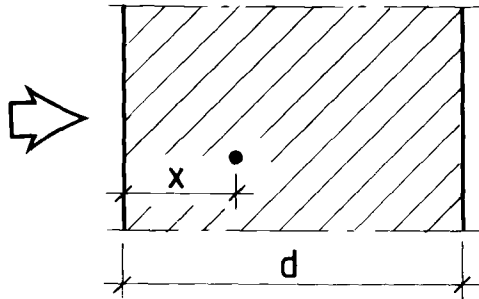


FIG. 6—When estimating the temperature at the distance x from the exposed surface, the wall may be assumed infinitely thick if the penetration depth $\delta > (2d - x)$, where $\delta = 3.6\sqrt{at}$.

may be assumed semi-infinitely thick if δ is less than $2d - x$. The penetration depth may be estimated as

$$\delta = 3.6 \sqrt{at} \quad (16)$$

In the case of a wall exposed to a fire from both sides, the temperature response from each side may be superimposed and η_x is approximated by $\eta_x^{\text{left}} + \eta_x^{\text{right}}$, where the distance from the two sides is considered.

Two-dimensional square corners may as well be analyzed by superposition with contributions from the two perpendicular boundaries. The temperature right in the corner edge is assumed equal to the fire temperature, and the formula

$$\Theta_{xy} = [\eta_w (\eta_x + \eta_y - 2\eta_x \eta_y) + \eta_x \eta_y] \Theta_f \quad (17)$$

is derived where Θ_{xy} is the temperature rise at the distances of x and y to the two perpendicular surfaces, respectively. The values of η_x and the corresponding η_y are obtained from Fig. 4 as just described for one-dimensional configurations.

Average Temperature of Reinforcement Bars at Different Distances from Exposed Surface

In some cases it is of interest to calculate an average temperature of a group of reinforcement bars embedded at various depths. This of course could be carried out by calculating the temperature of each bar of interest, but a more convenient way is to calculate an effective depth x and then calculate the temperature at that point. Figure 4 shows that the temperature rise is linear with x in a logarithmic scale.

As the equation $\log(t/x^2) = [\log(t/x_1^2) + \log(t/x_2^2)]/2$ yields $x = (x_1 x_2)^{1/2}$, the effective distance x is in the general case the geometric mean value

$$x = (x_1 x_2 x_3 \dots x_n)^{1/n} \quad (18)$$

where x_i is the distance to the surface of each of the reinforcement bars. In the case of bars of various areas A_i , the weighted effective distance is obtained as

$$x = (x_1^{A_1} x_2^{A_2} x_3^{A_3} \dots x_n^{A_n})^{1/A} \quad (19)$$

where A is the sum of A_i .

Observe that it is an approximation on the unsafe side to assume x as the arithmetic mean value. The arithmetic mean value of 2 and 8 is, for instance, 5, while the corresponding geometric mean value is not more than 4.

Maximum Temperatures Reached During a Complete Compartment Fire

When designing a structure exposed to natural fire conditions, the cooling phase must in general be included. The heating phase duration is dependent

on the amount and the characteristics of the fuel load and the ventilation conditions. The assumption that the fire is ventilation-controlled and that the fuel burns wholly inside the compartment implies that the heating phase duration is proportional to the fuel load density divided by the ventilation factor; although very crude, such an estimate is suggested, for example, in the Swedish building code. The fuel load density is defined as the total heat of combustible material per unit internal surface area of the compartment. While during the heating phase the temperature may be estimated as just described, a good estimate during the cooling phase is more difficult to express in a general way.

In the international standard ISO 834, the cooling phase temperature is assumed to decline linearly with time; the rates vary with the fire durations. These decay curves are rough approximations of natural fires [2], and, as the fire temperature in the cooling phase is hard to estimate on a sound physical basis, it is here suggested that the linear time temperature relation as described in ISO 834 should be employed for the type of analysis introduced here as well.

The maximum temperature at a point in a structure exposed to a complete fire history is not reached until some time has elapsed into the cooling phase. Figure 7 shows an example of how the maximum temperature rise may be obtained in a structure exposed to a fire with a heating phase duration $t_d = 1$ h, followed by a cooling phase according to ISO 834. An unlimited fire is simulated and $\Theta_{\max}$ is calculated at the time $t = t_d + \Delta t$ where Δt depends on the depth of the point considered. By experience from several computer analyses, the following approximate estimation is obtained

$$\Delta t = \mu x^2/a \quad (20)$$

With $\mu = 0.34$, this approximation procedure predicts the maximum temperature with an accuracy of about 5% on the condition that the predicted temperature is over 400°C and the fire duration is less than 1 h.

Examples

Estimate the temperature rise 4 cm into a semi-infinitely thick concrete wall after a fire duration, $t_d = 1$ h. Assume standard material properties as given in Table 2 with variable thermal conductivity as shown in Fig. 5 ($\gamma_i = \gamma_a = 1$), and a fire characterized by a fire compartment time factor, $\Gamma = 4$.

Start by calculating the modified fire duration $t_d^* = 4$ h and get $\Theta_f = 1130^\circ\text{C}$ from Fig. 2; then calculate $t_w = \gamma t_d = 2$ h and $\mu_x = t_d/x^2 = 625$ h m^{-2} and get from Fig. 4 $\eta_w = 0.97$ and $\eta_x = 0.345$, respectively. Now the temperature rise is calculated as $\Theta_x = \eta_x \eta_w \Theta_f = 379^\circ\text{C}$. A finite-element analysis of the same example gave $\Theta_x = 380^\circ\text{C}$.

Then calculate the maximum temperature at the same point if the cooling phase temperature development is according to ISO 834.

A time increment $\Delta t = 0.36$ h is calculated according to Eq 20. Thus, the effective time is 1.36 h or in the modified time scale 5.44 h. Now continue the cal-

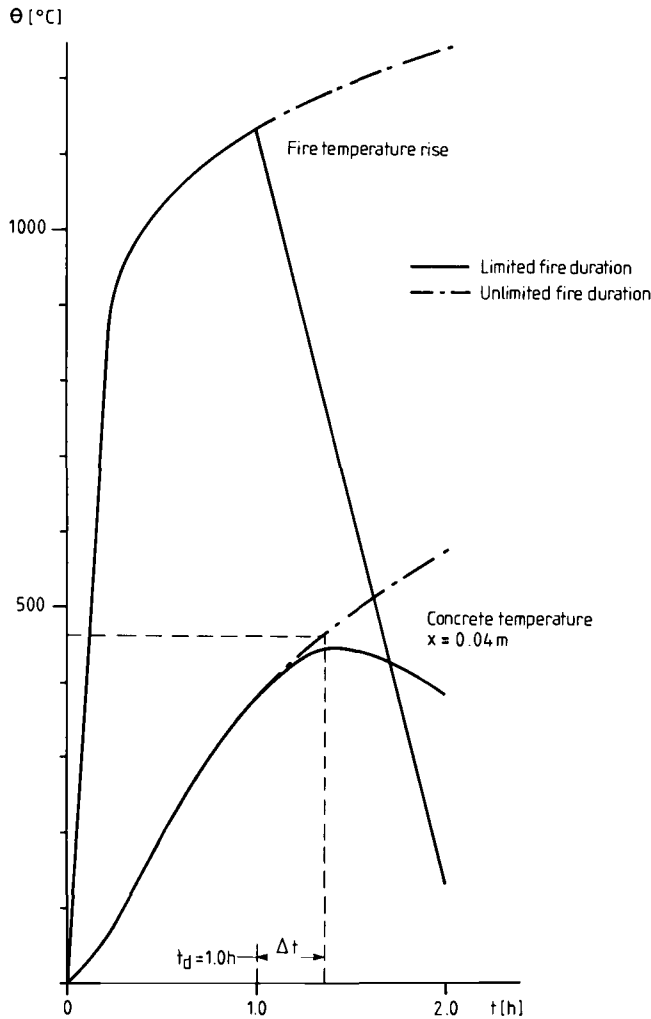


FIG. 7—The maximum temperature is estimated by increasing the fire duration and carrying out the analysis at a fictitious time ($t_d + \Delta t$). The graphs are obtained from finite-element computer calculations [11].

culatation as above and get the maximum temperature $\Theta_x^{\max} = 446^\circ\text{C}$. A computer analysis gave $\Theta_x^{\max} = 446^\circ\text{C}$.

Finally, calculate the temperature rise in a corner of a concrete structure at a point $x = 4\text{ cm}$ and $y = 6\text{ cm}$ from the boundaries after a 2-h exposure to the standard fire curve ($\Gamma = 1$). With the same procedure as above $\Theta_f = 1028^\circ\text{C}$, $\eta_w = 0.97$, $\eta_x = 0.475$, and $\eta_y = 0.325$. Then Eq 17 yields $\Theta_{xy} = 649^\circ\text{C}$. A corresponding computer analysis gave $\Theta_{xy} = 650^\circ\text{C}$.

Summary

The fully developed compartment fire has been analyzed, and the suggestion is made that the standard fire curve according to ISO 834 may be used as a basis for describing natural fires. The influence of ventilation condition and thermal properties of surrounding structures is considered by modifying the time scale. However, to avoid unrealistically high temperature for modified fire durations exceeding 10 h, the logarithmic expression of the ISO curve is replaced by a sum of exponential terms.

It has also been shown how the temperature in fire-exposed concrete structures can be estimated by employing simple diagrams, Figs. 2 and 4. Various fire conditions and material properties may be considered. One- and two-dimensional structures were studied and the suggested approximation procedures were shown to yield results that only deviated a few percent from the results obtained by comprehensive computer analyses.

Acknowledgments

Stig Andersson was most helpful with the calculation work of this paper. His contribution to the design and plotting of the diagrams is also greatly appreciated.

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Design of Buildings for Prescribed Levels of Structural Fire Safety

REFERENCE: Harmathy, T. Z. and Mehaffey, J. R., "Design of Buildings for Prescribed Levels of Structural Fire Safety," *Fire Safety: Science and Engineering, ASTM STP 882*, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 160-175.

ABSTRACT: Two techniques are outlined for designing buildings to meet prescribed probabilities of structural failure in fire. They require input information for seven variables; three of these variables, fire load, ventilation, and (experimentally determined) fire resistance, are random variables. Information on the randomness of the first and third variables is available. The second variable, ventilation, is taken into account with its most adverse value.

KEY WORDS: destructive spread potential, failure probability, fire load, fire resistance, fire-resistance requirements, fire safety design, normalized heat load, real-world fire, second moment analysis, specific fire load, standard fire test, target failure probability, test fire, ventilation factor

Nomenclature

A	Area, m^2
g	Gravitational acceleration, $m\ s^{-2}$
h	Height, m
H	Normalized heat load, $s^{1/2}\ K$
H'	Normalized heat load under real-world conditions, $s^{1/2}\ K$
$\bar{H}'$	Mean of H' , $s^{1/2}\ K$
H''	Normalized heat load pertaining to experimentally derived fire resistance, $s^{1/2}\ K$
$\bar{H}''$	Mean of H'' , $s^{1/2}\ K$
$\sqrt{k\rho c}$	Thermal inertia, $J\ m^{-2}\ s^{-1/2}\ K^{-1}$
L	Specific fire load, $kg\ m^{-2}$

¹Section head and research officer, respectively, Fire Research Section, Division of Building Research, National Research Council, Canada.

$\bar{L}$	Mean of L , kg m^{-2}
P_f	Failure probability (conditional), dimensionless
q	Heat flux penetrating the boundaries of an enclosure, W m^{-2}
t	Time, s

Greek Letters

β	Factor, dimensionless
δ	Factor, defined by Eq 6, dimensionless
ρ_a	Density of atmospheric air, kg m^{-3}
σ	Standard deviation (various dimensions)
τ	Fire resistance; time of exposure to standard test fire, h
$\bar{\tau}$	Mean of τ , h
τ^*	Duration of real-world fire exposure, s
Φ	Ventilation factor, kg s^{-1}

Subscripts

C	Of compartment
d	Design value
F	Of floor
H'	Pertaining to H'
H''	Pertaining to H''
i	Pertaining to the i th boundary surface
L	Pertaining to L
τ	Pertaining to τ
max	Maximum
min	Minimum
t	Total for the compartment
V	Of the ventilation opening
80	Pertaining to the 80th percentile
95	Pertaining to the 95th percentile

Specifications of fire safety measures in buildings are made largely by the writers of building codes, who rely heavily on their expertise and ability to weigh the relative merits of various solutions. Traditional solutions, borne out by favorable experience over long periods, are usually regarded as standards against which alternatives are judged.

The concept of fire-resistant compartmentation has long been the core of fire safety measures. A building is pictured as compartments perfectly isolated from one another and the spread of fire as taking place by successive destruction (or possibly thermal failure) of the compartment boundaries. If the boundaries are of sufficient fire resistance, it is argued, the probability of fire spread will remain within acceptable limits.

In making decisions about fire-resistance requirements, Ingberg's [1] guide is tacitly followed. He claimed that the severity of compartment fires depends solely on the specific fire load (that is, the amount of combustible materials per unit floor area); he suggested, therefore, that decisions regarding fire-resistance requirements be linked to the specific fire load. Suspecting, correctly, the shortcomings of this suggestion, code writers have tended to inflate the fire-resistance requirements specified in building codes over those suggested by Ingberg's guide.

Research over the last 25 years has proved that the concept of fire-resistance compartmentation and Ingberg's guide are both untenable. It has been found that building fires spread mostly by convection (advance of flames and hot gases) rather than by destruction. As to Ingberg's guide, to link the fire-resistance requirements to the specific fire load alone is overly simplistic. It appears now that the present practice of providing fire safety is not only misdirected but, largely because of the inflated fire-resistance requirements, wasteful as well.

Countering the potential of fires to spread by convection should be the main thrust of fire safety measures, although that subject will not be argued here. Rather, this paper will discuss how countering the destructive spread potential of fires can be done in a cost-effective manner. It is believed that fire-resistant compartmentation is responsible for a significant part of the cost of fire protection. The authors will introduce a technique for calculating the fire-resistance requirements for a building in such a way as to restrict the probability of failure of its elements to a prescribed level. Building designers and code writers will be able to examine the problem of structural fire safety in the light of economic considerations: to balance the cost of fire protection against the value at risk.

Concept of Normalized Heat Load

Detailed studies have shown [2-4] that the potential of a compartment fire to spread by destruction can be quantified by a single parameter, "normalized heat load," H , $\text{s}^{1/2} \text{K}$, which is defined as

$$H = \frac{1}{\sqrt{k\rho c}} \int_0^{\tau^*} q dt \quad (1)$$

where

q = the heat flux penetrating the boundaries of an enclosure (building compartment or test furnace) exposed to fire, W m^{-2} ,

t = time, s,

τ^* = duration of fire exposure, s, and

$\sqrt{k\rho c}$ = the thermal inertia of the enclosure boundaries, $\text{J m}^{-2} \text{s}^{-1/2} \text{K}^{-1}$

k = thermal conductivity,
 ρ = density, and
 c = specific heat.

Normalized heat load is the heat absorbed during fire exposure by the boundaries of the enclosure (per unit surface area) divided by the thermal inertia of the boundaries. If, as is usual, the boundaries of the enclosure are formed by a number of different materials, the thermal inertia should be looked upon as a surface-averaged value

$$\sqrt{k\rho c} = \frac{1}{A_t} \sum_i A_i \sqrt{k_i \rho_i c_i} \quad (2)$$

where

$$A_t = \sum_i A_i \quad (3)$$

In these expressions,

A_i = the surface area of the i th boundary of the enclosure, m^2 ,
 A_t = the total area of the enclosure boundaries, m^2 , and
 k_i, ρ_i, c_i = the properties of the material that forms the i th boundary surface.

If no information is available on the shape of the compartment, A_t can be estimated as

$$A_t = 2A_F + 4\sqrt{A_F}h_C \quad (4)$$

where

A_F = floor area, m^2 , and
 h_C = the height of the compartment.

Table 1 lists the thermal inertia for a number of compartment lining materials. (The values represent averages applicable to the usual temperature in-

TABLE 1—*Thermal inertias of compartment boundary materials.*

Material	$\sqrt{k\rho c}$, $\text{J m}^{-2} \text{s}^{-1/2} \text{K}^{-1}$
Marble	2273
Normal-weight concrete	2192
Brick	1520
Lightweight concrete	931
Plasterboard	742
Vermiculite plaster	667
Wood	436
Mineral wool	86

tervals encountered in fires.) In the event of lack of information on compartment size and lining materials, $\sqrt{k\rho c} \approx 700 \text{ J m}^{-2} \text{ s}^{-1/2} \text{ K}^{-1}$ may be used for common plasterboard-lined compartments.

According to the theorem of uniformity of normalized heat load, H is approximately the same for the fire enclosure as a whole as for the individual boundary elements. This implies that the destructive spread potential of fire is the same for all boundary elements of the compartment. (The concept of normalized heat load is not applicable to boundary elements made entirely from materials of high thermal inertia, for example, from unprotected steel or aluminum.)

Drawing on the concept of normalized heat load, determining fire-resistance requirements is as simple as requiring that building elements be able to endure, in a test fire, a normalized heat load (denoted by H'') equal to or greater than the normalized heat load expected to be imposed on the same building elements in real-world fires (denoted by H'). In reality, the problem is somewhat more complex. To understand why, it is necessary to examine the variables on which H' depends and the factors that cloud the meaning of H'' .

Real-World Fires

The most important variables on which the normalized heat load in real-world compartment fires depends are [4]:

- A_F Floor area of compartment, m^2
- A_t Total area of compartment boundaries, m^2
- h_C Height of compartment, m
- $\sqrt{k\rho c}$ Surface-averaged thermal inertia of compartment boundaries, $\text{J m}^{-2} \text{ s}^{-1/2} \text{ K}^{-1}$
- Φ Ventilation factor characterizing the rate of air flow into the compartment, kg s^{-1}
- L Specific fire load (mass of combustibles per unit floor area), kg m^{-2}

Values for the first three variables are available from building plans. The fourth may be calculated from the planned use of building materials. The last two, ventilation factor and specific fire load, are random variables. Ventilation may vary with climatic conditions, building height, and other factors related to the building design and state of the compartment at the outbreak of fire. There is little information on its frequency distribution; it is probable that values close to its minimum (to be discussed later) occur with high frequency.

The specific fire load is largely a function of building occupancy, but even for the same occupancy it may vary in an unpredictable fashion. A typical cumulative plot is shown in Fig. 1. It is assumed, mainly for convenience, that the frequency distribution of specific fire load follows the normal distribution pattern, acknowledging that other choices may be slightly more accurate.

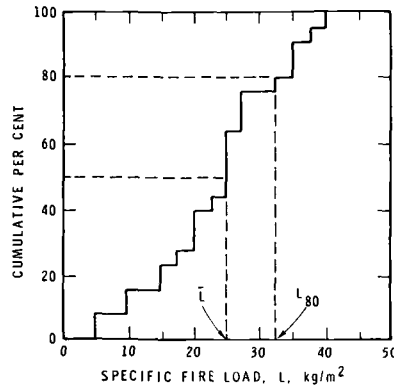


FIG. 1—Customary presentation of information on specific fire load.

In room-burn experiments, the values of the ventilation factor and specific fire load can be carefully set. Under such conditions the following approximate equation is applicable to the normalized heat load [4]

$$H' = 10^6 \frac{11.0\delta + 1.6}{A_t \sqrt{k\rho c} + 935 \sqrt{\Phi} L A_F} (L A_F) \quad (5)$$

where δ (dimensionless) is, in a loose sense, the fraction of the fuel energy released in the flaming combustion inside the fire compartment. (This interpretation of δ is strictly appropriate only for ventilation-controlled fires. At very high air flow rates δ becomes a correction factor.) An approximate equation for δ is

$$\delta = \begin{cases} 0.79 \sqrt{h_c^3 / \Phi} \\ 1 \end{cases}, \quad \text{whichever is less} \quad (6)$$

The mathematical model on which these two equations are based has been discussed and compared with 13 other models in Ref 6.

Although Eqs 5 and 6 were derived for compartments with cellulosic fire loads, their application to compartments with noncellulosic fire loads will result in an error on the safe side [5]. A recent experimental study, not yet reported, has revealed that for cellulosic fire loads the average error of H , as calculated from Eqs 5 and 6, is 7%.

The possibility of calculation error will not be taken into account in the design because, as will be discussed later, any such error is believed to be sufficiently compensated for (1) by selecting an adverse value for ventilation in the design, and (2) by a safety margin inherent in the practice of selecting suitable building elements to match the design requirements.

As Φ and L are random variables, by virtue of Eqs 5 and 6 the normalized heat load in real-world compartment fires, H' , is also a random variable.

Test Fires

As the temperature history of standard test fires is uniquely defined, the normalized heat load on specimens of building elements, H'' , is a function solely of the duration of fire test. (This statement is only approximately correct. There are secondary factors that may influence the nature of the relation between H'' and the duration of a fire test [3].) That function is shown in Fig. 2 for a highly efficient test furnace heated with "black" gases (Curve 1) and for the floor test furnace of the Division of Building Research, National Research Council, Canada laboratory (Curve 2). Here τ denotes length of exposure to the test fire in hours. If the specimen performs satisfactorily during that time, τ quantifies the fire resistance of the building element the specimen represents.

The relation between normalized heat load in standard fire tests and length of testing is described by the following equation

$$\tau = 0.11 + 0.16 \times 10^{-4} H'' + 0.13 \times 10^{-9} (H'')^2 \quad (7)$$

This is an approximate representation of Curve 2 in Fig. 2. (If the specimen does not fail during that period, τ is also the measure of fire resistance.)

When fire resistance is determined in standard fire tests, the test value, τ , is a random variable. (For some elements the fire resistance can be determined by calculation.) Its randomness is due to two factors: (1) the test specimen may not be fully representative of its real-world counterpart, mainly because of differences in workmanship or material properties; and (2) the measured

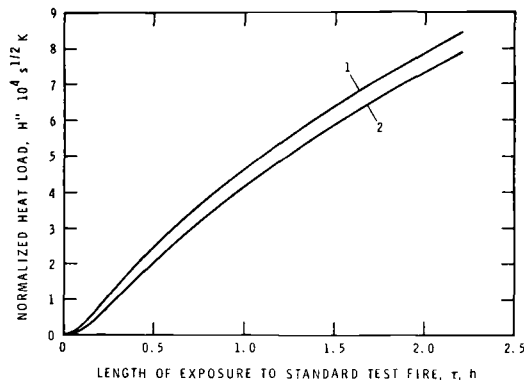


FIG. 2—Correlations between H'' and τ for standard fire tests: Curve 1—high-efficiency furnaces; Curve 2—floor test furnace at DBR/NRCC (estimated).

fire resistance, on account of small differences in fire test facilities or loading practices, may show some variation from one laboratory to another. It is again assumed, mainly for convenience, that the frequency distribution follows the normal distribution pattern. That pattern is defined by two variables: $\bar{\tau}$, the mean of fire resistance (successful fire exposure) times obtained by performing a multitude of tests on the same building element, in hours, and σ_{τ} , the standard deviation from $\bar{\tau}$, in hours. According to an ASTM study,² the coefficient of variation, $\sigma_{\tau}/\bar{\tau}$, is somewhere between 0.01 and 0.15, depending on the type of building element. It may be much larger, however, for a few specific types of construction, for example, for prestressed concrete beams. Because of the randomness of τ , clearly H'' is also a random variable.

Design Fire-Resistance Requirement

As fire safety design must address reasonably adverse conditions that may arise in real-world compartment fires, the design values of the two random variables, Φ and L , should be selected to represent such adverse conditions. A glance at Eq 5 will reveal that the normalized heat load, H' (that is, the destructive potential of real-world fire), decreases monotonically with increase in ventilation factor, Φ . Thus, as far as ventilation is concerned, the most detrimental conditions occur at low values of Φ . Two questions arise: (1) Is there a well-defined minimum for Φ and, if so, (2) is the design arrived at by the use of that minimum overly conservative? The answer to the first question is yes. The rate of air flow into the compartment (approximately proportional to Φ) is a minimum when it is not augmented by drafts or winds. Under such conditions the ventilation factor is determined by the dimensions of the ventilation opening, as follows

$$\Phi_{\min} = \rho_a A_V \sqrt{gh_V} \quad (8)$$

where

- ρ_a = density of atmospheric air, kg m^{-3} ,
- A_V = the area of ventilation opening, m^2 ,
- g = gravitational constant, m s^{-2} , and
- h_V = the height of the ventilation opening, m.

By substituting different values of Φ in Eq 5 one finds that under normal circumstances the maximum value of H' (which occurs when $\Phi = \Phi_{\min}$) is rarely more than twice that which may arise at very high ventilations (when Φ may be as high as five or six times $\Phi_{\min}$). It follows that the answer to the

²ASTM E05.11 task group report, "Repeatability and Reproducibility of Results of ASTM E 119 Fire Tests Research," Report No. RR: E5-1003, available from ASTM, Philadelphia, PA, 1982.

second question is no. It is reasonable, therefore, that $\Phi_{\min}$, as defined by Eq 8, be regarded as input value for fire safety design.

Focusing attention now on the other random variable, specific fire load, L , Eq 5 shows that the normalized heat load increases monotonically with increase in L . Again: (1) Is there an absolute maximum for L , and, if so, (2) is it reasonable to use this maximum in design considerations? The answer to the first question is yes. Clearly, the combustibles that can be present in a compartment are limited by the volume of the compartment. $L_{\max}$ is about 900 kg m^{-2} ; but if the compartment is filled from top to bottom with combustibles, they cannot burn for lack of air. Thus, the answer to the second question is no.

It is estimated that even if the compartment is used as a storage room, the combustibles that can burn in a fire cannot be much more than about 150 kg m^{-2} . This value is still overly conservative for design. Statistical data indicate that, depending on the type of occupancy, the specific fire load has a mean between 15 and 45 kg m^{-2} and rarely exceeds 100 kg m^{-2} .

How, then, should a design value be selected? If the mean value $\bar{L}$ is selected (see the cumulative plot in Fig. 1), the design may be inadequate in roughly 50% of the cases. If a value much higher than $\bar{L}$ is selected, the cost of fire protection may greatly increase. Specific fire loads applicable to either 80 or 95% of cases are usually considered as potential design values. (The meaning of this 80th percentile, L_{80} , is illustrated in Fig. 1.)

Assuming normal distribution, the design value of L_d can be described by the following equation

$$L_d = \bar{L} + \beta \sigma_L \quad (9)$$

where

$\bar{L}$ = the mean of specific fire load, kg m^{-2} (see Fig. 1), and
 σ_L = standard deviation, kg m^{-2} .

(Information on $\bar{L}$ and σ_L , based on Swedish data [7], is presented in Table 2.) β is a dimensionless factor whose value depends on the percentile specific

TABLE 2—Information on fire load [7].

Specific Fire Load, kg m^{-2}		
Occupancy	Mean, $\bar{L}$	Standard Deviation, σ_L
Dwelling	30.1	4.4
Office	24.8	8.6
School	17.5	5.1
Hospital	25.1	7.8
Hotel	14.6	4.2

fire load (in the cumulative plot) selected for the design. The relation between β and the percentile fire load in the cumulative plot is shown in Fig. 3. (For the 80th and 95th percentiles, L_{80} and L_{95} , $\beta = 0.84$ and 1.64 , respectively.)

After deciding on the design values for Φ and L (namely, $\Phi_d = \Phi_{\min}$ and $L_d = L_{80}$ or L_{95} , or anything else compatible with the design considerations), one can proceed to calculate the design value of the normalized heat load on the compartment in question, H' (Eqs 5 and 6), that is, the destructive potential of a "design" fire. Clearly, specimens of the compartment boundaries must be capable of withstanding the same destructive potential in standard fire tests. By substituting H' for H , Eq 7 becomes an expression for the calculation of the "design" fire resistance requirement, τ_d .

In general, the fire resistance of the building elements actually used greatly exceeds the value contemplated by design. The fire resistance of various building elements is always listed by the testing laboratories in rounded-down values: 0.5, 0.75, 1.0, 1.5, 2.0, . . . h. Assume that the design value for fire resistance is $\tau_d = 1.12$ h. The applicable building elements have to be selected from among those whose fire resistance is listed as 1.5 h. Yet, because of the rounding, a building element listed as having a fire resistance of 1.5 h usually has a fire resistance somewhere between 1.5 and 2.0 h, or, in the present example, 34 to 79% higher than the design value.

Thus, the very system of listing the (experimentally determined) fire resistance of building elements and the practice of selecting components for a building from among those on the list may ensure a quite substantial, though usually unknown, margin of safety as to the ability of the selected elements to withstand the spread of fire by destruction. This margin of safety will not be taken into consideration in the discussions to follow.

Designing for Target Failure Probability

Because three of the input variables needed in the design and selection process, namely, L , Φ , and τ (if determined by test), are random variables, the design fire-resistance requirement, τ_d , is subject to some uncertainty. The question that arises is this: How can the values of these random variables be selected such that the probability of failure of the building elements in fire will remain below a specified value? Two methods, discussed earlier in detail [8], will be outlined.

Method I

A very simple method of designing for target failure probability can be devised on the assumption that specific fire load, L , is the only variable whose randomness need be considered in the design and selection process. This assumption is realistic since, as discussed earlier, with the selection of $\Phi_d =$

$\Phi_{\min}$ a safe condition is ensured as to ventilation, and since the coefficient of variation for the fire test results, $\sigma_\tau/\bar{\tau}$ is as a rule not more than 0.15.

If the specific fire load, L , is the only random variable, the failure probability, P_f (dimensionless), can be controlled at any desired level by an opportune selection of the percentile specific fire load and, in turn, by the factor β to be used in Eq 9. Clearly, if the 80th percentile, L_{80} , is selected as design value, the probability that the compartment boundaries will survive the fire is 80% and the failure probability is 20%, that is, $P_f = 0.2$. Thus the β versus percentile specific fire load can be converted into a β versus P_f plot simply by changing the abscissa scale. To facilitate the design, the β versus P_f correlation also is shown in Fig. 3. (Although the figure was prepared on the assumption that the specific fire load follows the normal distribution pattern, it is deemed to yield acceptable accuracy for any distribution encountered in practice.)

A value for L defined according to Fig. 3 and Eq 9, when used in Eq 5 will yield a design value for H' ; this, in turn, if substituted in Eq 7 (remembering that the condition of satisfactory performance of a building element in fire is $H'' \geq H'$) will yield a value for fire resistance, τ_d , that ensures that the failure probability for the building element will not exceed P_f .

Method II

A more elegant method is based on the "second moment analysis" [9,10]. It can be expanded to cover the other principal random variable, the (experimentally determined) fire-resistance test results, τ , at this time and perhaps also ventilation, Φ , at some time in the future. According to this technique, the probability of failure for the compartment boundaries will not exceed P_f if

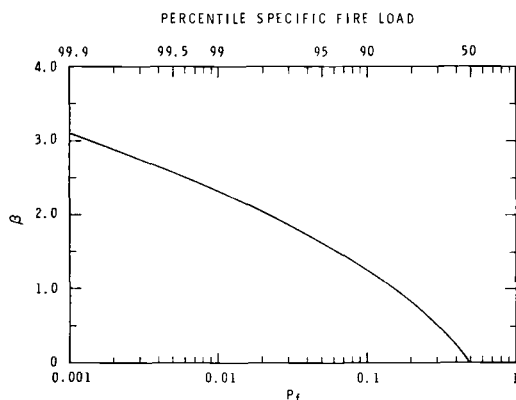


FIG. 3—Correlation between β and the percentile specific fire load in the cumulative plot (upper scale) or failure probability (lower scale).

the fire resistance of the boundary elements, τ , is determined from the following modified form of Eq 7

$$\tau = 0.11 + 0.16 \times 10^{-4} \bar{H}'' + 0.13 \times 10^{-9} (\bar{H}'')^2 \quad (10)$$

where

$$\bar{H}'' = \bar{H}' \exp \left[\beta \sqrt{\left(\frac{\sigma_{H''}}{\bar{H}''} \right)^2 + \left(\frac{\sigma_{H'}}{\bar{H}'} \right)^2} \right] \quad (11)$$

$\bar{H}''$ and $\bar{H}'$ are the means for H'' and H' , respectively, and $\sigma_{H''}$ and $\sigma_{H'}$, the standard deviations for H'' and H' , respectively. β is the same factor that appears in Eq 9 and is plotted as a function of P_f in Fig. 3.

The mean value of H' is determined by substituting $L = \bar{L}$ in Eq 5. The ventilation factor is again taken into account using its most adverse value, $\Phi_{\min}$. With this simplification, H' can be looked upon as dependent on one random variable, namely, fire load, L .

Following a procedure outlined in Refs 9 and 10, expressions have been derived for $\sigma_{H'}/\bar{H}'$ and $\sigma_{H''}/\bar{H}''$

$$\frac{\sigma_{H'}}{\bar{H}'} = \frac{\sigma_L}{\bar{L}} \frac{A_t \sqrt{k\rho c} + 467.5 \sqrt{\Phi_{\min} \bar{L} A_F}}{A_t \sqrt{k\rho c} + 935 \sqrt{\Phi_{\min} \bar{L} A_F}} \quad (12)$$

and

$$\frac{\sigma_{H''}}{\bar{H}''} = 0.9 \frac{\sigma_\tau}{\bar{\tau}} \quad (13)$$

where $\sigma_L/\bar{L}$ is to be selected from Table 2 and $\sigma_\tau/\bar{\tau}$ is available from the previously mentioned ASTM study.

Example

Design the boundary elements of a 10-story office building for a maximum of 5% failure probability in fire (that is, for $P_f = 0.05$). The floor area per story is 456 m²; there are 16 compartments on each story; they are 2.5 m tall and have, on average, a floor area of 26 m². Their boundaries will be lined with conventional materials: walls and ceiling with plasterboard and floor with wood parquet. Each compartment has two windows 1.8 m tall and 1 m wide.

Calculate the fire resistance requirement for the compartment boundaries in such a way as to have the failure probability in fire remain less than 5%:

Area of compartment boundaries (Eq 4)

$$A_t = 2 \times 26.0 + 4 \times \sqrt{26.0} \times 2.5 = 52.0 + 51.0 = 103.0 \text{ m}^2$$

Average thermal inertia for boundaries (Table 1 and Eq 2)

$$\sqrt{k\rho c} = \frac{1}{103.0} [(26.0 + 51.0) \times 742 + 26 \times 436] = 665 \text{ J m}^{-2} \text{ s}^{-1/2} \text{ K}^{-1}$$

Minimum value of ventilation factor (Eq 8): $\rho_a = 1.21 \text{ kg m}^{-3}$ (at 20°C), $g = 9.8 \text{ m s}^{-2}$

$$\Phi_{\min} = 1.21 \times 2 \times 1.8 \times 1.0 \sqrt{9.8 \times 1.8} = 18.3 \text{ kg s}^{-1}$$

Specific fire load characteristics for office building (Table 2)

$$\bar{L} = 24.8 \text{ kg m}^{-2}$$

$$\sigma_L = 8.6 \text{ kg m}^{-2}$$

The β factor (Fig. 3, $P_f = 0.05$)

$$\beta = 1.64$$

The δ factor (Eq 6)

$$\delta = 0.79 \times \sqrt{2.5^3/18.3} = 0.73$$

Method I

Equation 9

$$L_d = 24.8 + 1.64 \times 8.6 = 38.9 \text{ kg m}^{-2}$$

Equation 5

$$\begin{aligned} H' &= 10^6 \frac{11.0 \times 0.73 + 1.6}{103.0 \times 665 + 935 \sqrt{18.3 \times 38.9 \times 26.0}} (38.9 \times 26.0) \\ &= 49769 \text{ s}^{1/2} \text{ K} \end{aligned}$$

Equation 7

$$\begin{aligned}\tau &= 0.11 + 0.16 \times 10^{-4} \times 49769 + 0.13 \times 10^{-9} \times 49769^2 \\ &= 1.23 \text{ h}\end{aligned}$$

Fire resistance requirement: 1.5 h

Method II

Equation 5 (With $L_d \equiv \bar{L} = 24.8 \text{ kg m}^{-2}$)

$$\begin{aligned}\bar{H}' &= 10^6 \frac{11.0 \times 0.73 + 1.6}{103.0 \times 665 + 935\sqrt{18.3 \times 24.8 \times 26.0}} (24.8 \times 26.0) \\ &= 36513 \text{ s}^{1/2} \text{ K}\end{aligned}$$

Equation 12

$$\begin{aligned}\frac{\sigma_{H'}}{\bar{H}'} &= \frac{8.6}{24.8} \times \frac{103.0 \times 665 + 467.5\sqrt{18.3 \times 24.8 \times 26.0}}{103.0 \times 665 + 935\sqrt{18.3 \times 24.8 \times 26.0}} \\ &= 0.243\end{aligned}$$

Equation 13 (with $\sigma_r/\bar{r} \approx 0.1$)

$$\frac{\sigma_{H''}}{\bar{H}''} = 0.9 \times 0.1 = 0.09$$

Equation 11

$$\bar{H}'' = 36513 \times \exp[1.64\sqrt{0.243^2 + 0.09^2}] = 55849 \text{ s}^{1/2} \text{ K}$$

Equation 10

$$\begin{aligned}\tau &= 0.11 + 0.16 \times 10^{-4} \times 55849 + 0.13 \times 10^{-9} \times 55849^2 \\ &= 1.41 \text{ h}\end{aligned}$$

Fire resistance requirement: 1.5 h.

As Method I treats only the fire load as a random variable, it is not surprising that its application results in moderately lower fire-resistance require-

ments. Both methods have suggested that building elements listed as having 1.5-h fire resistance are acceptable.

In both calculation techniques the ventilation factor was considered at its most adverse value, $\Phi_{\min}$. In Fig. 4 the design fire-resistance requirement, τ (as calculated by Method I), is plotted against $\Phi/\Phi_{\min}$. It may be seen that τ_d decreases markedly with increasing ventilation.

Summary

Two methods are outlined for calculating fire-resistance requirements for buildings on the condition that the probability of failure for any of its elements will remain below a specified value. It should be pointed out that this probability of failure is conditional upon there having been flashover in the compartment. A procedure exists for estimating the likelihood of the occurrence of fire and subsequent flashover [11].

The technique requires seven input variables. Three, fire load, ventilation, and (experimentally determined) fire resistance are random variables. Statistical data on the randomness of fire load and fire resistance are already available. As the randomness of ventilation is not known, the most adverse value of ventilation is taken into consideration.

The first method is a simplified one, treating only the fire load as a random variable. The second employs Zahn's second moment analysis and takes account of the randomness of the (experimentally determined) fire-resistance values also.

It is believed that the error associated with the formula recommended for the calculation of the normalized heat load (that is, the potential for fire to spread by destruction) for the compartment is sufficiently compensated for by

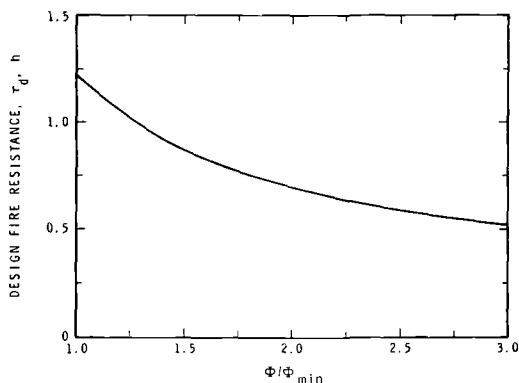


FIG. 4—The effect of ventilation on fire-resistance requirement at a given failure probability (see example for details).

performing the calculations for the most adverse ventilation conditions. Further margin of safety results from choosing building elements whose (experimentally determined) fire resistances are included in listings published by test laboratories.

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Fire Performance of Reinforced Concrete Columns

REFERENCE: Lie, T. T. and Lin, T. D., "Fire Performance of Reinforced Concrete Columns," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 176-205.

ABSTRACT: To develop general methods for the prediction of the fire resistance of reinforced concrete columns, experimental and theoretical studies were carried out. Results obtained in the first phase of the study, which involved the testing of twelve columns, are given. Of the variables studied in this phase, load, cross-section size, and type of aggregate had the largest influence on the fire resistance of the columns. The influence of concrete moisture content was, within the range studied, insignificant. Column temperatures, deformations, and fire resistance, calculated using a mathematical model, were in good agreement with those measured. The variables that will be studied in a second phase study are outlined.

KEY WORDS: fire tests, calculation, reinforced concrete, columns, temperature, deformation, failure, load, cross section, aggregate, moisture

Nomenclature

c	Specific heat, J/kg°C
f_c	Compressive strength of concrete at temperature T , MPa
f'_c	Cylinder strength of concrete at temperature T , MPa
f'_{co}	Cylinder strength of concrete at room temperature, MPa
f_y	Strength of steel at temperature T , MPa
f_{yo}	Yield strength of steel at room temperature, MPa
h	Coefficient of heat transfer at fire-exposed surface, W/m ² °C
k	Thermal conductivity, W/m°C
K	Effective length factor

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L	Unsupported length of column, m
P	Point
T	Temperature, °C
x	Coordinate
y	Lateral deflection of column at midheight, m
z	Coordinate

Greek Letters

α	Coefficient of thermal expansion
Δ	Increment
$\Delta\xi$	Mesh width, m
ϵ	Emissivity, strain, m
λ	Heat of vaporization, J/kg
ρ	Density (kg/m ³), radius of curvature (m)
σ	Stefan-Boltzmann constant, W/m ² K ⁴
τ	Time, h
ϕ	Concentration of moisture (fraction of volume)
χ	Curvature of column at midheight, m ⁻¹

Subscripts

o	At room temperature
c	Of concrete
f	Of the fire
m, M	At the points, m, M in column
max	Maximum
min	Minimum
n, N	At the points n, N in a row
L	Left of the x -axis
R	Right of the x -axis
p	Pertaining to proportional stress-strain relation
s	Of steel
T	Pertaining to temperature
w	Of water

Superscripts

j	At $\tau = j\Delta\tau$

Studies on reinforced concrete columns were started a few years ago for the purpose of updating existing fire resistance ratings for these columns. The ratings in most North American building codes were based on test results obtained in the 1920s. In early 1970 the National Building Code of Canada

adopted a conservatively based rational method for the determination of the fire resistance of reinforced concrete columns. Over the years, design procedures and the safety factor for these columns have changed. Thus, there is a need for the revision of present ratings.

In a cooperative effort between the National Research Council of Canada and the Portland Cement Association, a multiphase program has been worked out for the study of the behavior of full-size reinforced concrete columns exposed to fire. Its main objectives are:

1. To generate fire resistance data on reinforced concrete columns designed in accordance with current Building Code Requirements for Reinforced Concrete (ACI 318-77)³ or the Code for the Design of Concrete Structures for Buildings (CSA-CAN A23.3-M77).⁴
2. To develop general methods for the calculation of the fire resistance of concrete columns and walls for a wide range of applications.

Program

In the joint study program, theoretical as well as experimental studies will be carried out. A newly constructed column test furnace is used for the experimental studies, which are also intended for validation of a recently developed mathematical model for the calculation of fire resistance of reinforced concrete columns [1]. The study variables in the program include the following:

1. Load intensity.
2. Eccentricity of loading.
3. Fire exposure intensity.
4. Cross section of column or wall.
5. Column height.
6. Thickness of concrete cover.
7. Amount of steel.
8. Type of aggregate.
9. Strength of concrete.
10. Moisture content of concrete.
11. Restraining effect.
12. Possibly, unsymmetrical fire exposure.

This paper deals with the first phase of the program, which involved the testing of twelve columns. The columns were designed and manufactured by Portland Cement Association, in Skokie, Illinois. The specimens were transported to the fire research laboratories of the National Research Council of Canada, where the fire tests were conducted. The factors studied in this phase

³American Concrete Institute.

⁴Canadian Standards Association.

were the temperatures attained in different section sizes by the concrete and reinforcing steel, and the influence of load, moisture, and aggregate on the fire resistance of the columns.

Test Specimens

In the first phase of the study program, a series of twelve square reinforced concrete columns were fabricated and tested. Ten of the columns had a cross section of 305 by 305 mm (12 by 12 in.), one of 203 by 203 mm (8 by 8 in.), and one of 406 by 406 mm (16 by 16 in.). All columns were 3810 mm (12 ft 6 in.) long. Three specimens were made with carbonate aggregate and nine with siliceous. The average 28-day concrete strengths were 33.8 MPa (4898 psi) for carbonate aggregate concrete and 34.2 MPa (4958 psi) for siliceous aggregate concrete.

Table 1 gives details of cross section, the type of aggregate used in the concrete, the relative humidity measured in the center of the column at the time of the test, and the concrete cylinder strength measured at different times.

For the steel reinforcement, deformed 25M (No. 8) longitudinal reinforcing bars and 10M (No. 3) ties were used. Their location in the column is shown in Fig. 1. The yield strength of the 25M bars was 443.7 MPa (64.3 ksi) and that of the 10M bars 426 MPa (61.8 ksi).

Chromel-Alumel thermocouples were used to measure concrete and steel temperatures in the column. The thermocouples in the concrete were arranged to measure temperatures at various locations: at one-quarter height, at midheight, and at three-quarter height of the column. The location of the thermocouples in the concrete and on the steel are given in Figs. 1 and 2.

Further details of the specimens are given in Ref 1.

Calculation Procedure

The calculation of the fire performance of the columns is carried out in various steps. It involves the calculation of the temperatures in the column and its deformations and strength during the exposure to fire.

Temperatures of Column

The column temperatures are calculated by a finite difference method [2]. This method has been previously applied to the calculation of temperatures in square reinforced concrete columns [3]. Because the method of deriving the heat transfer equations and of calculating the temperatures is described in detail in those studies, it will not be discussed here; only the equations used for calculation of the column temperatures and information not dealt with in previous studies will be given.

Division of Cross Section into Elements—The cross-sectional area of the

TABLE 1—*Details concerning first phase column test specimens [cross sections 305 by 305 mm (12 by 12 in.) unless otherwise noted].*

Specimen No.	Cast Date (yr., mo., day)	Test Date (yr., mo., day)	Moisture Condition of Specimen at Middepth at Test Date ^d (% RH)	Compressive Strength of Cylinders	
				At 28 Days	At Test Date ^c
				MPa (Psi)	MPa (Psi)
SILICEOUS AGGREGATE					
1	77-6-27	79-9-19	5	34.2 (4953)	not measured
2	77-7-7	81-6-19	15	31.0 (4502)	37.3 (5402)
3	77-7-14	80-4-25	70	34.8 (5046)	36.6 (5305)
4	77-7-20	80-3-19	63	31.7 (4603)	34.2 (4961)
					34.2 (4952)
					35.9 (5199)
					34.5 (4996)
					35.8 (5190)
5 ^a	77-7-29	81-1-23	9	35.7 (5170)	42.6 (6172)
					38.8 (5633)
6 ^b	77-7-26	82-3-12	29	34.8 (5040)	42.6 (6172)
					42.1 (6109)
7	77-8-1	80-7-11	74	35.3 (5117)	36.8 (5332)
					35.4 (5129)
8	77-8-5	80-9-12	74	33.5 (4863)	34.2 (4952)
					35.4 (5138)
9	77-8-19	81-2-5	75	36.8 (5330)	39.7 (5757)
					37.0 (5359)
CARBONATE AGGREGATE					
10	77-8-19	81-12-14	75	33.6 (4875)	40.9 (5925)
11	77-8-25	81-10-13	75	33.5 (4860)	37.1 (5386)
					36.7 (5315)
12	77-8-30	82-1-22	76	34.2 (4960)	38.5 (5580)
					41.4 (5996)

^a Cross section 406 by 406 mm (16 by 16 in.).^b Cross section 203 by 203 mm (8 by 8 in.).^c Results of one or more cylinder tests are given.^d See text for method of conditioning. For relation between relative humidity in center of concrete and its moisture content, see ASTM E 119-79.

column is subdivided into a number of elements, arranged in a triangular network (Fig. 3). The elements are square inside the column and triangular at the surface. For the inside elements, the temperature at the center is taken as representative of the entire element. For the triangular surface elements, the representative points are located on the center of each hypotenuse.

Because only columns with square cross sections (and four axes of symmetry) will be considered, it is possible to calculate the temperature distribution in only one eighth of the cross-sectional area of the column. As illustrated in

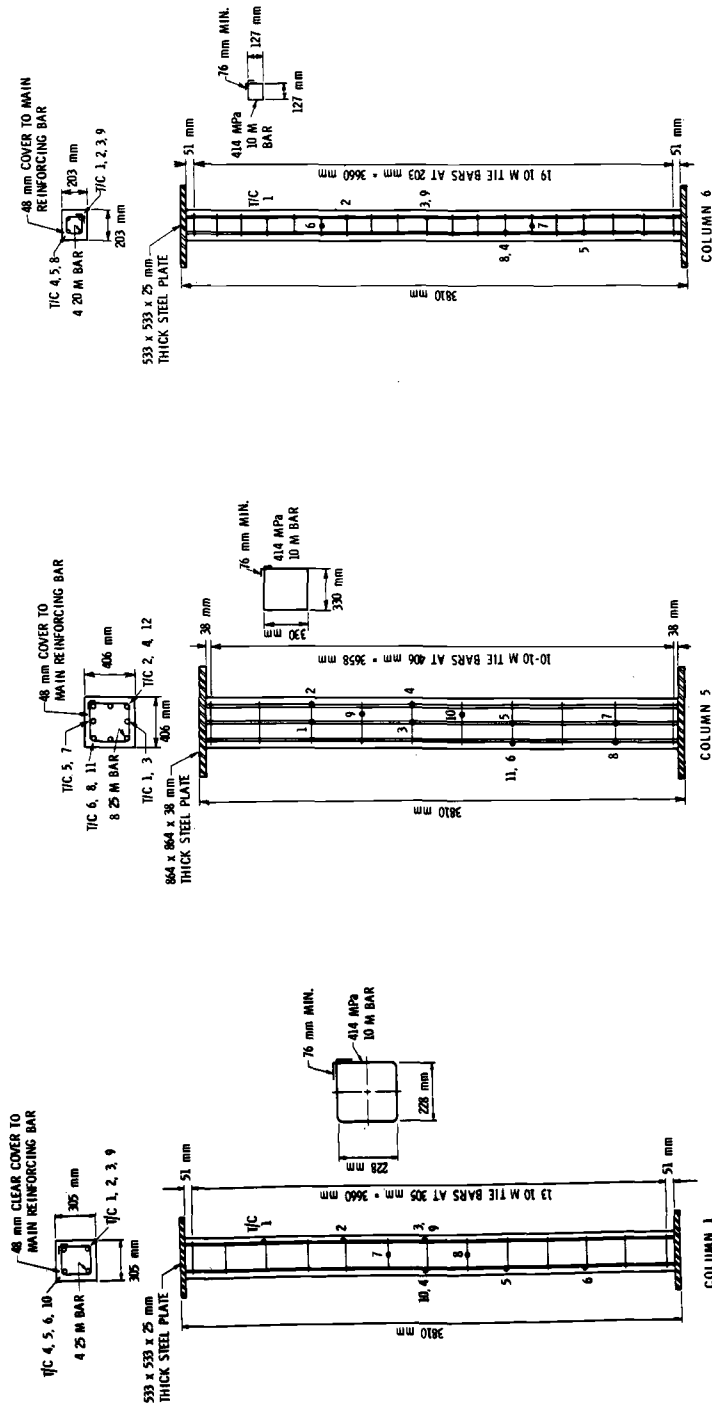


FIG. 1—Thermocouples on reinforcing bars (all thermocouples located on outer face of reinforcing bars closest to exposed surface).

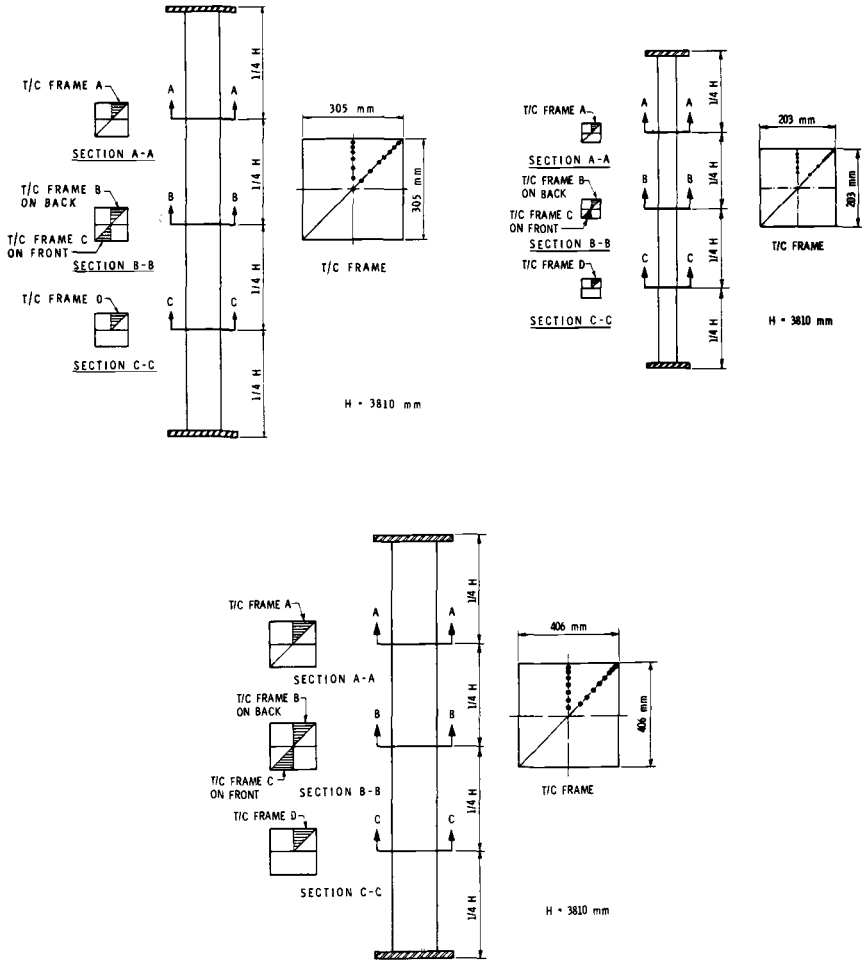


FIG. 2—Layout of thermocouple frames.

Fig. 3, in an x - z coordinate system, a point $P_{m,n}$ has the coordinates $x = (m - 1)\Delta\xi/\sqrt{2}$ and $z = (n - 1)\Delta\xi/\sqrt{2}$.

Equations for the Fire/Concrete Boundary—It will be assumed that the columns are exposed on all sides to the heat of a fire whose temperature course follows that of the standard fire described in ASTM Methods for Fire Tests of Building Construction and Materials (E 119-83). This temperature course can be described approximately by the following expression

$$T_f^j = 20 + 750[1 - \exp(-3.79553\sqrt{\tau})] + 170.41\sqrt{\tau} \quad (1)$$

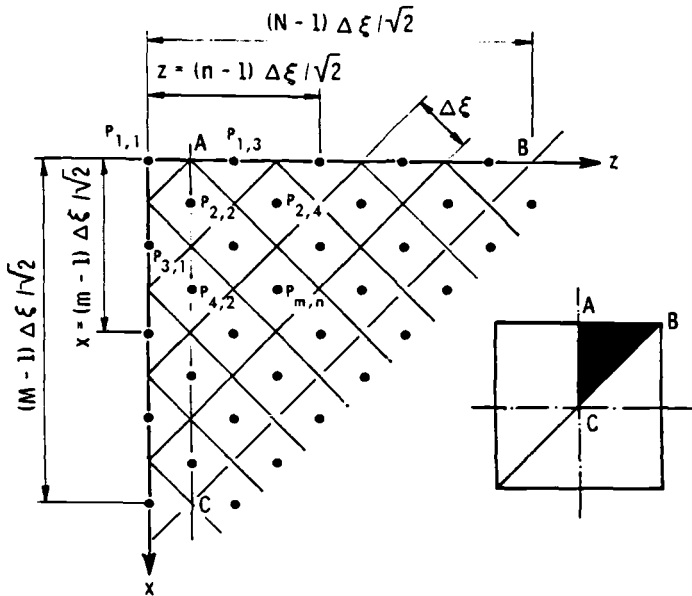


FIG. 3—Triangular network of elements in a one-eighth section of column.

where

τ = the time in hours, and

T_f^j = the fire temperature in $^{\circ}\text{C}$ at the time $\tau = j\Delta\tau$.

The temperature rise in each element can be derived by determining the heat balance for them. For a surface element, the temperature at a time $\tau = (j+1)\Delta\tau$ is given by the expression

$$T_{1,n}^{j+1} = T_{1,n}^j + \frac{2\Delta\tau}{[(\rho_c c_c)_{1,n}^j + \rho_w c_w \phi_{1,n}^j]} (\Delta\xi)^2 \left\{ \left(\frac{k_{2,(n-1)}^j + k_{1,n}^j}{2} \right) (T_{2,(n-1)}^j - T_{1,n}^j) + \left(\frac{k_{2,(n+1)}^j + k_{1,n}^j}{2} \right) (T_{2,(n+1)}^j - T_{1,n}^j) + \sqrt{2}\Delta\xi\sigma\epsilon_f\epsilon_c [(T_f^j + 273)^4 - (T_{1,n}^j + 273)^4] \right\} \quad (2)$$

Equations for Inside the Concrete—For the elements in the concrete, the temperature rise at the time $\tau = (j+1)\Delta\tau$ is given by

$$\begin{aligned}
& T_{m,n}^{j+1} + T_{m,n}^j + \frac{\Delta\tau}{[(\rho_c c_c)_{m,n}^j + \rho_w c_w \phi_{m,n}^j](\Delta\xi)^2} \\
& \left\{ \left(\frac{k_{(m-1),(n-1)}^j + k_{m,n}^j}{2} \right) (T_{(m-1),(n-1)}^j - T_{m,n}^j) + \left(\frac{k_{(m+1),(n-1)}^j + k_{m,n}^j}{2} \right) \right. \\
& (T_{(m+1),(n-1)}^j - T_{m,n}^j) + \left(\frac{k_{(m-1),(n+1)}^j + k_{m,n}^j}{2} \right) (T_{(m-1),(n+1)}^j - T_{m,n}^j) \\
& \left. + \left(\frac{k_{(m+1),(n+1)}^j + k_{m,n}^j}{2} \right) (T_{(m+1),(n+1)}^j - T_{m,n}^j) \right\} \quad (3)
\end{aligned}$$

Auxiliary Equations—To calculate the temperatures of the elements along the lines of symmetry A-C and B-C, the temperature has to satisfy the following symmetry conditions

Line A-C

$$T_{M,1}^{j+1} = T_{M,3}^{j+1} \quad (4)$$

Line B-C

$$T_{m,(N-m)}^{j+1} = T_{(m+1),(N-m+1)}^{j+1} \quad (5)$$

In order to ensure that any error existing in the solution at some time level will not be amplified in subsequent calculations, a stability criterion has to be satisfied which, for a selected value of $\Delta\xi$, limits the maximum of the time step ($\Delta\tau$). Following the method described in Ref 4, it can be derived that for the fire-exposed column the criterion of stability is most restrictive along the line $m + 1$, between fire and concrete. It is given by the condition

$$\Delta\tau \leq \frac{(\Delta\xi)^2}{\frac{4k_{\max} + 2\sqrt{2}\Delta\xi h_{\max}}{(\rho_c c_c)_{\min}}} \quad (6)$$

where the maximum value of the coefficient of heat transfer during exposure to the standard fire ($h_{\max}$) is approximately $3 \times 10^6 \text{ J/m}^2 \text{ h}^\circ\text{C}$ (147 Btu/ft² h[°]F).

Effect of Moisture—The effect of moisture is taken into account by assuming that in each element the moisture starts to evaporate when the temperature of the element reaches 100°C (212°F). During the period of evaporation, all the heat supplied to an element is used for evaporation of the moisture, until the element is dry. Migration of moisture is not taken into account.

From the heat balance equation, the moisture concentration in an element at the fire/concrete boundary at the time $\tau = (j + 1)\Delta\tau$ is given by

$$\begin{aligned}\phi_{1,n}^{j+1} = & \phi_{1,n}^j - \frac{2\Delta\tau}{\rho_w \lambda_w (\Delta\xi)^2} \left\{ \left(\frac{k_{2,(n-1)}^j + k_{1,n}^j}{2} \right) (T_{2,(n-1)}^j - T_{1,n}^j) \right. \\ & + \left(\frac{k_{2,(n+1)}^j + k_{1,n}^j}{2} \right) (T_{2,(n+1)}^j - T_{1,n}^j) \\ & \left. + \sqrt{2}\Delta\xi \sigma_{ef} \epsilon_c [(T_f^j + 273)^4 - (T_{1,n}^j + 273)^4] \right\} \quad (7)\end{aligned}$$

Similarly, the moisture concentration in an element inside the concrete at the time $\tau = (j + 1)\Delta\tau$ is given by

$$\begin{aligned}\phi_{m,n}^{j+1} = & \phi_{m,n}^j - \frac{\Delta\tau}{\rho_w \lambda_w (\Delta\xi)^2} \left\{ \left(\frac{k_{(m-1),(n-1)}^j + k_{m,n}^j}{2} \right) (T_{(m-1),(n-1)}^j + T_{m,n}^j) \right. \\ & + \left(\frac{k_{(m+1),(n-1)}^j + k_{m,n}^j}{2} \right) (T_{(m+1),(n-1)}^j - T_{m,n}^j) \\ & + \left(\frac{k_{(m-1),(n+1)}^j + k_{m,n}^j}{2} \right) (T_{(m-1),(n+1)}^j - T_{m,n}^j) \\ & \left. + \left(\frac{k_{(m+1),(n+1)}^j + k_{m,n}^j}{2} \right) (T_{(m+1),(n+1)}^j - T_{m,n}^j) \right\} \quad (8)\end{aligned}$$

With the aid of Eqs 1 to 8 and the relevant material properties given in the Appendix, the temperature distribution in the column and on its surface can be calculated for any time $[\tau = (j + 1)\Delta\tau]$ if the temperature distribution at the time $j\Delta\tau$ is known. Starting from a temperature of 20°C (68°F), the temperature history of the column can be calculated by repeated application of Eqs 1 to 8.

Calculation of Strength during Fire

Transformation into Square Network—To simplify the calculation of the deformations and stresses in the column, the triangular network is transformed into a square network. In Fig. 4 a quarter section of this network, consisting of square elements arranged parallel to the x - and z -axis of the section, are shown. The width of each element of this network is $\Delta\xi/\sqrt{2}$. The temperatures, deformations, and stresses of each element are represented by those of the center of the element. The temperature at the center of each ele-

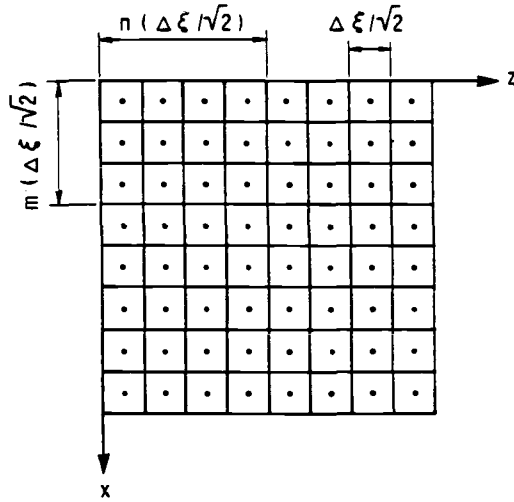


FIG. 4—Square network of elements in a quarter section of column.

ment is obtained by averaging the temperatures of the elements in the triangular network according to the relation

$$(T_{m,n}^j)_{\text{square}} = \left(\frac{T_{(m+1),(n+1)}^j + T_{m,(n+2)}^j}{2} \right)_{\text{triangular}} \quad (9)$$

where the subscripts “square” and “triangular” refer to the elements of the square and triangular network.

During exposure to fire, the strength of the column decreases with the duration of exposure. The strength of the column can be calculated by a method based on a load-deflection analysis which in turn is based on a stress-strain analysis of cross sections [5]. In this method, the columns, which are fixed at the ends during the tests, are idealized as pin-ended columns of reduced length KL , in which K was assumed to be 0.6 (Fig. 5). The load on the test columns is intended to be concentric. To represent imperfections in the columns, an initial deflection $y_o = 2.5$ mm (0.1 in.) is assumed.

The curvature of the column is assumed to vary from zero at pin-end to midheight according to a straight line relation, as illustrated in Fig. 5. For such a relation the deflection at midheight (y), in terms of the curvature (χ) of the column at this height, can be given by

$$y = \chi \frac{(KL)^2}{12} \quad (10)$$

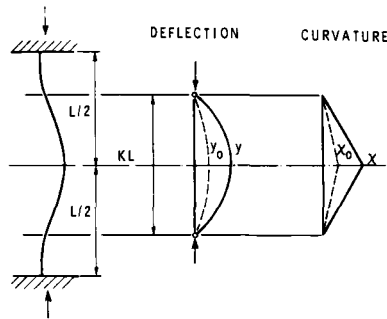


FIG. 5—Assumed load-deflection analysis.

For any given curvature (χ) and thus for any given deflection at midheight, the axial strain is varied until the internal moment at the midsection is in equilibrium with the applied moment given by the product of load and total deflection. In this way a load deflection curve can be calculated for specific times during the exposure to fire. From these curves the strength of the column (its maximum load-carrying capacity) can be determined for each time. In the calculation of column strength, the following assumptions were made:

1. The properties of the concrete and steel are as given in the Appendix and later in this section.
2. The influence of the presence of reinforcing steel on the temperature may be neglected. Thus the column, from a thermal point of view, may be treated as consisting entirely of concrete. The temperature of the steel is assumed to be equal to the temperature in the column section at the location of the center of the steel.
3. Concrete has no tensile strength.
4. Plane sections remain plane.
5. Initial strains in the column before the exposure to fire consist of free shrinkage of the concrete and creep. Because mortar was inserted at the top to make good contact between the concrete and the steel end plate, the initial shrinkage will be assumed to be negligible.

The tests of the columns were started after a preloading period of about 1 h. The shortening of the column due to creep in this period is assumed to be negligible.

Based on these assumptions, the change of column strength during the exposure to fire was calculated. In the calculations the square network shown in Fig. 4 was used. Because the strains and stresses of the elements are not symmetrical with respect to the x -axis, the calculations of the strains and stresses were performed for both the network shown and an identical network at the

left of the x -axis. The force and moment in the section were obtained by adding, respectively, the forces carried by each element and the moments contributed by them.

The equations used in the calculations of the strength of the column during the exposure to fire are given below.

Equations for the Steel—From the assumption that plane sections remain plane, the strain in the reinforcing steel is equal to the axial strain of the column (ϵ) and the strain due to bending of the column (z_s/ρ), where z_s is the horizontal distance of the reinforcing steel bar to the vertical plane through the x -axis of the section of the column and ρ is the radius of curvature. The strain causing stress, however, is determined as the difference between this strain and the free strain $[(\epsilon_T)_s]$ that would take place due to thermal expansion of the steel. For the steel at the right of the x -axis, this strain $[(\epsilon_s)_R]$ is given by

$$(\epsilon_s)_R = -(\epsilon_T)_s + \epsilon + \frac{z_s}{\rho} \quad (11)$$

For the steel elements at the left of the x -axis, the strain $[(\epsilon_s)_L]$ is given by

$$(\epsilon_s)_L = -(\epsilon_T)_s + \epsilon - \frac{z_s}{\rho} \quad (12)$$

The stresses in the elements of the network are calculated using stress-strain relations derived from data provided by Ingberg and Sale [6] and Witteveen, Twilt, and Bylaard [7]. These relations include the effect of creep at elevated temperatures and were obtained at heating rates approximately those that occur in a fire in actual practice. The relations have been generalized for other structural steels by assuming that, for a given temperature, the curves are the same for all steels, but the stress below which the stress-strain relation is linear is proportional to the yield strength of the steel. The equations that describe the relation between the stress in the steel (f_y), the strain (ϵ_s), and the temperature of the steel (T) are as follows (Fig. 6)

for

$$\epsilon_s \leq \epsilon_p f_y = \frac{f(T, 0.001)}{0.001} \epsilon_s \quad (13)$$

where

$$\epsilon_p = 4 \times 10^{-6} f_{y0} \quad (14)$$

and

$$f(T, 0.001) = (50 - 0.04T) \times \{1 - \exp[(-30 + 0.3T)\sqrt{0.001}]\} \times 6.9 \quad (15)$$

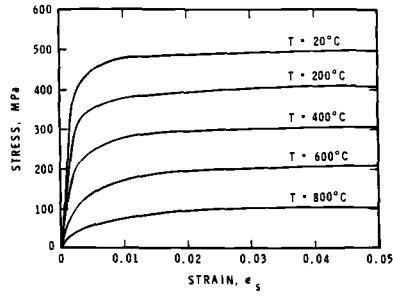


FIG. 6—Stress-strain curves for the reinforcing steel at various temperatures ($f_{y0} = 443$ MPa).

for

$$\epsilon_s > \epsilon_p$$

$$f_y = \frac{f(T, 0.001)}{0.001} \epsilon_p + f[T, (\epsilon_s - \epsilon_p + 0.001)] - f(T, 0.001) \quad (16)$$

The value of 0.001 was selected as the proportional limit of a standard steel of 250 MPa (36 ksi) strength. With the aid of Eqs 10 to 16, the stresses at midheight in the steel can be calculated for any value of the axial strain (ϵ), curvature ($1/\rho$), and temperature (T). From these stresses the load that the steel carries and the contribution of the steel to the moments can be derived.

Equations for the Concrete—In the same way as for steel, the strain in the concrete causing stress for elements at the right of the x -axis (Fig. 4) can be given by

$$(\epsilon_c)_R = -(\epsilon_T)_c + \epsilon + \frac{z_c}{\rho} \quad (17)$$

and for elements at the left of the x -axis by

$$(\epsilon_c)_L = -(\epsilon_T)_c + \epsilon - \frac{z_c}{\rho} \quad (18)$$

where

$(\epsilon_T)_c$ = free strain due to thermal expansion of the concrete,

ϵ = axial strain of the column,

z_c = horizontal distance of the center of the element to the vertical plane through the x -axis of the column section, and

ρ = radius of curvature.

The stresses in the elements are calculated using stress-strain relations based on the work of Ritter [8] and Hognestad [9]. These relations have been slightly modified to take into account the creep of concrete at elevated temperatures. The modifications are based on results of work by Schneider and Haksever [10] and consist of a movement of the maxima in the stress-strain curves to higher strains with higher temperatures. These curves are shown in Fig. 7 for a concrete with a cylinder strength of 35 MPa (5 ksi). The equations that describe these curves are as follows

for

$$\epsilon_c \leq \epsilon_{\max}$$

$$f_c = f'_c \left[1 - \left(\frac{\epsilon_{\max} - \epsilon_c}{\epsilon_{\max}} \right)^2 \right] \quad (19)$$

for

$$\epsilon_c > \epsilon_{\max}$$

$$f_c = f'_c \left[1 - \left(\frac{\epsilon_c - \epsilon_{\max}}{3\epsilon_{\max}} \right)^2 \right] \quad (20)$$

where

$$f'_c = f'_{co} \quad \text{if } T \leq 450^\circ\text{C} \quad (21)$$

$$f'_c = f'_{co} \left[2.011 - 2.353 \frac{T - 20}{1000} \right] \quad \text{if } T \geq 450^\circ\text{C} \quad (22)$$

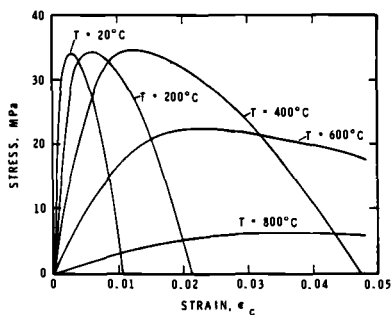


FIG. 7—Stress-strain curves for concrete at various temperatures ($f'_{co} = 35$ MPa).

$$\epsilon_{\max} = 0.0025 + (6.0T + 0.04T^2) \times 10^{-6} \quad (23)$$

In these equations

- f_c = compressive strength of concrete at temperature T ,
- f'_c = cylinder strength of concrete at temperature T ,
- f'_{co} = cylinder strength of concrete at 20°C (68°F),
- ϵ_c = strain of the concrete, and
- $\epsilon_{\max}$ = strain corresponding to maximum stress.

With the aid of the Eqs 17 to 23, the stresses in each of the concrete elements at midsection can be calculated for any value of the axial strain (ϵ) and curvature ($1/\rho$). From these stresses the load that the concrete carries and the contribution of the concrete to the moments can be derived.

Test Conditions and Procedure

The tests were carried out by exposing the columns to heat in a furnace specially built for this purpose. The heat input into the test furnace was controlled in such a way that the average temperature followed as closely as possible the standard temperature/time curve given by Eq 1. The instrumentation and characteristics of the furnace are described in detail in Ref 11.

Several of the columns were tested under a load, which was concentric. The loads were applied about one hour prior to the test. The columns were fixed at the ends.

On the date of testing, the relative humidity in the center of the concrete and the cylinder strength of the concrete were measured. The measured values are given in Table 1. Four columns, tested in near oven-dry condition, were kiln-dried at about 93°C (200°F) and 0 to 5% relative humidity. After the designated moisture condition was reached, the columns were wrapped in plastic to prevent change in their moisture content. The other columns were conditioned in an atmosphere controlled at 21 to 24°C and 30 to 40% relative humidity and later kept for at least six months in an atmosphere of about 20°C and 50 to 75% relative humidity, prior to testing.

During the test, measurements were made of the temperatures of the concrete and steel in the column at the locations described earlier. The axial strain of those columns that were tested under a load was also measured. These columns were considered to have failed, and the test was terminated, when the hydraulic jack, which has a maximum speed of 76 mm/min (3 in./min), could no longer maintain the applied load. Unloaded columns, which were tested to obtain information on the temperature history in the column, were exposed to fire for 3 to 5 h, depending on their size. The duration of the various tests and the loads that were applied are given in Table 2.

TABLE 2—*Load and performance of test columns [cross section 305 by 305 mm (12 by 12 in.) unless otherwise indicated].*

Specimen No.	Load, kN (kips)	Length of test, h:min	Mode of Failure
SILICEOUS AGGREGATE			
1	0	4:00	none
2	1333 (300)	2:50	compression
3	800 (180)	3:38	compression
4	711 (160)	3:40	compression
5 ^a	0	5:00	none
6 ^b	169 (38)	3:00	buckling
7	1067 (240)	3:28	compression
8	1778 (400)	2:26	compression
9	1333 (300)	3:07	compression
CARBONATE AGGREGATE			
10	800 (180)	8:30	compression
11	1067 (240)	6:06	compression
12	1778 (400)	3:36	compression

^aCross section 406 by 406 mm (16 by 16 in.).^bCross section 203 by 203 mm (8 by 8 in.).

Results⁵ and Discussion

Temperatures in Concrete

The temperatures measured in the concrete at various heights and depths are shown in Fig. 8 for Columns 1, 5, and 6 in Table 2. For specific depths, the differences in temperature between the various heights are small after an exposure time of about one hour. The small differences indicate that, for exposure periods of 1 h or longer, the heat transfer from the furnace to the column may be regarded as uniform.

In Fig. 9, the average of the temperatures, measured in the same columns at one-quarter height, midheight, and three-quarter height, is shown for three depths in each column. Also shown are calculated temperatures for the corresponding depths. The calculations were carried out for dry concrete. Although the test columns were in near oven-dry condition at the time of testing, they still contained a small amount of moisture. There is a rapid rise of the temperatures measured in the deeper part of the concrete in the earlier stages of the tests. This rise may result from thermally induced migration of moisture towards the center of the column. At a later stage, however, which is the important stage for predicting the fire resistance of the columns, there is good agreement between measured and calculated temperatures.

⁵Detailed information on test results is available from T. T. Lie.

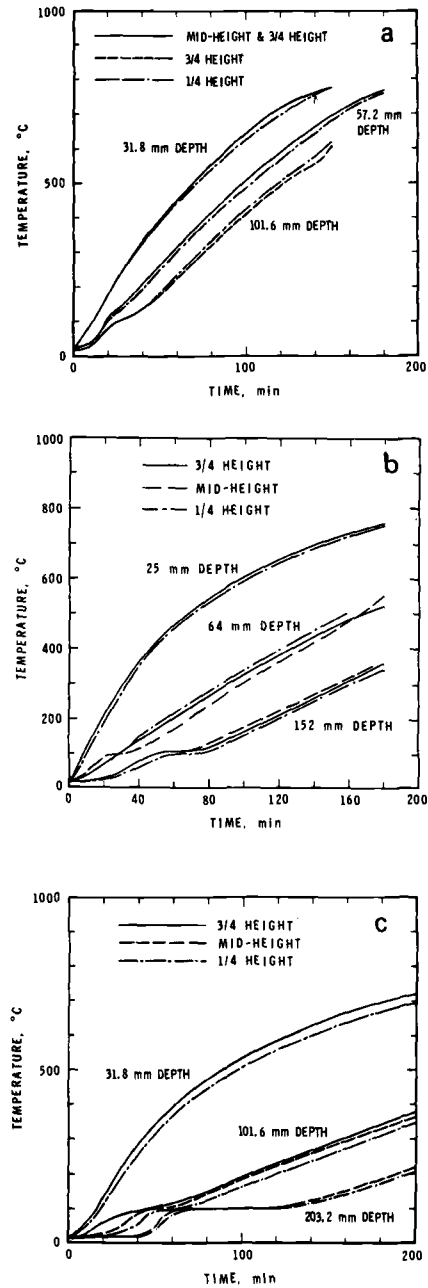


FIG. 8—Temperatures measured along centerline of cross-axial section at various depths and column heights: (a) Column 6, Section: 203 by 203 mm; (b) Column 1, Section: 305 by 305 mm; (c) Column 5, Section: 406 by 406 mm.

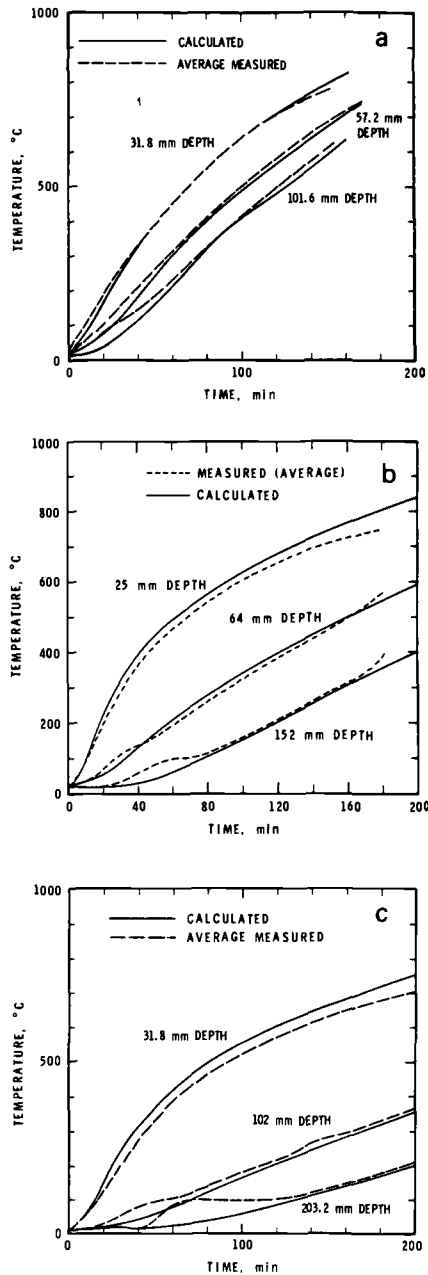


FIG. 9—Concrete temperatures along centerline of cross-axial section at various depths: (a) Column 6, Section: 203 by 203 mm; (b) Column 1, Section: 305 by 305 mm; (c) Column 5, Section: 406 by 406 mm.

Temperatures of Steel

The temperatures measured on the main reinforcing steel bars are shown in Fig. 10 for Columns 1, 5, and 6 in Table 2. These measurements were made in Column 1 with Thermocouples 3 and 9, located opposite each other with respect to the center of one bar, and with Thermocouples 4 and 10, located opposite each other on another bar (Fig. 1). In Column 5, Thermocouples 6 and 11 and Thermocouples 4 and 12 were used. In Column 6, Thermocouples 3 and 9 and Thermocouples 4 and 8 were used. The curves in Fig. 10 show that the differences in temperature between two opposite points of the bar are relatively small. Therefore, the average of the temperatures measured at opposite locations on a bar is a good approximation of the average steel temperature.

In Fig. 11 the average steel temperature is compared with temperatures calculated as described earlier in this paper. In this method, the column is treated as consisting entirely of concrete, and the temperature at the location of the center of the steel is chosen as representative of the average temperature of the steel. Calculated steel temperatures are somewhat higher than measured steel temperatures. The differences are small, however. Because at high temperatures the steel is contributing only a relatively small part to the strength of the column, small deviations in steel temperature have little influence on the column strength. The method is therefore sufficiently accurate for calculating steel temperatures.

Influence of Load

To study the influence of load on fire resistance, five identical columns, made with siliceous aggregate, were tested under different loads. The columns tested, which had a section size of 305 mm (12 in.) square, were Specimens 3, 4, 7, 8, and 9 in Table 2. During the tests, concentric loads of 800, 711, 1067, 1778, and 1333 kN (180, 160, 240, 400, and 300 kips), respectively, were applied.

Calculations also were made of the fire resistance of these columns as a function of the load. It was assumed that the moisture content in the concrete was five percent by volume, which, according to ASTM 119, is the moisture condition in concrete in equilibrium with that in an environment of about 70% relative humidity. The concrete strength was 35 MPa (5 ksi).

How the load affects the fire resistance of the columns is shown in Fig. 12, where measured and calculated fire resistances for various loads are given. Both measured and calculated results show a strong influence of the load on the fire resistance of the columns. Calculated fire resistances, however, are somewhat lower than measured fire resistances. The differences range from about 2 to 16% of the measured fire resistances.

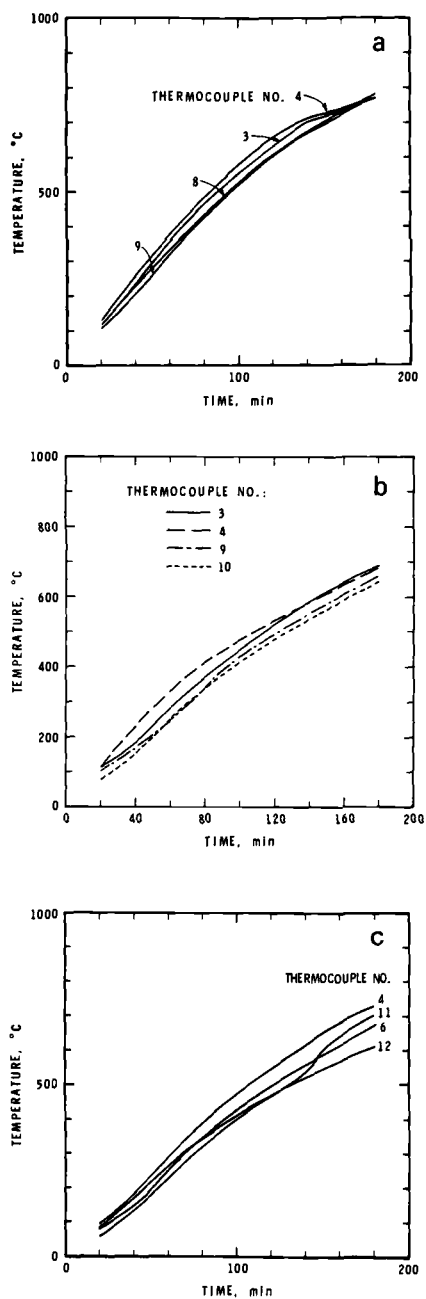


FIG. 10—Temperatures measured on main reinforcing bars: (a) Column 6, Section: 203 by 203 mm; (b) Column 1, Section: 305 by 305 mm; (c) Column 5, Section: 406 by 406 mm.

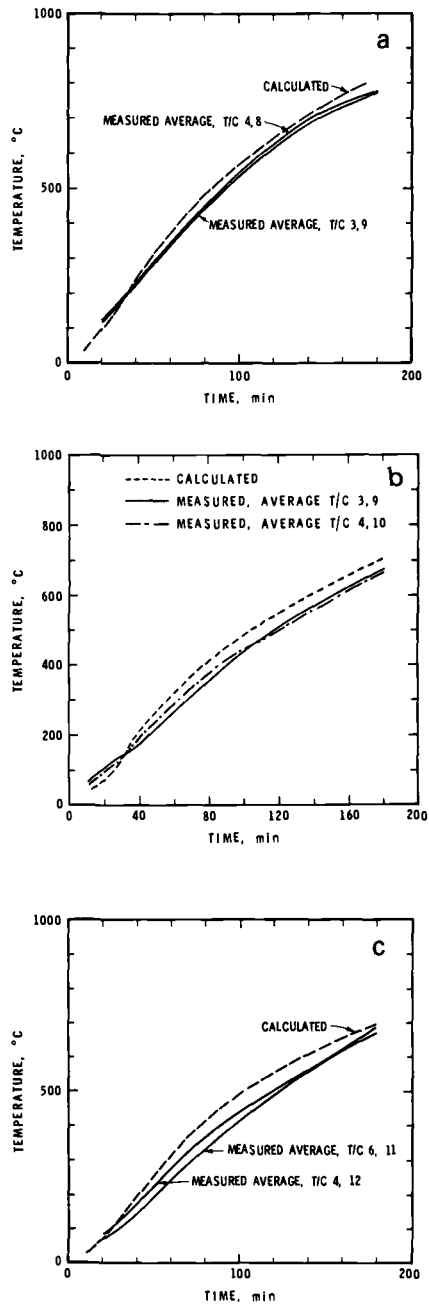


FIG. 11—Calculated and measured average temperatures of main reinforcing bars: (a) Column 6, Section: 203 by 203 mm; (b) Column 1, Section: 305 by 305 mm; (c) Column 5, Section: 406 by 406 mm.

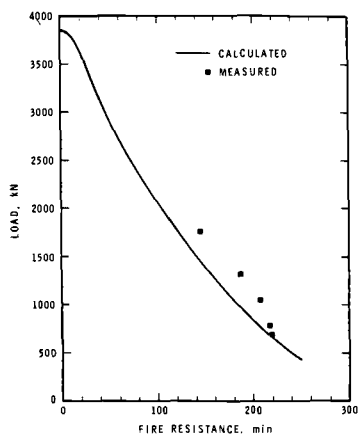


FIG. 12—Influence of load on fire resistance (305 by 305-mm section size; Column 3, 4, 7, 8, and 9).

The maximum allowable load for a column with a section of 305 mm (12 in.) square and 3810 mm (12 ft 6 in.) long, fixed at the ends, is about 900 kN (200 kips), according to ACI 318 or CSA A23. For this load, the fire resistance of this column is more than 3 h, according to the test and calculated results. This is considerably higher than the fire resistance assigned at present to such columns in the National Building Code of Canada⁶ and the Guide for Determining the Fire Endurance of Concrete Elements (ACI 216R-81).⁷

Part of the procedure of calculation of fire resistance is the calculation of the axial deformation of the columns. This deformation was also measured during the tests on loaded columns. Typical curves for measured and calculated axial deformations are shown in Figs. 13 and 14. In Fig. 13, the axial deformation is shown as a function of time for Column 3 (Table 2), which was tested under a load of 800 kN (180 kips) and in Fig. 14, for Column 7, tested under a load of 1067 kN (240 kips). In all cases where the axial deformation was measured, there was relatively good agreement between the theoretical and the experimentally derived curves. Near the failure point, however, measured contractions of the column are greater than those calculated. A possible reason for the difference is that the concrete is more ductile at high temperature than assumed in the model. The influence of this difference on the fire resistance is about 10 min. It is small in comparison with the fire resistance of the column, but can eventually be taken into account empirically.

⁶Supplement to the National Building Code of Canada, Associate Committee on the National Building Code, National Research Council of Canada, Ottawa, NRCC 17724, 1980.

⁷Concrete International, Report ACI-216R-81.

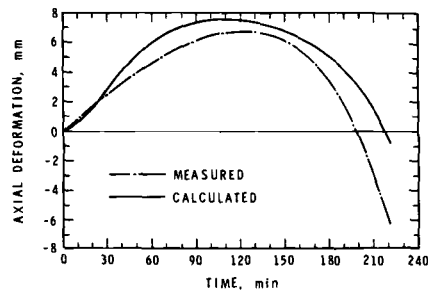


FIG. 13—Axial deformation as a function of time (load = 800 kN; Column 3).

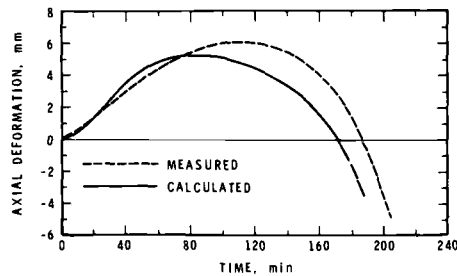


FIG. 14—Axial deformation as a function of time (load = 1067 kN; Column 7).

Influence of Cross Section

In this phase the influence of cross section was studied mainly from a thermal point of view. All columns tested under a load had a section size of 305 by 305 mm (12 by 12 in.) except one, which had a section size of 203 by 203 mm (8 by 8 in.). All columns failed in compression except the smaller one, which failed by buckling.

The influence of section size on the temperatures in the concrete can be derived from the temperature curves given in Fig. 9. For example, it takes about 60 min to reach a temperature of 300°C at middepth in the 203 by 203-mm column section, 90 min in the 305 by 305-mm section, and 180 min in the 406 by 406-mm section. The influence of section size on fire resistance, for a specific load, will be even greater than the influence on temperature because, in addition to the slower temperature rise, the larger column also has more area of low temperature that can contribute to carrying the load.

Influence of Moisture

The influence of moisture on fire resistance was studied theoretically as well as experimentally. For this purpose a column (No. 2, Table 2) was tested

in a nearly dry state under a load equal to that on a similar, but normally conditioned, test column (No. 9). Calculations also were made of the temperatures in the column during fire exposure and of its fire resistance as a function of load.

In Fig. 15, measured temperatures, and in Fig. 16 calculated temperatures, at various depths in the concrete column are given as a function of time. The maximum delay in temperature rise at 180 min, due to moisture, is

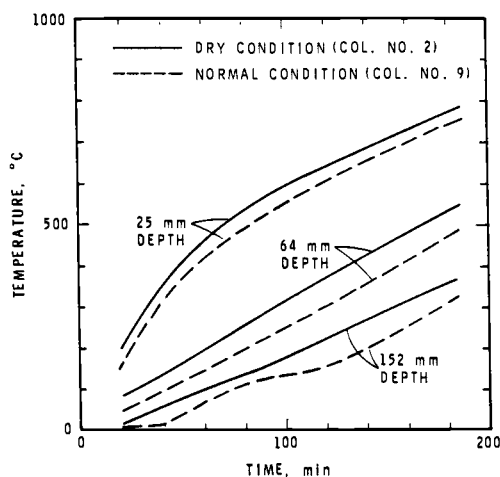


FIG. 15—Influence of moisture on concrete temperatures (measured at various depths along centerline of midheight section).

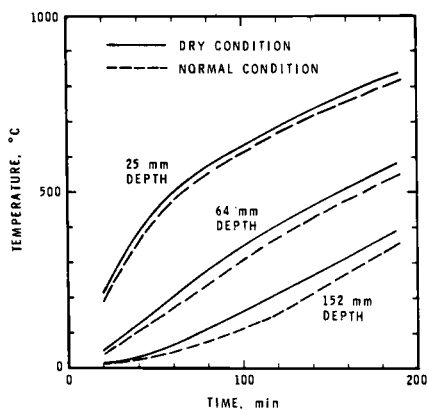


FIG. 16—Influence of moisture on concrete temperatures (calculated for various depths along centerline of column cross-axial section).

about 20 min according to the measurements and about 15 min according to the calculations.

The influence of moisture on the fire resistance of the column is shown in Fig. 17. In this figure, calculated fire resistances are given as a function of load for a dry column and for a column with a moisture content of five percent by volume, which, according to ASTM E 119, is the moisture condition in concrete in equilibrium with that in an environment of about 70% relative humidity. Also, the fire resistances measured in the tests are shown. The results of tests and calculations indicate that for normal fluctuations in the moisture condition of the concrete (for example, a moisture condition in equilibrium with that in an environment of 40 to 70% relative humidity, or a moisture content of 3 to 5%) moisture has only a small effect on the fire resistance of the column.

Influence of Aggregate

Three columns made with carbonate aggregate were tested under different loads (Columns No. 10, 11, and 12 in Table 2). A comparison with the fire resistance of similar columns, made with siliceous aggregate and tested under the same loads, showed that the fire resistance of the carbonate aggregate columns was much higher than that of the siliceous aggregate columns. Under approximately the maximum allowable load, the fire resistance of the carbonate aggregate column (No. 11) was almost twice as high as that of the siliceous aggregate column (No. 7). The difference was partly caused by a

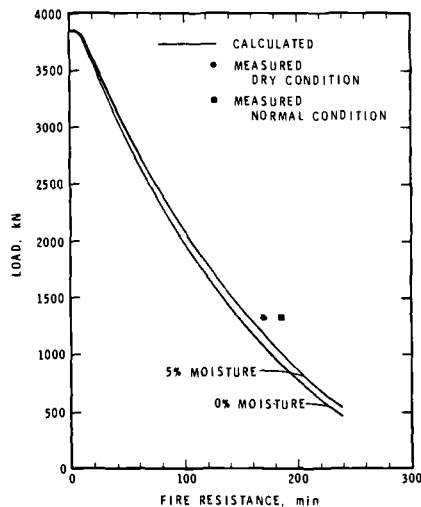


FIG. 17—Influence of moisture on fire resistance.

slower temperature rise in the carbonate aggregate column, particularly at higher temperatures (Fig. 18). A substantial part of the difference was due to the high ductility of the carbonate aggregate concrete, which enables the column to undergo large axial deformations without failure (Fig. 19).

Conclusions

Of the variables studied in the first phase of the study program, load, cross-section size, and type of aggregate have the largest influence on the fire resistance of reinforced concrete columns. Particularly, the use of carbonate aggregate instead of siliceous aggregate will substantially increase the fire resistance of the column. The influence of concrete moisture content on fire resistance is, in the practical range (40 to 70% relative humidity), insignificant.

The fire resistances of the columns studied are considerably higher than those assigned to them in the National Building Code of Canada and in the American Concrete Institute Guide 216.

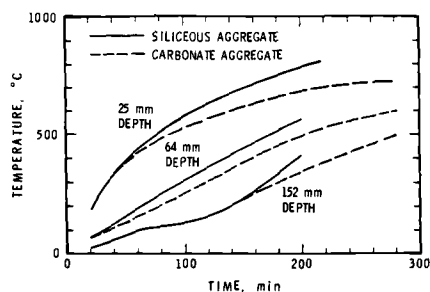


FIG. 18—Influence of aggregate on temperatures in column (measured in midheight section along centerline).

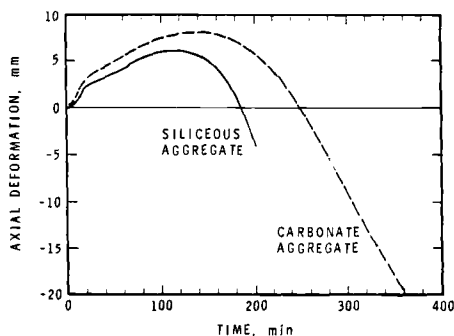


FIG. 19—Influence of aggregate on axial deformation of column.

Column temperatures, deformations, and fire resistances calculated using the mathematical model described in the present paper are in good agreement with those measured.

The experimental results provide basic data on the fire resistance of columns under standard fire test conditions. They also enable the evaluation of assumptions used for calculating the fire resistance by methods based on heat transfer and structural analyses.

To develop general methods for the determination of the fire resistance of concrete columns and walls for a wide range of applications, a second phase of studies is planned. In this phase the effect of the following on fire resistance will be studied:

1. Restraint of column expansion.
2. Strength of concrete.
3. Use of lightweight aggregate.
4. Amount of steel.
5. Thickness of concrete cover to steel.

This paper is a contribution of the Division of Building Research, National Research Council of Canada, and the Portland Cement Association. It is published with the approval of the director of the Division of Building Research.

APPENDIX

Material Properties and Physical Constants

The values of the material properties and physical constants used in this study are given below. With the exception of those for the thermal conductivity of concrete, the values are approximately those used in previous studies on reinforced concrete columns [3,5]. For the thermal conductivity, the values assumed to be valid for concrete made with pure quartz aggregate were replaced by the values obtained by Harmathy and Allen [12] from tests on concrete with predominantly siliceous aggregate. Instead of tabulated values, approximate equations are used in this study to describe the relationship between the properties and temperature.

Concrete Properties

Thermal capacity of concrete

for $0 \leq T \leq 200^\circ\text{C}$

$$\rho_c c_c = (0.005T + 1.7) \times 10^6 \quad \text{J/m}^3\text{C}$$

for $200^\circ\text{C} < T \leq 400^\circ\text{C}$

$$\rho_c c_c = 2.7 \times 10^6 \quad \text{J/m}^3\text{C}$$

for $400^{\circ}\text{C} < T \leq 500^{\circ}\text{C}$

$$\rho_c c_c = (0.013T - 2.5) \times 10^6 \quad \text{J/m}^3\text{C}$$

for $500^{\circ}\text{C} < T \leq 600^{\circ}\text{C}$

$$\rho_c c_c = (-0.013T + 10.5) \times 10^6 \quad \text{J/m}^3\text{C}$$

for $T > 600^{\circ}\text{C}$

$$\rho_c c_c = 2.7 \times 10^6 \quad \text{J/m}^3\text{C}$$

Thermal conductivity of concrete

for $0 \leq T \leq 800^{\circ}\text{C}$

$$k = -0.000625T + 1.5 \quad \text{W/m}^{\circ}\text{C}$$

for $T > 800^{\circ}\text{C}$

$$k = 1.0 \quad \text{W/m}^{\circ}\text{C}$$

Coefficient of thermal expansion

$$\alpha_c = (0.008T + 6) \times 10^{-6}$$

Steel Properties

Coefficient of thermal expansion

for $T < 1000^{\circ}\text{C}$

$$\alpha_s = (0.004T + 12) \times 10^{-6}$$

for $T \geq 1000^{\circ}\text{C}$

$$\alpha_s = 16 \times 10^{-6}$$

Water Properties

Thermal capacity

$$\rho_w c_w = 4.2 \times 10^6 \quad \text{J/m}^3\text{C}$$

Heat of vaporization

$$\lambda_w = 2.3 \times 10^6 \quad \text{J/kg}$$

Physical Constants

σ = Stefan-Boltzmann constant: $5.67 \times 10^{-8} \text{ W/m}^2 \text{ K}^4$

ϵ_f = emissivity of fire: 1

ϵ_c = emissivity of concrete: 0.9

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Effect of Fire Exposure on Structural Response and Fireproofing Requirements of Structural Steel Frame Assemblies

REFERENCE: Bresler, B. and Iding, R. H., "Effect of Fire Exposure on Structural Response and Fireproofing Requirements of Structural Steel Frame Assemblies," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 206-222.

ABSTRACT: Fire endurance ratings of structural steel frame assemblies are based on limiting temperature rise and on preventing structural collapse during a specified fire. Currently, a single standard fire, ASTM Method for Fire Tests of Building Construction and Materials E 119-83, is specified as the fire exposure. Thermal and structural responses can be determined either by a standard test or by calculations using computer programs such as FIRES-T3 and FASBUS II. Use of analytical methods in predicting thermal and structural responses of steel frame assemblies, with or without fireproofing, permits evaluation of the effect of different fire exposures. In this paper, a general approach for the evaluation of structural responses and fireproofing requirements of steel assemblies under different fire exposures is described and the results of three case studies are presented.

Data required for and procedures used in evaluating the response to fire, as well as the response acceptance criteria for the desired level of fire protection, are discussed. Two fire exposures, short duration high intensity (SDHI) and long duration low intensity (LDLI), are described. These fire exposures together with the ASTM E 119 standard fire, are used in the evaluation of fire responses of a typical steel deck floor assembly specimen. Response of this assembly with different thicknesses of fireproofing is determined analytically, and the results are summarized.

KEY WORDS: standard fire, nonstandard fires, analysis, test results, structural steel, fireproofing

Design of steel buildings for fire resistance generally has been based on the concept of fire endurance as determined by tests of components exposed to a

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standard fire. Furnace fire temperature during a standard test is controlled to conform to the ASTM E 119 time-temperature curve. Based on these standard tests, the Underwriters Laboratories, or other certified laboratories publishing certified endurance ratings, can issue approval for certain types of construction. Either temperature or behavior under load is used as a criterion to determine if an element has passed a standard test.

However, there are a number of deficiencies in current testing methods and fire endurance ratings. First, the temperature history used in a standard test may be quite different from that to which a structural element is subjected in an actual fire in which the rate of heating, the peak temperature, and the duration of exposure may differ substantially from that used in the test. Second, such tests provide only for performance during the heating stage, neglecting the cooling stage and any residual performance. Third, specimens are normally tested in isolation from structural systems, ignoring various factors such as end restraint, stress level under service loading, and other parameters which may significantly affect fire resistance.

Available engineering tools, computers in particular, can overcome these deficiencies, enabling design for fire safety which accounts for factors such as nonstandard fire curves and the structural interaction between a fire-involved compartment and the surrounding structure. Temperature distribution, structural deformations, stresses, and load-carrying capacities at elevated temperatures can be determined analytically in much of the same way as has been customary in ultimate strength design for wind or earthquake loading. The analytical process involves: (1) characterization of a design fire; (2) identification of thermal and structural material properties of the assembly being evaluated; (3) modeling of the assembly's insulation, restraint, and initial loading conditions; (4) numerical discretization and solution for internal temperature history and structural behavior (and failure mode if applicable); and (5) comparison of calculated response with fire endurance acceptance criteria.

In this paper, two fire exposure cases are considered, and the calculated thermal and structural response of typical steel beams due to these fire exposures is compared with their response to the standard ASTM E 119 fire.

Fire Exposure Categories

The determination of fire temperatures inside a compartment is complex. Heat released by combustion of materials, heat lost through windows and walls, and heat required to maintain and spread a fire must be accounted for. By properly modeling combustion in a given enclosure, the temperature inside a compartment can be calculated. In Fig. 1, typical results obtained by Swedish researchers [7] are illustrated, with each curve reflecting different quantities of fuel and ventilation factors. Thus, a broad range of curves may be considered for design purposes. Whether the idealization proposed by the

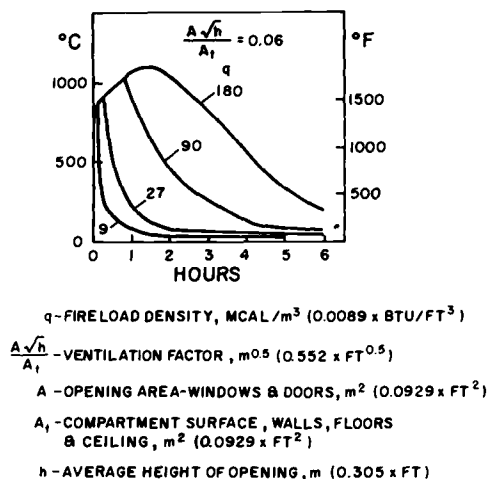


FIG. 1—Fire time-temperature curves (from Ref 1).

Swedish model is precisely realistic is beside the point; the idealization is used here only to illustrate the nature of rational time-temperature fire curves for particular zones in a compartment.

Two fire curves are used as examples in this study, a short-duration, high-intensity (SDHI) fire and a long-duration, low-intensity (LDLI) fire, as shown in Fig. 2. The LDLI fire curve represents a moderate-temperature, but long-duration fire for which nonstructural damage may be tolerated with only minor structural damage expressed in terms of an acceptable limit state. The SDHI fire curve represents an extremely high-temperature but short-duration

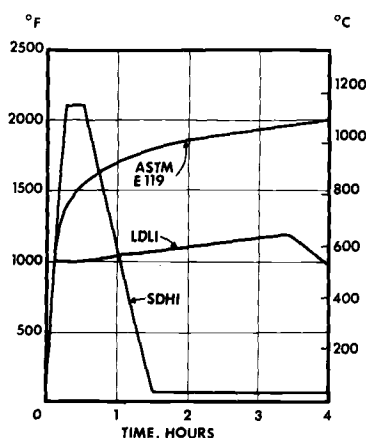


FIG. 2—Fire exposure models.

fire for which moderate structural damage may be acceptable provided that the system does not collapse within a specified endurance limit. Since damage may depend on structural behavior after a fire peaks and a structural system begins to cool, each curve has a cooling branch, unlike the ASTM E 119 fire (Fig. 2).

Thermal Response Under SDHI and LDLI Fires

Temperature distribution within typical steel beams exposed to the SDHI and LDLI fire curves can be determined analytically as a function of exposure time. The steel beam cross section with steel decking, concrete slabs, and spray-on fireproofing, as shown in Fig. 3a, is subjected to convective and radiative heat flux along its boundaries, and the internal temperature history is found by solution of the heat conduction boundary value problem. A computer program, FIRES-T3 [2], has been written specifically for this type of problem using a finite-element discretization in the spatial domain coupled with a step-by-step integration in the time domain. A finite-element mesh appropriate for this assembly, composed of steel, concrete, and spray-on fireproofing elements, is shown in Fig. 3b. A thorough development of thermal analysis techniques can be found in Refs 2, 3, and 4 and will not be developed here.

To generate data for a wide range of steel beams, analyses are conducted for standard steel section sizes of W21 $\times$ 50 and W36 $\times$ 194 (approximately 53 and 93 cm deep and 75 and 290 kg/m in weight, respectively.) and for spray-on fireproofing thicknesses of 13 and 32 mm (1/2 and 1 1/4 in., respec-

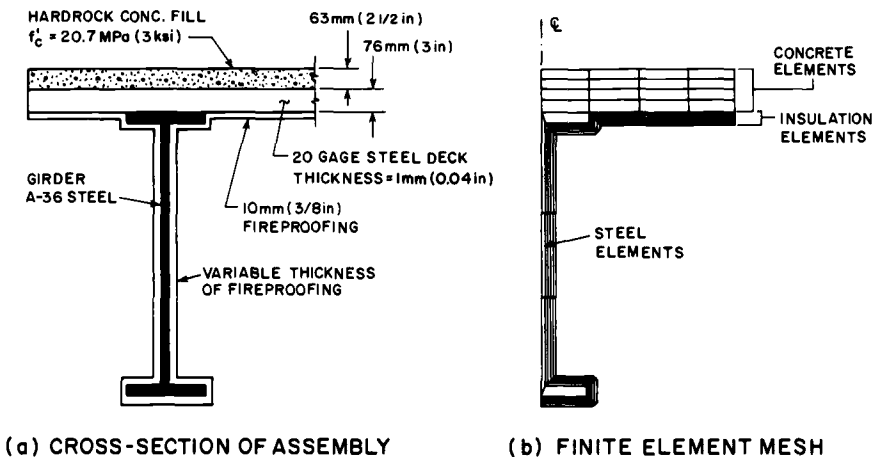


FIG. 3—Computer model for thermal analysis of fireproofed steel beams.

tively). The steel decking is fireproofed with a constant 9.5 mm (3/8 in.) thickness for all cases.

Calculated average steel temperatures for 4 h of exposure to the three different fire curves are shown in Figs. 4 and 5 for the case of W21 $\times$ 50 with 13 and 32-mm (1/2 and 1 1/4-in.) fireproofing, and in Figs. 6 and 7 for the case of W36 $\times$ 194 with 13 and 32-mm (1/2 and 1 1/4-in.) fireproofing. Average temperature corresponds to the definition

$$T_{\text{avg}} = 1/4 (T_{\text{top flange}} + T_{\text{mid web}} + 2 T_{\text{bottom flange}})$$

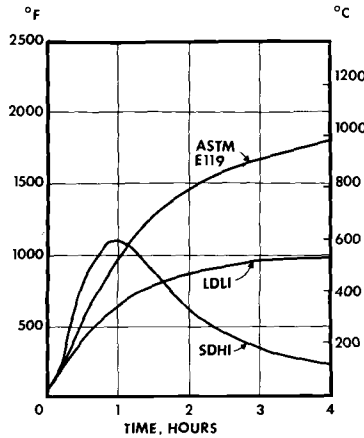


FIG. 4—Average steel temperatures in W21 $\times$ 50 with 13-mm (1/2-in.) fireproofing for different fire exposures.

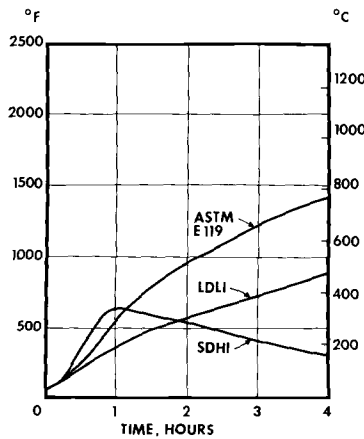


FIG. 5—Average steel temperatures in W21 $\times$ 50 with 32-mm (1 1/4-in.) fireproofing for different fire exposures.

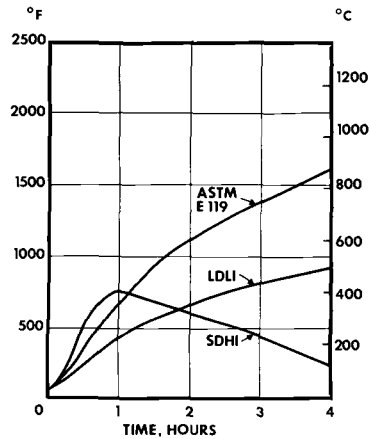


FIG. 6—Average steel temperatures in W36 $\times$ 194 with 13-mm (1/2-in.) fireproofing for different fire exposures.

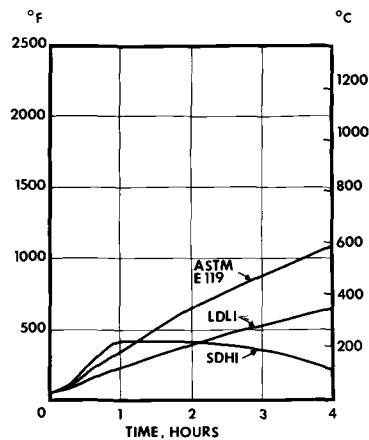


FIG. 7—Average steel temperatures in W36 $\times$ 194 with 32-mm (1 1/4-in.) fireproofing for different fire exposures.

which is based on the customary locations of four thermocouples in a beam cross section. It is readily apparent that the SDHI and LDLI fire models result in much lower steel temperatures than the ASTM E 119 model as shown in Figs. 8–11, where the ratios between SDHI/LDLI and ASTM E 119 temperatures are plotted. The one exception to this pattern is that the SDHI model gives higher temperatures in the early exposure times (less than 1 h). However, for the cases considered, the maximum temperatures during this time do not exceed 590°C (1100°F).

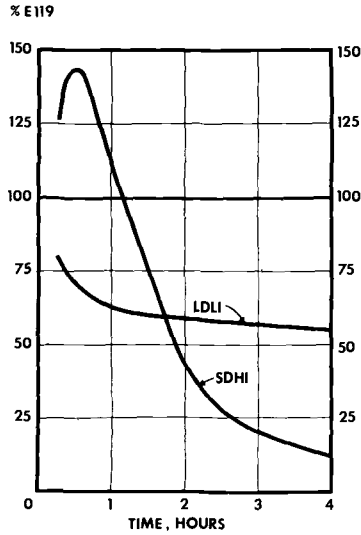


FIG. 8—Ratio between *SDHI* or *LDLI* and *ASTM E 119* average steel temperatures in *W21 × 50* with 13-mm (1/2-in.) fireproofing.

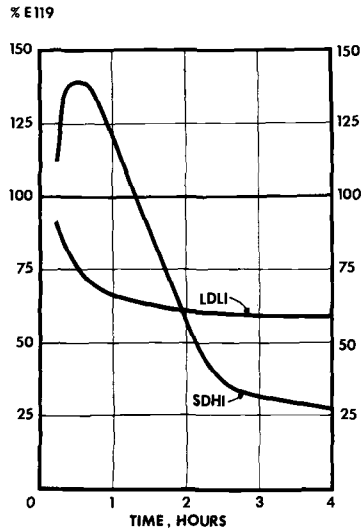


FIG. 9—Ratio between *SDHI* or *LDLI* and *ASTM E 119* average steel temperatures in *W21 × 50* with 32-mm (1 1/4-in.) fireproofing.

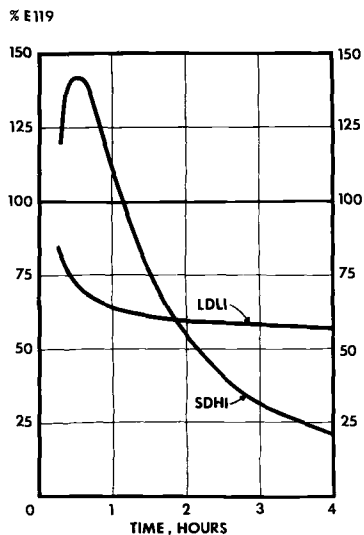


FIG. 10—Ratio between SDHI or LDLI and ASTM E 119 average steel temperatures in W36 $\times$ 194 with 13-mm (1/2-in.) fireproofing.

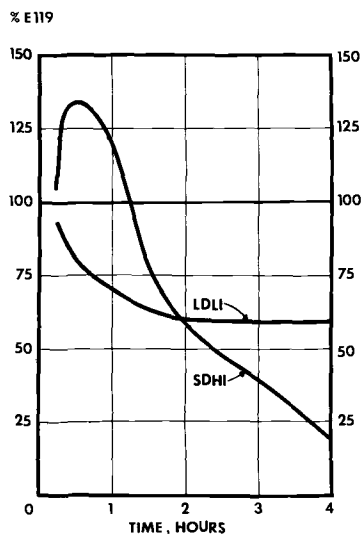


FIG. 11—Ratio between SDHI or LDLI and ASTM E 119 average steel temperatures in W36 $\times$ 194 with 32-mm (1 1/4-in.) fireproofing.

The thermal analyses point out a well-known characteristic of steel beams: lighter beam sizes become hotter than heavier sizes for the same thickness of fireproofing and fire exposure. The relationship between the temperature and beam size is usually related to the weight-to-exposed-perimeter ratio W/p , where

W = weight of beam in pounds per foot (or in kilograms per metre $\times 1.5$ conversion factor), and

p = fire-exposed perimeter in inches (or in centimetres $\times 2.54$ conversion factor)

Average steel temperatures are plotted as a function of W/p ratio and fireproofing thickness in Fig. 12 after 3-h exposure to each fire curve. The temperature data show a very regular relationship between W/p ratio, fireproofing thickness, and temperature and, for these three fire models, permit estimation of temperatures after 3-h fire exposure in a wide range of beam sizes by interpolation without additional thermal analyses. Similar relationships can be plotted for other lengths of fire exposure. Figure 12 points out again that the design fires SDHI and LDLI give rise to significantly lower steel temperatures than the ASTM E 119 fire for exposures greater than 1 h. SDHI average steel temperatures after 3 h are about 50% of the ASTM E 119 values for all W/p ratios, as shown in Fig. 13. Similarly, LDLI steel temperatures are about 60% of the ASTM E 119 values. In practical terms, this means that significantly less fireproofing would be required to meet a particular acceptance criterion.

Structural Response for SDHI and LDLI Fires

Structural response of a steel beam assembly subjected to a specified fire exposure can be calculated using a structural analysis model. Such a model

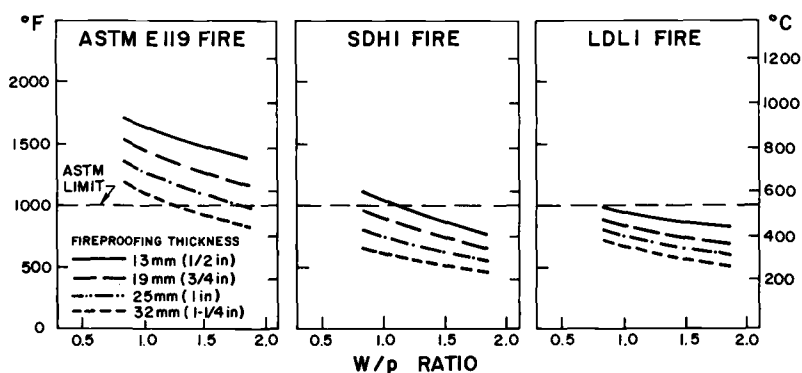


FIG. 12—Average steel temperature versus W/p ratio after 3-h fire exposure.

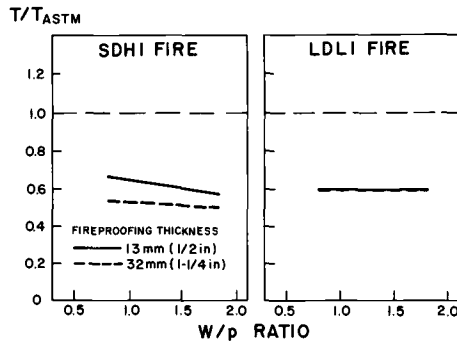


FIG. 13—Ratio between SDHI or LDLI and ASTM E 119 average steel temperatures at 3 h fire exposure for varying W/p ratios.

must account for the nonlinear behavior of steel and concrete under high thermal stress, particularly the effects of yielding and high-temperature creep in steel, and cracking, strain softening, and crushing under high compressive forces in concrete. The computer program FASBUS II [5] has been developed to carry out such an analysis. Composite steel deck floor assemblies, steel girders and beams, and steel columns can be modeled in the program, which requires as input a discretization of the geometry of the assembly, characterization of the temperature-dependent material properties of steel (elastic modulus, yield strength, creep rate), and the member temperatures determined during the previously discussed thermal analysis phase. The results of structural analysis include deformation, stresses, and strains in the assembly as the fire progresses.

Structural response due to the ASTM E 119 fire model was studied using data from a series of tests conducted on simply supported, unrestrained floor beams at Ohio State University [6]. The configuration of these 5.12-m (16-ft, 9 1/2-in.) $W12 \times 27$ specimens is shown in Fig. 14. The specimens with 22-mm (7/8-in.) sprayed-on fireproofing on the steel beams and 13-mm (1/2-in.) fireproofing on the deck support full service load levels with maximum flexural stress of 158 MPa (23 ksi) and are subjected to the ASTM E 119 furnace test. Specimen B1 [6] is chosen as the basis of a structural analysis using FASBUS II. Two analyses are made, one using calculated steel temperatures based on the ASTM E 119 fire and one using calculated temperatures based on the SDHI fire model. All other variables in the computer models are identical. Material properties are strongly temperature-dependent and are summarized in Table 1. It is interesting to note that calculated steel temperatures correspond closely to the thermocouple values measured during the ASTM E 119 fire test of this specimen, as shown in Fig. 15. No experimental data, of course, were recorded for a SDHI fire case during this test program.

For the ASTM E 119 fire exposure case, analytically calculated midspan

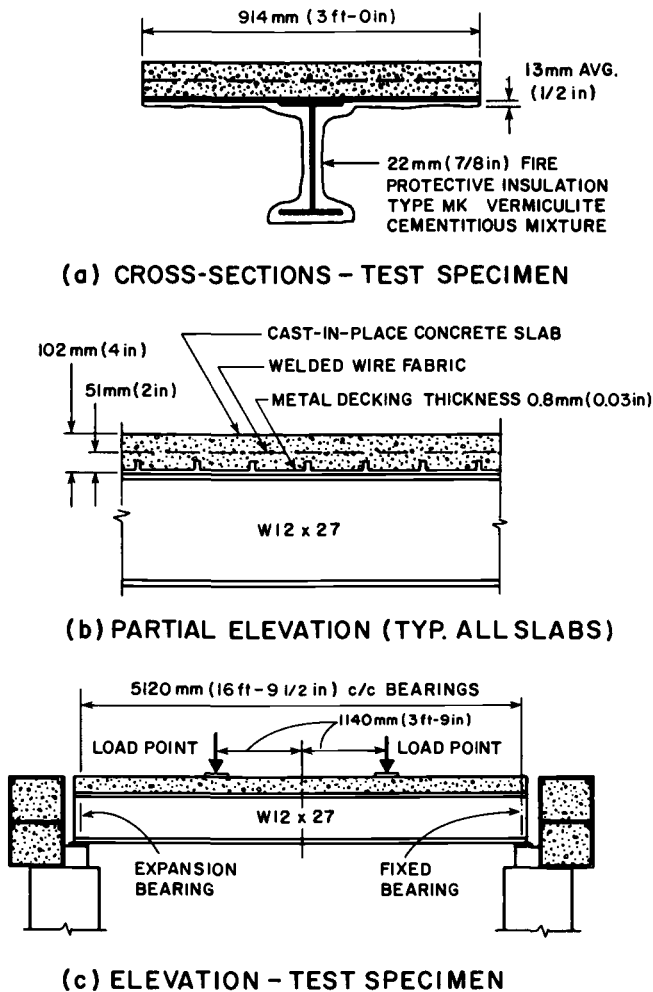


FIG. 14—Test specimen configuration.

deflections are compared with experimentally reported deflections in Fig. 16. Deflections are calculated at 5-min increments; that is, the 5-min temperature increase is applied to the beam and the program iterates until the solution which satisfies equilibrium is found. Deflection increases for each time step as stresses increase, and the portion of the steel beam which is plastic increases in size, as shown in Fig. 17. The analysis corresponded very closely to experimental data up to and including the 90-min increment. At 95 min, the first iteration of the analysis indicates a plastic hinge forming at midspan, (Fig. 17), and subsequent iterations fail to find a solution which satisfies equi-

TABLE 1—Material properties used in analyses.

	Structural Steel		Reinforcing Steel		Concrete ^a	
	Temperature ^b	f_y	Temperature	f_y	Temperature	f'_c
Yield Strength f_y or Compressive Strength f'_c (ksi ^b)	60°F	36.0	60°F	60.0	60°F	2.53
	200°F	36.0	...	...	...	...
	400°F	35.0	400°F	54.0	...	...
	600°F	34.0	600°F	45.0	600°F	2.53
	800°F	28.0	...	...	...	...
	1000°F	20.0	1000°F	18.0	...	...
	1200°F	14.4	1200°F	9.0	1200°F	1.50
	1400°F	8.0	...	...	...	...
	1600°F	5.0	...	...	...	...
	1800°F	5.0	1800°F	3.0	1800°F	1.50
	Temperature	E	Temperature	E	Temperature	E
Young's Modulus E (ksi)	60°F	29 500	60°F	29 000	60°F	2500
	200°F	28 900	...	...	...	...
	400°F	27 700	400°F	27 700	...	...
	600°F	26 000	600°F	26 000	600°F	2000
	800°F	25 200	...	...	...	...
	1000°F	21 300	1000°F	21 300	...	...
	1200°F	14 700	1200°F	14 700	1200°F	1500
	1400°F	5 600	...	...	...	...
	1600°F	5 500	...	...	...	...
	1800°F	4 500	1800°F	4 500	1800°F	1500
	Temperature	α	Temperature	α	Temperature	α
Coefficient of Ther- mal Expansion (in./in./°F) $\times$ 10^{-6}	60°F	6.2	60°F	6.2	60°F	6.0
	200°F	6.5	...	...	...	...
	400°F	6.9	400°F	6.9	...	...
	600°F	7.2	600°F	7.2	600°F	6.0
	800°F	7.6	...	...	...	...
	1000°F	8.0	1000°F	8.0	...	...
	1200°F	8.4	1200°F	8.4	1200°F	10.0
	1400°F	8.8	...	...	...	...
	1600°F	9.1	...	...	...	...
	1800°F	9.5	1800°F	9.5	1800°F	10.0

^aTensile cracking strain of concrete $\epsilon_t = 0.000125$ in./in.^bConversion Factors: MPa = $6.9 \times$ ksi, °C = $0.56 \times$ °C - 32.

librium. Therefore, the computer analysis results indicate failure in the interval 90 to 95 min, which corresponds very well with the observed fire endurance of slightly greater than 90 min. The computer analysis results also give much useful information about stress states and yield zones (Fig. 17) that could not be measured experimentally.

For the SDHI fire case, calculated midspan deflections also are plotted on Fig. 16, and it can be seen that deflection is slightly greater than for the ASTM E 119 fire up to about 70 min but is considerably less after this point.

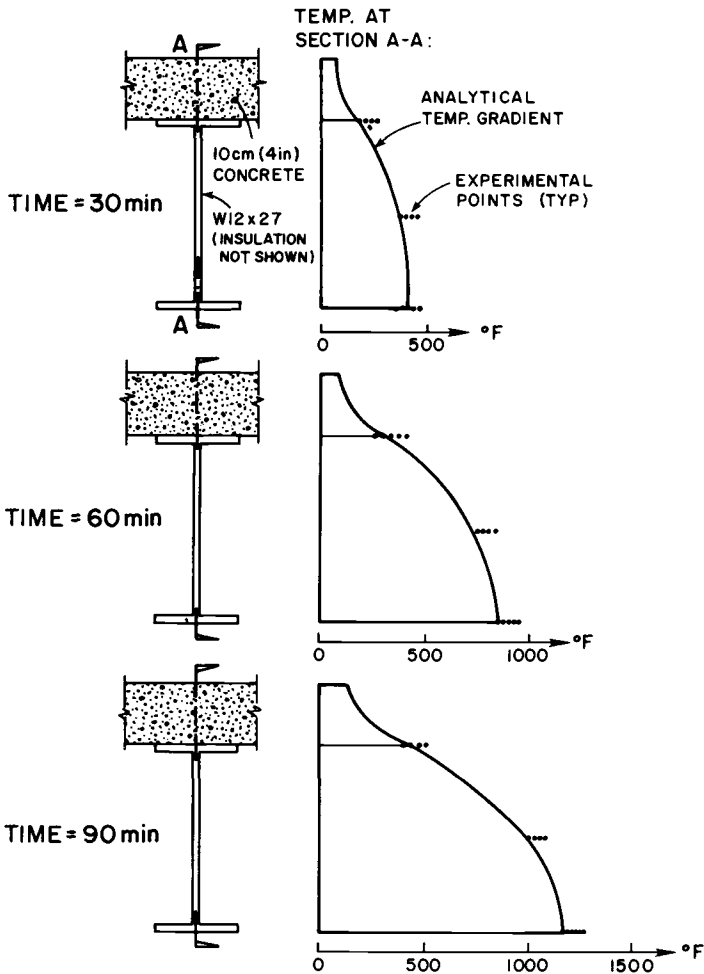


FIG. 15—Comparison of analytical and experimental temperatures in W12 × 27 with 22-mm (7/8-in.) fireproofing exposed to ASTM E 119 fire.

Deflection peaks without structural failure at about 9.5 cm (3 3/4 in.) after 80 min of fire exposure and then gradually relaxes to about 7 cm (2 3/4 in.) by 2 1/2 h of fire exposure. The analysis results show that if this specimen were exposed to the SDHI design fire (rather than the ASTM E 119 furnace fire), it would not collapse under service load but would experience some permanent deformation and high residual stresses. Stress levels and plastic zones in the steel cross section are shown in Fig. 18. The lower flange experiences some yielding at about 45 min of exposure, but stress-related strains return to elastic levels when steel temperatures begin to drop. However, high temperature

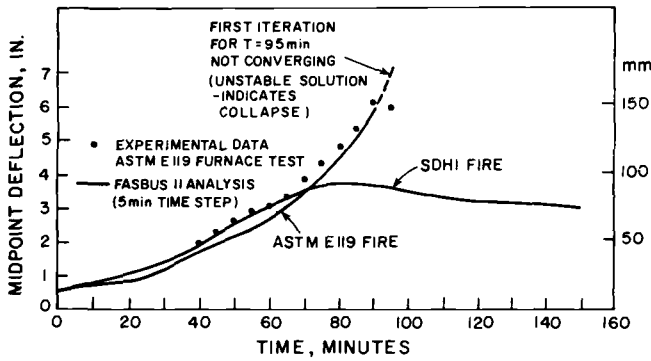


FIG. 16—Calculated structural response of specimen to ASTM E 119 and SDHI fire exposures.

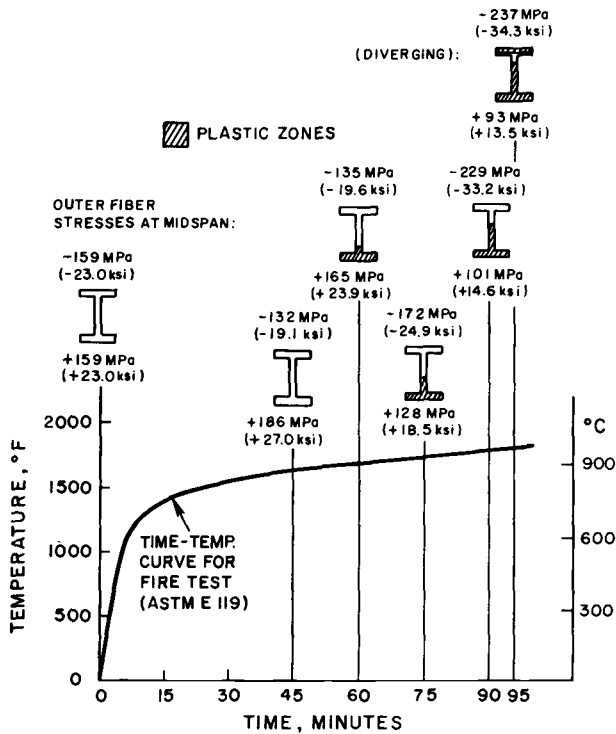


FIG. 17—Stresses and plastic zones at midspan—ASTM E 119 fire case.

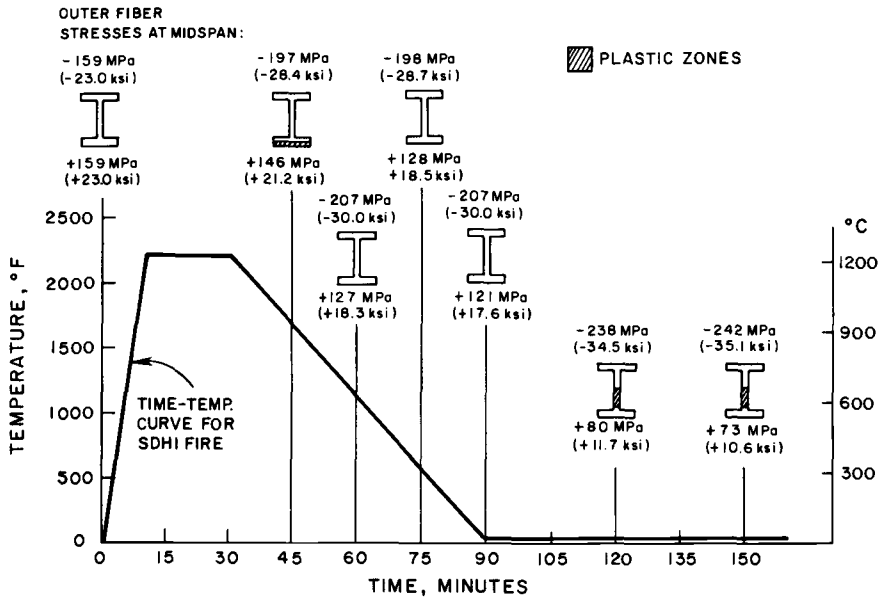


FIG. 18—Stresses and plastic zones at midspan—SDHI fire case.

tensile creep strains continue to accumulate during the first part of the cooling phase, and by the time creep ceases at lower temperatures, considerable residual extension exists in the lower flange. As cooling continues and the thermal stress gradient unloads, large residual stresses develop throughout the cross section. A portion of the web adjacent to the lower flange becomes so highly stressed in the final stages of cooling that a small residual plastic zone develops.

Whether the performance of the beam were acceptable would depend on the acceptance criteria. If the criteria were that the beam carry the superposed load without collapse, then this design would be considered acceptable. However, it is often useful to use the more explicit acceptance criteria proposed by Ryan and Robertson [7]:

Beam, floor or roof construction mounted on end supports and subjected to standard fire exposure under design load shall be regarded as having failed to sustain the load when both the following limits have been reached or exceeded:

$$D \geq \frac{L}{800} \times \frac{L}{d} \text{ and } R \geq \frac{L}{150} \times \frac{L}{d}$$

D = midspan deflection resulting from fire exposure (inches or centimetres),

R = deflection rate (inches per hour or centimeters per hour or $60 \times$ centimetres per minute),

L = span length (inches or centimetres), and

d = distance between upper and lower extreme fibers of structural components (inches or centimetres).

For Specimen B1 where $L = 512$ cm (16 ft, 9 1/2 in.), $d = 30.5$ cm (12 in.), the Ryan and Robertson midspan deflection criterion is 10.7 cm (4.2 in.), and the deflection rate criterion is 57 cm/h (22.6 in/h). At no point during the SDHI fire exposure (Fig. 16) do calculated deflections or the deflection rate reach these limits, so the structural performance of the beam would be considered acceptable, according to the Ryan and Robertson criteria. For the ASTM E 119 fire exposure case, the Ryan and Robertson acceptance limits are almost reached at 90 min when the collapse mechanism begins (Fig. 16) and are exceeded during the numerical divergence process, which indicates structural collapse.

It is important to note that in fire analyses of this type, high-temperature creep can be a very important determinant of behavior. To point this out, the beam was reanalyzed with the FASBUS II creep model removed. Deflections with and without creep are shown in Fig. 19. Without creep, deflections begin to decrease at the same time as the input steel temperatures begin to decrease—at 60 min when the lower flange and web temperatures peak at about 550°C (1030°F). However, with the creep model, deflections increase until about 90 min, when temperatures have cooled sufficiently [to 480°C (900°F)] so that the creep rate becomes less than the thermal unloading rate.

Conclusions

The analyses show that significantly lower temperatures and less structural distress can be expected in steel beams when exposed to a fire model more closely approximating an actual fire than when exposed to the standard ASTM E 119 fire. Such results point out the conservative nature of fire endurance criteria based on the ASTM E 119 fire, especially for fire ratings of 3 or 4

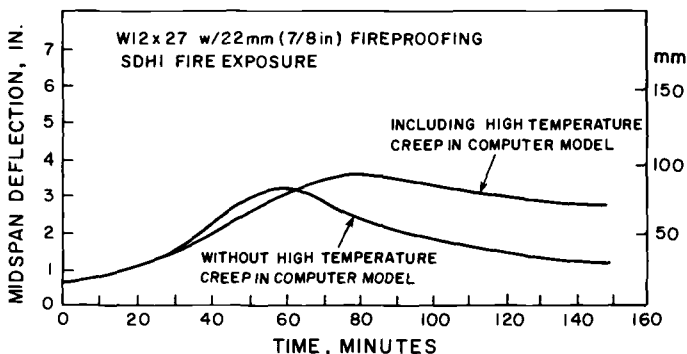


FIG. 19—Calculated midspan deflections with and without high temperature creep model.

h. Design of fireproofing using fire categories of the type proposed is now possible if acceptance criteria (structural or thermal) can be developed and standardized.

Acknowledgments

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David C. Jeanes¹

Computer Modeling the Fire Endurance of Floor Systems in Steel-Framed Buildings

REFERENCE: Jeanes, D. C., "Computer Modeling the Fire Endurance of Floor Systems in Steel-Framed Buildings," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 223-238.

ABSTRACT: The rational approach for the design of building structures to resist the effects of fire has advanced significantly over the past decade. Part of this advancement has been the development of a computer model, FASBUS II, which predicts the structural response of steel-framed floor systems exposed to fire. An evaluation of this model using large-scale test data has demonstrated its ability to accurately predict the influence of heating on the overall performance of these floor assemblies. The approach represented by the traditional test method, Method for Fire Tests of Building Construction and Materials (ASTM E 119-83), contrasts markedly with the engineering-based solution utilized in the FASBUS II model. In order to evaluate this difference, the performance associated with each approach has been characterized and a comparison made. As a result, a relationship between thermal restraint in the test and restraint due to predicted structural interaction has been suggested.

Using the analytical solution represented by FASBUS II, an evaluation of actual details of construction can be made and the interaction between the exposed and unexposed portions of the structure can be examined. Such an analysis provides the designer with information that will contribute to a more accurate and effective means of assessing structural fire endurance.

KEY WORDS: fire endurance, structural response, computer model, FASBUS II, steel frames, floor systems, restrained construction

The development of a method for the rational determination of structural fire endurance is a complex process. To evaluate all aspects of the problem, the solution needs to consider three distinct components: the fire exposure, the transfer of heat from the fire to the structure, and the response of the structure. The solution is complicated by the many variables affecting each of

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these components and by the expertise necessary to make a proper assessment. Combustion chemistry tells us the way fires grow. Thermodynamics explains how heat is transferred from the fire to the structure. Metallurgy defines the effects of high temperatures on the properties of the structural steel. Statistical methods help identify the probable risk. The building authorities specify the level of acceptable performance. Finally, a proper design method needs to account for the combined effect of all the prescribed conditions. Consequently, structural fire endurance has traditionally been handled as a problem separate from the other design requirements of a building. The structural design is made largely independent of any consideration of the thermal effects from fire. Fire protection is then added to the completed assembly in accordance with the established test ratings.

With the advances being made in all areas of design, there is an increasing need to better define the structural conditions that develop during exposure to a building fire. A more realistic fire endurance analysis can be made based on established engineering principles. Computer models now make this approach practical. With the appropriate models, the designer is better able to evaluate the influence of structural response on the performance of supported utilities and systems, the effect of compartment size and “natural” fires on exposure severity, and the potential damage to unexposed portions of the building.

Background

The structural fire endurance of a building system is a measure of its ability to resist collapse during exposure to a fire. The approaches available to make this assessment range from the use of standardized laboratory tests to the application of engineering methods. In either case, a certain level of damage is acceptable provided it does not result in the collapse of any part of the assembly or contribute to the spread of fire, smoke, or hot gases. The influence of a building fire on a structural steel frame is not generally significant until or unless the fire becomes fully developed. Therefore, the initial period of fire growth is not usually considered as part of the structural fire endurance time period.

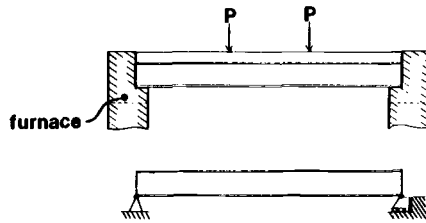
The established approach to determine structural fire endurance is through the use of laboratory tests conducted in accordance with ASTM Method for Fire Tests of Building Construction and Materials (E 119-83). This test is performed on a “representative” portion of a construction assembly mounted in a furnace. The primary objective of the test method is to determine the length of time an assembly can withstand exposure to the controlled test conditions before reaching certain temperature limits. While this approach provides a reasonably simple solution to an otherwise complex problem, it does not reproduce actual structural conditions. To this point, the standard specifically states that it “. . . should not be used to describe or

appraise the fire hazard or fire risk of materials, products, or assemblies under actual fire conditions. . . ."

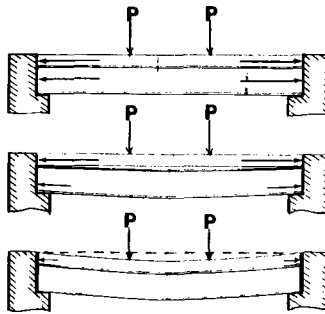
The ASTM E 119 test method allows comparisons to be made of different types of construction but provides limited information on actual performance of a building assembly during exposure to fire. For the testing of floor constructions, the top surface of a furnace is enclosed with the test assembly, such as steel beams and concrete slab, to be used in the building. With the test assembly under load, it is then exposed to a regulated fire. Both the size and span of the steel beams and the aspect ratio of the assembly are limited by the test furnace. The criteria for acceptable structural performance in the case of laboratory tests include visual observations along with the measurement of temperatures in the structural elements. The critical temperature limits are defined based on a relationship between the reduction in material strength and usual design stress levels.

The test assembly is not continuous beyond the furnace walls and does not consider the effect of the fire on the unexposed portion of the structure. However, because the interaction of the framing system is known to influence structural performance, the test method does attempt to recognize the effect of the surrounding structure on resisting expansion of the assembly. Assemblies tested with resistance to thermal expansion are defined as "restrained," while those tested without this resistance are considered "unrestrained." An unrestrained assembly is tested as an isolated simple span with roller supports. There is negligible resistance in the assembly to horizontal or vertical movement. The restrained assembly also can be characterized as a simple span with roller supports, but it is surrounded by a heavy frame to resist thermal expansion at the perimeter of the assembly (Fig. 1a). During the fire exposure, the assembly experiences various degrees of interaction with the "restraining" frame. As the assembly is heated by the fire, thermal expansion is resisted by the frame. During this period of the test, the restrained assembly usually demonstrates a marked improvement in performance over the same assembly tested as unrestrained. As the fire exposure continues, the assembly goes through a complex transition which includes vertical deflections and redistribution of the imposed compression forces. A shift in the line-of-action of the resulting axial force can produce a bending moment in the supporting member. Eventually, the exposure of the assembly results in sufficient vertical deflection that interaction with the restraining frame is no longer sufficient, and the assembly acts as if simply supported. The progressive response of this assembly can be characterized into three general stages of performance (Fig. 1b).

Although the standard clearly identifies the types of construction systems that are considered to be thermally restrained and unrestrained (refer to ASTM E 119, Table X3.1), there is much confusion over the use of these terms when applying the ratings to actual building construction. This is partly due to the fact that "restrained" as understood by the structural engi-



a. Representation of structural support.



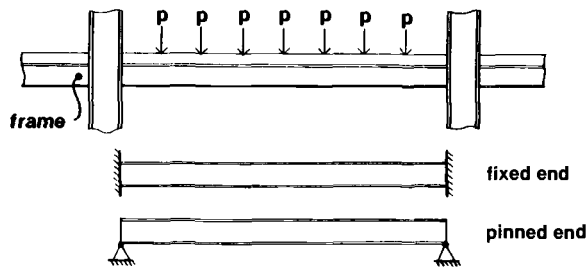
b. Response of Assembly under Increasing Fire Exposure.

FIG. 1—Thermally restrained ASTM E 119 floor test assembly.

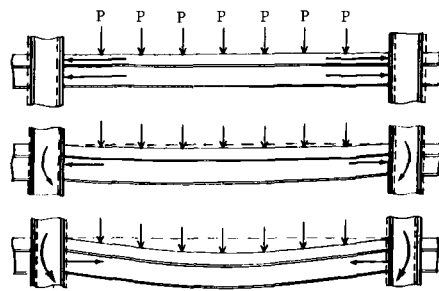
neer bears little resemblance to the conditions in the test. The distinction is that the restrained test assembly only demonstrates resistance to thermal expansion of the fire-exposed portion of the structure, as just described. It does not depend on design structural restraint.

Structural Response

In contrast to the limited evaluation of an isolated assembly represented by the test method, the manner in which a real structure performs during a fire is influenced, to varying degrees, by the interaction of the connected structural elements in both the exposed and unexposed portions of the assembly (Fig. 2a). In general, the structure experiences both axial and moment restraint along the boundaries of the fire-exposed area. Axial restraint is difficult to quantify because of constantly changing conditions. It is influenced by both the relative stiffness of the structure and localized details. During the early portion of the fire-exposure period, the structural member will expand, thereby pushing out on the surrounding frame. The heated member will experience increased compressive stresses. As the exposure continues, the mem-



a. Representation of Structural Support Alternatives.



b. Response of Assembly under Increasing Fire Exposure.

FIG. 2—Structurally framed floor system exposed to fire.

ber will begin to lose strength and deflect. Once deflection is sufficiently large, the span length of the member will be effectively shortened and the member will begin to pull in on the surrounding frame. The axial compressive forces in the heated member decrease while tension increases. As the deflection of the member becomes larger, the restraining moment becomes more significant. These restraining conditions occur at the boundary and supports of the exposed assembly. The result is that the surrounding frame tends to resist further vertical deflection of the exposed portion. Accordingly, the greater the deflection the more significant is the restraining action, both axial and moment, on overall fire endurance. In general, the performance of the frame over the fire endurance period can be characterized by three stages according to the support/reaction conditions (Fig. 2b).

The analysis of the conditions described above, superimposed on the structural assembly, can be accomplished using established principles of engineering mechanics. The analysis, however, needs to recognize the continuously changing properties of the materials at elevated temperatures and inelastic deformation characteristics. With the ability of the computer to quickly solve

iterative-type problems, modeling techniques have been developed making it possible to provide this kind of analysis for steel-framed floor systems.

The Computer Model

The computer model considered in this paper is FASBUS II (*Fire Analysis of Steel BUilding Systems*). It is a structural analysis program specifically designed to analyze the fire endurance of steel-framed floor systems. The product of a long-term research and testing program, the model was originally developed by the Illinois Institute of Technology Research Institute [1]. Subsequent work was done by Wiss, Janney, Elstner, and Associates [2].

The model utilizes the finite element method of analysis where beam elements and triangular plate bending elements are used to represent the frame and slab, respectively (Fig. 3). The incremental solution used by the model provides for consideration of changes in temperature, with corresponding changes in material properties, throughout the exposure period. The material models for both steel and concrete are illustrated in Fig. 4. In addition to the mechanical strains represented by these models, thermal and creep strains also are calculated. Using an iterative process, the model determines the dis-

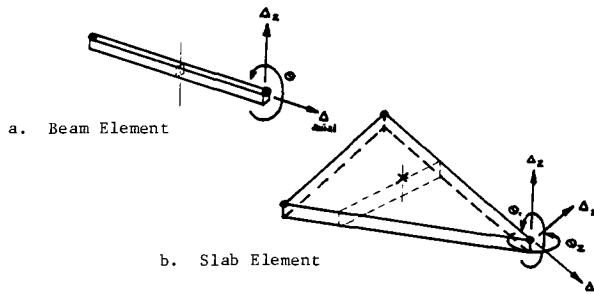


FIG. 3—Modeling elements used in FASBUS II.

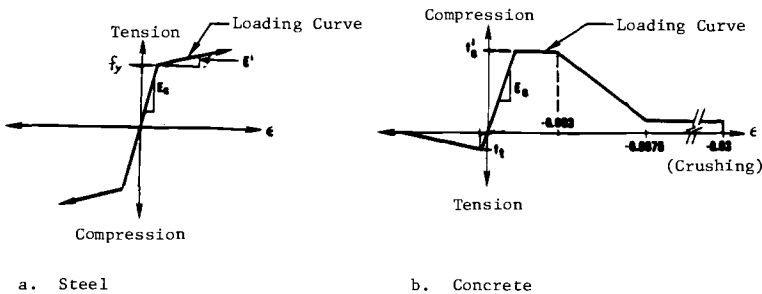


FIG. 4—Material models used in FASBUS II.

placements necessary to bring the structure to a point of static equilibrium under the loads and conditions imposed.

The FASBUS II program is specifically designed to model floor systems consisting of structural steel beams with concrete slabs formed over steel deck. This type of construction, typical of most modern steel-framed buildings, can be of either composite or noncomposite design. The members are normally joined with bolted or welded connections which range from "simple" pinned supports to full moment design. Vertical framing members, such as columns or braces, are not modeled directly by the FASBUS II but are recognized as restricting movement at the points where they connect to the floor assembly. The computer model can be used to evaluate an entire floor system assembly, a portion of the slab and frame, or an isolated beam.

The computer model allows the designer to consider the following construction details and exposure conditions for evaluation in a specific application:

1. Geometry and arrangement of structural elements.
2. Changes in material properties.
3. Application of loads and restraining conditions.
4. Temperature profiles and distributions in the structural members due to the fire exposure.

Several limitations associated with the assumptions used in the FASBUS II computer model are:

1. The material models do not provide for "cool down" conditions.
2. The beam element model does not recognize the capacity of the member to resist forces transverse to the beam, while the beam is assumed to have continuous lateral support.
3. The forces applied to a slab element at the node are assumed to be distributed over the element, and, therefore, localized cracking or crushing conditions in the slab are not always predicted.
4. The vertical frame of the structure is assumed to remain stable, providing continuous support to the floor assembly throughout the exposure period.

The results of a FASBUS II analysis provide the designer with predictions of deflections and rotations across the floor system and stress and strain conditions within the structural members.

Evaluation of the Model

The computer model is based on the mathematical relationships which best define the engineering mechanics applicable to the structural system under consideration. As with any such approach, certain assumptions need to be made to provide a model which is suitably efficient for both design and analysis applications. It is, therefore, desirable to be able to verify the theory and

the assumption contained in the model through the demonstrated agreement between actual and predicted responses of fire-exposed assemblies.

During the development of the FASBUS computer model, it was routinely used to evaluate the results of various fire endurance tests [1,2]. The assemblies investigated included floor slabs with and without framing members, beams with composite and noncomposite slabs, and a reinforced concrete slab model. In all cases, the assemblies were exposed to the time-temperature curve defined by ASTM E 119. In general, it was found that the measured performance was accurately predicted when sufficient detail was recorded to clearly describe the test assembly (with boundary and support conditions) and the structural performance during the fire exposure.

Because the engineering approach represented by the computer model is a significant departure from the laboratory test methods referenced in the building codes, a substantial evaluation of the computer model was necessary. The analysis of data collected from a large-scale test program conducted at the National Bureau of Standards provided the basis for this evaluation [3]. The objective of this test program was to evaluate the response of a structural system representative of actual building construction when a portion of that assembly is exposed to fire. The actual test assembly utilized a two-story, four-bay structural steel frame with a concrete and steel deck floor slab. The frame was fabricated from structural steel beams and columns selected to be representative of the midheight of a midrise office building.

Two types of measurements were recorded during each of the tests, deflections and temperatures. The deflections, which provided an indication of structural response, were measured vertically at 27 points across the area of the test bay and horizontally at 19 points along the outside edges of the exposed slab and the outside corners of the structure. Temperatures were recorded on the structural frame, as well as through the floor slab and in the fire compartment. From these measurements, representative temperature profiles were derived and then used as input to the structural analysis program (FASBUS II).

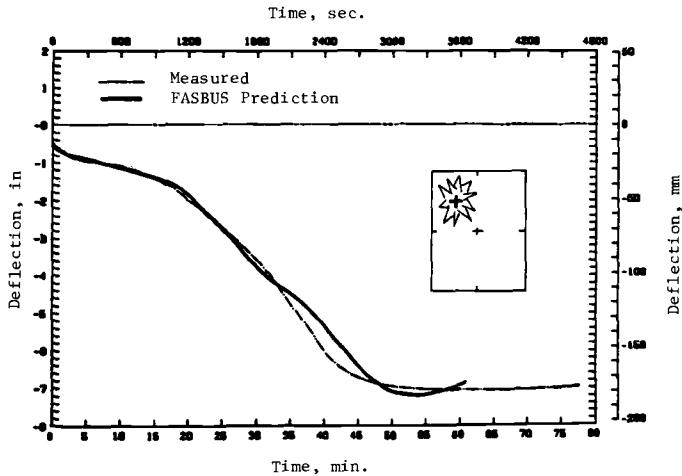
The portion of the structure actually being evaluated by the test was the floor system at the second floor level. This was accomplished by enclosing one full bay of the structure at the first level to form the fire compartment. There were a total of three fire tests conducted on the assembly: two tests involved exposures to the ASTM E 119 time-temperature curve of 90 min and 45 min each, while a third test consisted of exposure to a natural ventilated wood fire of 48.8 kg/m^2 (10 lb/ft^2) of floor area.

Evaluations of the fire exposure conditions recorded during each of the tests have been made using FASBUS II. Comparisons made between the recorded and predicted performance for each set of test conditions demonstrated good agreement for both the deflected shape of the floor assembly and level of damage to the concrete slab and steel frame. Comparisons made between the predicted and recorded vertical movement of a point on the structure over the period of the test as well as the deflected profile at the time of

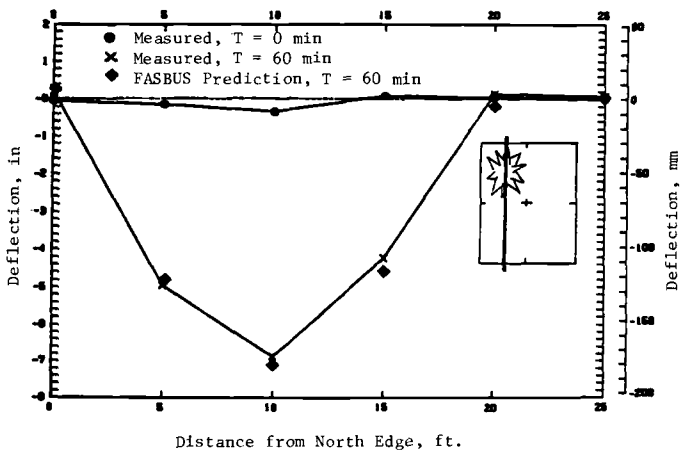
peak fire exposure also resulted in good agreement in all cases. An illustration of these comparisons is provided in Fig. 5.

Application of the Model

With the validation of the FASBUS II computer model, the engineer has a "tool" which can be used in evaluating the fire resistance of steel-framed floor



a. Vertical Deflection at Center Span of Test Bay.



b. Vertical Deflection Across Test Bay After 60 Minutes of Fire Exposure.

FIG. 5—Comparison of test data and FASBUS II analysis for "natural" fire exposure ($1/2$ -in. fireproofing on frame, bare floor deck).

systems. The computer model is more specifically an analytical program than a design program. As such, the model is used to analyze a certain set of exposure conditions of a particular structural assembly to determine the acceptability of the predicted performance.

The result of a successful analysis using FASBUS II provides the designer with detailed data predicting the conditions of strain and deflection over the modeled assembly. The deflections and rotations calculated at each point of the modeled assembly are provided in the form of displacements in the x , y , and z directions and rotations about the x and y axes. By plotting these data the engineer can develop a composite of the overall structural fire endurance by examining the predicted conditions in each of the elements and the displacement of the assembly. Maximum displacements and deflection rates of assemblies then can be evaluated.

The conditions of stress-strain within the individual elements and the structural components they make up are computed through the centroid of the elements (see Fig. 3). The stresses in the elements are based on the computed mechanical strains. The analysis provides a listing for determining the conditions in each layer of the slab, reinforcing steel, and structural steel, thereby allowing the designer to examine the stress profile through the elements. From these data, the development of conditions resulting in yielding in the beam and cracking or crushing in the slab can be observed.

The evaluation process described above not only provides for a detailed examination of the capabilities of the FASBUS II computer model but also results in a demonstration of the relative significance of each of the various conditions and details associated with the modeling effort. To properly apply the model, it is necessary to have a basic understanding of the problem being considered and the solution technique being used. The finite element method of analysis does not provide a "black box" solution to the problem. The user needs to understand the structural characteristics of the construction assembly and be able to define them in terms appropriate to the mathematical expressions used in the model. As a result of parametric studies using the model, certain conditions and details were found to be particularly important to an accurate analysis. They include the relative stiffness of the assembly, the method by which each of the structural components are linked together, the character of the fire exposure, and the limitations appropriate to the model. The following discussion summarizes these findings:

Composite Assembly—The structural floor system is easily modeled as a compositely designed assembly. Satisfactory modeling of noncomposite floors of the same type of construction can be accomplished by eliminating the effective eccentricity between the slab and frame (that is, $e = 0.0$).

Beam Connections—Where beam connections are designed to develop the moment capacity of the beam, the beam elements are modeled as continuous members. However, if the beam is simply supported at the ends, the discontinuity introduced by the connection must be reproduced.

Columns—For purposes of analysis, the columns are assumed to remain structurally stable throughout the exposure period. Recognition of the influence of the columns on restraining the floor slab against lateral movement can be considered by applying resistance equivalent to the stiffness of the column at the point at which the column is connected to the floor frame.

Exposure Temperatures—The temperature history of the structural elements must be derived from experimental data or by analytical methods such as the FIRES-T3 computer model [4]. The FASBUS II model then is used to analyze the performance of the structural elements at the assigned temperature distributions over the depth of a member and across the assembly.

Material Properties—The material properties, as a function of temperature, used in the structural analysis are yield strength, modulus of elasticity, and coefficient of thermal expansion.

The material is further characterized by a plastic modulus (given as a ratio of the elastic modulus) and a subroutine for creep [based on ASTM Specification for Structural Steel (A 36-81a)].

Discussion

Many of the conditions and details influencing actual structural fire endurance are too complex to evaluate outside the test furnace without the use of appropriate computer models. Such models must be evaluated in order to demonstrate the validity of the design parameters and conditions of analysis. The FASBUS II computer model has been evaluated and found to provide a valid approach for the analysis of structural response of steel-framed floor systems exposed to fire. Using this model, a realistic prediction of structural performance can be more realistically accomplished.

Restraint

Much of the original interest in the development of an engineering solution, represented by the computer model and test program, was in response to confusion over the influence of thermal restraint on the performance of an assembly in a furnace test, as discussed earlier. While it is possible to model an assembly as constructed for a furnace test, the transient conditions of structural support are complicated and limited information is recorded during such tests to adequately compare structural performance. Such an analysis would be of limited value since the results would not directly predict structural fire endurance of actual building construction.

An evaluation of the practical significance of the restrained assembly ratings, however, was made considering a structural floor system protected using published rating criteria. The system analyzed was the corner bay of a building frame with a stiffened core and noncomposite slab (Fig. 6). This approach required the determination of fireproofing thicknesses for the floor system

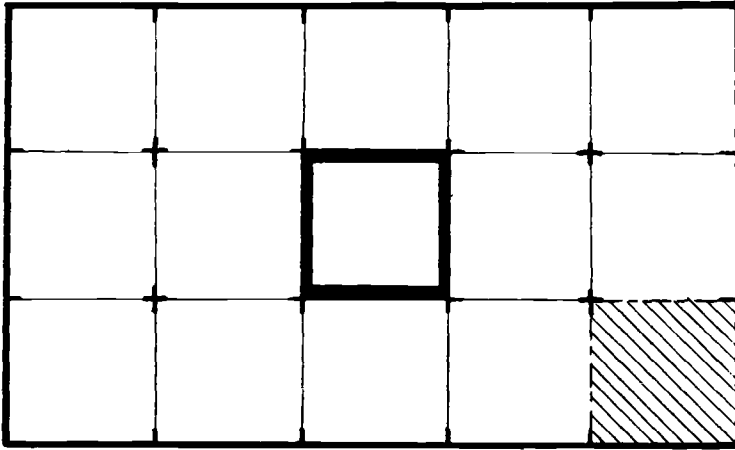


FIG. 6—Corner bay of sample building frame with stiffened core used for evaluation.

based on “designs” contained in the Underwriters Laboratories’, Inc. *Fire Resistance Directory* [5]. A thermal analysis was then made of the protected assembly using the FIRES-T3 computer model. The fire exposure was taken as the ASTM E 119 time-temperature curve over a fire endurance period of 2 h, as specified by the building code. The structural performance of the assembly was determined using the FASBUS II computer model. From this analysis it could be predicted that the structural frame, protected using fireproofing thicknesses based on the restrained test ratings, would remain stable through the specified fire endurance period.

The fire response of building construction tested in the furnace is different from that same construction analyzed as part of the building structure. To bridge the gap between the test method and engineering approach, the relative stability of each method was examined. The stability of the assembly can be best characterized by the vertical deflection curve developed during the evaluation. For the restrained assembly test, the stability is maximum during the first and second stages of response and then begins to diminish rapidly during the third stage, as identified in Fig. 7. This third stage usually indicates a “run away” condition typical of the unrestrained beam assembly. For practical reasons, the test will normally be terminated near the beginning of this stage. The fire endurance rating for a restrained assembly is the time recorded up to this point (provided it does not exceed twice the unrestrained rating period). In contrast to the test assembly, the analysis of the steel-framed floor system indicates a more gradual shift in the structural response (Fig. 8). The transition between each stage of structural response indicates that a more stable assembly is provided by the structural interaction of the entire framing system.

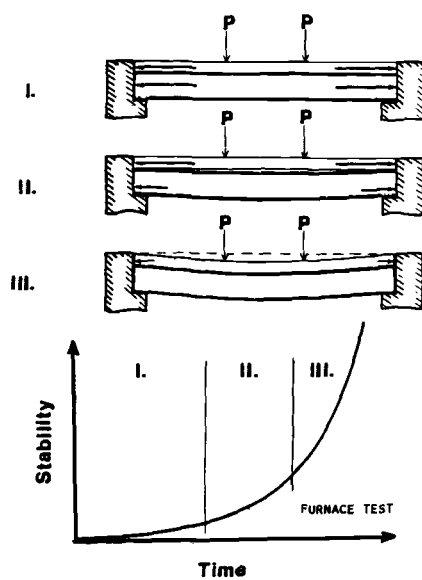


FIG. 7—Characterization of stability of floor system in the test furnace.

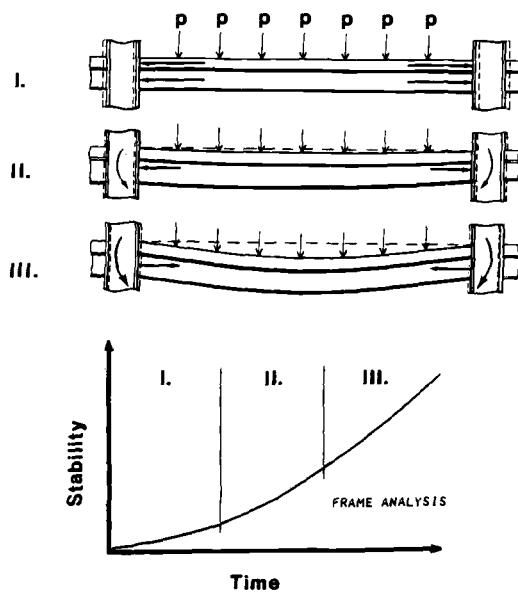


FIG. 8—Characterization of stability of fire-exposed steel-framed floor system.

Based on the analysis of the derived two-hour rated floor assembly, a comparison can be made between the test method and the engineering calculations represented by the computer model. This comparison is best made by examining the stability characteristic curve associated with each method (Fig. 9). While the conditions of structural support in each approach are clearly different, there is demonstrated agreement in the fire endurance rating. The improved performance due to thermal restraint during the first several stages of the restrained furnace test appears to compensate for the beneficial performance resulting from the interaction of the structural building frame throughout a fire. Such a comparison suggests that the restrained ratings from the ASTM E 119 test method more accurately demonstrated the structural fire endurance of steel-framed buildings than the simply supported system of the unrestrained rating. However, because of the many variables in both the test and the structural designs, further evaluations should be made to determine if this is a universal relationship.

Considerations for Use

The application of computer models to determine the fire endurance of structural assemblies obviously represents a change from the long-established practice of using laboratory tests. The result is that the designer can now more effectively address the design of the structure to resist thermal loads due to fire in a building.

Aside from offering a rational solution for structural fire endurance, the value of the computer model approach is that it provides an ability to evaluate the design features and details unique to a particular design and type of construction. FASBUS II provides for this type of analysis as it applies to typical steel-framed floor systems.

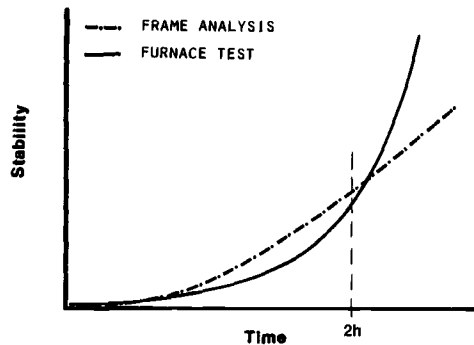


FIG. 9—Comparison between structural fire response of test assembly and floor frame.

Conditions of a particular structural assembly that may benefit from such an analysis are the following:

1. Size or span of the structural member is sufficiently different from those typically included in the furnace test assembly.
2. Design stress levels in the members are different from those typically assumed in the test.
3. Design loads or load combinations that call for different levels of protection during exposure to fire (beams in some structures are sized on the basis of drift control, wind loads, or earthquake forces).
4. The structural assembly surrounding the exposed construction provides restraint which will influence the fire endurance characteristics (particularly where the building code does not recognize the use of ASTM E 119 restrained ratings).
5. Examining the effects of the fire on the surrounding unexposed portions of the structure (portions of the structure not directly exposed to the fire can be damaged).
6. Evaluating the influence of structural response to the fire on supported or attached systems and assemblies.
7. Assessing trade-offs in structural fire endurance versus compartment size, controlled combustible contents, and sprinkler systems.

The application of the analytical techniques for determining structural fire endurance is now permitted in most building codes under the general provisions for "alternates" to the prescribed code requirements. The first practical application of the analytical approach represented by the FASBUS II computer model was made on a 42-story office building [6]. The interest of the designer developed when he considered the fireproofing requirements placed on the large spandrel beams to be excessive. These beams, designed to resist earthquake forces, provided substantial bracing to the structure through moment connections at the columns. The fireproofing requirements for the 3-h rating specified by the code would ordinarily have been based on the fire test results of a simply supported beam significantly smaller in size than those in the structure.

Using the temperature history conditions predicted from a FIRES-T3 analysis of the 3-h period, the structural analysis was made using FASBUS II. From the results of the analysis it was clearly demonstrated that the structural members could perform satisfactorily with fireproofing thicknesses reduced to 1.9 cm ($3/4$ in.). This same analytical approach has now been made and accepted in several different jurisdictions.

Summary

The application of the FASBUS II computer model for determining the structural fire endurance of steel-framed floor systems represents a signifi-

cant departure from the laboratory test methods traditionally used. Utilizing engineering mechanics to calculate the performance of actual building construction, FASBUS II provides the designer with a tool that develops a detailed prediction of the deflection, rotations, stress, and strain conditions within steel-framed floor systems.

The computer model employs the finite element method of analysis, and therefore does not provide a "black box" solution to the problem. The user must have a basic understanding of the nature of the problem being solved and the solution technique used. The proper evaluation of the predicted condition can provide the designer with information on the structural performance of the assembly, response of the surrounding unexposed structure, and the influence of these conditions on attached systems and assemblies.

The validation of computer modeling to predict the fire endurance of steel-framed floor systems is the result of a substantial evaluation program. This approach has demonstrated the accuracy and validity of the FASBUS II computer model. The application of the model has been described by identifying conditions under which the analysis might be most beneficial with consideration given to the present building code requirements.

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New Concepts for Measuring Flame Spread Properties

REFERENCE: Quintiere, J. G. and Harkleroad, M., "New Concepts for Measuring Flame Spread Properties," *Fire Safety: Science and Engineering*, ASTM STP 882. T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 239-267.

ABSTRACT: An experimental procedure is described which can be used to derive data relevant to the prediction of ignition and flame spread on materials. This procedure offers a direct method for determining material property data suitable for use in mathematical fire models. The apparatus utilizes a radiant heat source capable of supplying up to 6.5 W/cm² to a vertically oriented specimen. The test results pertain to piloted ignition of a vertical sample under constant and uniform irradiation and to lateral flame spread on a vertical surface due to an external applied radiant heat flux. The results can be used to display the maximum velocity and ignition time as a function of irradiance. Critical or minimum irradiances for spread and ignition are determined. An empirical correlation, based on heat conduction principles, is found to correlate the ignition data and also provides a more general interpretation for the flame spread results. Further analyses of the data yield effective values for the thermal inertia of the material ($k\rho c$), its ignition temperature, and a parameter related to flame temperature. These parameters appear to be phenomenological constants for each material, rather than factors dependent on the apparatus. Results are presented for a wide range of materials. Suggestions for extending the results to other flame spread conditions are presented.

KEY WORDS: flame spread, ignition, materials, properties, measurement method, modeling

Nomenclature

- b Parameter in Eq 5
- c Specific heat
- C Parameter in Eq 8
- D Damkohler number (see Refs 1,11,12)

¹Group head and physicist, respectively, Center for Fire Research, National Bureau of Standards, Washington, DC.

$F(t)$	Thermal response function, Eqs 1, 2, 5
g	Gravitational acceleration
h	Heat loss coefficient
h_c	Convective heat transfer coefficient
k	Thermal conductivity
l	Length scale
$\dot{q}''$	Heat transfer per unit time and per unit area
$\dot{q}_{o,ig}''$	Critical flux for ignition
$\dot{q}_{o,s}''$	Critical flux for spread
$\dot{q}_e''$	External radiant flux
t	Time
t_m	Characteristic equilibrium time, Eq 5
T	Temperature
T_f	Adiabatic flame temperature
T_∞	Ambient and initial temperature
T_s	Surface temperature before flame effects
T_{ig}	Ignition temperature
$T_{s,min}$	Minimum temperature for spread
V	Flame (pyrolysis front) velocity
V_a	Opposed gas velocity
x	Distance along sample
y	Coordinate normal to sample-exposed surface
$Y_{ox,\infty}$	Mass fraction of oxygen in ambient
ρ	Density
Φ	Flame heating parameter, Eq 8
δ_f	Flame length
σ	Stefan-Boltzmann constant

Test methods for the flammability of materials differ widely among and within countries. Yet, research on flame spread and ignition has developed to permit, at least conceptually, a universal framework for their description. Moreover, certain essential material data are needed to enable the researcher to assess his theories and correlations against experimental results. Consequently, it appears logical to organize that process so that material flammability tests might supply the data needed in theoretical models of ignition and flame spread. This paper will present such a process.

In considering flame spread, it is realized that it depends on the orientation of the material and on the conditions of the surrounding fluid. It is interesting that several principal standard flammability tests consider distinct modes of flame spread. Generally, flame spread can be divided into spread in an opposed gas flow (that is, the ambient gas flow is in the opposite direction to the propagating flame front) and spread in a concurrent flow (that is, the local gas flow is in the same direction as the propagating flame). The opposed flow case is typified by ASTM Test for Surface Flammability of Materials Using a

Radiant Heat Energy Source (E 162-83), in which the flame spread is downward on an inclined sample and the opposed flow is buoyance-induced primarily by the flame. In contrast, ASTM Test for Surface Burning Characteristics of Building Materials (E 84-84) is an example of concurrent spread along a ceiling-mounted material in which the ambient flow is held fixed. Also, ASTM Test for Critical Radiant Flux of Floor-Covering Systems Using a Radiant Heat Energy Source (E 648-78) is another example of opposed flow spread under natural convection for an upward facing horizontal surface. Except for ASTM Method E 648, these test results are expressed in terms of indices which have no relationship to particular physical or chemical data. However, this does not preclude the possibility of an interpretation of these data in more scientific terms. To derive these scientific results, mathematical solutions of the flame spread processes are needed.

The literature on flame spread and ignition is abundant, and reviews on flame spread by Fernandez-Pello and Hirano [1] and on ignition by Kanury [2] provide good sources of information. From results found there and in particular from analyses by deRis [3] on opposed-flow spread and Sibulkin and Kim [4] on upward (concurrent) spread, it can be shown that common combustible materials respond to flame spread and to ignition (for exposures $> 2 \text{ W/cm}^2$) as thermally thick materials for thicknesses greater than approximately 1 mm. Thus, the semi-infinite solid solutions for ignition and spread will most often apply and may even apply to thin materials on a thick substrate where some effective properties of the composite solid must be considered. Yet, under low heat transfer conditions, as would occur in the early development of compartment fires, the thickness of the solid and the conditions at its back surface are important to its temperature rise. Thus, under these developing fire conditions, the upstream surface temperature can be assumed to be the initial temperature of the solid. In other words, even when a temperature gradient is present in the solid, only about 1 mm of its depth responds to the flame heat transfer, and the temperature over that depth represents the initial temperature. Fernandez-Pello [5] has shown that this is a valid representation for downward spread under external radiant heating.

From an examination of the simplest solutions for flame spread [3,4] under the thermally thick assumption, several common parameters emerge. One is the product of the solid's conductivity, density, and specific heat ($k\rho c$); another is a critical surface temperature to produce fuel vapors from the solid—this can be considered as an "ignition temperature." Since the theoretical results do not consider melting, charring, regression, or temperature effects on the solid's properties, the parameter $k\rho c$ must be considered as an effective property when such theories are applied to realistic flame spread conditions. Also, the ignition temperature must be viewed as a modeling parameter in the formula, rather than the exact surface temperature of the solid when a flammable mixture is produced. Moreover, both of these parameters occur in the simplest models for ignition [2]. Hence, the determination of these quan-

ties offers a consistent way of relating test data to theoretical models. For flame spread, however, these two parameters are not sufficient to describe the various modes of spread. From deRis's result for opposed flow spread, at least a flame temperature is required, and for concurrent spread, the downstream flame length and flame heat transfer to the surface are required [4]. The former case is perhaps resolvable and will be examined here. The prospects of resolving the latter case will be discussed later.

The present work considers a method for deriving the parameters: $k\rho c$, ignition temperature, and a flame temperature factor suitable for use in models for piloted ignition and opposed flow flame spread. The apparatus used here was developed by Robertson [6]; subsequent studies [7,8,9] describe the evolution of the use of the apparatus and the interpretation of its results. Here, we attempt to provide a more general interpretation of these previous results, a compilation of results for a wide range of materials, and a guide for others who wish to explore this procedure.

Experimental Procedure

The apparatus consists basically of a gas-fired (air-natural gas) radiant panel and a framed sample-holder assembly to contain a vertically oriented sample. A schematic of the arrangement is shown in Fig. 1. The radiant panel is inclined from the sample orientation so as to yield an incident heat flux distribution along the center of the sample as shown in Fig. 2. By varying the fuel-air flow rate to the panel, the incident heat flux at 50 mm could be varied from 1.5 to 7 W/cm². Thus, the normalized heat flux result in Fig. 2 represents the geometric radiation factor for this apparatus and served as a calibration reference in subsequent flame spread measurements.

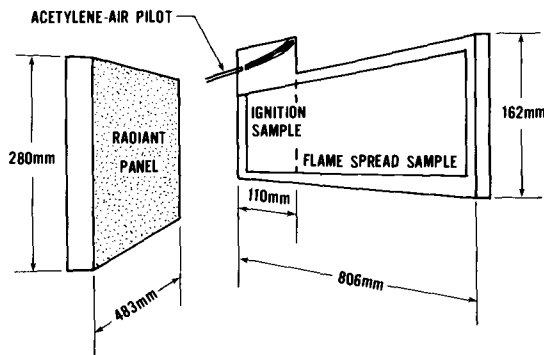


FIG. 1—Schematic of test apparatus.

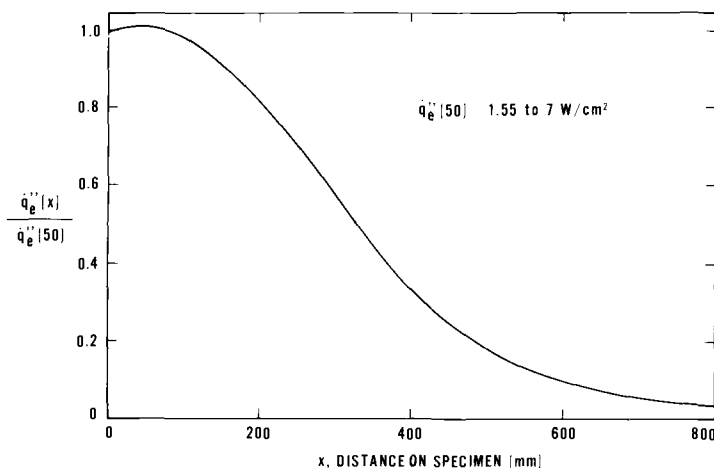


FIG. 2—Normalized irradiance over the sample.

Ignition

Ignition results were obtained for small samples mounted over the 0 to 110-mm region of nearly uniform heat flux as shown in Figs. 1 and 2. Initially the incident heat flux was measured at the 50-mm position. Once stable conditions were recorded, the sensor was removed and the sample was mechanically mounted in place. The mounting process could be done in several seconds or less, so that ignition times of this magnitude will be subject to uncertainty. It was found important to upwardly extend the plane of the sample surface with a steel plate. This enabled the boundary layer containing the pyrolyzed gases and the induced air flow to be maintained above the sample. An acetylene-air pilot flame located approximately 25 mm from the top of the sample and 5 mm from the steel extension plate interrupted this boundary layer mixture. This plate and pilot arrangement ensured a sustained pilot flame, ignition based on the mixture concentration generated at the sample, and a propensity for flame propagation upstream through the boundary layer to the sample face. Sustained ignition was noted and its time recorded when continuous combustion was visible at the center of the specimen. In most cases this was unambiguous, but, on approaching the minimum flux for ignition ($\dot{q}_{o,ig}''$), oscillatory or incomplete surface combustion was sometimes observed. In some cases of, for example, melting or delaminating materials, judgement was necessary in deciding on a primary ignition time when spurious effects occurred. The data were recorded for a series of decreasing flux conditions until a minimum flux, below which ignition would not occur, was found. An example of a typical set of results is shown in Fig. 3 for fiber insulation board. Here, no ignition would occur below 1.4 W/cm^2 .

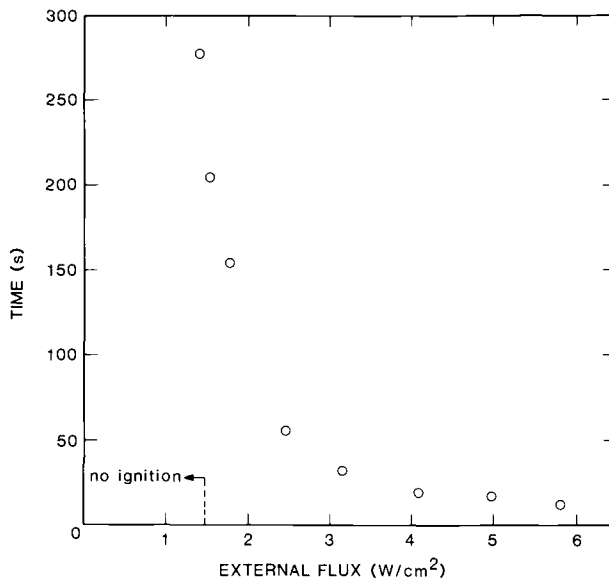


FIG. 3—Ignition results for fiberboard.

Flame Spread

A test for flame spread was initiated by selecting the proper external heat flux at the 50-mm position. If the ignition pilot flame was used, the external heat flux was required to exceed the minimum flux for ignition. The optimum procedure is to adjust $\dot{q}''(50)$ to be slightly greater than $\dot{q}_{o,ig}''$. In obtaining the flame spread data, it should be realized that the ignition process itself is unimportant, but $\dot{q}''(50)$ must be greater than $\dot{q}_{o,ig}''$. Indeed, several ignition techniques were explored and two distinct processes will be illustrated.

The sample size for flame spread spans the frame length as shown in Fig. 1. Once the external heat flux was set and stabilized, the calibration board was removed and the sample was inserted in place. This was done manually and took several seconds. Following sample ignition, a flame propagated in the direction of decreasing external flux (Fig. 2). Its position (x) was recorded as a function of time measured from the instant of sample insertion. Varying degrees of curvature of the flame front were observed so that the flame position was based on the centerline of the sample. The curvature of the propagating front was primarily caused by the nature of its initial shape following ignition. It was not obviously dependent on sample edge or air flow effects. Except near extinction, the curved front advanced uniformly. These midfront positions were recorded until the flame stopped. Two examples of results are shown in Figs. 4 and 5. Figure 4 illustrates results for a fiber glass-based roofing shingle in which $\dot{q}''(50)$ was set at 3.0 W/cm² compared to its $\dot{q}_{o,ig}''$ of 2.1 W/cm².

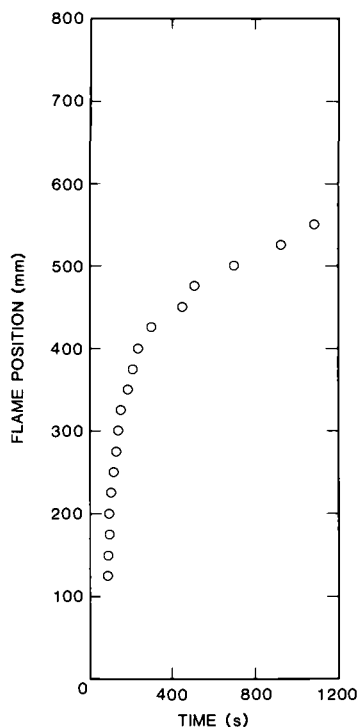


FIG. 4—Flame position as a function of time for a fiber glass roofing shingle.

The ignition pilot was used and ignited the sample in slightly more than 1 min. The results in Fig. 5 are for a hardboard wood product which was exposed to a heat flux distribution of $\dot{q}_e''(50) = 3.0 \text{ W/cm}^2$ for more than 11 min before a pilot flame initiated ignition. This long preheating method was used to ensure that thermal equilibrium would exist under flame spread conditions. Although considerable charring and gasification occurs under long preheat conditions over the region where $\dot{q}_e''(x) > \dot{q}_{o,ig}''$, this should not affect the flame spread results. To ignite the sample in this case, a small diffusion flame was slowly moved along the bottom edge of the sample in the x -direction until ignition occurred. The idea was to initiate the flame spread process at $T_s(x) = T_{ig}$. Regardless of the ignition process, surface flame spread will only occur where the external heating conditions cause surface temperatures less than the ignition temperature. Once ignition temperature is reached at the surface, this implies that sufficient degradation (volatilization, charring, etc.) is present to sustain ignition. Hence the introduction of a pilot flame at these conditions will cause surface ignition. Although rapid gas-phase propagation is perceived in the preheating operational mode, it does not relate to "surface" flame spread, and the data are not recorded or are applicable as

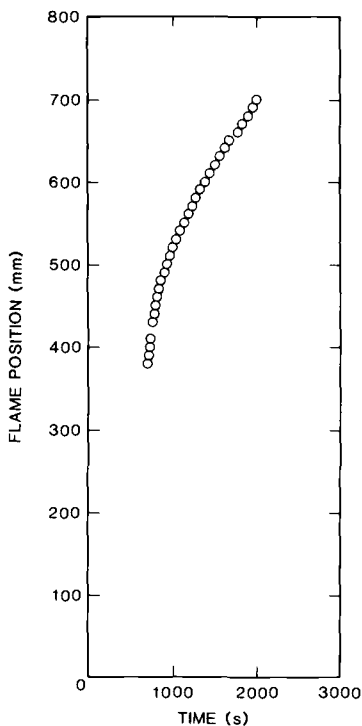


FIG. 5—*Flame position after delayed ignition of hardboard.*

such. Most of the data were taken when either of these two ignition techniques was used to initiate flame spread. In both cases a preheating time at a position x was achieved before the arrival of the flame front. It will be shown that the analysis of the data can account for these variations in preheat times.

Sample Preparation

All samples tested were conditioned at approximately 55% relative humidity and 20 to 22°C. Aluminum foil was wrapped around the edges and back to prevent the release of volatiles from these surfaces. The samples were supported in the spring-loaded frame by a calcium silicate board 13 mm thick. Other backing materials of lower density and conductivity were employed to a limited extent and were found to have an insignificant effect on the results.

Analysis

The objectives in analyzing the data have been to seek consistent results for a material, to simplify the process of analysis, and to generalize the results

beyond the context of the test apparatus. Since a test procedure has evolved with practice, no attempt has been made to standardize testing, to automate the data analysis, or to evaluate statistical deviations. The emphasis has been on describing the physical significance of the data and streamlining the procedure. The following analysis forms the bare essence of the general features of ignition and flame spread. It is intended as a framework for interpreting data in a generally useable form for prediction. It does not purport to address all possible factors that could represent a specific material. Where features such as melting, variable properties, and diathermancy are present, they would be accounted for by effective values of the properties in the simple inert thermal model described subsequently.

Ignition

The framework for analyzing and interpreting the ignition data is the thermal conduction model for ignition with a critical surface temperature. This is fully discussed by Kanury [2] and was utilized successfully by Simms [10] in ignition studies of wood materials. Those results suggest that the form of solution for the surface temperature (T_s) under constant external radiant heat flux ($\dot{q}_e''$) can be given as

$$T_s - T_\infty = \frac{\dot{q}_e''}{h} F(t) \quad (1)$$

where

T_∞ = the ambient and initial temperature,

h = the surface heat loss coefficient, and

$F(t)$ = a function of time and thermal properties of the solid.

Although an exact expression can be given for $F(t)$ under conditions of constant properties and a thermally thick (semi-infinite) or a finite thick solid, it will be determined empirically. This approach is motivated by the limits of the thermally thick solid solution and by the preponderant form of the ignition results in these tests. For the semi-infinite solid under these heating conditions, it can be shown [7] that

$$F(t) = \begin{cases} \frac{2h\sqrt{t}}{\sqrt{\pi k \rho c}}, & t \rightarrow \text{small} \\ 1, & t \rightarrow \text{large} \end{cases} \quad (2)$$

The thermal model for ignition is that $T_s = T_{ig}$ at ignition, and thus the ignition time can be determined from an expression of the form of Eq 1. More-

over, the critical radiant flux is found at long time where $F(t)$ becomes asymptotic to unity. Thus, from Eq 1

$$\dot{q}_{o,ig}'' = h(T_{ig} - T_{\infty}) \quad (3)$$

when $F(t) \rightarrow 1$. Here, the heat loss coefficient is understood to include both radiative and convective effects. In general, the steady-state energy balance which holds after long heating time can be written as follows

$$\dot{q}_e'' - \dot{q}_k'' = \epsilon\sigma(T_s^4 - T_{\infty}^4) + h_c(T_s - T_{\infty}) \equiv h(T_s - T_{\infty}) \quad (4)$$

where

- $\dot{q}_k''$ = the conductive loss into the solid due to its finite thickness,
- ϵ = the surface emissivity, which is usually greater than 0.8 for common combustibles under infrared conditions, and
- h_c = the convective heat transfer coefficient.

For the conditions and materials used in this study, it can be estimated that $\dot{q}_k''$ is less than 10% of $\dot{q}_e''$.

Surface temperature measurements as a function of external radiant flux are shown in Fig. 6 for several materials. The idealized material represents $\dot{q}_k'' = 0$ and $\epsilon \approx 1$, while a low bulk density aircraft panel and a wood particle board demonstrate departure from the ideal conditions. A theoretical result based on $\epsilon = 1$ and $h_c = 15 \text{ W/m}^2 \text{ K}$, characteristic of the natural convection conditions in the test apparatus, provides a good overall fit to the idealized data. It is assumed that this theoretical curve, applicable to the test apparatus, can be used to infer surface temperature for a material under long-time heating conditions. Thus, from an experimental determination of $\dot{q}_{o,ig}''$, T_{ig} can be found from the theoretical curve in Fig. 6 and h can be subsequently computed from Eq 3. The data in Fig. 6 suggest that the true surface temperature can be as much as 50°C below the inferred idealized temperature.

Within the framework of Eqs 1-3, an empirical result has been found to successfully describe all of the ignition results from the apparatus. The results have been correlated by the expression

$$\frac{\dot{q}_{o,ig}''}{\dot{q}_e''} = F(t) = \begin{cases} b\sqrt{t}, & t \leq t_m \\ 1, & t \geq t_m \end{cases} \quad (5)$$

where, for each material,

b = a constant, and

t_m = a characteristic time indicative of the time to reach thermal equilibrium, since $F(t) = 1$ at $t = t_m$.

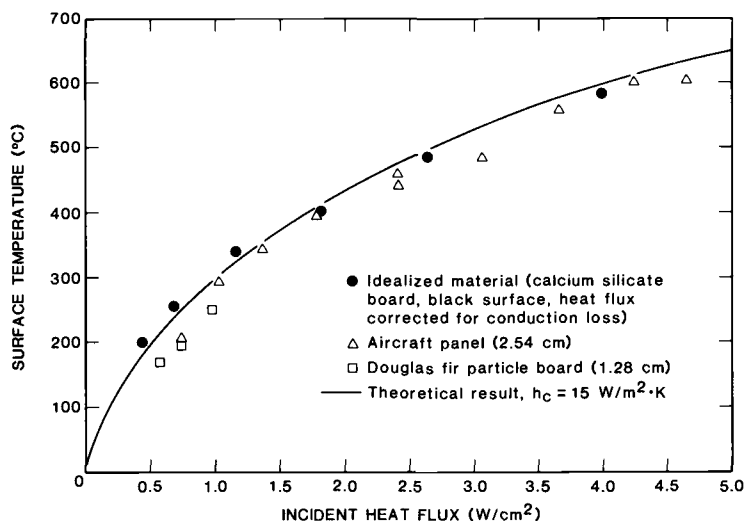


FIG. 6—Equilibrium surface temperatures as a function of external radiant heating in the test apparatus.

An example of a typical fit to the data is shown in Fig. 7 for the results of the fiber insulation board (Fig. 3). The parameters b and t_m are found from the slope and intercept, respectively. This correlation, although not theoretically justified, must account for variable property effects which are significant for the range of temperatures encountered and must account for the effect of heat

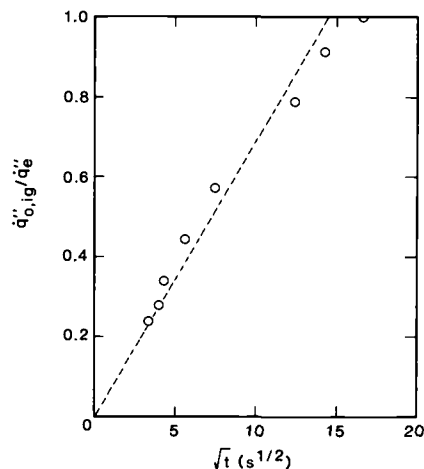


FIG. 7—Correlation of ignition results for fiberboard (see Fig. 2).

losses from the back of the sample and the backing board. Thus, this result is characteristic of the apparatus to some extent.

Having obtained Eq 5 to represent the ignition results, it can be related to the thermally thick result in Eq 2. Since Eq 5 implies that the results hold for small times as well as up to t_m , then the parameter b can be related to $k\rho c$. Hence, an effective $k\rho c$ can be computed

$$k\rho c = \frac{4}{\pi} \left(\frac{h}{b} \right)^2 \quad (6)$$

Here, the value for h is determined at T_{ig} from Eq 3.

Flame Spread

The formula derived by deRis [3] for the thermally thick case can be used as a framework to analyze and generalize the flame spread data. That result, modified to account for an elevated solid temperature, is given as

$$V = \frac{V_a (k\rho c)_g (T_f - T_{ig})^2}{k\rho c (T_{ig} - T_s)^2} \quad (7)$$

where

- $(k\rho c)_g$ = the gas-phase properties,
- T_f = the flame temperature, and
- V_a = the opposed flow gas velocity.

Several studies, both experimental and theoretical [1], have used this formula to correlate results for a wide range of conditions including chemical kinetic effects which reduce the spread velocity to a point where extinction occurs. Notable among them are the work of Fernandez-Pello et al [11], who examined the effect of ambient oxygen concentration and opposed velocity on flame spread over horizontal polymethyl methacrylate (PMMA), and the work of Altenkirch et al [12], who examined the effects of oxygen, gravity, and pressure on the downward spread over PMMA. These studies show that the spread velocity normalized by the value given in Eq 7, for which T_f is computed as the stoichiometric adiabatic flame temperature, is solely a function of a Damkohler number (D), which represents the ratio of the residence time of the gas in the flame to the chemical reaction time. Thus, they find that this dimensionless spread velocity decreases from a value of approximately 1 to some finite value at extinction as D decreases, or, in other words, as chemical kinetic effects become relatively more important. These results provide important guidance for generalizing opposed flow spread results for a wide range of conditions, but do not address their applicability to different materials.

In order to deal with differences among materials, Eq 7 is represented in this analysis as

$$V = \frac{\Phi}{k\rho c(T_{ig} - T_s)^2} \quad (8)$$

where Φ can be considered to represent the numerator of Eq 7 and any effects of the Damkohler number under the test conditions. Thus Φ depends on the test conditions, but some insight can be given to extending it. For example, under the lateral (or downward) spread conditions in the apparatus, V_a is induced by natural convection, and that characteristic velocity is given by [3]

$$V_a = \left[\frac{\left(\frac{k}{\rho c} \right)_g (T_f - T_\infty)}{T_\infty} \right]^{1/3} \quad (9)$$

For typical values in air [for example, $T_f = 2080^\circ\text{C}$, $T_\infty = 22^\circ\text{C}$, $(k/\rho c)_g = 0.216 \times 10^4 \text{ m}^2/\text{s}$, $g = 9.8 \text{ m/s}^2$], it is found that V_a is 11 cm/s, a value consistent with measurements [2]. Also, opposed flow speeds of this magnitude or less tend to have an insignificant effect on the spread rate [11]. Thus, this suggests that the natural convection conditions of the apparatus configuration mitigate effects of V_a on the results, in contrast to the sensitivity of spread rate to V_a at high oxygen concentrations. But extension to higher flow speeds in air must require a modification of Φ based on its dependence on V_a in accordance with the correlations of Fernandez-Pello et al [11].

Another factor influencing Φ is seen to be the flame temperature. Under stoichiometric and adiabatic conditions, it can be shown [12] that this temperature is approximately given by

$$T_f = \frac{Y_{ox,\infty} \Delta H_{ox}}{c_g} \quad (10)$$

where

$Y_{ox,\infty}$ = the ambient oxygen concentration,

c_g = the specific heat of the gas, and

ΔH_{ox} = the heat of combustion per unit mass of oxygen, which is generally a constant (13 kJ/g) for most fuels.

Equations 7, 8, and 10 suggest that Φ depends on $Y_{ox,\infty}^n$, where $n \geq 2$, and, in fact, it is found that n increases as extinction conditions are approached [1]. Moreover, the effects of nonadiabaticity, decreasing Damkohler number, and other departures from the ideal conditions assumed in Eq 7 should decrease Φ . Under natural convection conditions with values for PMMA at $Y_{ox,\infty} = 0.23$ with an adiabatic flame temperature of 2080°C and an ignition

temperature of 390°C [11], Φ was computed as $10 \cdot (\text{kW})^2/\text{m}^3$. Thus it is expected that Φ should range from this order of magnitude to smaller values for materials tested in air under natural convection conditions. For other conditions, the value of Φ must be modified accordingly.

Equation 8 then forms the basis for analyzing the test data. For direct application it must be put in terms of the heating conditions encountered in the apparatus. Several assumptions are necessary. First, it is assumed that one-dimensional conduction applies at any position x as shown in Fig. 2. From Fig. 2, an appropriate length scale in the x -direction, which accounts for the flux distribution, was taken as

$$l_x = \frac{\dot{q}_e''(x)/\dot{q}''(50)}{\frac{d}{dx} [\dot{q}_e''(x)/\dot{q}''(50)]} \quad (11)$$

which can be greater than 200 mm. If l_y is taken as the thickness of the material, say $l_y \leq 10$ mm, then conduction in the x -direction compared to conduction in the y -direction is $(l_y/l_x)^2 \leq 1/400$. Hence the one-dimensional assumption is justifiable. Second, the surface temperature rise is assumed to be given by Eq 1 where $F(t)$ is given by Eq 5. This is an empirical, but reasonable, approximation, as illustrated by the results shown in Fig. 8 for particle board and in Fig. 9 for an aircraft honeycomb multilayered panel. The measured values for $F(t)$ were computed from surface temperatures using Eq 1 for

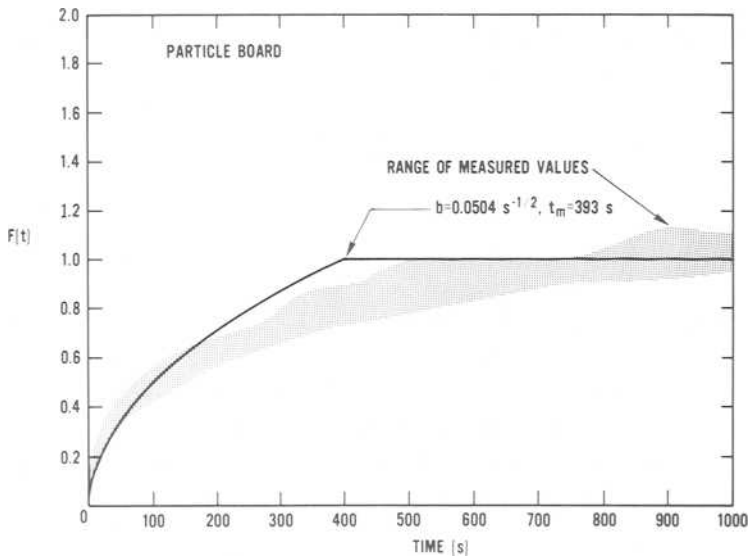


FIG. 8—Comparison of measured and approximate values for $F(t)$: particle board.

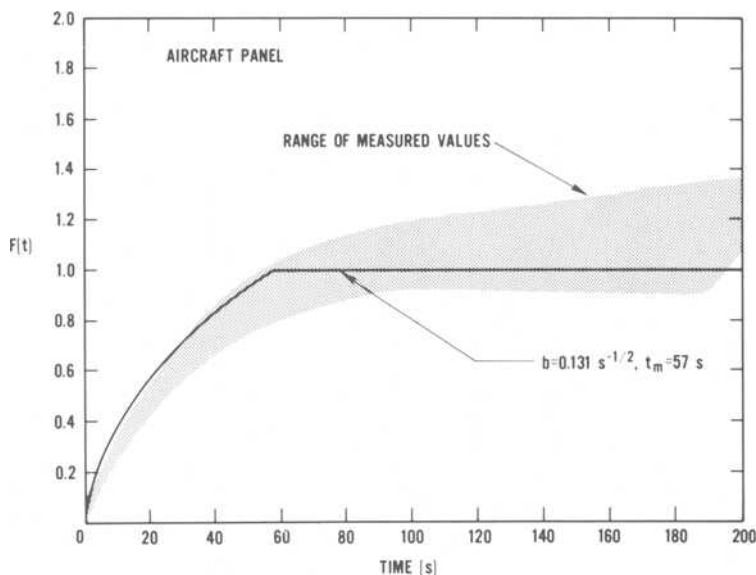


FIG. 9—Comparison of measured and approximate values for $F(t)$: aircraft panel.

several $\dot{q}_e''$ conditions. These are the same materials illustrated in Fig. 6 at steady-state conditions. With these assumptions, (that is, using Eqs 1 and 3), Eq 8 is expressed as

$$V^{-1/2} = C[\dot{q}_{o,ig}'' - \dot{q}_e''(x) \cdot F(t)] \quad (12)$$

where C is a constant related to Φ . This equation can be used to analyze the flame spread data as illustrated in Figs. 4 and 5. Although $F(t)$ must be determined from the ignition data, $\dot{q}_{o,ig}''$ can be found directly from the spread data as well as from the ignition data. The process proceeds as follows. The local slope of the position-time data is computed to yield the velocity as a function of x or equivalently $\dot{q}_e''(x)$. Herein, a three-point running least-squares fit was used to find V . The function $F(t)$ is applied to $\dot{q}_e''$ at the corresponding flame position and elapsed time from the initiation of heating. By plotting these results as $V^{-1/2}$ against $\dot{q}_e'' F(t)$, both C , the slope, and $\dot{q}_{o,ig}''$, the intercept, can be found. Figures 10 and 11 illustrate this process for the data corresponding to the fiber glass shingle of Fig. 4 and the hardboard of Fig. 5, respectively. For the shingle $F(t) < 1$ for most of the data, while $F(t) = 1$ for all of the data from the hardboard test. Although not clearly shown by these data, it is common to find departures from this $V^{-1/2}$ relationship at large values of $V^{-1/2}$, where extinction sometimes appears to cause a greater negative slope, and at small values of $V^{-1/2}$, where greater uncertainties are likely

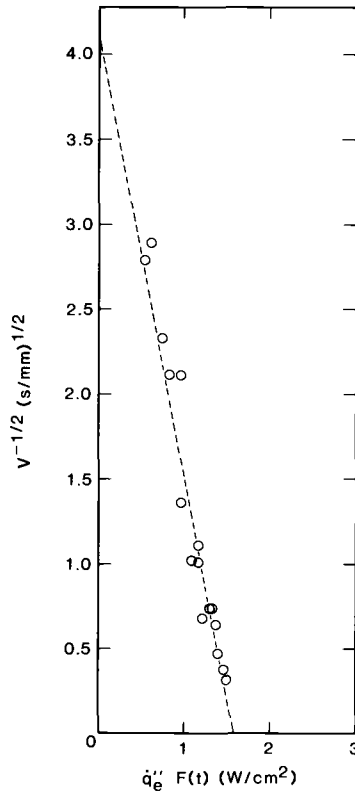


FIG. 10—Correlation of spread velocity for a fiber glass shingle (see Fig. 4).

in the data. Nevertheless, this analysis yields $\dot{q}''_{o,ig}$, C , and $\dot{q}''_{o,s}$, the minimum external flux for spread.

Agreement between $\dot{q}''_{o,ig}$ found from this process and $\dot{q}''_{o,ig}$ found from the ignition experiments demonstrates consistency and the complementary relationship between piloted ignition and flame spread. Having found these values, their counterparts in the temperature formulation can be computed. It follows from Eqs 1, 3, 6, 8, and 12 that

$$\Phi = \frac{4}{\pi} / (Cb)^2 \quad (13)$$

A minimum surface temperature for spread ($T_{s,min}$) can be found from Fig. 6 and $\dot{q}''_{o,s}$, the irradiance at the position the flame stops.

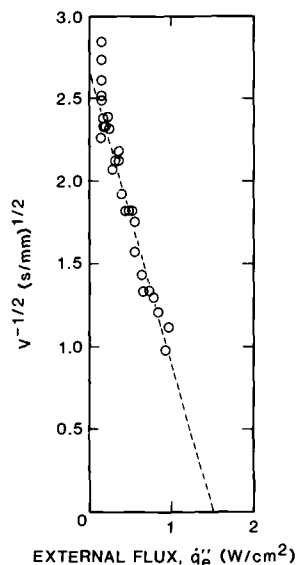


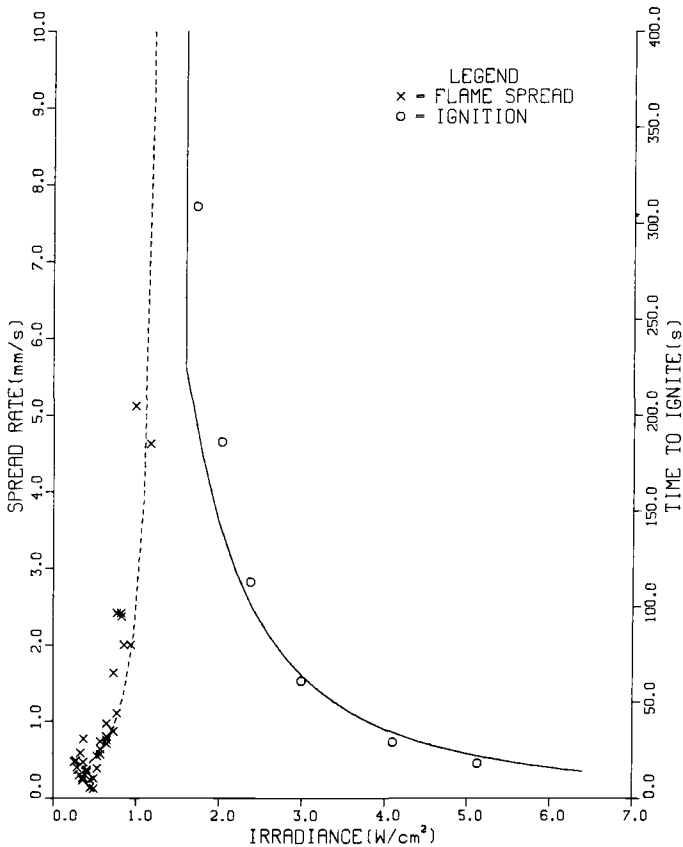
FIG. 11—Correlation of spread velocity for hardboard (see Fig. 5).

Results

Although the parameters Φ , $k\rho c$, T_{ig} , and $T_{s,min}$ are the most useful in generalizing these results, it is interesting to consider the maximum spread velocity [$F(t) = 1$] and the ignition time as a function of the external radiant heat flux. This might be considered as a “flammability diagram” for the material over the range of heating conditions in the apparatus. It applies under conditions of constant irradiance and for flame spread under long heating times. Several of these diagrams are shown for a varied set of materials: plywood (Fig. 12), rigid polyurethane foam (Fig. 13), gypsum board (Fig. 14), an asphalt shingle (Fig. 15), and a cast PMMA (Fig. 16). All are shown at the same coordinate scales; the analytical curves according to the values of C , b , etc. are shown with the data, and only velocity data for which $F(t) = 1$ or $t \geq t_m$ is plotted. These results show a range of heating conditions, $\dot{q}_{o,s}'' \leq \dot{q}_e'' \leq \dot{q}_{o,ig}''$, over which only opposed flow flame spread can occur; and a domain, $\dot{q}_e'' > \dot{q}_{o,ig}''$, for which only piloted ignition is possible. Of course, flame spread can occur for $\dot{q}_e'' > \dot{q}_{o,ig}''$, but only for heating times below which $T_s < T_{ig}$. Since $\dot{q}_{o,ig}''$ is considered to be the same for ignition and flame spread in the theory, the vertical asymptotic values of the spread and ignition results should coincide. The extent to which they do indicates the consistency in the results and perhaps their accuracy as well.

A more extensive listing of results is given in Table 1. It should be pointed

PLYWOOD, PLAIN (1.27cm)

FIG. 12—*Spread and ignition results for plywood.*

out that the materials tested came from several sources, all of whom were interested in the performance of these materials in fire. In many cases, therefore, additional measurements were made for these materials. In Table 1 the materials have been listed by their generic names and grouped into categories of woods, plastics, carpets, and composites (laminates). Painted surfaces have been included with the wood materials. Except for the hardboard painted with nitrocellulose paint, all of the laminated materials displayed a single flame front. However, delamination of the surface coating preceded flame spread for some laminates. The case of the nitrocellulose paint displayed an initial rapid surface spread due to the paint. This was then followed by a slower spread with continued burning behind the front. Only this latter result has been reported due to the rapidity of the first phenomenon. The

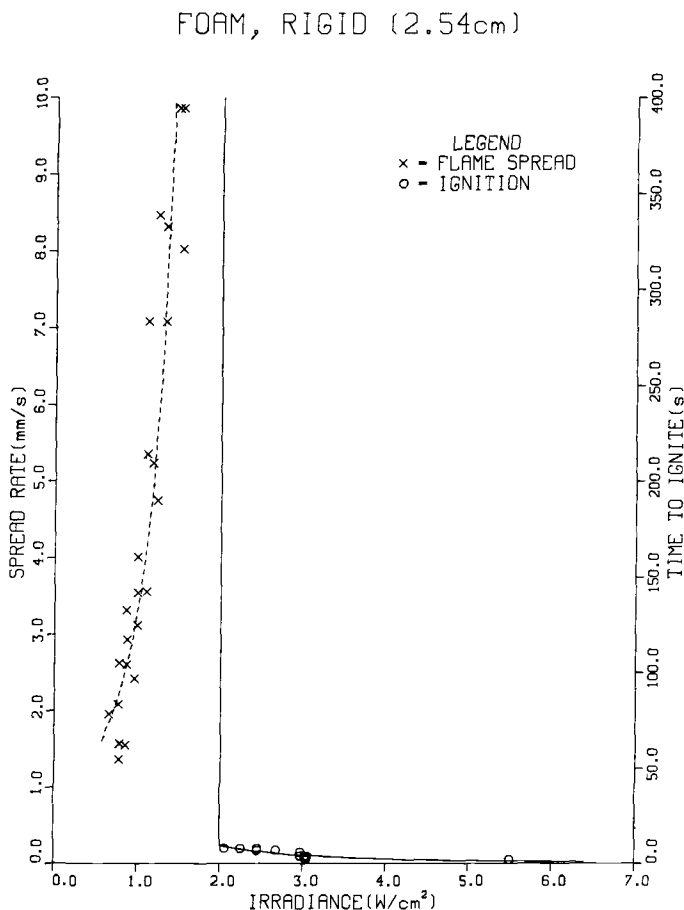


FIG. 13—Spread and ignition results for rigid polyurethane foam.

results presented in Table 1 can be used to construct the flammability diagrams as shown in Figs. 12-16. The applicability of these diagrams depends on the heating conditions. In contrast, Table 2 lists results that are more general and can be construed as "flame spread properties." There, the results are ordered in terms of ignition temperature. These results, the ignition temperature, the minimum temperature for spread, the effective $k\rho c$, and the Φ value all can be used to predict piloted ignition and opposed flow spread under natural convection conditions regardless of the heating conditions. Only the surface temperature need be computed for the specific heating conditions. Also, it is recognized that an estimation of $k\rho c$ from Eq 2 and the ignition data reported may be too approximate to be acceptable. Consequently, $\Phi/k\rho c$ is reported to skirt this possible uncertainty since $\Phi/k\rho c$ is the primary

GYPSUM BOARD, COMMON (1.27mm)

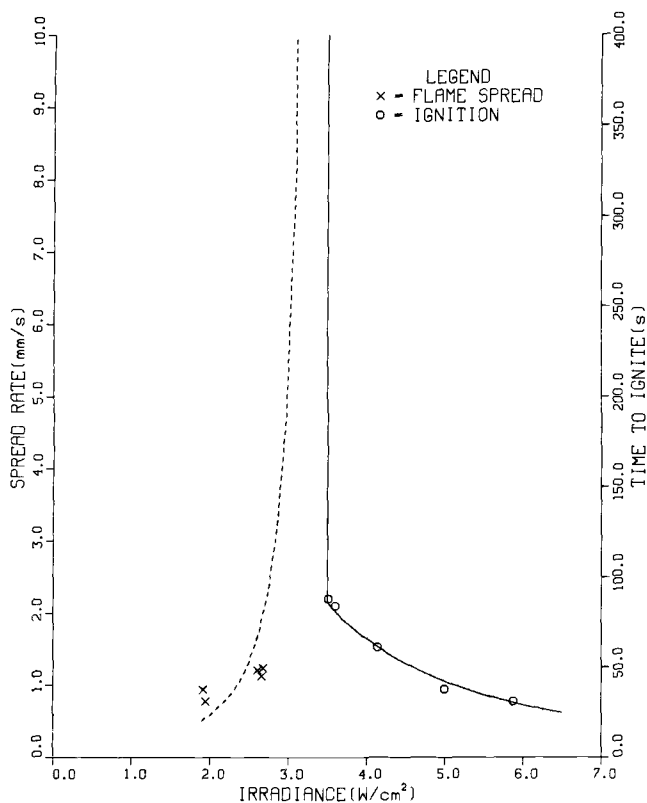


FIG. 14—Spread and ignition results for gypsum board.

parameter needed anyway. Moreover, when ignition data were not recorded for a material, the ignition temperature and $\Phi/k\rho c$ values were derived for Table 2 by using the flame spread data alone. In all other cases, T_{ig} was derived from the ignition data. In many cases the flame spread results were derived from several tests in which the heating conditions varied with respect to irradiance and preheating times.

Discussion

Although no direct check on the $k\rho c$ values was attempted, a compilation of thermal properties for common materials under normal environmental conditions is given in Table 3. Those values for $k\rho c$ appear to be generally lower than the correspondingly similar materials listed in Table 2. Several

ASPHALT SHINGLE

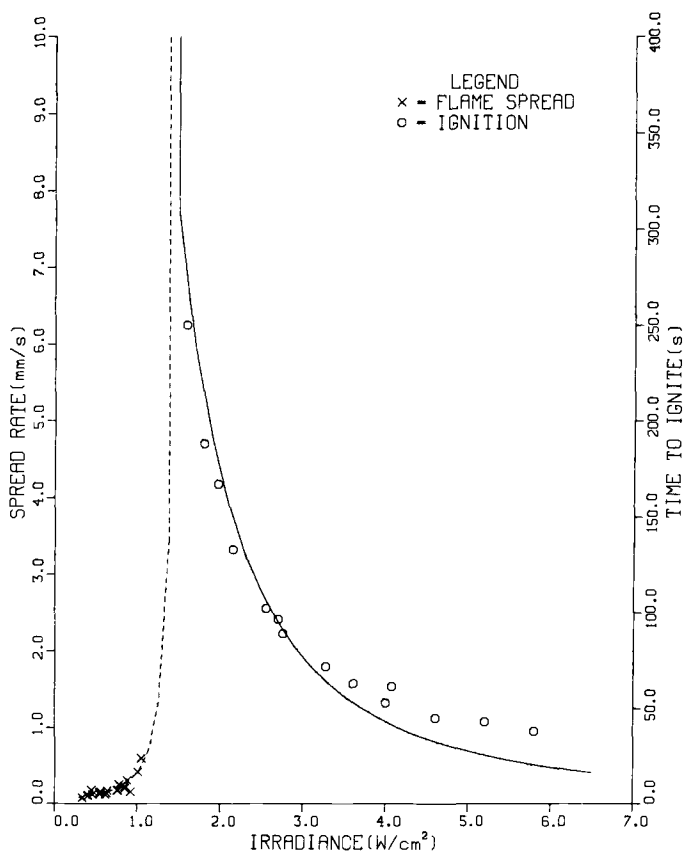


FIG. 15—Spread and ignition results for an asphalt shingle.

reasons can be offered to explain why $k\rho c$ derived from ignition data, and Eq 2 will be higher than the associated ambient value for $k\rho c$. Firstly, both k and c increase with temperature, and thus some higher than ambient temperature value must be used to compute temperature rise to ignition. Secondly, ignition requires the production of a gaseous fuel from the heated solid. Thus, the enthalpy changes required in pyrolysis, vaporization, and melting in some instances must be included in the derived $k\rho c$ values. These endothermic effects will be reflected in an increase in the $k\rho c$ parameter for an inert thermal ignition model. Thirdly, the $k\rho c$ values may be higher due to effects of sample thickness and the heat sink effects of the support board behind the sample. Except for these last sources of error, which should be further examined, it would appear that the derived $k\rho c$ values are the proper values to use in the

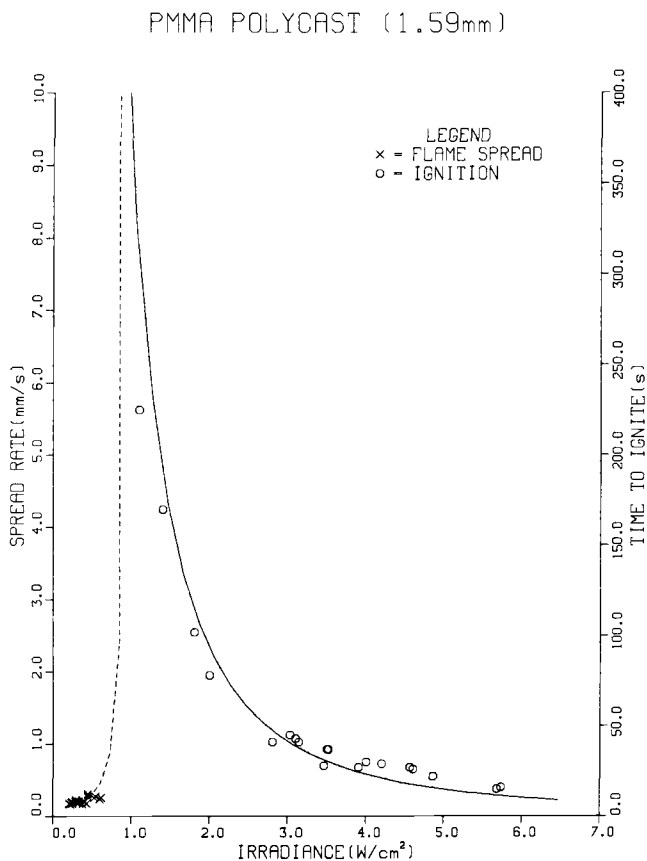


FIG. 16—Spread and ignition results for PMMA.

flame spread models that utilize an inert-ignition temperature model for the solid. In summary, the $k\rho c$ values of Table 2 appear somewhat high but credible under the circumstances of the models.

The results tabulated for the ignition temperatures appear plausible since values quoted in the literature for PMMA and cellulose range from 300 to 400°C for piloted conditions [1,2]. Of course, these values should not necessarily be construed as precise surface temperatures, just approximate indications thereof. This follows because T_{ig} is a derived temperature from an approximate model. As long as the piloting conditions and models of ignition and flame spread are consistent with inert solid models, it is probably a good general value to use. Moreover, since the ignition temperature is just a surrogate condition for a gas-phase flammability limit, it is possible to expect that it may depend to some extent on sample size and orientation and on oxygen

concentration. For example, it may increase somewhat for charring materials as the oxygen concentration is reduced since more fuel will be necessary to produce a combustible mixture, but solid oxidation effects may offset this increase.

The values for Φ are particularly intriguing. If one examines the correlations of Fernandez-Pello et al [11] and Altenkirch et al [12], Φ can be expressed as

$$\Phi = V_a(k\rho c)_g(T_f - T_{ig})^2 f(D) \quad (14)$$

where $f(D)$ is a function of the Damkohler number. The flame temperature (T_f) represents the energy available, and, although the adiabatic flame temperature is nearly constant for most hydrocarbons, it will decrease with a reduction of oxygen in the atmosphere or an addition of an inert diluent to the solid. The $f(D)$ will contain the chemical kinetic effects and the gas transport times available for combustion as dependent on V_a . As the oxygen concentration is reduced, D and also $f(D)$, at least for PMMA [1], are sharply reduced. Previously, a reference value for Φ was computed as $10 \text{ (kW)}^2/\text{m}^3$ based on $T_f = 2080^\circ\text{C}$, $T_{ig} = 390^\circ\text{C}$, $V_a = 11 \text{ cm/s}$, and $Y_{ox,\infty} = 0.23$ nominally representative of PMMA spreading under opposed flow natural convective conditions. The results in Table 2 show Φ ranges from roughly 1 to $15 \text{ (kW)}^2/\text{m}^3$.

Sufficient data are not available to assess these results against the dimensionless correlations for spread rate in terms of D , since they are only available for PMMA [11,12]. Nevertheless, the range of values inferred for Φ from the PMMA correlations appears consistent with the results for all the materials in Table 2, since $f(D)$ for PMMA ranges from approximately 0.1 to slightly greater than 1. Although this consistency may be fortuitous, it does offer some credibility and perhaps insight into the magnitude of Φ . Of course, the computation of Φ does depend on the estimate for $k\rho c$ (or b), so that any uncertainties in that estimation will affect the value for Φ .

Table 2 also lists the minimum surface temperature ($T_{s,\min}$) required for spread under the conditions of the experiment. It was computed using Fig. 6 from the critical external irradiance at the position at which the flame stopped. This generally occurred at a finite spread velocity, or, in other words, the flame spread velocity does not approach zero continuously as the surface temperature approaches $T_{s,\min}$. Borgeson and Tien [13] have developed a theoretical model for a thin fuel which shows these same characteristics. They found that as the surface temperature is decreased, the gas phase combustion zone at the leading edge of the flame recedes in the direction of the opposed air flow. The reduction in forward heat transfer and a decrease in the pyrolysis zone appears responsible for the cessation of flame spread. Of course, for the range of fuels listed in Tables 1 and 2, other effects could be responsible for extinction. Indeed, surface heat losses in charring materials and significant regression effects for some of the foam plastics could be con-

TABLE 1—Ignition and flame spread characteristics under constant irradiance conditions.^e

Material	$q_{o,s}$ (W/cm ²)	C (s/mm) ^{1/2} (cm ² /W)	$q_{o,ig}^a$ (W/cm ²)	$q_{o,ig}^b$ (W/cm ²)	b (s ^{-1/2})	t_m (s)
Plywood, plain, 0.635 cm	0.4	1.8	1.2	1.6	0.07	190
Plywood, plain, 1.27 cm	0.3	1.5	1.4	1.6	0.07	225
Plywood, FR, 1.27 cm	... ^c	... ^c	... ^c	4.4	0.10	110
Hardboard, 6.35 mm	0.4	5.8	1.0	1.0	0.03	1190
Hardboard, 3.175 mm	0.1	2.2	1.3	1.4	0.05	420
Hardboard (S159M)	0.1	1.8	1.5	... ^d	... ^d	... ^d
Hardboard, gloss paint, 3.4 mm	1.1	4.1	1.8	1.7	0.05	468
Hardboard, nitrocellulose paint	0.4	2.0	2.1	1.7	0.06	306
Particle board, 1.27-cm stock	0.9	3.2	1.7	1.8	0.05	342
Douglas Fir particle board, 1.27 cm	0.6	2.0	1.7	1.6	0.05	395
Chipboard (S118M)	0.4	2.2	1.6	... ^d	... ^d	... ^d
Wood panel (S178M)	0.4	1.1	1.6	... ^d	... ^d	... ^d
Fiber insulation board	0.6	3.4	1.4	1.4	0.07	205
Fiberboard, low density (S119M)	0.1	1.3	1.2	... ^d	... ^d	... ^d
Polyisocyanurate, 5.08 cm	0.9	0.4	1.7	2.1	0.36	8
Polystyrene, 5.08 cm	... ^c	... ^c	... ^c	4.6	0.14	53
Polyurethane (S353M)	0.2	1.0	0.9	... ^d	... ^d	... ^d
Polycarbonate, 1.52 mm	2.2	1.5	3.0	3.0	0.06	260
Foam, rigid, 2.54 cm	0.6	0.6	2.0	2.0	0.32	100

Foam, flexible, 2.54 cm	0.2	1.2	1.2	1.6	0.09	132
PMMA Type G, 1.27 cm	0.1	2.0	1.6	1.5	0.05	456
PMMA polycast, 1.59 mm	0.3	3.4	1.0	0.9	0.04	462
Carpet #1 (wool, stock)	2.1	1.5	2.5	2.3	0.18	32
Carpet #2 (wool, untreated)	1.2	1.2	1.6	2.0	0.11	83
Carpet #2 (wool, treated)	1.4	3.2	1.6	2.2	0.12	72
Carpet (nylon/wool blend)	0.8	1.7	1.8	1.8	0.06	248
Carpet (acrylic)	0.4	1.8	1.1	1.0	0.06	250
Gypsum board, common, 1.27 mm	1.9	0.9	3.5	3.5	0.11	87
Gypsum board, FR, 1.27 cm	1.0	1.1	2.1	2.8	0.10	95
Gypsum board, wall paper (S142M)	0.7	5.8	1.0	1.8	0.07	208
Mineral wool, textile paper (S160M)	0.2	1.2	1.7	... ^d	... ^d	... ^d
Asphalt shingle	0.3	2.7	1.6	1.5	0.06	306
Fiberglass shingle	1.8	1.5	2.7	2.1	0.08	161
GRP, 2.24 mm	0.1	1.3	2.0	1.6	0.09	132
GRP, 1.14 mm	1.4	2.9	1.6	1.7	0.06	279
Aircraft panel epoxy fiberite	... ^c	... ^c	... ^c	2.8	0.13	57

Note: GPR = glass-reinforced polyester, FR = fire-retardant treatment.

^a From spread data.

^b From ignition data.

^c Flame spread was not measureable.

^d Data were not taken.

^e Values are only significant to two places.

TABLE 2—*Flame spread properties.*^c

Material	T_{ig} , °C	$k\rho c$ (kW/m ² K) ² s	Φ , (kW) ² /m ³	$T_{s,min}$, °C	$\Phi/k\rho c$, mK ² /s
PMMA polycast, 1.59 mm	278	0.73	5.45	120	8
Polyurethane, S353M	280	... ^b	... ^b	105	82
Hardboard, 6.35 mm	298	1.87	4.51	170	2
Carpet (acrylic)	300	0.42	9.92	165	24
Fiberboard, low density (S119M)	330	... ^b	... ^b	90	42
Fiber insulation board	355	0.46	2.25	210	5
Hardboard, 3.175 mm	365	0.88	10.97	40	12
Hardboard (S159M)	372	... ^b	... ^b	80	18
PMMA Type G, 1.27 cm	378	1.02	14.43	90	14
Asphalt shingle	378	0.70	5.38	140	8
Douglas Fir particle board, 1.27 cm	382	0.94	12.75	210	14
Wood panel (S178M)	385	... ^b	... ^b	155	43
Plywood, plain, 1.27 cm	390	0.54	12.91	120	24
Chipboard (S118M)	390	... ^b	... ^b	180	11
Plywood, plain, 0.635 cm	390	0.46	7.49	170	16
Foam, flexible, 2.54 cm	390	0.32	11.70	120	37
GRP, 2.24 mm	390	0.32	9.97	80	31
Mineral wool, textile paper (S160M)	400	... ^b	... ^b	105	34
Hardboard (gloss paint), 3.4 mm	400	1.22	3.58	320	3
Hardboard (nitrocellulose paint)	400	0.79	9.81	180	12
GRP, 1.14 mm	400	0.72	4.21	365	6
Particle board, 1.27-cm stock	412	0.93	4.27	275	5
Gypsum board, wall paper (S142M)	412	0.57	0.79	240	1
Carpet (nylon/wool blend)	412	0.68	11.12	265	16
Carpet #2 (wool, untreated)	435	0.25	7.32	335	30
Foam, rigid, 2.54 cm	435	0.03	4.09	215	141
Polyisocyanurate, 5.08 cm	445	0.02	4.94	275	201
Fiberglass shingle	445	0.50	9.08	415	18
Carpet #2 (wool, treated)	455	0.24	0.89	365	4
Carpet #1 (wool, stock)	465	0.11	1.83	450	17
Aircraft panel epoxy fiberite	505	0.24	... ^a	505	... ^a
Gypsum Board, FR, 1.27 cm	510	0.40	9.25	300	23
Polycarbonate, 1.52 mm	528	1.16	14.74	455	13
Gypsum board, common, 1.27 mm	565	0.45	14.44	425	32
Plywood, FR (1.27 cm)	620	0.76	... ^a	620	... ^a
Polystyrene (5.08 cm)	630	0.38	... ^a	630	... ^a

Note: GPR = glass-reinforced polyester.

^a Flame spread was not measurable.

^b Data were not taken.

^c Values are only significant to two places.

tributary. In any case, this temperature limit is a significant characteristic, and, in fact, is related to the measurement of results in ASTM E 648. It should be noted, however, that due to the limits of the flux conditions in the apparatus, values of $T_{s,min} < 80^{\circ}\text{C}$ could apply to much lower temperatures.

Extended use of the results in Table 2 might be considered in practical engineering analyses of fire spread. Current knowledge must be used to guide the necessary assumptions. For example, the results here show that the same

TABLE 3—*Properties of common materials.*^a

Material	Density, ρ (kg/m ³)	Conductivity, $k \times 10^3$ (kW/m K)	Specific Heat, c (kJ/kg K)	$k\rho c$ (kW/m ² K) ² s
Concrete, stone	2200	1.7	0.75	2.8
Polymethyl methacrylate (PMMA)	1200	0.26	2.1	0.66
Gypsum board	950	0.17	1.1	0.18
Calcium silicate board	700	0.11	1.1	0.085
Particle board	650	0.11	2.0	0.14
Plywood	540	0.12	2.5	0.16
Cork board	200	0.040	1.9	0.015
Balsa wood	160	0.050	2.9	0.023
Polyurethane, rigid	32	0.020	1.3	0.0008
Polystyrene, expanded	20	0.034	1.5	0.0010

^a Compiled from various sources.

ignition temperature can be used to describe piloted ignition and opposed flow flame spread. Thus, it might be assumed that the same value would be applicable under concurrent spread conditions. If the ambient oxygen or flow velocity varies, it is necessary to infer resultant changes in opposed flow spread from the Damkohler-number correlations [11,12]. As a first approximation for natural convection conditions in room fires, the values of Φ in Table 2 might be modified by $(Y_{\text{ox},\infty}/0.233)^2$ to account for vitiation conditions. Under concurrent flow spread conditions, Sibulkin and Kim [4] derived an approximate expression for spread velocity over a thermally thick solid, which is given as

$$V = \frac{(\dot{q}'')^2 \delta_f}{k\rho c(T_{ig} - T_s)^2} \quad (15)$$

where

$\dot{q}''$ = the average flame heat flux over the surface downstream of the pyrolysis zone, and

δ_f = the flame length over this heated region.

Thus, to utilize this formula in an analysis of fire growth it will be necessary to understand the behavior of $\dot{q}''$ and δ_f in terms of material properties and environmental conditions. Although this is feasible, it has not yet been demonstrated. Nevertheless, the denominator can be estimated from the current results of Table 2.

Conclusions

It has been shown that it is feasible to derive ignition and flame spread properties for a wide range of materials. Ignition temperatures range from

about 250 to 600°C and may be overestimated by 50°C at most, and consistent values were found for piloted ignition and flame spread. The derived $k\rho c$ values applicable to flame spread over thermally thick solids tend to be higher but in relative agreement with literature values for similar materials under normal room conditions. A parameter Φ has been defined which is representative of the available flame energy and applies to opposed flow flame spread. These three parameters respectively correspond to (1) the conditions necessary for combustion (T_{ig}), (2) the time response of the material to heat ($k\rho c$), and (3) the energy available for spread (Φ). Evidence suggests that these properties, although not fundamental, can be generally used in mathematical models to predict the performance of materials. Although the results given here only apply to natural convection conditions in opposed air flow, extension to other environmental conditions or to concurrent flow spread is possible.

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Prediction of Upholstered Chair Heat Release Rates from Bench-Scale Measurements

REFERENCE: Babrauskas, V. and Krasny, J. F., "Prediction of Upholstered Chair Heat Release Rates From Bench-Scale Measurements," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 268–284.

ABSTRACT: In an earlier study a full-scale furniture calorimeter was used to determine the heat release rates for upholstered chairs containing various construction materials. Samples of these same material combinations have now been tested in a bench-scale apparatus, the cone calorimeter. A correlation was established between bench-scale and full-scale data. Thus, it appears that prediction of flashover potential of a single upholstered item may be possible by using bench-scale results.

KEY WORDS: chairs, cone calorimeter, flammability tests, furniture calorimeter, rate of heat release, upholstered furniture

The development of the oxygen consumption principle [1] allowed a full-scale apparatus to be built—the furniture calorimeter—which can be used to readily determine the rate of heat release and other burning properties of upholstered furniture [2]. In the first full-scale test series, a number of actual upholstered furniture items were tested, including chairs, love seats, and sofas [3]. This was supplemented by a second test series where steel-frame mock-ups were used and a number of additional fabric, padding, and geometric variations were explored [4]. The same oxygen consumption principle also was recently utilized to design a bench-scale calorimeter [5]. This instrument, termed the cone calorimeter, was used to test fabric/padding composites taken from upholstered furniture. The purpose of the present work is to develop a relationship which can be used to predict full-scale behavior, specifically the full-scale peak rate of heat release, from bench-scale measurements.

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Uses of Heat Release Rate Data

The furniture calorimeter is used to determine the free-burn behavior of furniture items, that is, the burning not influenced by room effects such as increased burning due to hot gas layer radiation or diminished burning due to air restriction or vitiation effects. A study was recently completed demonstrating that, specifically, for upholstered chairs, these effects are small not only up to flashover but also in the fuel-limited burning regime after flashover [6]. Thus, furniture calorimeter data are properly descriptive of the room fire in the preflashover stage and after flashover but prior to ventilation-limited burning. The peak heat release rate value determines whether a given room will or will not reach flashover, which is the most important single event in a room fire. Methods for making flashover calculations, based on the full-scale heat release rate values, are available [6].

Full-scale testing of furniture is relatively costly and significantly more cumbersome than bench-scale testing. Thus, a method to replace full-scale testing with bench-scale tests is desirable. In some cases, such as hazards surveying of existing occupancies, any destructive testing, even bench-scale, may be precluded. Nonetheless, some hazards assessment can be made by weighing the articles and identifying their materials of construction. A method for doing this was first presented in conjunction with the first series of furniture calorimeter tests [3]; based on the additional data taken [4], a revised version of that procedure also will be presented.

The Model for Rate of Heat Release

A full analysis of the upholstered furniture problem would entail subdividing the exposed surface of the item into m , small area elements. At any given time, t , the rate of heat release, $\dot{q}$, would be obtained as a summation over all the elements. It also would have to be determined if each element is yet ignited or if it is burned out (this constitutes the flame spread analysis). If the element is ignited and not burned out, its instantaneous rate of heat release would have to be determined. To do this from test data would require knowing the external heat flux (irradiance, in the case of radiatively dominated fires) incident upon the element. The rate of heat release for each small element would still not necessarily be expected to be a constant but could vary according to how much specimen is consumed. In the case of n objects in the room, the general expression for the heat release rate would be

$$\dot{q}(t) = \sum_n \sum_m \dot{q}_{n,m}'' \cdot A_{n,m}$$

which is a summation over the element areas, $A_{n,m}$. The value $\dot{q}_{n,m}''(t)$ is obtainable from bench-scale rate of heat release tests on small samples of furniture items. Suitable, quantitative relationships for predicting the ignition/

burnout of elemental areas are not available; neither are ones for characterizing the local irradiances. Thus, this approach, at present, is difficult to implement.

It is more realistic to expect to obtain a correlation in the form

$$\dot{q}_{\text{peak}} = b_1 \dot{q}_{bs}'' A_{fs}$$

where

$\dot{q}_{bs}''$ = the measured bench-scale value (kW/m²) for a specified irradiance,

b_1 = a proportionality factor, and

A_{fs} = the total exposed surface area of the full-scale item.

The actual area involved at peak burning time—or any other time—is not likely to be identically equal to A_{fs} . Thus, the factor, b_1 , is needed to account for this difference. Also, at any time in the full-scale burning process the elemental areas will not be uniformly burned but will be at different stages of burning. This suggests that from the bench-scale data what is needed is not an instantaneous value but rather a suitably chosen average.

For real furniture there are additional complications. The total area, A_{fs} , may be difficult or impossible to measure conveniently. The frame used in construction may have an effect by deforming, collapsing, or contributing to the rate of heat release. Flame spread can be influenced by construction style, being faster over convoluted geometries than over planar surfaces. In the initial study [3] it was determined that predictiveness could be achieved if the measurement of the specimen combustible mass were substituted for the measurement of A_{fs} and if empirical factors for frame type and for the curved or planar construction style were adopted. Some of these assumptions can now be more fully evaluated due to the availability of additional data.

Bench-Scale Tests

The cone calorimeter apparatus is shown in Fig. 1. Its construction, operation, and calibration details have been described [5]. A heating flux of 0 to 100 kW/m² may be applied to a specimen. The rate of heat release is determined by measuring the oxygen values and the air flow rate in the exhaust duct. Specimen mass loss is determined simultaneously with the use of a load cell. Specimens were prepared to a size 100 by 100 by 50 mm thick. Padding material was cut slightly smaller than the just-mentioned size. Upholstery fabric was cut to 200 by 200 mm with each corner, 50 by 50 mm, removed. The fabric was placed over the padding with the four flaps covering the sides. The fabric was stapled to the bottom edge of the padding along each side. The specimen was then wrapped in a single sheet of aluminum foil that covered the sides and bottom. Corner sections were folded, rather than cut off, to give the foil rigidity. Specimens were all tested in a horizontal orientation since foam padding materials would tend to melt and drip in a vertical orientation.

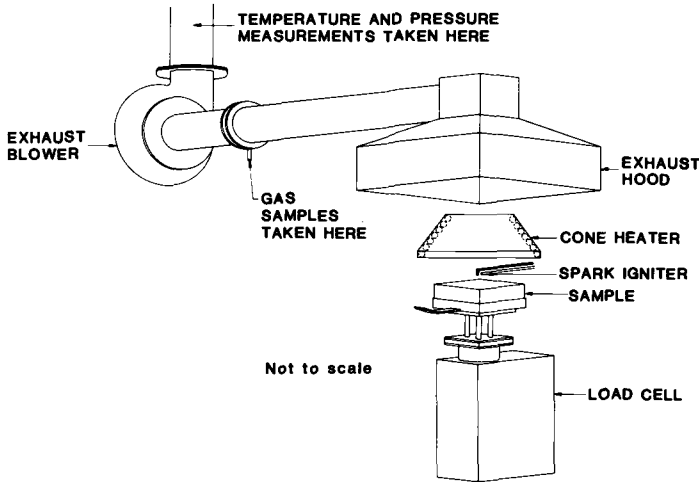


FIG. 1—Schematic representation of cone calorimeter.

Spark ignition was used. Table 1 shows the specimens tested, corresponding to full-scale tests performed earlier [3]. Also included in the bench-scale testing were three specimens, coded FA, FB, and FC, which were different versions of the materials tested in the F21 and F33 series.

To prescribe fully the bench-scale test conditions, a value for the heater irradiance is needed. This is the heat flux that is needed to simulate the effect of flames from other parts of the chair adding to the local flame heating value. Physical measurements of this sort are not practical during full-scale upholstered furniture tests. Consequently, the approach was to conduct bench-scale tests under various irradiance conditions and then to select that value which gives a best data correlation with the full-scale tests. Irradiances of 25, 30, 40, and 50 kW/m² were used. The period over which the heat measured in the cone calorimeter is to be averaged also needed to be determined. Data are tabulated in Table 2 for the initial 60, 120, 180, 240, and 300-s averaging periods, plus peak values. Typical heat release rate curves are shown in Fig. 2. Many, but not all, specimens showed two peaks: the first corresponded to full involvement of the fabric, while the second occurred at peak padding combustion. Not all specimens F21 through F32 are listed in Table 2. Others were different in size or frame from tested specimens but had identical fabric/padding materials, to wit F26 similar to F21; F29 similar to F25; F30 similar to F25; F31 similar to F21; and F32 similar to F21.

Based on the considerations just mentioned, the following model was selected for determining $\dot{q}_{fs}$, the peak full-scale rate of heat release

$$\dot{q}_{fs} = b (\dot{q}_{bs}'') \left(\frac{\text{mass}}{\text{factor}} \right) \left(\frac{\text{frame}}{\text{factor}} \right) \left(\frac{\text{style}}{\text{factor}} \right) \text{ (kW)}$$

TABLE 1—Chairs tested in full scale (with frames) and bench scale (fabric and padding only) [3].

Specimen	Mass, kg	Style	Frame	Padding	Fabric	Full-Scale Peak $\dot{q}$, kW
F21	28.3	traditional easy chair	wood	PU (FR) ^a	olefin	1970
F22	31.9	traditional easy chair	wood	cotton (FR) batting	cotton	370
F23	31.2	traditional easy chair	wood	cotton (FR) batting	olefin	700
F24	28.3	traditional easy chair	wood	PU (FR)	cotton	700
F25	27.8	traditional easy chair	wood	PU	olefin	1990
F26	19.2	thinner easy chair	wood	PU (FR)	olefin	810
F27	29.0	traditional easy chair	wood	mixed batting	cotton	920
F28	29.2	traditional easy chair	wood	mixed batting	cotton	730
F29	14.0	traditional easy chair	polypropylene	PU	olefin	1950
F30	25.2	traditional easy chair	polyurethane	PU	olefin	1060
F31	40.0	traditional easy chair	wood	PU (FR)	olefin	2890
F32	51.5	traditional love seat	wood	PU (FR)	olefin	3120
FA	...	...	...	cotton batting	olefin	...
FB	...	...	...	PU	cotton	...
FC	...	...	...	cotton batting	cotton	...

^aPU = polyurethane; FR = fire retardants added.

TABLE 2—*Bench-scale heat release rate results, values in kW/m².*

Specimen	Irradiance			
	25 kW/m ²	30 kW/m ²	40 kW/m ²	50 kW/m ²
PEAK VALUES				
F21	420	625	637	713
F22	247	282	313	342
F23	362	514	620	745
F24	268	363	407	461
F25	481	556	627	794
F27	380	473	526	686
F28	141	216	254	332
FA	386	493	678	653
FB	359	362	401	459
FC	268	339	402	413
60-s AVERAGE VALUES ^a				
F21	223	354	350	407
F22	124	150	150	171
F23	206	242	257	292
F24	177	204	233	261
F25	292	339	358	383
F27	226	226	340	376
F28	107	120	136	213
FA	166	199	244	255
FB	174	190	216	222
FC	140	154	172	187
120-s AVERAGE VALUES				
F21	291	429	460	530
F22	96	117	123	139
F23	154	190	208	224
F24	136	145	171	190
F25	336	387	472	531
F27	260	267	305	317
F28	105	123	145	210
FA	134	151	177	185
FB	135	144	162	172
FC	93	103	112	135
180-s AVERAGE VALUES				
F21	326	389	399	398
F22	83	103	109	124
F23	128	165	182	205
F24	119	129	173	201
F25	357	374	367	388
F27	204	233	280	315
F28	99	118	158	235
FA	110	123	143	157
FB	108	121	157	185
FC	75	83	89	113

TABLE 2—Continued.

Specimen	Irradiance			
	25 kW/m ²	30 kW/m ²	40 kW/m ²	50 kW/m ²
240-s AVERAGE VALUES				
F21	288	298	301	299
F22	76	95	101	118
F23	115	157	172	208
F24	123	137	188	204
F25	283	287	277	291
F27	200	272	330	333
F28	103	136	165	186
FA	97	108	127	146
FB	101	125	172	190
FC	59	65	75	101
300-s AVERAGE VALUES				
F21	232	238	241	239
F22	73	90	97	112
F23	107	155	169	185
F24	131	139	169	178
F25	227	230	221	233
F27	219	297	339	312
F28	107	131	138	149
FA	89	99	118	142
FB	104	127	152	158
FC	56	52	67	94

^aAll averages refer to a given time period after specimen ignition.

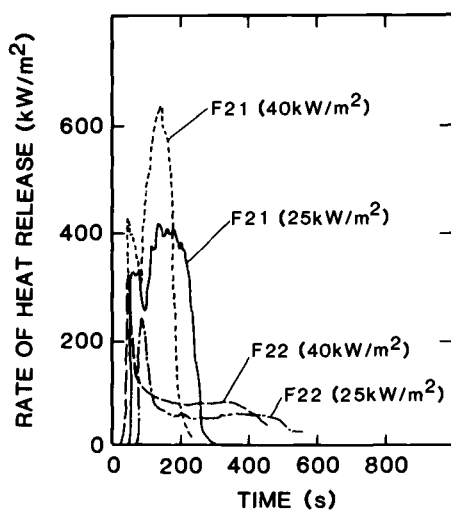


FIG. 2—Typical curves for two specimens tested in the cone calorimeter at two irradiances.

The mass factor is equal to specimen combustible mass (kg), the style factor is as set forth in Ref 3, and the frame factor is discussed in detail in following sentences. Values of the proportionality factor, b , for the comparison of the full-scale maximum heat release rate and results of the various bench-scale test conditions are shown in Table 3, while specific factors and computed heat release values are given in Table 4. The best test conditions are the ones yielding the lowest coefficient of variation (CV). These ranged from 0.124 for 40-kW/m² exposure and 300-s averaging to 0.051 for a 25-kW/m² exposure and 180-s averaging. Thus, 25 kW/m² is identified as the optimal irradiance and 180 s as the most suitable averaging period on the basis of this limited number of tests.

The specimens identified as fire-retardant (FR) in Table 1 were treated, in the case of polyurethane foam, with sufficient retardants to pass a Bunsen burner test prescribed in California [7]. For cotton batting, the fire retardants were sufficient to prevent smoldering ignition from cigarettes. From the bench-scale results of Table 2 for the chosen 25-kW/m² irradiance and 180-s averaging, it can be seen that there is no significant improvement in heat release characteristics for these FR specimens. Comparing the matched pair specimens (F21 and F24; F25 and FB; F23 and F22; and FA and FC), it can be seen that in one case the FR specimen offers 9% better performance, but, in the remaining three cases, the performance is worse by 10%, 16%, and

TABLE 3—*Prediction of upholstered furniture performance using bench-scale tests.*

Averaging Period (s)	Irradiance, kW/m ²	Values Obtained from Least-Squares Fitting ^a		
		Standard b	Deviation	CV
Peak	25	0.429	0.037	0.086
Peak	30	0.323	0.028	0.087
Peak	40	0.296	0.028	0.095
Peak	50	0.142	0.024	0.098
60	25	0.74	0.071	0.096
60	30	0.58	0.040	0.069
60	40	0.54	0.047	0.086
120	25	0.66	0.044	0.067
120	30	0.51	0.029	0.057
120	40	0.45	0.024	0.053
180	25	0.63	0.032	0.051
180	30	0.56	0.029	0.052
180	40	0.54	0.032	0.059
240	25	0.74	0.040	0.054
240	30	0.68	0.050	0.072
240	40	0.64	0.061	0.095
300	25	0.86	0.065	0.076
300	30	0.77	0.082	0.106
300	40	0.74	0.092	0.124

^aUsing data given in Tables 1 and 2.

TABLE 4—Full-scale peak rate of heat release—measured and predicted values.

Specimen	Mass Factor	Frame Factor	Style Factor	Padding Factor	Fabric Factor	Predicted $\dot{q}$ kW ^a	Bench-Scale $\dot{q}$ ", kW/m ²	Predicted $\dot{q}$, kW ^b	Actual $\dot{q}$, kW
F21	28.3	0.30	1.0	1.0	1.0	1780	326	1740	1970
F22	31.9	0.30	1.0	0.4	0.4	320	83	500	370
F23	31.2	0.30	1.0	0.4	1.0	790	128	750	700
F24	28.3	0.30	1.0	1.0	0.4	710	119	640	700
F25	27.8	0.30	1.0	1.0	1.0	1950	357	1880	1990
F26	19.2	0.30	1.0	1.0	1.0	1210	326	1180	810
F27	29.0	0.30	1.2	1.0	0.4	880	204	1340	920
F28	29.2	0.30	1.2	1.0	0.4	880	99	660	730
F29	14.0	0.58	1.2	1.0	1.0	1820	357	1950	1950
F30	25.2	0.18	1.2	1.0	1.0	950	357	1020	1060
F31	40.0	0.30	1.0	1.0	1.0	2520	326	2460	2890
F32	51.1	0.30	1.0	1.0	1.0	3220	326	3150	3120
F33	39.2	0.30	1.0	1.0	0.4	990	N.A.	N.A.	940

^aBased on generic factor for padding and fabric.^bBased on actual bench-scale measurements.

11 %, respectively. These variations may not be significant. The appropriate conclusion is that in bench-scale tests, just as previously observed in full-scale tests [3], the addition of fire retardants sufficient just to pass the California test has minor or negligible effects on the rate of heat release behavior, even though this may delay the peak.

Additional Data From Mock-up Tests

The first full-scale test series included only two fabric types and only combustible frames. To expand this data base and also to obtain data on flame spread, a second full-scale test series was conducted [4]. In this series, non-combustible steel mock-up frames were used. Fabrics included two weights of olefins (360 and 560 g/m²) and two weights of cottons (110 and 650 g/m²). Fillings included polyurethane foam, FR and non-FR, and neoprene foam. Also explored were cushion thickness, number of cushions, and ignition source type. The tests are identified in Table 5. Bench-scale tests on the second series specimens were conducted only at the optimal 25-kW/m² irradiance. Results are shown in Table 5. Figure 3 shows the agreement for the two test series. No systematic errors are seen, with one exception. The predicted heat release rates were significantly higher than the actual release rates for samples that release small amounts of heat and do not burn completely in full-scale tests, for example, neoprene. Such samples for which the prediction rule fails are identified in Table 5. A similar limitation would be encountered with any computational method that requires the use of the specimen combustible mass. If these highly fire-resistive specimens are not excluded from consideration, their heat release rates would be significantly overpredicted. Detailed statistical measures of the goodness of fit of the correlation (for example, variance) in Fig. 3 are not appropriately sought, since the sample set was not randomly selected but chosen to cover specific constructions of interest; therefore, a goodness-of-fit test based on random statistics may be misleading.

Frame Factor

The relationship between wood, thermoplastic, and charring plastic frames was first explored in connection with the initial test series [3]. A wood frame does not contribute significant fuel until after the peak of the fire. The total combustible mass of the chair, however, includes the frame (it is not feasible to separate that component out without completely disassembling a chair). Thus, the frame factor could be looked upon as a correction of the combustible mass factor that removes the frame mass component. This view is useful but incomplete. By this reasoning alone, plastic frames would not require different frame factors, but it was seen that they do [3]. A suitable hypothesis to explain plastic frame behavior is the following. A thermoplastic frame fails

TABLE 5—Measurements on chair mock-ups.

Test	Foam	Fabric	Combustible mass, kg	Number of Cushions	Bench-Scale $\dot{q}$ ", kW/m ²	Predicted Full- Scale Peak $\dot{q}$, kW	Actual Full- Scale Peak $\dot{q}$, kW
1	NFR PU	light olefin	1.00	1	259	270	320
2	NFR PU	light olefin	2.00	2	259	540	540
11	NFR PU	light olefin	3.11	3	259	850	1120
12,27	NFR PU	light olefin	4.14	4	259	1130	1460
13	NFR PU	light olefin	2.60	4	259	710	1050
25	NFR PU	light olefin	4.06	4	259	1100	1370
20	NFR PU	heavy cotton	5.32	4	113	630	430
29	NFR PU	none	2.52	4	...	...	760
3	FR PU	light olefin	1.17	1	265	330	260
4	FR PU	light olefin	2.38	2	265	660	410
5,8	FR PU	light olefin	3.53	3	265	980	790
6	FR PU	light olefin	7.02	6	265	1950	1360
7,19	FR PU	light olefin	4.77	4	265	1330	1340
9	FR PU	light olefin	2.26	3	265	630	660
10,23	FR PU	light olefin	2.98	4	265	830	930
16	FR PU	light olefin	4.70	4	265	1310	1430
24	FR PU	light olefin	4.72	4	265	1310	1160
26	FR PU	light olefin	4.74	4	265	1320	1460
14	FR PU	heavy olefin	5.84	4	304	1860	1020
21	FR PU	light cotton	3.62	4	137	520	900
17	FR PU	heavy cotton	5.84	4	132	810	530
28	FR PU	none	3.02	4	...	...	1210
18	NP	light olefin	9.92	4	64	... ^a	110
15	NP	light olefin	20.08	4	64	... ^a	120
22	NP	heavy cotton	21.34	4	49	... ^a	≈ 0

^aPrediction method not suitable for high mass, low rate of heat release constructions.

Note: PU = polyurethane; NP = neoprene; FR = fire retardant treated; NFR = not fire retardant treated.

structurally much sooner than a wood frame. In so doing, the chair partially collapses and exposes padding material for increased burning. In the case of a charring plastic, the structural behavior is better than for a wood frame, and stability is preserved for a longer time.

In the second test series, noncombustible frames were introduced. These constructions do not require a combustible frame mass correction. An expression for the frame factor is needed which allows the unified presentation of both combustible and noncombustible frame data. The frame factor for wood frames can be estimated by one of two physical models.

1. A simple physical assumption would be to take an average mass ratio value for wood-frame chairs

$$\text{Frame factor} = \frac{\text{padding and fabric mass}}{\text{total combustible mass}}$$

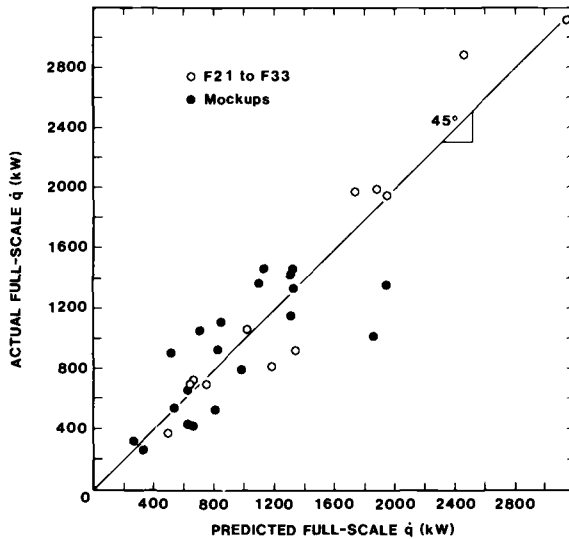


FIG. 3—Correlation between the predicted full-scale peak rate of heat release and predicted values, based on bench-scale cone calorimeter tests.

This fraction was not available for the test series considered here. However, in an earlier study, where wood-frame chairs were disassembled and component masses determined, it was found [8] that

$$\frac{\text{padding and fabric mass}}{\text{total combustible mass}} = 0.28 \text{ to } 0.33$$

This average 0.30, which would then be the factor for a wood frame.

2. The fraction of mass lost up to the time of peak burning can be considered. Table 6 lists these values for combustible and noncombustible assemblies. The average is 0.17 for wood and 0.50 for noncombustible assemblies. The ratio 0.17/0.50 is 0.33, which is very similar to the value 0.30 just mentioned; therefore, 0.30 can be taken as the frame factor for wood frames.

The frame factor for plastic frames is based on the studies with specimens F29 and F30 reported earlier [3]. For the charring plastic frame, the factor is 0.58 times the wood frame factor, which gives the 0.18. For the thermoplastic frame, the factor similarly becomes $1.93 \times 0.30 = 0.58$.

The frame factor for noncombustible frames can then be determined from data correlation for the mock-up series, as compared to the wood-frame tests. The proportionality factor must be the same for both series. Using the data of the mock-up series (see Table 5), the frame factor for a noncombustible frame can be determined. This gives the frame factor for noncombustible frames as 1.66. This value has to be considered tentative only, since the mock-up series

TABLE 6—*Mass fraction lost up to time of peak rate of heat release.*

COMBUSTIBLE FRAME TESTS		
Specimen		Fraction Lost at Peak
F21		0.09
F22		0.16
F23		0.22
F24		0.18
F25		0.13
F26		0.12
F27		0.25
F28		0.16
F29		(0.33) ^a
F30		(0.15) ^a
F31		0.14
F32		0.22
Average		0.17

NONCOMBUSTIBLE FRAME TESTS		
Fabric	Padding	Fraction Loss at Peak
Light olefin	NFR polyurethane	0.44
Light olefin	FR polyurethane	0.38
Light olefin	neoprene	N.A.
Heavy olefin	FR polyurethane	0.43
Light cotton	FR polyurethane	0.55
Heavy cotton	NFR polyurethane	0.64
Heavy cotton	FR polyurethane	0.58
Heavy cotton	neoprene	N.A.
Average		0.50

Note: FR = fire retardant; NFR = not fire retardant.

^a Not wood frame.

construction details differed substantially from actual chair construction practice, as typified by the wood-frame specimens.

Correlations Based on Specimen Area or Specimen Mass

For commercial furniture of common construction types, it is considered impractical to attempt to define and determine the actual exposed surface area. Thus, correlations have to be on a mass basis. In the mock-up series, however, the construction was so simplified that areas readily could be determined. A comparison then can be made on the quality of the predictiveness using the area basis versus the mass basis. Table 7 shows the results. The style factor is $\equiv 1.0$. Taking the frame factor = 1.66 gives $b_1 = 0.682$ (area basis) and $b = 0.576$ (mass basis). The coefficients of variation are 0.048 and 0.071, respectively. This demonstrates, not unexpectedly, the smaller scatter in the

TABLE 7—Comparison of area-basis and mass-basis predictions for the mock-up series.

Test	Combustible Mass, kg	Area, m ²	Bench-Scale $\dot{q}''$, kW/m ²	Predicted ^b Full-Scale Peak $\dot{q}$, kW		Actual Full-Scale Peak $\dot{q}$, kW
				Based on Mass	Based on Area	
1	1.00	0.99	259	248	290	320
2	2.00	1.98	259	496	581	540
3	1.17	0.99	265	297	297	260
4	2.38	1.98	265	603	594	410
5,8	3.53	2.97	265	895	891	790
6	7.02	5.70	265	1780	1710	1360
7,19	4.77	3.96	265	1209	1188	1340
9	2.26	2.61	265	573	783	660
10,23	2.98	3.48	265	756	1044	930
11	3.11	2.97	259	771	871	1120
12,27	4.14	3.96	259	1026	1161	1460
13	2.60	3.48	259	644	1020	1050
14	5.84	3.96	304	1699	1363	1020
15	20.08	3.76	64	...	...	120
16	4.70	3.96	265	1192	1188	1430
17	5.84	3.96	132	738	592	530
18	9.92	3.37	64	...	...	110
20	5.32	3.96	113	575	507	430
21	3.62	3.96	137	474	614	900
22	21.34	3.76	49	...	...	≈ 0
24	4.72	3.96	265	1197	1188	1160
25	4.06	3.96	259	1006	1161	1370
26	4.74	3.96	265	1202	1188	1460
28	3.02	3.96	...	...	...	1210
29	2.52	3.96	...	...	...	760

^aPrediction method not suitable for high mass, low rate of heat release constructions.^bBased on *this* data set alone (not combined with F21–F33 test series).

area-basis computation; however, more importantly, it shows that satisfactory results can be achieved with the more practical mass-basis computation.

General Prediction Rule

Combining the values for b and frame factors derived as just stated, together with considerations of style factor developed earlier [3], gives the final equation, based on bench-scale tests

$$\dot{q}_{fs} = 0.63 (\dot{q}_{bs}'') \left(\frac{\text{mass}}{\text{factor}} \right) \left(\frac{\text{frame}}{\text{factor}} \right) \left(\frac{\text{style}}{\text{factor}} \right)$$

where

$\dot{q}_{fs}$ = estimated rate of heat release peak (kW) in full-scale; the proportionality factor b is equal to $0.63 \text{ m}^2/\text{kg}$,

$\dot{q}_{bs}''$ = rate of heat release (kW/m²) in bench-scale test, under specified conditions

$\frac{\text{mass}}{\text{factor}}$ = combustible mass, in kg

$$\frac{\text{frame}}{\text{factor}} = \begin{cases} 1.66 & \text{for noncombustible} \\ 0.58 & \text{for melting plastic} \\ 0.30 & \text{for wood} \\ 0.18 & \text{for charring plastic} \end{cases}$$

$$\frac{\text{style}}{\text{factor}} = \begin{cases} 1.0 & \text{for plain, primarily rectilinear construction} \\ 1.5 & \text{for ornate, convolute shapes} \\ & \text{and intermediate values for intermediate shapes} \end{cases}$$

An improvement in predictability might be obtained if a factor were included for the flame spread tendencies of the different construction types. Some initial flame spread data have been obtained in the course of the mock-up tests [4]. Flame spread studies are continuing, and this possibility will be investigated.

In the earlier study [3], a very simple method was proposed based only on generic material factors and not requiring any bench-scale testing. Its uses are obviously more limited; nonetheless, it can be valuable for fire hazards surveying work, where destructive testing is not possible. Expressed in the same units, it becomes

$$\dot{q}_{fs} = 210 \left(\frac{\text{fabric}}{\text{factor}} \right) \left(\frac{\text{padding}}{\text{factor}} \right) \left(\frac{\text{mass}}{\text{factor}} \right) \left(\frac{\text{frame}}{\text{factor}} \right) \left(\frac{\text{style}}{\text{factor}} \right)$$

where

$$\text{fabric factor} = \begin{cases} 1.0 & \text{for thermoplastic fabrics (fabrics such as polyolefin} \\ & \text{which melt prior to burning)} \\ 0.4 & \text{for cellulosic fabrics (cotton)} \\ 0.25 & \text{for poly(vinyl chloride)/polyurethane type coverings} \end{cases}$$

$$\text{padding factor} = \begin{cases} 1.0 & \text{for polyurethane foam or latex foam} \\ 0.4 & \text{for cotton batting} \\ 1.0 & \text{for mixed materials} \\ 0.4 & \text{for neoprene foam} \end{cases}$$

and the remaining factors are as just given.

In either the generic factors rule or the bench-scale testing rule, certain items must be excluded. They are (1) pieces which do not include a more-or-less conventional frame, for example, single-foam-block chairs, beanbag chairs, etc., and (2) items of highly fire-resistive character which do not tend to burn nearly to completion in a flaming fire.

Conclusions

In addition to an earlier method for estimating peak chair fire heat release rate that was based on generic materials factors, a new method has been developed based on the use of bench-scale tests on actual padding/fabric composites. The generic materials factor method remains useful for cases where no destructive testing is possible. The new method, while requiring some bench-scale testing, permits an evaluation of improved materials, which would not be possible with generic factors alone. The derivation of frame and style factors is based on rather limited data; this is an area where further work would be desirable. Justification for using the specimen mass, rather than surface area, as the relevant size factor was established by a direct comparison using mock-up experiment data. Finally, no predictive rule appears adequate to quantify the behavior of highly fire-resistive constructions occurring in fire scenarios where they are not likely to burn completely. This is not a significant obstacle since the predictive rules, if straightforwardly applied to these cases, would yield a highly conservative estimate for fires of low intrinsic hazard.

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Smoke Production in Fires: Small-Scale Experiments

REFERENCE: Drysdale, D. P. and Abdul-Rahim, F. F., “**Smoke Production in Fires: Small-Scale Experiments**,” *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, 1985, pp. 285–300.

ABSTRACT: The quantity of smoke released in a compartment fire depends both on the nature of the combustible materials involved and on the conditions of burning. The final yield of “cold” smoke is much greater than the quantity predicted from dynamic measurements of hot combustion products issuing from a test rig and is likely to be considerably more than the amount predicted from the results of small-scale tests such as Method for Specific Optical Density of Smoke Generated by Solid Materials (ASTM E 662-83).

KEY WORDS: compartment fires, smoke production, smoke testing

By far the major proportion of fatalities in fires is attributable to inhalation of smoke and toxic gases. An apparent solution to this problem would be to find a means of controlling the use of materials, which contribute significantly to smoke production in a fire. In an effort to achieve this, a number of tests have been developed for assessing the smoke production potential of combustible materials. These tests involve burning the material under a set of precisely defined conditions which are intended to be representative of a typical fire. With some notable exceptions [for example, Method for Surface Burning Characteristics of Building Materials (ASTM E 84-84)], the results are expressed in terms of the optical density of the smoke collected in a known volume and may be used to place in order different materials according to their propensity to produce smoke.

The most widely used test of this type is ASTM Method for Specific Optical Density of Smoke Generated by Solid Materials (E 662-83), often referred to as the “NBS Test.” While many variants have been proposed (for example,

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Refs 1-3) both in the apparatus itself and in the procedure, the basic method remains the same. A small sample of material in a vertical configuration is exposed to a radiant heat flux in the presence or absence of flame, and the opacity of the smoke produced is measured as it is collected in a known volume (0.51 m^3). The smoke yield can be expressed either in terms of the surface area exposed to the radiant heat flux or in terms of the mass loss during the test [1,3]. In adopting such a test, it is tacitly assumed that smoke yield is a property of the material burning and that the conditions of burning are of secondary importance. This is known to be incorrect. For example, a change in the radiant heat flux may not only change the amount of smoke produced, but it may alter the ranking order of a series of combustible materials [3,5].

Attempts have been made to establish whether or not the small-scale test results are relevant to real fire situations, but these attempts have been of limited success [6-8]. One of the difficulties that exists is that to measure smoke production in a full-scale fire, the volume of smoke is too great to be collected; instead, it is measured by monitoring the obscuration of a light beam which passes through the plume emerging from the test rig (for example, at the end of the corridor of a room-corridor assembly [8]). This will be referred to as the dynamic method. The total amount of smoke generated is obtained then by integrating over the duration of the test. The temperature of the smoke measured in this way can be several hundred degrees higher than that of the smoke which accumulates in the NBS chamber. The consequences of this have not been investigated systematically.

If a fire occurs within a building, the smoke which invades escape routes is relatively cool and hinders escape by reducing visibility. Other products of combustion may exacerbate the situation in other ways, for example, by causing irritation of the eyes and respiratory tract, thus impeding movement still further, but these effects are difficult to quantify. Taking reduced visibility as a primary hazard, it would seem that the yield of "cold smoke" is more relevant to the problem of life safety. For this reason it may be argued that tests involving "static" measurements (for example, ASTM E 662) should be used for materials selection, although the question of whether the results from such small-scale tests are relevant to the full-scale fire situation remains to be answered.

A series of experiments involving a seventh-scale room-corridor assembly has been undertaken to investigate these and other features of smoke production. One of the main objectives was to compare smoke yields determined by the dynamic (or flow) method directly with those from static measurements. The program was designed also to study any effects arising from restricted ventilation and "corridor length," as there are good reasons to suspect that the fire scenario has a significant influence on the final amount of smoke. Such effects may be detected quickly and relatively cheaply on the small scale; the results may be used to design a limited number of large-scale tests if these tests are deemed necessary. Preliminary results and data analysis are presented in paragraphs that follow.

Smoke Measurement

In this paper, smoke yield is expressed in terms of its obscuration potential per gram of material burned, that is, as the smoke potential, D_o , with units of m^3/g . The unit "obscura" (ob) was introduced by Rasbash and Phillips [4] and refers to an optical density of 1 dB/m. The choice of using decibels (dB) rather than bels (B) was for convenience: 1 dB/m (that is, 1 ob) corresponds to a visibility of about 10 m [9]. The smoke potential of a material is obtained by burning a known quantity in a chamber of Volume V and measuring the optical density of the accumulated smoke ($D = 10 \log (I_o/I)$) by monitoring the attenuation of a light beam of length L passing vertically through the smoke. I and I_o refer to the intensities of light from a filament lamp falling on the photocell (cadmium sulphide photoresistor) in the presence and absence of smoke, respectively. Thus

$$D_o = \frac{10V}{WL} \log \frac{I_o}{I} \quad (1)$$

where W is the mass of material volatilized and is taken as the difference in the weight of the sample before and after the test [1, 4, 7]. This will be referred to as D_o (static).

In the small room-corridor tests, the quantity of smoke is determined using the "dynamic" method just described (see Fig. 1). The volume of smoke (V_s) is calculated from [8]

$$V_s = \int D_\ell \cdot V_f \cdot dt \text{ ob } m^3 \quad (2)$$

where D_ℓ is the instantaneous value of the optical density per metre of the smoke as it discharges from the test rig, that is

$$D_\ell = \frac{10}{\ell} \log \frac{I_o}{I} \quad (3)$$

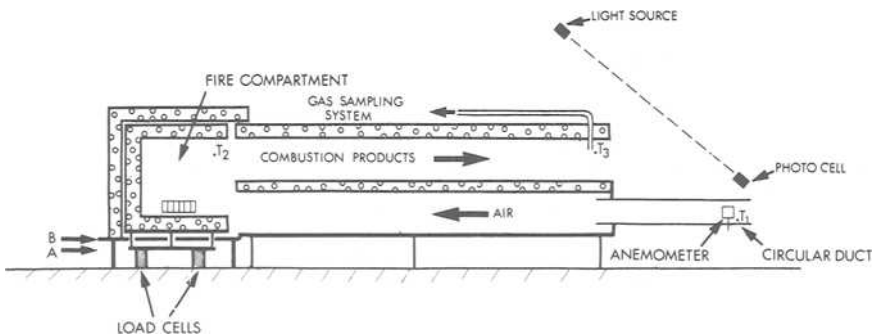


FIG. 1—The small-scale room-corridor assembly (not to scale). T_1 to T_3 indicate thermocouples.

where l is the effective path length through the smoke, which is taken to be the depth of the smoke layer at the end of the corridor. V_f is the volumetric rate of discharge of the fire gases from the corridor; this is calculated from the ratio of air inflow to the fire (V_{air}) and the temperature ratio T_3/T_1 , where T_3 (K) is the temperature at the end of the corridor and T_1 (K) refers to the ambient (Fig. 1). Thus

$$V_f = V_{\text{air}} \frac{T_3}{T_1} \quad (4)$$

Equation 2 gives the total amount of smoke in terms of the volume which has an optical density corresponding to 1 ob. The dynamic smoke potential [D_o (dynamic)] is then obtained from

$$D_o(\text{dynamic}) = \frac{V_s}{W} \text{ ob } m^3/g \quad (5)$$

where W is as just defined. Some of the experiments that have been carried out provide an opportunity of comparing D_o (static) and D_o (dynamic) directly.

Apparatus

The small-scale "room-corridor assembly" is shown in Fig. 1. It was designed to enable the firebox (the "room" or "compartment") to be weighed continuously while the flows of air and combustion products entered and left through a square duct (the "corridor"). The assembly was constructed from angle iron with panels of Supalux, lined with a ceramic fiberboard (Kaoboard). The cubical firebox had external and internal dimensions of 0.5 m and 0.4 m, respectively; one side was open and could be fitted with Kaoboard panels to reduce the opening from 0.4 m down to 0.1 m, as shown in Fig. 2. The box rested on a wooden platform, 0.5×0.5 m, which in turn was supported on five vertical posts (40 mm high by 12-mm diameter rods) fixed to a lower platform (A) which rested directly on two 25 kg capacity load cells. A third platform (B), lying between the latter pair, had holes drilled to

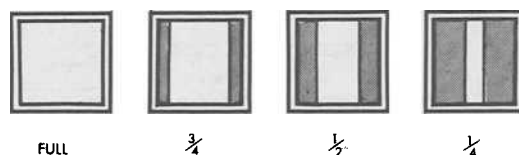


FIG. 2—Ventilation openings to the fire compartment.

allow independent movement of the firebox/load cell assembly (Fig. 1). This platform had supports resting on the ground and in turn supported an outer box which enclosed the fire compartment on four sides. The end of the "corridor" fitted snugly inside the opening of the outer box with which it was sealed by packing the gap with mineral wool. In this way the "room" and the "corridor" were almost continuous, yet the corridor did not interfere with the weight loss measurements. The loss of combustion products into the space between the firebox and the outer enclosure was assumed to be negligible, although there was no way of quantifying this. A deposit of soot accumulated on top of the box during a series of tests.

The corridor, which had a cross section of $0.5 \text{ m} \times 0.5 \text{ m}$, was divided into a lower and upper section, the latter being lined with Kaoboard, giving inner dimensions of 0.4 m wide by 0.2 m high. This arrangement separated the flows and prevented mixing which could stimulate flaming in the corridor under conditions of poor ventilation (for example, one-quarter opening, Fig. 2). The air inflow to the fire was determined by means of a vane anemometer located within a circular duct attached to the end of the lower section of the corridor, as illustrated in Fig. 1. This was effectively the only means by which air could enter the fire box as the outer openings were sealed; the area through which air could enter around the supporting posts was negligible. The volumetric flow of ambient air into the fire (V_{air}) was calculated on the basis of the measured linear flow rate within the duct. This procedure was calibrated directly in separate experiments in which air flowing along the duct (driven by a remote fan) was channelled through a separate vane anemometer of known cross section. The error in the calculated flow (V_{air}) is unlikely to be more than 10%.

The corridor was made in two sections, each 1 m long, and rested on an angle iron frame whose height enabled the height of the firebox to be matched. A 2-m extension to the upper part of the corridor was constructed to enable the effect of corridor length on D_o (dynamic) to be examined in more detail.

Temperatures were measured at the anemometer (T_1), under the ceiling of the firebox (T_2) and at the end of the corridor in the outflowing gases (T_3). A small stream of combustion products was withdrawn continuously from the end of the corridor through a 6-mm diameter sampling line, incorporating a filter and drying tube, by means of a diaphragm pump, capacity 6 l/min . The carbon monoxide (CO), carbon dioxide (CO_2), and molecular oxygen (O_2) content of the outflow from the pump was monitored continuously using commercial analysis units. The opacity of the smoke flowing from the end of the corridor was determined by a diagonal light beam/photocell arrangement similar to that used by Woolley et al [8]. Lateral spread of the plume was restricted by two Supalux boards placed so as to maintain the plume width at the height at which the measurement was being made. The depth of the smoke layer was established first by observation and then by limiting the

opening roughly to this depth with a single thickness of Clingwrap (thin polythene sheet used to wrap food); this helped confine the airflow to the lower section of the corridor during the early stages of each fire. This material softens and sags at temperatures between 100 to 150°C and will increase the depth of the opening when and as necessary. The depth can be measured at the end of each test. This was used as the path length, ℓ , in Eq 3. This tacitly assumes that dilution of the plume does not influence the properties of the particulate smoke which determine obscuration. While this requires experimental verification, the procedure is a common one [6,8].

Most of the experiments described in this paper were carried out at the Scottish Fire Service Training School at Gullane, East Lothian. The test rig was located in the Fire House, a chamber 3 m high with total volume of 240 m³, which could be sealed and used to collect the smoke produced during an experiment. In the early tests, only static measurements were made, with four vertical optical beams located near the corners of the chamber to enable an average smoke density to be calculated and any effect of ambient wind conditions on the uniformity of smoke distribution to be assessed (Fig. 3). A limited number of tests were carried out with one diagonal and three vertical beams so that measurements of D_o (static) and D_o (dynamic) could be made at the same time. Latterly, dynamic measurements only were made in the Fire Safety Engineering Laboratory with effective corridor lengths of up to 4 m.

Up to 13 sets of measurements were made (Table 1) at 5-s intervals, using a data acquisition system supplied by Datron, Ltd., based on the system developed for the Fire Research Station, Borehamwood. The software was modified to suit the requirements of this project.

Three materials were chosen for this investigation: wood (*pinus silvestris*), polymethyl methacrylate (PMMA), and polypropylene (PP). Most of these

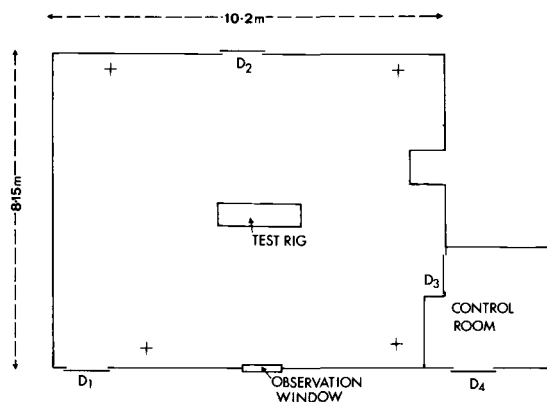


FIG. 3—Plan of the firehouse (Scottish Fire Service Training School). Crosses indicate the location of the vertical light beams. Doors are identified by D_1 to D_4 . D_1 to D_3 and all ventilation openings were sealed during the tests.

TABLE 1—*Details of the data channels.^a*

Channel No.	Device	Measurement
1-3	thermocouples (chromel/alumel) (nonaspirated)	temperature
4	anemometer (Edra 5, Airflow Development Ltd., UK)	air flow
9	load cell (Straininstall, Ltd.)	weight loss
10-13	photocell ^b (ORP12: cadmium sulfide light-dependent resistor)	obscuration

^aChannels 5-8 were used to record data on the radiant heat flux within the firebox and CO₂, CO, and O₂ levels in the outflowing gases, respectively. These data are not reported in this paper.

^bA simple light beam/photocell assembly was used. The light source contained a 3-w tungsten filament lamp and provided a parallel beam of light which was focused onto the photocell device. For the "static" measurements, the path length (L) was taken as the distance between the lenses of the light source and the receiver assemblies.

preliminary experiments were carried out with wood cribs of three sizes, the dimensions of which are given in Table 2. Moisture content was maintained between 10 and 12%. The plastic fuel beds were prepared as cribs but burned for most of the time as liquid pools contained in a light metal tray 0.22 m square. The original intention was to burn all five fuel beds under the following conditions:

1. Free burning, no enclosure.
2. Full and one-quarter ventilation, no corridor.
3. Full and one-quarter ventilation, 1-m corridor.
4. Full and one-quarter ventilation, 2-m corridor.

Each test was carried out at least twice. However, a full set of data has not been obtained for all five fuel beds, but some one-half and three-quarters ventilation experiments were carried out, as well as some with a 4-m corridor.

In addition to the small-scale room-corridor experiments, the smoke production potential of the materials used in this investigation was determined by the NBS smoke chamber method and the test developed at Edinburgh, in which the smoke is collected in a volume of 13.75 m³ [4, 7].

TABLE 2—*Details of the fuel beds.*

Type of Crib	Average Weight, kg	Stick Size, L × B × H, mm	Number of Layers	Stick Separation, mm
Small (wood)	0.5	200 × 5.5 × 5.5	7	27
Medium (wood)	1.2	(a) 300 × 22 × 22 (b) 150 × 22 × 22	6 (alternate layers of a and b)	34
Large (wood)	2.5	300 × 22 × 22	6	34
PMMA	0.24	200 × 10 × 6	3	...
PP	0.08	200 × 10 × 3	3	...

Results

The results are presented in terms of smoke potentials, as calculated from Eq 1 and 5, in Tables 3 and 4, and Figs. 4–7. The values quoted for D_o (static) refer to the maximum obscuration achieved in the firehouse during the test, in accord with the practice adopted in ASTM E 662. The subsequent decrease in obscuration merits further study on this scale.

Freeburning

It was found that with wood cribs and PMMA the values calculated for D_o (static) were within about a factor of 2 of D_o as obtained in the Edinburgh

TABLE 3—Experiments with wood crib fires.

Fuel ^a	Initial Weight of Fuel ^b , g	Conditions of Burning	Length of Corridor, m	Maximum Exit Temperature, T ₃ °C	D_o (Static), ob m ³ /g	D_o (Dynamic), ob m ³ /g
Small crib	511	FB ^b	...	...	0.18 ± 0.02	...
	504	B ¹ / ₄	0	761	0.92 ± 0.08	...
	509	B ¹ / ₂	0	691	0.35 ± 0.01	...
	534	B ³ / ₄	0	...	0.22 ± 0.01	...
	513	B1	0	556	0.19 ± 0.01	...
	505	B ¹ / ₄	1	349	0.87 ± 0.11	0.09 ± 0.01
	505	B ¹ / ₄	2	259	0.86 ± 0.1	0.21 ± 0.01
	490	B ¹ / ₄	4	196	...	0.13 ± 0.02
	514	B1	1	427	0.24 ± 0.03	0.08 ± 0.02
	498	B1	2	299	0.17 ± 0.03	0.06 ± 0.02
	490	B1	4	244	...	0.03 ± 0.005
Medium crib	1195	FB	...	...	0.17 ± 0.06	...
	1162	B ¹ / ₄	0	797	0.76 ± 0.09	F ^b
	1185	B1	0	779	0.28 ± 0.03	F
	1194	B ¹ / ₄	1	675	0.47 ± 0.06	F
	1216	B ¹ / ₄	2	421	0.53 ± 0.03	0.11 ± 0.005
	1122	B ¹ / ₄	4	295	...	0.18 ± 0.01
	1182	B1	1	424	0.23 ± 0.04	F
	1183	B1	2	455	0.26 ± 0.04	0.08 ± 0.005
	1124	B1	4	306	...	0.10 ± 0.02
Large crib	2412	FB	...	...	0.22 ± 0.06	...
	2313	B ¹ / ₄	0	...	0.28 ± 0.005	...
	2381	B ¹ / ₂	0	...	0.49 ± 0.07	...
	2353	B1	0	...	0.29 ± 0.005	...
	2300	B ¹ / ₄	1	800	0.67 ± 0.06	F
	2468	B ¹ / ₂	1	...	0.67 ± 0.11	F
	2305	B1	1	...	0.83 ± 0.18	F
	2304	B ¹ / ₄	2	588	0.57 ± 0.10	F
	2468	B ¹ / ₂	2	542	0.73 ± 0.07	F
	2336	B1	2	...	0.80 ± 0.04	F

^a D_o (EU) = 0.2 ob m³/g; D_o (NBS) = 0.4 ob m³/g.

^b FB = free burning; B = crib enclosed in fire box—¹/₄, ¹/₂, etc. ventilation opening (Fig. 2); weight loss (w) taken to be 80% of the initial weight of fuel (see Eqs 1 and 5); F = flames out of corridor.

TABLE 4—Experiments with PP and PMMA.

Fuel	Initial Weight ^a , g	Condition of Burning	Length of Corridor, m	Maximum Exit Temperature, T ₃ °C	D _o (Static), ob m ³ /g	D _o (Dynamic), ob m ³ /g
PMMA ^b	243	F	...	...	0.48 ± 0.1	...
	243	B ^{1/4}	0	737	1.50 ± 0.15	F
	244	B ^{1/4}	1	...	1.2 ± 0.2	F
	235	B ^{1/4}	2	327	1.1 ± 0.2	0.29 ± 0.04
	250	B ^{1/4}	4	154	...	0.4 ± 0.03
	232	B1	1	404	...	0.24 ± 0.05
	227	B1	2	329	...	0.2 ± 0.02
	245	B1	4	160	...	0.23
PP ^c	79	F	...	...	3.43 ± 0.45	...
	79	B ^{1/4}	0	710	3.95 ± 0.37	F
	80	B ^{1/4}	2	233	5.08 ± 0.49	1.18 ± 0.21
	85	B ^{1/4}	4	177	...	1.16 ± 0.13
	84	B1	2	221	...	1.34 ± 0.11
	85	B1	4	191	...	1.3 ± 0.09

^aIn these tests, all the fuel was burned, so that W = initial weight (see Eqs 1 and 5).

^bPMMA: D_o (EU) = 0.25 ob m³/g; D_o (NBS) = 0.12 ob m³/g.

^cPP: D_o (EU) = 0.35 ob m³/g; D_o (NBS) = 1.7 ob m³/g (see text).

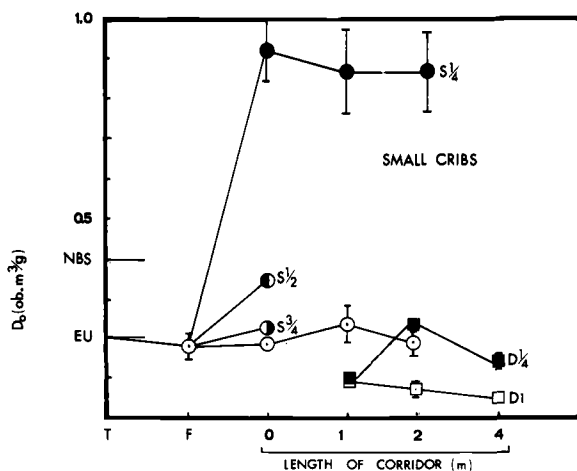


FIG. 4—Variation of smoke potential (D_o) with conditions of burning for small cribs. T = small-scale test (NBS = ASTM E 662; EU = Edinburgh University); F = free burning; S = static measurement ($\circ$ and $\bullet$); D = dynamic measurement ($\square$ and $\blacksquare$). Ventilation to the compartment is indicated by 1, $\frac{3}{4}$, $\frac{1}{2}$, and $\frac{1}{4}$ (see Fig. 2) and is distinguished by shading of the symbols.

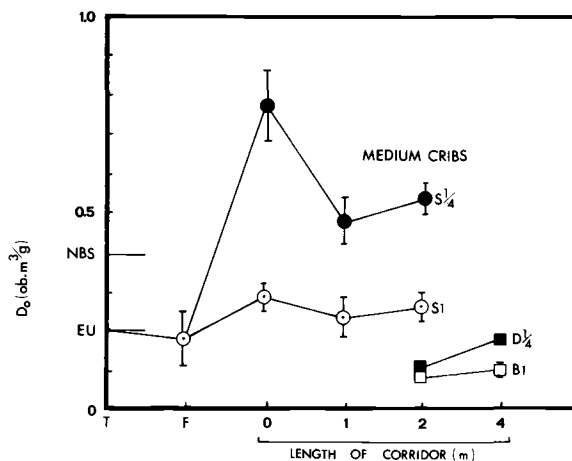


FIG. 5—Variation of smoke potential (D_o) with conditions of burning for medium cribs. Symbols as in Fig. 4.

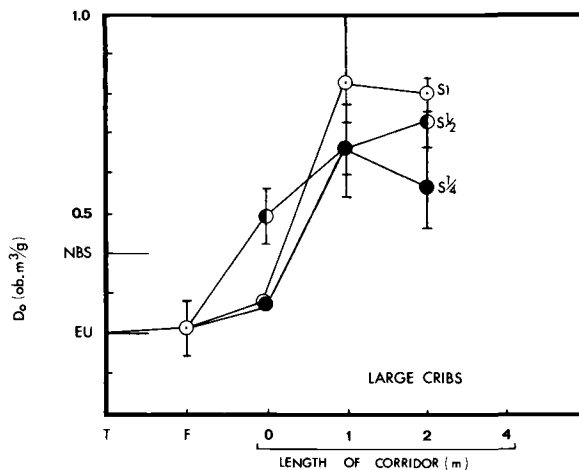


FIG. 6—Variation of smoke potential (D_o) with conditions of burning for large cribs. Symbols as in Fig. 4.

University smoke chamber (Table 4). PP behaved quite differently, giving $D_o \cong 3.4 \text{ ob m}^3/\text{g}$, compared with $0.35 \text{ ob m}^3/\text{g}$ from the laboratory test (see Tables 3 and 4).

Crib Fires Within the Fire Box (No Corridor)

With the small cribs ($\cong 0.5 \text{ kg}$), there was a clear trend towards increasing smoke yield as the ventilation was reduced from 40 to 10 cm (Fig. 4). How-

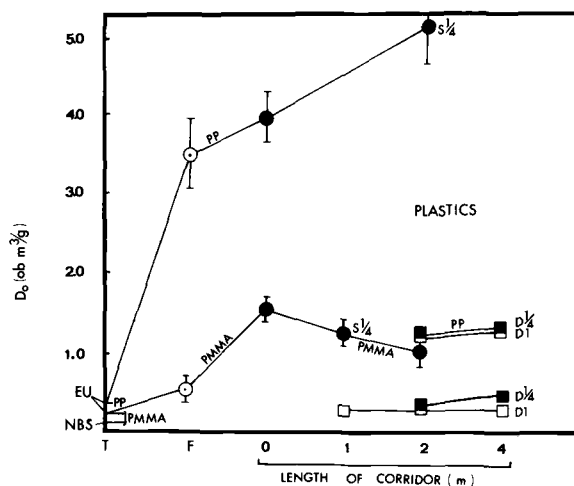


FIG. 7—Variation of smoke potential (D_o) with condition of burning for PP and PMMA. Symbols as in Fig. 4.

ever, the yield of smoke from the large cribs was approximately the same for the 40 and 10-cm openings, while a significantly greater amount was detected for the 20-cm opening (Fig. 6). [The medium cribs appear to behave as the small cribs, but insufficient data are available to confirm this with any confidence (Fig. 5).]

Crib Fires Within the Fire Box (with Corridor)

Dynamic smoke measurements could not be made in those experiments in which flame emerged from the end of the corridor; these measurements are indicated in Tables 3 and 4. No significant trends were observed which could be attributed unambiguously to the effect of corridor length. In this respect (see Discussion) only the data from the small cribs should be inspected. The variation of D_o (static) and D_o (dynamic) with corridor length may not be significant, although clearly the two types of measurements give considerably different values of D_o .

With the large cribs, there is a definite increase in D_o (static) when a 1-m corridor is attached to the firebox. The yield with a 2-m corridor was not significantly different from that with a 1-m corridor.

Plastics in the Fire Box

The plastics PMMA and PP burned as liquid pool fires. The yield of smoke from PMMA at one-quarter ventilation was a factor of three greater than that from free burning, but the presence of the corridor was found to have only a

small effect ($\cong 30\%$ reduction for 2 m). On the other hand, the increase in smoke from PP with one-quarter ventilation was only 15% over that from free burning, and a 2-m corridor increased this by a further 25%, giving a value of D_o (static) 15 times greater than that measured in the Edinburgh University (EU) smoke chamber test.

As with wood crib fires, D_o (dynamic) was found to be very much less than D_o (static) for both PMMA and PP. However, while trends are apparent in Fig. 7 which suggest an increase in D_o (static) with corridor length, these trends cannot be regarded as significant.

Discussion

The reproducibility of the data obtained in these experiments was reasonable considering that even under the strictly controlled conditions of the NBS test it is difficult to improve the precision to better than $\cong 25\%$. Sufficient data have not yet been gathered to make a meaningful statistical analysis, but one or two general observations may be made which indicate trends in behavior. Thus the results indicate that the static smoke yields from small cribs ($\cong 0.5$ kg) burning within the firebox with different ventilation openings are in accord with conventional thinking, that is, decreased ventilation gives rise to more smoke. The results with the large cribs appear to contradict this, particularly in that one-quarter and full ventilation give the same D_o (static) when there is no corridor. This can be explained in qualitative terms. The fire plume issuing from the 10-cm opening is seen to be very strong, giving a highly turbulent flame in which the efficiency of combustion is likely to increase as a result of greater air entrainment. This will act as an "after-burner," and less smoke will be released as a result. The same argument can be applied to the results (S_1 and $S_{1/4}$) obtained with the corridor (1 and 2 m) in place (Fig. 6); flames issued from the end of the corridor for both ventilation states, but those from quarter ventilation were much more vigorous. Comment on $S_{1/2}$ is reserved until more data are available.

The presence of a corridor does not appear to make any significant difference to the values of D_o (static) for small cribs under full and quarter ventilation. These fires were burning under air-rich conditions, and while some flaming was observed outside the ventilation opening of the firebox on its own, none appear from the end of the 1 m corridor. Combustion of the volatiles would seem to be largely complete within the fire compartment. While the burning behavior of the medium cribs appear to be similar, that of the large cribs was quite different, apparently falling well within the ventilation-controlled (fuel-rich) regime. Continued combustion in the gases flowing from the compartment followed by vigorous burning in the fire plume outside the test rig introduces several other factors which are likely to affect the final smoke. Further experimental work is planned to study this scenario in more

detail; at the present time, interpretation of these data must be at least partly speculative.

The data from the small and medium cribs show most clearly that D_o (static) is considerably greater—by a factor of at least three—than D_o (dynamic). Such a large discrepancy could not be due to experimental error, although there are certain features of the dynamic measurement that will introduce uncertainty. Thus, while any error in the volumetric flow of ambient air is likely to be small, the observed temperature of the outflowing gases exhibited fluctuations (± 20 K max) which will inevitably affect the instantaneous value of V_f (Eqs 2 and 4) and thus D_o (dynamic) (Eq 5). However, even in the worst case this would introduce an error of no more than 10 to 15%. (No correction was made for radiation losses from the exit thermocouple, but this is unlikely to be significant in comparison with the uncertainty in T_3 .) There are several tacit assumptions involved in using the “dynamic method” which are not addressed here but which are demonstrably invalid. These assumptions include uniformity of temperature, concentration of smoke, and velocity along the light beam, but these problems are common to all studies in which this particular “dynamic” method is used, so that the present results are still valid in identifying the observed differences.

While it is just conceivable that the discrepancy between D_o (static) and D_o (dynamic) could be attributed to the shortcomings of the method, there are other factors capable of accounting for the differences.

One possible explanation would be that the smoke flowing from the end of the corridor is hot and has not fully matured; condensation and coagulation processes will occur as the hot combustion products cool as they mix with ambient air, yielding a much higher concentration of suspended matter, liquid and solid. For example, water still will be in the vapor phase at the temperatures measured in the outflow (Tables 3 and 4). It was to test this hypothesis that a 2-m extension to the 2-m corridor was prepared, to discover if additional cooling would result in a higher value of D_o (dynamic). As can be seen in Figs. 4, 5, and 7, the results are inconclusive. If smoke temperature is the main factor in creating this discrepancy, then it would seem that greater cooling will be necessary to produce a significant effect.

It is interesting that the values obtained for D_o (dynamic) are similar to D_o (EU). Rasbash and Pratt [7] compared the dynamic smoke yields from fires in a full-scale room-corridor assembly [8] with the yields predicted from small-scale test results [that is, D_o (EU)] and found reasonable agreement. However, in view of the results obtained in the present series of experiments, this would appear to be fortuitous.

The present result that D_o (dynamic) < D_o (static) (see Fig. 8) is contrary to a report by Paul [10] which indicated that for upholstered polyether foam cushions D_o (dynamic) is greater than D_o (static) by as much as a factor of three. While the reason for this apparent discrepancy has not been resolved, it is worth noting some differences in the experiments, in addition to the mate-

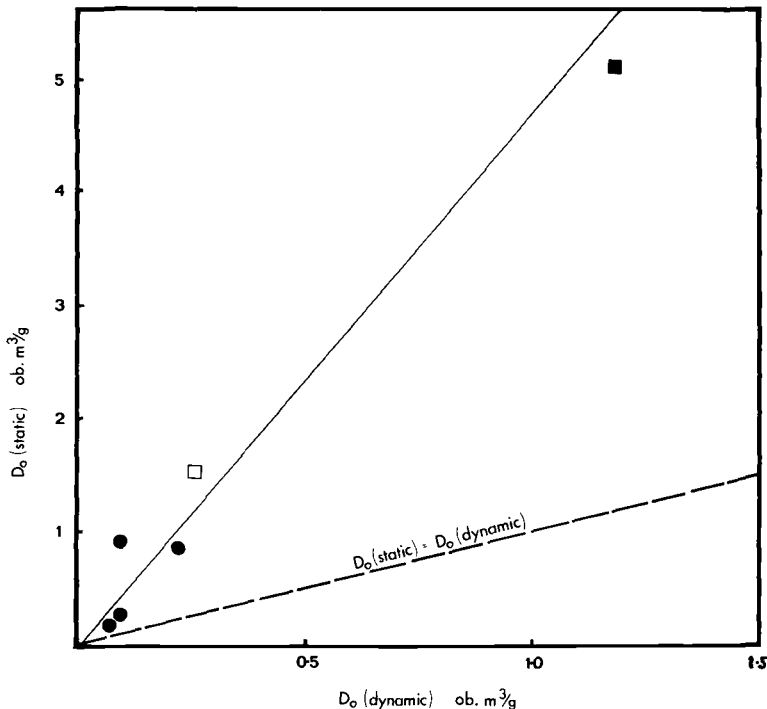


FIG. 8—Comparison of D_o (static) and D_o (dynamic) for small wood cribs (●), PMMA (□), and PP (■).

rial involved. The volume of the chamber used by Paul was 100 m^3 , in which substantially greater quantities of smoke were being collected than in the present tests with a volume of 240 m^3 . Secondly, Paul waited for 15 to 20 min after the fire had ceased before making his static measurements. An aging process will certainly be operating and may be significantly more rapid at higher concentrations. However, the present results indicate that this would not account for more than 25 to 30% drop in D_o (static) over a similar period. Two other differences may be mentioned. In Paul's experiments, the combustion gases were discharged vertically from the rig through a duct, which would have resulted in their direct impingement on the ceiling, approximately 1 m above the outlet. In the present tests, the discharge was horizontal, and the distance to the ceiling was approximately 2 m. Loss of smoke particles by deposition on the ceiling could have been significant in Paul's experiments, which were carried out in a closed room-corridor assembly with a much higher surface to volume ratio than the firehouse at Gullane.

The comparative behavior of PMMA and PP merits comment. Under free burning conditions, D_o (static) for PP is almost ten times greater than D_o (EU), a far greater difference than that for PMMA. In this study, smoke pro-

duction was not measured in the NBS test, but results obtained by Christian [11] with the same material may be used to estimate D_o (NBS) $\sim 1.7 \text{ ob m}^3/\text{g}$. The difference between this and D_o (EU) is substantial and may be a consequence of the difficulties associated with testing a material which melts and flows so readily in the vertical orientation (PMMA softens but does not flow significantly). However D_o (static) for PMMA increases by a factor of three when it is burned within the fire box with one-quarter ventilation, while the increase is only 15% for PP (Fig. 7). Further tests will be necessary to establish the cause of this, but one factor that deserves consideration is flame emissivity. The flames from PMMA are less emissive than those from PP [12], and consequently it may be more sensitive to radiative feedback within the firebox. (This observation implies a number of assumptions relating to smoke production as a function of rate of burning and the structure of the diffusion flame, as well as the role of ventilation in determining smoke yields if high rates of burning are involved.) In contrast, the yield of smoke from the wood cribs burning in the fully ventilated firebox was not significantly different from that from free burning; this is consistent with the fact that the greater proportion of the burning surfaces of a wood crib are shielded from the radiative feedback from the compartment.

A more extensive analysis of the experimental data will be presented elsewhere.

Conclusions

The yield of cold smoke from fires in enclosures is likely to be significantly greater than that predicted on the basis of small-scale laboratory tests. Dynamic measurements of hot smoke also underestimate the final yield. Small-scale experiments have shown the need for greater understanding of the effect of confinement on smoke production from different materials. Until this has been achieved, it will not be possible to incorporate realistic smoke production rates into smoke movement models, and the application of the results of small-scale tests such as ASTM E 662 to selection of materials will have to be treated with circumspection.

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Control Safety Considerations for HVAC Smoke Management Techniques

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ABSTRACT: The modern heating, ventilating, and air conditioning (HVAC) system has been designed efficiently for its specific function. Use of this system for smoke control stresses many of its components.

Central fans can be overloaded, outlet air terminals can hamper or obstruct efforts, and dangerous static pressures can be generated that can hold fire doors open or closed or blow out windows. Efforts to provide fresh air can cause freezing damage to critical central equipment.

This paper discusses the various problems without providing a specific course of action. The Summary is a checklist of considerations if the HVAC system is to be part of a smoke control system. The primary suggestion is that smoke control be part of the initial specifications to assure that certain safety factors are considered.

Specifications should consider:

1. Control fan load limiting controls.
2. Compressed air fuses to keep the system operating despite damaged lines.
3. Compressed air interlocks to abort smoke management efforts if all control fails.
4. A means to protect equipment from freeze damage in northern climates.
5. Positive position terminals, either fail open or fail closed depending on the concept selected.
6. Interlocks on self-contained terminals to drive open or closed.
7. Programmed control of terminals as opposed to “kill the main” approaches.
8. Matching pressurization areas to fan capacity.
9. Pressure testing of the system after completion, including fan load and forces on doors.
10. Possible use of dedicated fans on each floor (not part of the HVAC system) for pressurization and for exhaust.

KEY WORDS: smoke control, HVAC system, air terminals, central fans, building static pressures, pressurization, fail open, fail closed, compressed air interlocks, load limiting

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Fire and smoke management is not a new concept. Technically, the very act of fire fighting has always involved both fire and smoke management. This management act has been extended forward in time, prior to the arrival of the fire fighters, by the intervention of various automatic devices. Sensors have been closing dampers and fire doors, shutting off fans, starting sprinklers, etc. for many years. What we are seeing today is the natural extension and sophistication of these fire and smoke management techniques.

Today a major emphasis is being placed on the smoke management side of the problem. This primarily is because of the more widespread awareness that most fire-related deaths are due to smoke inhalation and often occur some distance from the actual fire. One method of smoke management advocated is to “sandwich” the fire floor with higher static space pressures on the floor(s) above and below the fire (increased air supply, reduced exhaust). This hopefully will prevent smoke migration to these floors.

Conversely, producing a negative static pressure (increased exhaust, reduced air supply to the floor) on the fire floor produces the same effect. Sometimes both steps are taken.

An air handling and distribution system in a public building dangling in front of the eyes of the would-be smoke manager might be legally classified as an attractive nuisance. Like a backyard pool, it can be a useful, beneficial addition or a deadly one, depending on the skill of the user.

In the following discussion, I will try not to favor any particular approach but will instead give the pros and cons of various approaches to smoke management. The aim is to make the reader more aware of the idiosyncrasies of today's heating, ventilating, and air conditioning (HVAC) equipment, and then to let him judge and select the concept of his choice.

The biggest danger today involving the use of the HVAC system for smoke management is probably the lack of up-to-date knowledge of how today's HVAC system works. Today's HVAC systems have been completely reengineered, thanks to the energy crunch.

For most of our engineering lives, the HVAC systems had constant air distribution and had a full capacity fan pushing a constant flow of air with a balanced, even distribution around the building through ductwork and terminal units with built-in, spring-loaded flow regulators. Slam it on, shut it off, or cut off sections—it was hard to hurt the system or the building. *This is not true today.*

Today's HVAC system is usually variable air volume (VAV), has a fan designed for a VAV system, and is selected 20 to 35% smaller than the total design air capacity of the building. Its sizing is based on the fact that all zones in a building will not peak together but will “follow the sun” (the east zone peaks in the morning, the south zone at noon, and the west zone in the afternoon). The system feeds through ductwork, sometimes made of fiber glass, with significantly lower overpressure protection.

The engineer utilizes the fact that the peak load in various parts of the building occurs at different times of the day (known as diversity) so that the

instantaneous demand typically hits a peak of 65 to 80% of the total. He selects a fan to only deliver this amount, considering initial cost, controllability, and operating cost. On the delivery end, the fan is feeding a sophisticated pneumatic or electronically powered, ultra-low resistance, VAV terminal. Gone is the high resistance spring-loaded mechanical regulator. Cut off the power to today's terminals and they may slam wide open, grossly exceeding design flows. Only a few terminals doing this near the fan can push it into an electrical overload state, which, if properly protected, will blow the fuses and abort any further smoke control efforts and will, if not properly protected, damage the blower motor. HVAC fans, both today's and yesterday's, always have had the capability to produce statics of several hundred pascals, typically 500 to 1500 Pa (2 to 6 in.) of water pressure.

These pressures never got out of the ductwork, thanks to the old spring-loaded, mechanical regulators. They can easily escape with today's energy efficient, low resistance regulators dependent on external power.

Five hundred Pa (2 in.) of water pressure will produce over 90 kg (200 lb) of force on a typical 90 by 200 mm (36 by 80 in.) exit door. You don't have to worry about this happening, however, because the windows probably will have blown out long before this and will have relieved the pressure.

So 500 Pa (90 kg) is not likely, but what about 250 Pa static (45 kg) (100 lb) or 125 Pa (22 kg) (50 lb) or even 65 Pa (12 kg) (25 lb) against a child or the elderly? These lesser values are quite likely to occur.

A typical commercial builder utilizes the space above the ceiling as the return air plenum. The average 61 by 122 mm (24 by 48 in.) ceiling tile weighs 2 kg (4.5 lb); only 40 Pa (0.15 in.) between the pressurized space and the return plenum will provide the lifting force necessary to raise the tile and jeopardize the fire-rated ceiling.

These figures are not only possible but quite probable if the system is operated incorrectly. (Most commercial HVAC workers can tell you true tales of blown windows, of ductwork collapsed or blown, of doors held open or closed, etc.)

Most of these events occur during start-up and balancing when the building is unoccupied and being checked out. Therefore, they go unreported to the general public, but the conditions that caused them are similar to conditions that might be intentionally or unintentionally imposed during fire management.

Today's HVAC system has been optimized to do its specified task. If it's going to be applied as a part of the smoke management system, that should be part of the design criteria. Otherwise, the smoke management system must work around the shortcomings of the HVAC system.

The equipment problems are:

1. Small VAV fans.
2. Low resistance terminals.
3. Terminal fail-safe mode.

4. Self-contained terminals.
5. Freeze damage to central equipment.
6. Vulnerable terminal power supply lines.

I'd like to discuss the ramifications of each of these problems and what measures might be taken to avoid the problem.

1. Small VAV Fans

Any excessive demand on the VAV fan can cause it to electrically overload and, if properly protected, shut down, defeating smoke management efforts (or damage itself if not properly protected). The fan could readily overload if "fail open" terminals are used and fire damages the main control lines supplying the terminals or if part of the smoke management approach is to kill terminal power, intentionally driving terminals wide open.

The supply fan in a VAV system is usually controlled by a static pressure pickup in the ductwork. This pickup often is located remotely in the supply duct system of the building proper. Sometimes multiple pickups are used for control purposes, locating them at the ends of various long duct runs with a logic system to select the lowest static signal. If a pickup happens to be located in the supply duct on the fire floor, the signal resulting from the supply duct being cut off on this floor will drop off and cause the supply fan to run wide open. A fire-damaged pickup will also drive the fan wide open. With a pickup at any other nonfire location, the fan will remain at some normal cubic feet per minute (CFM). In a typical VAV system, depending on the season and time of day, this normal CFM could be at a very low level, inadequate for the pressurization of surrounding floors. Therefore, the supply fan in a VAV system must always be disconnected from its static controls and programmed to its safe maximum for smoke management. Limiting this air flow is best done by programming the terminals properly; this is covered in Nos. 2 and 3.

As far as the fan itself is concerned, it will always have a control system. A protective high limit flow control can be added which will not only protect the fan motor but will also allow smoke control efforts to continue.

In new designs, the use of a larger fan motor that will allow greater overvolumes without failure is recommended. (You cannot specify larger capacity fans, as a controllability problem will occur during normal VAV operation.)

Another concept is to close the returns on the floors above and below the fire, thereby preventing exhaust air from leaving these floors. Since the supply continues open, this will increase the pressure on these floors but will not use large quantities of CFM.

While this concept seems to protect the fan from overloads, it can produce dangerous pressure levels on these floors as discussed in No. 2. Also, if panicked occupants or overpressure knock out a few windows, the large exhaust path to the outside will increase the possibility of overvolume.

The return fan's primary function is to maintain the building static slightly positive relative to the outside. It does this by controlling the amount of return air flowing through it. It is typically operated at a CFM 5 to 20% below the supply fan. The difference allows for exfiltration, bathroom and hood exhausts, etc.

Usually, the return fan is simply controlled by a static pickup in the space, referenced to the outside by a pickup on the roof or in an open (exposed to the outside atmosphere via wall louvers) equipment room.

With this type of return fan control and no assurance that the pickup won't be in or around the fire zone, it's obvious that we must take control of the return fan during smoke management, if for no other reason than to increase its exhaust capacity.

In high-rise buildings, a flow synchronization approach is used between the supply fan and the return fan in these cases. This system is located in the equipment room, so concerns for fire damage are minimal. It basically involves measuring the flow in the supply duct and compensating the return fan accordingly. Consideration should be given to leaving this system operational during smoke control operation, but programming it to maximum operational CFM, utilizing flow and static protective limits.

2. Low Resistance Terminals

As mentioned earlier, the modern VAV terminal utilizes a very low resistance concept for energy conservation. Unlike its predecessor, which had a built-in spring-loaded mechanical damper and usually required a minimum 250 Pa pressure drop before design CFM was reached, today's terminal often reaches design CFM at 50 or 75 Pa static or less.

The old approach to smoke control in VAV systems with mechanical regulator-type boxes was to "kill the main" (since most controls were pneumatic). "Kill the (pneumatic) main" resulted in design maximum CFM to all spaces, thanks to the limiting effect of the built-in mechanical regulators.

If you do that to today's terminal, it will go wide open, fully close, or remain in its last position. If wide open (of normally open design), the terminal will want to overvolume, significantly so if near the fan or if the duct statics are their typical 1 to 2 in. of water pressure or both. An approximate flow formula is: $\text{Air Flow} = K\sqrt{\Delta P}$, where K is the flow resistance constant for the terminal (or floor). If $K\sqrt{50} = \text{design CFM}$ (where 50 Pa = wide open box ΔP at design CFM), then $K\sqrt{500} = 3 \times \text{design CFM}$ (where 500 Pa = possible duct pressure). (K is the flow coefficient of the terminal.)

This produces several problems: first, the fan unit must have protective flow high limits; second, if only the floors surrounding the fire are driven wide open, the increased flow, several times the design and applied to floors above and below the fire floor (particularly if returns are also closed), will quickly

pressurize those floors to probable excessive pressures affecting both doors, windows, and ceiling tiles.

With no designed air flow path out of the pressurized floors, it falls upon an unknown space leakage factor to determine how close the floor pressure will approach the excessive duct pressure.

Reapplying the just-mentioned formula, if design CFM produces a pleasant 7.5 Pa positive pressure with returns open, then $3 \times$ design CFM will produce an undesirable 75 Pa pressure. Closing the returns will really aggravate the situation by blocking the exhaust path.

Killing the main to normally open terminals will work if a large enough percentage of the total terminals are opened to avoid excessive pressures, but not so many that the fan is overloaded. Keeping returns open will prevent an excessive overpressure situation.

Electric and electronic boxes mostly fail “as is” with no normally open or closed mode. This creates a problem as most terminals are considerably throttled (closed) except during “design” periods. Nights, weekends, and most of the year the terminals are operating well below design CFM, and space pressurization through these terminals using a “kill the main” (kill the power) approach would be random and difficult.

Obviously, “fail closed” terminals would not be cooperative with a “kill the main” concept at all.

All terminals are capable of being “captured” and programmed to design maximum or off. This approach, instead of a “kill the main,” coupled with the proper protective flow and static limits on the central fans, may be best.

3. Terminal Fail-safe Mode

All terminals are capable of being captured and programmed to maximum or off.

Using programmed flows, you can assure proper air flows to the space, minimize central fan risks, and generally assure a stable smoke management system.

If normally closed terminals are selected, then fire breaking through undetected to a pressurized floor will automatically close those terminals when the control lines are damaged.

If normally open terminals are selected, then the risk exists of fire damage reopening a programmed closed terminal. You will have to count on controllable smoke or fire dampers in supply ducts for programmed closing.

4. Self-contained Terminals

Some terminals are totally self-contained. Thermostat and dampers are both powered from duct static pressure. These terminals mostly fail open and if not captured (they can be) may resist pressurization tactics. For example, a

fire on a cold day would find these and all other terminals operating at minimum or off.

Other smoke management tactics, such as no return air, 100% fresh air supply, and exhaust, will keep cooling the space and drive closed terminals not captured. Attempts to pressurize these areas would be fruitless. These terminals must be captured and programmed open (or closed on the fire floor).

5. Protecting Central Coils

In some central system designs, severe damage can occur if 100% outside air is introduced to the building at below freezing temperature.

A worst case scenario depicts a small closet or wastebasket fire triggering a smoke management system on a below zero day, resulting in freezing and cracking a chilled water coil that typically could take six months to replace, disabling the building for that period of time.

If you're going to use the central system for smoke management in freezing climates, you must consider protection of the equipment. Northern U.S. buildings almost all have preheat coils on the outside air which can be turned full on. Otherwise coils must be freeze-protected with antifreeze solutions or automatically drained in the event of a freezing situation. (Check capabilities of preheat coils; some are not capable of handling 100% outside air and will still freeze.)

Other equipment which could be damaged and may be in supply ductwork are humidifiers, damper actuators, electronic air cleaner washer, compressed air lines, and possibly reheat coils downstream near the terminals.

6. Vulnerable Terminal Power Supply Lines

This is the bottom line—you can't use the HVAC system for smoke management in today's VAV system if you lose control of the system.

Most systems today (some 90%) in large buildings are pneumatically operated. The air lines are mostly plastic. A melt through on a main on the fire floor will most likely bleed down the compressor tank in a few minutes, more rapidly if a high side main (usually 500 to 700 kPa) and more slowly if a low side main (approximately 140 kPa).

Normally open terminals will all dump, even with a protected fan; air distribution will be poor and lack ability to pressurize floors.

Normally closed terminals obviously will prevent any smoke management. Regardless of the terminal, the central fan volume control is also pneumatically operated and is usually normally closed for safety upon start-up (starts unloaded). For safety reasons, the outside air dampers, exhaust and supply, also fail closed while the return fails open. This would recycle any exhaust smoke.

Air fuses are an existing product and should be specified on the main to every floor. This will help assure the integrity of the rest of the system even though fire damages the lines on its floors.

An "air fuse" is a spring-loaded restriction which will pass its rated flow with little resistance but close down the line should an unusual demand be made on it, such as a break. The "air fuses," of course, should be located at or before the entrance to the floor where fire damage cannot affect the fuse itself.

An interlock on main air is recommended to abort smoke control efforts should all terminal power be lost.

7. Static Feedback

Building computer automation people are looking at static feedback to a central computer to monitor smoke control pressures and apply corrections. This should reduce the possibility of overpressurization. The sensing equipment should be protected from fire damage itself, and an analysis should be done of how the computer itself will act when some of its routine room sensors, etc. are opened or shorted by fire.

8. Separate Smoke Control Fans

Killing the central system and pressurizing or evacuating zones with individual dedicated fans is a safe, simple approach requiring little sophistication but probably high initial cost.

The fan can be sized for a specific area and pressure and easily tested. All terminals could fail closed, making the task simpler.

The problem would be with the exhaust mode. How can you assure that you will draw smoke from any area in the zone where it might be generated, and how can you remove it from the building? Through the wall? Or a dedicated shaft?

This is why working with the HVAC system is so appealing. Within a zone are multiple inlets and returns, and the likelihood of the smoke leaving occupied areas close to its origin is good. (The initial costs of separate fans might not be as costly when compared to the protective measures required when using the building system.)

Summary

Smoke control systems needs should be part of the basic specification on new construction. If a retrofit or afterthought, strong consideration should be given to the weaknesses mentioned. As part of the initial specification, they should consider:

1. Control fan load limiting controls.
2. Compressed air fuses to keep the system operating, despite damaged lines.
3. Compressed air interlocks to abort if all control fails.
4. A means to protect control equipment from freeze damage in northern climates.
5. Positive position terminals, either fail open or fail closed depending on the concept selected.
6. Interlocks on self-contained terminals to drive open or closed.
7. Programmed control of terminals as opposed to "kill the main" approaches.
8. Matching pressurization areas to fan capacity.
9. Pressure testing of the system after completion, including the fan load and the force on doors.
10. Possible use of separate fans for pressurization and for exhaust.

The Need and Availability of Test Methods for Measuring the Smoke Leakage Characteristics of Door Assemblies

REFERENCE: Cooper, L. Y., "The Need and Availability of Test Methods for Measuring the Smoke Leakage Characteristics of Door Assemblies," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 310-329.

ABSTRACT: This paper identifies and places into perspective relevant information that would assist in focusing future research and development on test methods to measure the smoke leakage characteristics of door assemblies. The concept of smoke compartmentation is introduced and developed. The importance of cross-door pressure differential in establishing the performance of door assemblies in fire-generated environments is discussed. Door assembly performance then is related to life safety, in general, and to the design of compartments of safe refuge, in particular. The entire discussion suggests a listing of required door assembly test methods and, finally, leads to a review of the availability and development status of existing and potential future test method candidates.

KEY WORDS: compartmentation, compartment fires, door assemblies, high-rise buildings, leakage, life safety, pressure differential, pressurization, property protection, safe refuge, smoke control, smoke movement, stack effect, test methods

The fire-stopping characteristics of door assemblies during certain practical fire scenarios can be estimated with some confidence by means of well-established test procedures. In contrast to this, the development of analogous test procedures for estimating a door assembly's smoke-stopping characteristics are being fostered only now by appropriate institutions such as American Society for Testing and Materials (ASTM), International Organization for Standardization (ISO), and the National Fire Protection Association (NFPA). Yet, it is well recognized that in real fire scenarios contact with envi-

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ronments contaminated with a fire's gaseous and solid products of combustion (that is, smoke), even though far from the fire's combustion zone (that is, flames), can pose as serious a threat to life and property as can the fire itself [1-3]. It is the purpose of this paper to assemble and place into perspective relevant information that would assist in addressing facility design problems related to the latter situation.

The discussion to follow will focus attention on the relation between smoke leakage of door assemblies and smoke spread throughout a facility. However, smoke spread is a function of the leakage of *all* construction elements of a facility's partitions—leakage, for example, through joints, construction cracks, and penetrations around pipes, conduits, and ducts, all of which are commonplace in floor/ceiling and wall assemblies. The relevance of the basic discussion to smoke leakage of generalized facility partitions therefore should be emphasized here at the outset.

Smoke Compartmentation

Clarification of the Term "Compartment"

To fix ideas it is useful to clarify the terms "fire compartment" and "smoke compartment," which will be used frequently in the discussion to follow. Here, a fire compartment is meant to denote a single facility space or group of contiguous spaces (on the same or even on different levels of the facility) which is/are bounded by a partition envelope assembly engineered and constructed to have significant, known, fire-stopping characteristics. For a fire compartment of more than one facility space, the design fire-stopping performance of the compartment should be assured regardless of which compartment space(s) contain the fire.

A smoke compartment is analogous to a fire compartment. Its envelope is designed and constructed to have known smoke-stopping or smoke-leakage rather than fire-stopping characteristics. A smoke compartment of fire involvement is the smoke compartment which actually contains the fire. The smoke-stopping performance of a smoke compartment partition element would be assured independent of the location of the fire within the compartment.

By design, it is possible that a smoke or fire compartment of fire involvement will reliably maintain its integrity for only a specific, limited time interval into the fire. After this time interval, failure in a relatively weak component of the compartment envelope, say, onset of significant warpage of a compartment door or failure of seals due to high temperature exposure, could lead to a new, enlarged, smoke or fire compartment of fire involvement. Whereas the original compartment may have involved a relatively small portion of the facility, the new compartment could be much larger, possibly even

encompassing all parts of the facility except for smoke compartments of safe refuge.

While it is possible for a smoke compartment and a fire compartment to be identical, this need not be the case. For example, a fire-rated compartment envelope may be designed to provide reliable fire stopping during the course of likely hazardous fires for a 1-h period. Yet, if the smoke leakage characteristics for all or part of the envelope, for example, of the door assemblies, are unknown and if the facility is not equipped with an appropriate system for pressurization, then it may be necessary to consider a much larger portion of the facility as the smoke compartment of fire involvement.

Test Methods for Implementing Fire Compartmentation

In the design of facilities, fire compartmentation has been classically used to impede the spread of fires from one space to another. In this regard, several test methods [4-10] have been developed to measure the time that a particular construction element will reach agreed-upon criteria of failure when exposed to a standard, fully developed fire. Thus, by proper choice of rated construction elements, which include door assemblies, one can under certain limited circumstances anticipate containment of a fire to the compartment of fire origin for a time interval of, for example, one, two, or more hours. The limited circumstances referred to include those where wind loading, stack effect, and/or facility ventilation would not lead to significant positive pressures (relative to adjacent compartments) in fire compartments of fire involvement.

While compartment constructions might predictably contain the spread of fire to adjacent spaces, they may or may not necessarily abate the spread of smoke to any significant degree. Yet, most would probably agree that with care in design, construction, and installation, significant reduction in typical smoke leakage from a smoke compartment of fire involvement to an adjacent compartment can indeed be achieved. If compartment envelopes could be so implemented, and, just as important, if meaningful characterizations of the smoke leakage performance of such envelopes during real fire scenarios could be established, then, as in the case of fire compartmentation, smoke compartmentation would be an important concept. Smoke compartmentation would be convenient and useful for understanding, developing, and implementing methods of controlling smoke spread.

Mechanisms of Intracompartment Smoke Migration—Two Types of Smoke Compartments

It is useful to identify characteristics of the two basic types of smoke migration mechanisms which are depicted in Fig. 1. One of these types would be

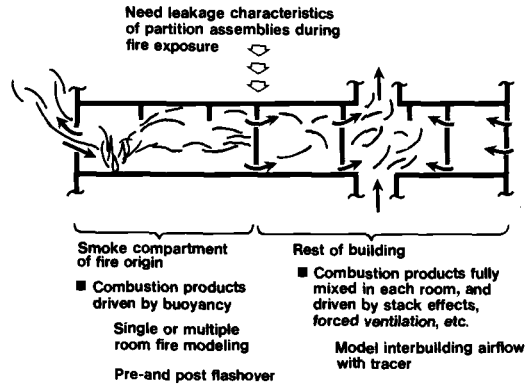


FIG. 1—Mechanisms of intracompartments smoke migration (from Ref 11).

associated with the smoke compartment of fire involvement, and the other with all other smoke compartments of a facility.

Because of relatively high smoke temperatures and associated large temperature differences, the buoyancy forces which lead to strong intracompartments stratification will dominate the dynamics of smoke migration within one or all spaces of a smoke compartment of fire involvement. However, once smoke has been transported from this compartment through penetrations (for example, leaks in a doorway assembly) in its boundaries into an adjacent smoke compartment, different primary mechanisms of further smoke migration can come into play. If the rate of smoke leakage into adjacent compartments is limited by well-designed partition assemblies, then it is reasonable to expect² success in a model of further smoke spread which assumes that entering smoke is continuously and uniformly mixed throughout adjacent compartment environments. Such mixing would be driven, for example, by forced ventilation-generated air currents and by buoyancy-driven wall flows [12,13]. This suggests a second, "tracer-gas migration" type of mechanism or the smoke's continued transport throughout the facility. The term "tracer-gas migration" is meant to indicate a mechanism of intrafacility smoke movement outside the smoke compartment of fire involvement, which is characterized by time-dependent, compartment-to-compartment, migration and dilu-

²There is an important exception to this expectation. This has to do with the movement and mixing of smoke in tall, narrow spaces such as shafts, stairwells, and tall, narrow atriums. The dynamics of smoke introduced into such spaces, even if at a temperature only moderately above the local ambient, will tend to bear strong similarities with the aforementioned smoke dynamics of the fire compartment. Thus, for better or for worse, strong stratification of the smoke may develop, and an analysis or design based on the assumption of a uniform, fully mixed environment throughout the shaft, stairwell, or atrium can lead to significant error. Modeling of the dynamic environment within such high aspect ratio spaces (that is, large ratio of height to compartment width) is an important and relevant problem whose solution is only now in the research stage [11].

tion of relatively cool smoke. The intrafacility smoke migration and dilution is driven by pressure differences between uniform environment compartments, which are generated by forced ventilation systems, wind effects, stack effects, or a combination of these driving forces.

A Key to Successful Smoke Compartmentation

If one accepts the just-mentioned description, successful smoke compartmentation can be achieved if, by design, there is a switchover from strong and rapid smoke spread, due to significant and unavoidable stratification on the fire side of a smoke compartment barrier, to a conceptually simpler and more easily analyzed tracer-gas migration smoke spread on the other side of the barrier.

The designed switchover to the tracer-gas migration mechanism of smoke spread outside the smoke compartment of fire origin is key in that it would allow an achievable and practical method of analysis of total intrafacility hazard development. The method of analysis referred to would involve an enclosure fire model of appropriate detail (for example, like one of those of Refs 14–18) for predicting events in the smoke compartment of fire involvement and a facility infiltration and ventilation model (for example, like one of those of Refs 19–21) for all other smoke compartments of a facility. To specify or evaluate fire safety, requirements of safe available egress time [22] would be established for the different types of smoke compartments of the facility. Criteria for a safe compartment environment would be established, where these criteria would be based on predictable fire environment descriptors (for example, the level of smoke dilution relative to concentrations in the smoke compartment of fire involvement).

In the just-mentioned type of facility fire safety evaluation, it would not necessarily be the role of the fire test community to establish requirements of safe available egress time and criteria for a safe environment (a task partially attempted, for example, in Ref 23). However, this community would clearly have a role in the development of door assembly, smoke leakage, test methods. Indeed, the ability to carry out the indicated type of analysis of hazard development at any level of sophistication is totally dependent on a quantitative description of the leakage characteristics of door assemblies.

The Importance of Cross-Door Pressure Differential in Establishing a Rating for a Fire Compartment Door Assembly

Pressure Differential and the Smoke Leakage of Fire Compartment Door Assemblies

The last section discussed mechanisms for smoke migration both inside and outside the compartment of fire involvement. As depicted in Fig. 1, the

physical and operational link between these two spaces, the fire compartment and the rest of the facility, is the actual compartment boundary components, for example, connecting door assemblies and their leakage characteristics. In the context of a compartment containing a fire, we specifically refer to the leakage of smoke across door assemblies which are potentially exposed on one side to developing and fully developed fire environments and where the smoke is driven by a cross-door pressure differential.

The existence of a cross-door pressure differential is key to the evaluation of a door assembly design! In particular, a variety of possible pressure differential magnitudes and directions (higher or lower pressure in the compartment relative to the adjacent space) can occur in practice. For example, stack effect alone can lead to (relatively) uniform, positive or negative, cross-door pressure differentials of the order of several tens of pascals or higher when compartments of fire involvement are exposed to the outside environment through broken windows [24]. Steady wind velocities of, say, 11 m/s would add or subtract up to 75 Pa [25,26], and this value is proportional to the square of the wind velocity. Over and above these uniform pressures, fire-generated cross-door pressure differentials can vary from top to bottom of a doorway on the order of ± 5 –10 Pa [24]. Depending on which pressure conditions can be anticipated for a particular compartment in a given facility, totally different quantities of smoke could be transferred across door assemblies of identical design.

Pressure Differentials and the Fire-Stopping Characteristics of Door Assemblies

Not enough attention has been paid in the past to the potential impact of cross-door pressure differentials on the actual fire-stopping capabilities of door assemblies. Thus, with unknown consequences, a fire door assembly may be subjected to the just-mentioned type of fire scenario involving positive, cross-door, pressure differentials of several tens of pascals. For example, a door assembly may receive a 1-h fire endurance rating in an ASTM E 152 [6] or ISO 3008 [8] test, where the test conditions involved a relatively modest, cross-door, pressure differential condition of, say, ± 10 Pa. But, when similarly tested under a pressure condition which simulated a realistic, high-overpressure fire scenario, it is possible that the same door assembly would receive a significantly reduced fire endurance rating. Alternatively, consider a facility which includes an automatic smoke control system which is designed to maintain a negative pressure (relative to all adjacent compartments) in any potential compartment of fire origin. Then, depending on the magnitude of the designed cross-door pressure differential, it is reasonable to speculate that a desired 1 h of protection against the spread of fire may be achievable with a door assembly having an ASTM E 152 or ISO 3008 rating (obtained, say,

under modest positive overpressure conditions), significantly less than 1 h. Safety and cost trade-offs implicit in these examples are obvious.

The point of this discussion is that there may be good reason to rate and take into account the fire-stopping capability of door assemblies relative to and as a function of cross-door pressure differential. To obtain such ratings, a revised ASTM E 152 or ISO 3008 with a significant furnace pressure control capability and/or with other means of achieving a cross-door pressure differential control capability (for example, by the method proposed in Ref 24) would be required.

Some Naturally Occurring Pressure Differentials During High-Rise Building Fire Scenarios

In order to place the cross-door pressure differential question into perspective, and as a case in point, it is instructive to consider the use of door assemblies in high-rise buildings or, more directly, in buildings where stack effects are significant. (Even in low facilities, steady winds could lead to the type of pressure loadings to be discussed here.) Assume, for example, that cross-exterior-wall pressure differences during normal winter days in a building of interest are of the order of several tens of pascals at the lower floors (higher pressure outdoors) and at the upper floors (higher pressure indoors). Now consider the following scenario.

Fire breaks out in a compartment on a low floor of the building during the winter. The compartment flashes over, and a ventilation-controlled fire develops where ventilation is from broken windows on exterior walls. Once the windows break, the pressure in the fire compartment is substantially maintained at the (relatively high) outdoor pressure. The fire compartment is connected to adjacent compartments by partitions with relatively small penetrations, for example, (closed) door assemblies. The pressure in these adjacent compartments is substantially maintained at an (relatively low) indoor pressure which is characteristic of the low floor under consideration. The pressure drop across the partitions therefore is maintained at levels which are of the order of magnitude of several tens of pascals, that is, at levels which are of the order of magnitude of indoor-to-outdoor, stack-effect-generated pressure differentials.

Note that in the just-mentioned scenario it is reasonable to anticipate rates of smoke leakage across fire compartment door assemblies which could be significant. (This would depend, of course, on the actual in situ leakage characteristics of the door assemblies involved.) It is also noteworthy that if the fire compartment was on a high floor, it would be maintained at a smaller pressure than adjacent compartments, and smoke leakage to the rest of the building would be substantially eliminated.

Door Assemblies, Compartments of Safe Refuge, and Life Safety

General Considerations

Outside of a fire compartment and by virtue of the existence of closed door assemblies and other barriers to fire and smoke spread, every compartment of a facility through which occupant egress could occur will be maintained at safe conditions at least for some limited time into the fire. However, in order to achieve facility designs which are compatible with life safety, it is desirable to designate and design certain specific spaces as compartments of safe refuge. The environment in such compartments would be maintained under tenable conditions for significant (although, not necessarily indefinite), specified time intervals.

Three types of information are required to design compartments of safe refuge with confidence. First is some knowledge of the leakage characteristics of candidate door assemblies. Second are estimates of the likely smoke conditions which will occur in those smoke compartments adjacent to a smoke compartment of safe refuge. As suggested by the discussion in the previous section, a third and final type of input is the anticipated level of pressure differential between potential smoke-laden adjacent smoke compartments and the protected compartment itself.

Pressurized stairwells are examples of smoke compartments of safe refuge. The discussion of these kinds of compartments that follows illustrates the type of inputs required for their successful design. This discussion highlights the general types of door assembly leakage measurements that a facility designer might require. For specific design information about pressurized stairwells and other smoke control systems the reader is referred to Ref 26.

The Pressurized Stairwell—An Example of a Compartment of Safe Refuge

It would clearly enhance the safety of a facility if stairwells were designed as smoke compartments of safe refuge for time intervals required to evacuate all occupants to other, more secure compartments or to the outside. Toward this end, the pressurized stairwell design concept has been developed [1,3]. By introducing fresh air into a stairwell with the use of appropriate blower hardware and control, it is possible to maintain the stairwell at a practical positive pressure relative to all adjacent compartments during *many* realistic fire scenarios and related stairwell door usage. Provided the stairwell door to the floor of fire involvement is closed, such stairwell designs would lead to a positive flow of fresh air across door assemblies from the stairwell compartment to all adjacent spaces (including those on the fire floor). Thus, the fully successful pressurized stairwell will “never” allow leakage of smoke (or air) into the stairwell compartment from adjacent building spaces. In particular, all

smoke infiltration into the stairwell is eliminated so long as the doors and other partition construction elements of the stairwell maintain their integrity.

The design of the stairwell pressurization system clearly requires knowledge of the leakage characteristics of entry/exit door assemblies. Except for the fire floor, we are talking about leakage of unheated air across a (unheated) door assembly under design conditions of cross-door pressure difference. Leakage characteristics of this type could be acquired with ASTM E 283 [27] or with the proposed ISO 5925 Part 1 [28] test method. For the fire floor (assuming no protected lobby on the other side of the door) one also requires knowledge of the leakage characteristics of the door assembly as it is exposed to fully developed fire conditions. Here we are talking about leakage rates that would be measured, for example, in a door assembly fire endurance test (such as ASTM E 152 or ISO 3008, appropriately revised to allow for leakage measurements) where the test laboratory furnace was maintained at such a level of negative pressure relative to the pressure at the unexposed side of the door as to simulate the stairwell-to-fire compartment pressure differential.

There is at least one class of realistic fire scenario and stairwell door usage where the latter pressurized stairwell design considerations may not be adequate (unless the most up-to-date pressurization design technology is used) [26]. Reference is made to the use of doors during high-rise building evacuation at times of cold winter conditions. Under such circumstances, it may not be practical to maintain a positive pressure difference between the stairwell and the outside at the elevation of the lower floors of the building (see, for example, the building measurements reported in Fig. 10 of Ref 29). Thus, with a fire in a lower floor which is vented directly to the outside through broken windows (as in the fire scenario referred to in an earlier section), the smoke compartment (floor) of fire origin will be at a positive pressure relative to the stairwell. Under this circumstance there will be unavoidable continuous smoke leakage into the stairwell. But it is clear that the stairwell pressurization system could accept some limited smoke intake without leading to any serious degradation in the stairwell environment during the required time interval for safe refuge. Therefore, one design task would be to choose a candidate door assembly whose leakage characteristics were such that this established design limit would not be exceeded. Such leakage characteristics could only be established by carrying out a test (such as the one recommended in Ref 24, depicted here in Fig. 2 and as yet to be developed) which measured the leakage rate of a door assembly while undergoing a fire endurance test exposure with positive furnace-to-unexposed-side pressure differential.

General Prediction and Control of the Development of Threatening Conditions Throughout a Facility

Door assembly leakage characteristics are required to describe a facility's fire-generated environment, in general, and to design smoke compartments

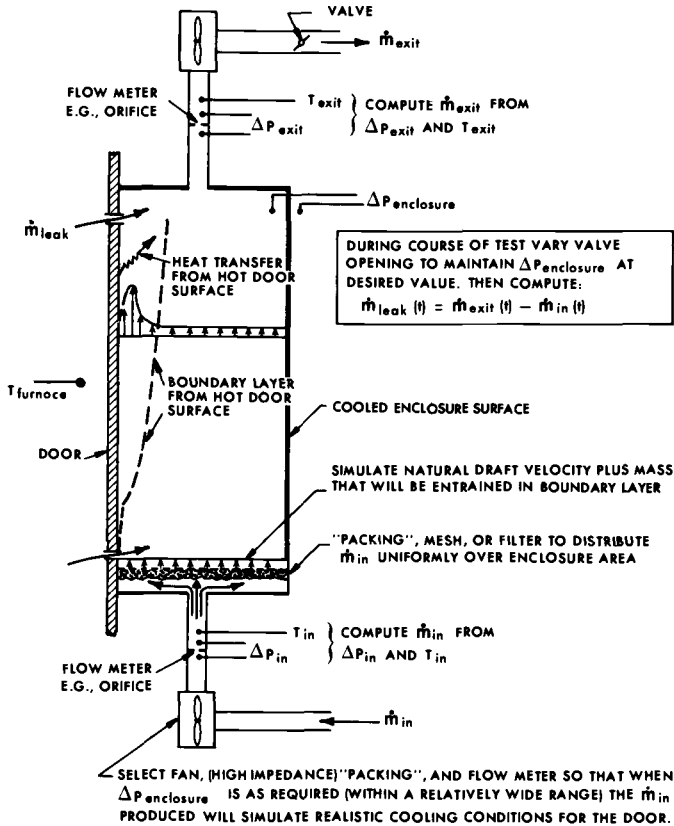


FIG. 2—A test concept for measuring leakage of door assemblies. During course of test, valve opening is varied to maintain $\Delta P_{enclosure}$ at the desired value (from Ref 24).

of safe refuge, in particular. As suggested in earlier discussion, by implementing an appropriately conservative design concept like the "perfectly" pressurized compartment of safe refuge (that is, no smoke entry under design conditions), it is often possible to simplify the latter design task and to do this in a manner which requires only the most approximate door leakage characteristics. Indeed, it is with the use of "perfect" pressurization that the safe egress path problem is now being solved [26]. However, if facility designs are to be responsive to more flexible fire safety performance criteria, it is evident that approaches for solving the inherently coupled problems of compartment-to-compartment smoke spread are required. Such approaches would be based on tools for predicting or mathematically simulating the fire-generated smoke environments which develop throughout a facility subsequent to the ignition of a potentially threatening fire. These tools would include mathematical models for predicting fire growth phenomena in multiroom spaces of

a compartment of fire involvement and the migration of smoke to all other compartments of the facility (for example, to compartments of designed safe refuge), whether as a result of forced ventilation, stack effect, wind-generated pressure differences, smoke buoyancy, or a combination of same.

Key to overcoming the challenges in developing fire environment simulation models are the compromises which must be made between accuracy and detail in simulation of physical phenomena on the one hand and practicality of implementation on the other (that is, to be successful, the analytic tools to be developed will have to be both reliable and "user friendly"). Once developed, dynamic fire environment prediction models will be used together with models which predict the response of people and property to such environments. It then will be possible to systematically establish the life or property safety performance of a facility on a fully integrated, economical, and rational basis [11].

Required New and/or Revised Door Assembly Test Methods

Required Measurements

Based on the entire previous discussion it follows that test methods are required to evaluate the following performance characteristics of door assemblies used in the partitions of smoke compartments:

1. Rate of leakage under ambient temperature conditions, as a function of cross-door pressure differential.
2. Fire endurance time (according to criteria similar to those set forth in ASTM E 152) of an interior door assembly under conditions where one side is exposed to a fully developed (flashed-over) fire simulation, the other side is exposed to an ambient environment, and where the assembly is subjected to a cross-door pressure differential between, say, ± 100 Pa.
3. Rate of leakage as a function of time (up to the assembly's fire endurance time, as established in Measurement 2, just mentioned) under conditions where one side is exposed to a fully developed (flashed-over) fire simulation, the other side is exposed to an ambient environment, and where the assembly is subjected to cross-door pressure differential between, say, $+100$ Pa and values that are (slightly) negative enough as to lead to zero leakage conditions.

It has been suggested [23,30] that another test method is required to evaluate leakage characteristics under medium temperature (100 to 250°C) exposure conditions. Such a test method would be used to estimate the leakage characteristics of door assemblies which were not (yet) exposed to flashover conditions, but which, by reason of proximity to a smoke compartment of fire involvement, were exposed to significantly elevated temperature environments (as might be eventually anticipated in an "intermediate" compartment between the fire compartment and a compartment of safe refuge).

A test method developed to acquire and report any of the just-mentioned measurements on a particular door assembly likely would include a short-hand means of summarizing the test results. Such a summary would take the form of a rating or classification system (as, for example, suggested in Ref 23) which could be used as a convenient means of reference between facility design guides, standards, codes, etc.

Leakage Under Ambient Conditions

The ambient leakage measurements under Item 1 could be acquired by implementing ISO 5925, Part 1 (Fig. 3) or ASTM E 283 (Fig. 4).

The measurements obtained by one of these test methods would provide door assembly leakage characteristics required in the analysis of pressurized stairwell designs in the early time analysis of smoke leakage from fire compartments to the rest of a facility and, generally, in analyses of total intrafacility smoke migration.

Besides actual laboratory evaluations of ambient temperature leakage, a method of carrying out in situ testing of door assemblies also would be of interest. The results of such tests on in situ elevator and stair shaft doors are reported, for example, in Ref 31, and ASTM E 783 [32] is an existing field test method for exterior doors.

For an in-depth discussion on measurements, calculations, and specifica-

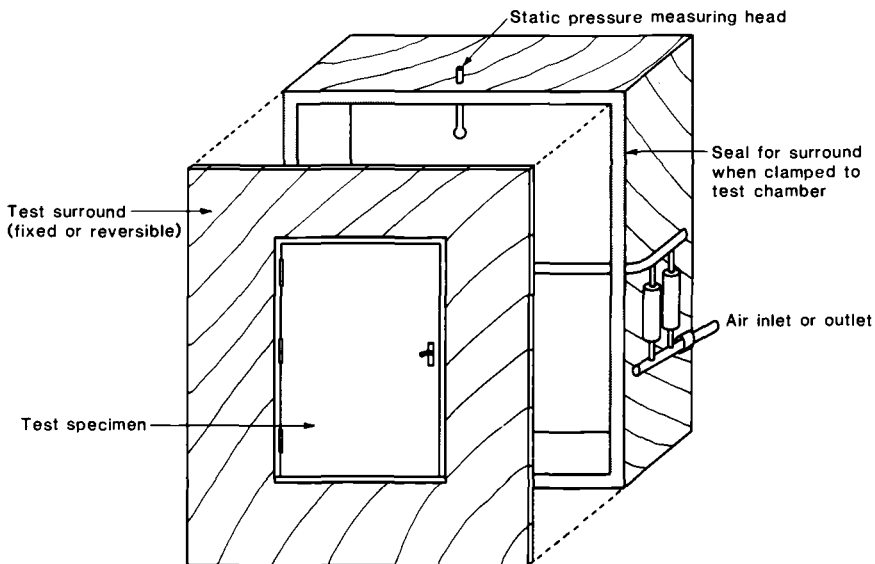


FIG. 3—Example of an air leakage test chamber that might be used to carry out ISO DIS 5925/1 (from Ref 28).

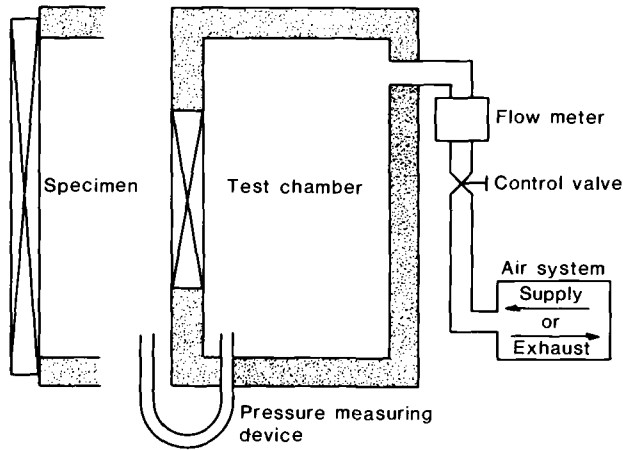


FIG. 4—General arrangement of the air leakage apparatus to be used to carry out ASTM E 283-73 (from Ref 27).

tions of door assembly leakage under ambient conditions, the reader is referred to Ref 25.

Testing for Fire Endurance

As discussed in an earlier section, the fire endurance of a door assembly may be very sensitive to the magnitude and direction of cross-door pressure differentials. To obtain fire endurance ratings under practical, nonzero, cross-door pressure differentials, a significantly revised ASTM E 152 or similar type furnace-driven test method would be required. The major aspect of the revision would be in the operation of the furnace at pressures significantly higher or lower (say, between ± 100 Pa) than the pressure on the unexposed side of a subject door assembly. Short of actually operating the furnace at such positive or negative pressures relative to the test laboratory (that is, relative to the ambient), one possible alternate means of achieving such cross-door pressure differentials would be by use of a door enclosure box (as in the tentative ISO 5925, Part 3 test method [33], depicted in Fig. 5). The door box would be attached to the wall in which the door assembly was mounted, and it would enclose the entire subject door assembly on its unexposed side. By appropriate ventilation and surface cooling of the box, as recommended in Ref 24 (Fig. 2), the unexposed door surface would be continuously subjected to a simulated ambient environment. Further, the box would be operated at such a negative or positive pressure relative to the (ambient pressure) furnace as to achieve the desired cross-door pressure differential.

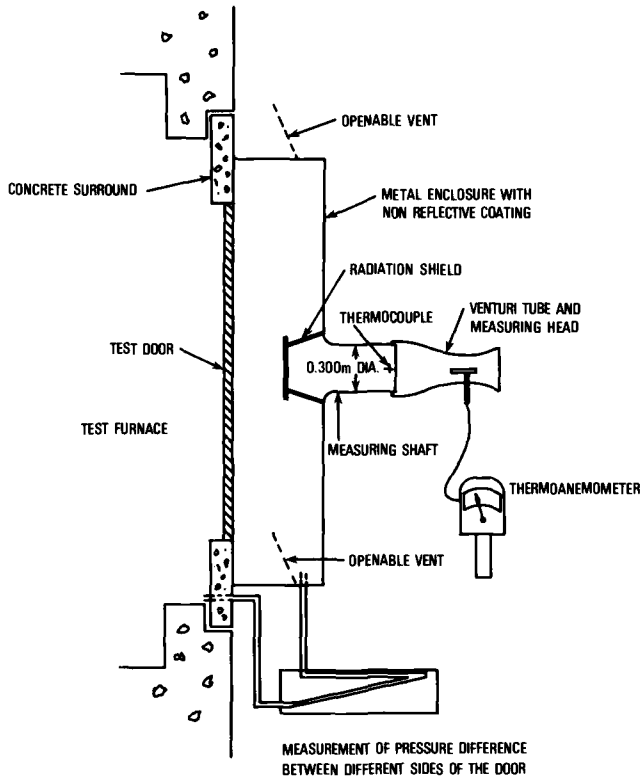


FIG. 5—The test equipment arrangement for measuring leakage by the proposed ISO DP 5925, Pt. 3 test method (redrawn from Ref 33).

Leakage Under Fully Developed Fire Conditions

During the last decade much attention has been focused on the development of test methods to measure the leakage of door assemblies under fully developed fire exposures. The first of these test methods, which was limited to near zero cross-door pressure differentials, was developed in Finland [34-36] in the early 1970s and proposed to a working group of ISO. The working group documented the test method [33] with the anticipation that, if acceptable, it would eventually be issued by ISO as the third of a three-part series of tests for measuring the leakage of door assemblies under ambient temperature [27,36] (DP 5925, Part 1), medium temperature [37] (DP 5925, Part 2), and high temperature [33] (DP 5925, Part 3). Also planned for this series is an introductory or commentary document [23,30] (DP 5925, Part 0). An annotated sketch of the high temperature, DP 5925, Part 3 test method is presented in Fig. 5.

An in-depth experimental [37,38] and theoretical analysis of the DP 5925, Part 3 test method was carried out in the United States. A report of this study [24] indicated that, for a variety of reasons, the test method is generally unreliable. The report went on to recommend an alternate test concept which would hopefully remove the problems as well as the significant limitation of a near-zero, cross-door pressure differential. The proposed test concept, which is depicted in Fig. 2 (and has yet to be implemented), involves the use of a ventilated enclosure box referred to earlier.

Independent of the latter analysis, experimental studies on an improved DP 5925, Part 3 test method were carried out in the Netherlands [39-42]. The hardware of this revised test method is illustrated in Fig. 6. As can be seen, it also makes use of a ventilated enclosure box. While this test method finally may prove to be reliable, it does not address the issue of elevated, cross-door pressure differentials.

The just-mentioned test methods all make use of structures which enclose the subject door assembly and which collect and measure the total rate of leakage of furnace gases. Another method for measuring the rate of leakage under fully developed fire exposures, which does not involve the use of a cum-

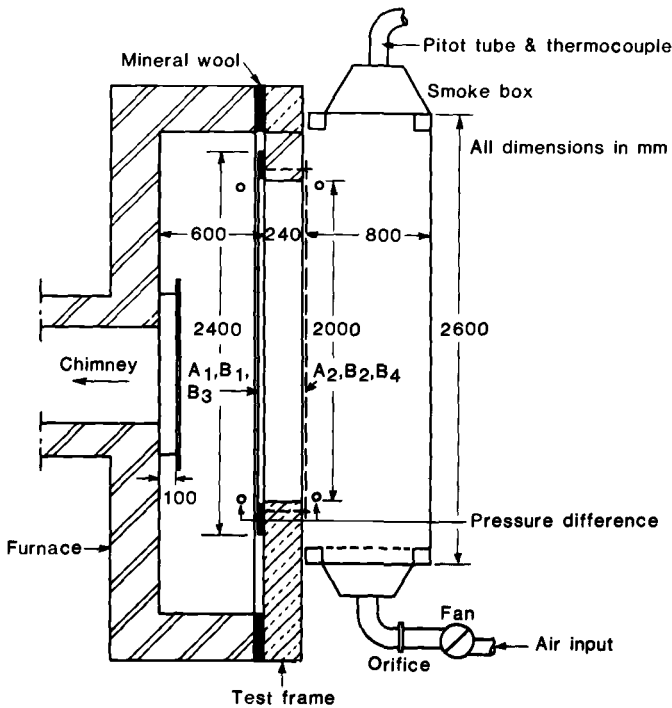


FIG. 6—Test equipment arrangement for a proposed high temperature leakage test (from Ref 42).

bersome enclosure box, is now under development in Finland [43,44]. This method, depicted in Fig. 7, makes use of carbon dioxide (CO_2) as a tracer gas of known (measured) concentration within the furnace. The tracer gas, uniformly mixed with other furnace gases, leaks past the door assembly. On the unexposed side of the door, all leaked gases rise due to buoyancy, and they are collected in an open, ventilated canopy (reminiscent of the canopy smoke collection used in Ref 45). The rate of tracer gas flow is measured and related to the total leakage rate of the door assembly.

Two criticisms of the latter test method have been raised and partially refuted [44]. The first has to do with the strong variation of CO_2 , tracer gas concentration within the furnace, and the second has to do with the fact that unknown amounts of CO_2 may be introduced at burning surfaces near leakage gaps of the door assembly, thereby leading to spurious estimates of total leakage rate.

Short of pressurizing the furnace relative to the laboratory ambient or using the ventilated enclosure box idea, it does not appear that the just-mentioned tracer gas test method will provide leakage rates of door assemblies under significant cross-door pressure differentials.

Combined Tests

Under significant cross-door pressure differentials, it is likely that successful methods to measure fire endurance and leakage under fully developed fire exposures will require substantial research and development activity. Furthermore, to implement such test methods and obtain ratings for a particular door assembly design will clearly require resource commitments which will be

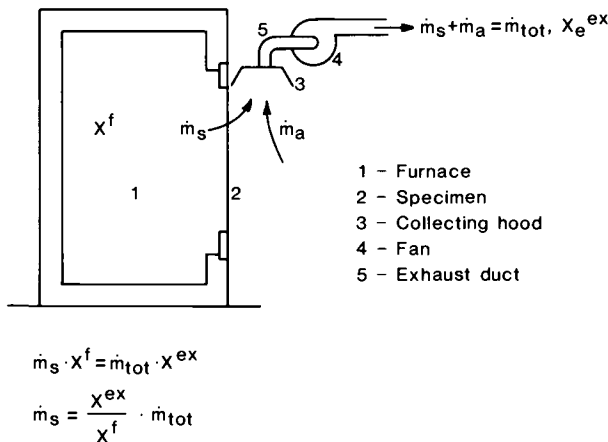


FIG. 7—The test equipment arrangement for measuring leakage by the CO_2 tracer gas method (from Ref 44).

even greater than those presently required under ASTM E 152. (For example, to obtain a leakage classification under two different cross-door pressure differentials would likely require destructive tests on each of two separate door assemblies.) For this reason, it would be a particularly attractive feature of the test methods if both types of evaluations, fire endurance and leakage, could be carried out during the course of a single test procedure. Thus, for example, if the leakage test concept of Ref 24 (Fig. 2) proved to be practical, then (except for the possible difficulty of carrying out a hose stream test) there is no particular reason why fire endurance and leakage rate could not be established simultaneously.

Leakage Under Medium Temperature Conditions

Leakage of door assemblies under both ambient and medium temperature (up to 200°C) conditions and with cross-door pressure differentials of up to ± 100 Pa have been successfully carried out in Germany with a single test apparatus [37]. The apparatus, which is depicted in Fig. 8, uses a 60-kW electric heating device. The elevated temperature tests were initiated at ambi-

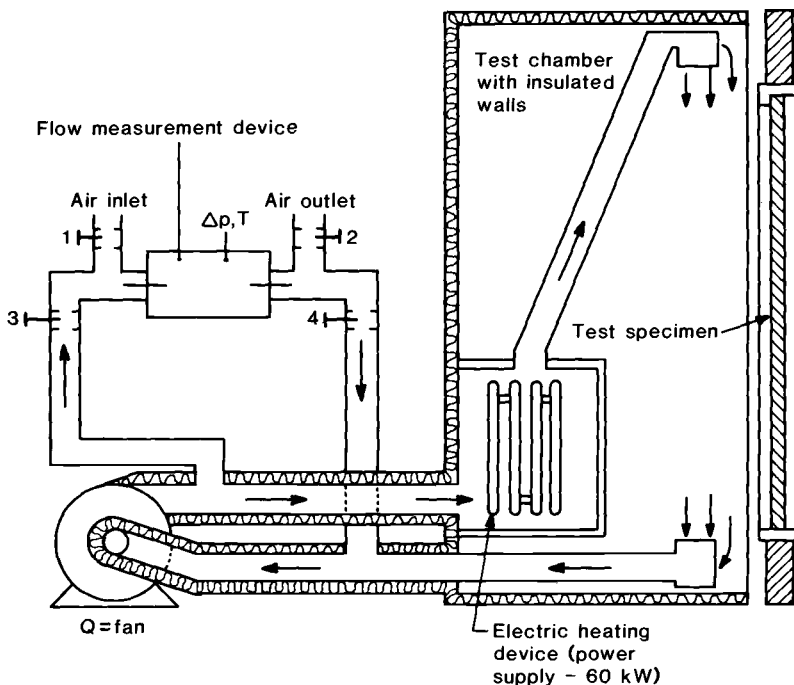


FIG. 8—Example of an air leakage test chamber designed to test door assemblies at ambient and at medium air temperature (from Ref 37).

ent conditions and required from 25 to 30 min to reach maximum temperature.

Summary and Concluding Remarks

Key to the protection of life and property during fires is the control of smoke migration. Such control can be designed into a facility to the degree that the structural integrity and leakage characteristics of compartment partitions are known during real fire conditions. One parameter of real fire conditions which will have a great impact on these characteristics is the cross-partition pressure differential. In practical fire scenarios such pressure differentials can be sustained by steady wind loadings, stack effect, and/or ventilation systems at amplitudes which easily could be of the order of several tens of pascals.

The measurement of the structural integrity and leakage characteristics of compartment partition elements requires test methods which are at various stages of development. Required measurement capabilities for door assemblies, in particular, include leakage rate under ambient temperature conditions (in situ as well as laboratory testing), under fully developed fire conditions, and, possibly, under intermediate temperature exposures for fixed cross-door pressure differentials within some range to be determined. Also required is a capability to evaluate door assembly fire endurance under a similar range of fixed, cross-door pressure loadings. At a given pressure loading there is reason to expect that with a carefully designed test method the latter fire endurance evaluation could be successfully carried out together with leakage measurements during a single test procedure.

A method of reporting summary results of test evaluations, basically a rating or classification system, has to be developed. For example, summary results presented in a shorthand form might include: cross-door pressure differential, test exposure (for example, ambient or fully developed fire temperatures), and characteristic rates of leakage (in the case of elevated temperature exposure, for example, maximum leakage during the first 30 min, second 30 min, etc., up to the fire endurance rating of the assembly). Such a rating or classification system would be used in performance or specification guides, standards, and codes for selecting door assemblies to be used in different applications within different types of facilities. In the future, such use documents will have to be prepared by appropriate organizations.

Institutions such as ASTM, ISO, and NFPA clearly have a role to play in establishing, improving, promulgating, and encouraging the development of the just-mentioned test methods, reporting procedures, and use documents.

Acknowledgments

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An Evaluation of Toxic Hazards from Full-Scale Furnished Room Fire Studies

REFERENCE: Grand, A. F., Kaplan, H. L., Beitel, J. J. III, Switzer, W. G., and Hartzell, G. E., "An Evaluation of Toxic Hazards from Full-Scale Furnished Room Fire Studies," *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 330-353.

ABSTRACT: Fully furnished and finished 3.7 by 5.5-m (12 by 18-ft) rooms, designed to simulate a typical hotel/motel arrangement, were ignited in identical scenarios and allowed to proceed past flashover. The test facility consisted of the burn room attached perpendicular to the end of a 13.7-m (45-ft) corridor with another room off the corridor 4.9 m (16 ft) from the test room. The second room contained animals for an assessment of both preflashover and flashover toxic effects. The rooms and corridor were fully instrumented with thermocouples, heat flux radiometers, smoke meters, gas sampling trains, and, in the burn room only, smoke detectors and sprinkler heads. In the few minutes prior to visual flashover, the toxic hazard of this fire scenario increased dramatically. At the 1.7-m (5 1/2-ft) level in the burn room, temperature rose to 650°C (1200°F), carbon monoxide (CO) concentration reached 70 000 ppm, oxygen concentration dropped to zero, and the hydrogen cyanide level exceeded 1000 ppm. In the remote room, the animals died from carbon monoxide poisoning around 12 min after the dramatic change in conditions (approximately 10 min after visual flashover occurred). Other factors besides CO contributed to the incapacitation of the animals.

KEY WORDS: fire hazards, fire tests, toxicity, smoke toxicity

Laboratory-scale test procedures using animals generally are employed to assess the toxicity of smoke from materials [1]. Very few full-scale fire evaluations in which animals have been exposed to the products of combustion have been reported in the literature [2,3].

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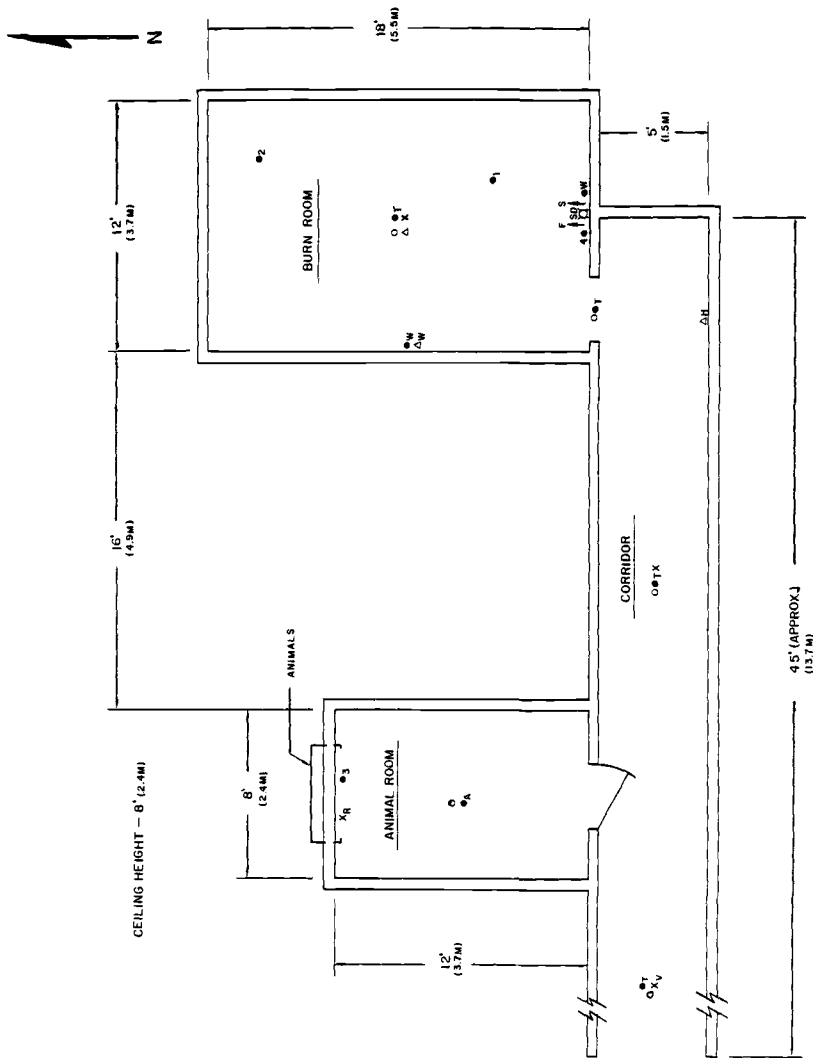
It is not surprising to those in fire research that the fire performance of a material in a full-scale scenario may be different from that in a laboratory test. By the same reasoning, the smoke produced from a full-scale fire may not be the same as that produced in the laboratory, resulting in a difference in toxicity. There are many reasons for a lack of correlation between laboratory and full-scale toxicity assessments. Firstly, the laboratory procedures use small samples of individual materials, often not in a final or processed form (however, composites can be tested by some methods). In the full-scale tests, there are many different objects, comprising multiple materials and various physical configurations. Secondly, the combustion conditions in the laboratory tests are reasonably controlled, whereas the conditions in the full-scale scenario may be constantly changing. Finally, the sample surface area (or mass) to volume ratios and how the smoke is transported to the animals may be very different for laboratory versus full-scale experiments. Although a comparison of laboratory- to full-scale combustion toxicity procedures is beyond the scope of this paper, knowledgeable readers may be surprised at the results of this study compared to those usually obtained from laboratory experiments.

The objective of this report is to present a test procedure performed at Southwest Research Institute as an example of a particular full-scale fire scenario, including a methodology for exposing laboratory animals (rats) to the smoke from such a fire. The purpose of the overall study was to assess the toxic hazard produced in a full-scale fire environment. In these evaluations, simulated fully furnished hotel/motel rooms, attached to a long corridor, were used. Toxicological assessments were made using animals, exposed in a remote room, along with combustion gas analyses and temperature measurements. Temperature, gas analyses, heat flux, and light-obscuration data also were collected at various other locations in the burn room and corridor. Due to the enormous quantity of data collected in this study, only summaries of the data collected are given in this report. A more comprehensive survey of the data will be presented in subsequent publications.

Experimental Procedures

Test Facility

Test rooms were constructed as shown in Fig. 1. The overall test configuration consisted of a 3.7 by 5.5-m (12 by 18-ft) room perpendicular to the end of a 13.7-m (45-ft) corridor, with another room off the corridor 4.9 m (16 ft) from the test room. The rooms and corridor were 2.4 m (8 ft) high. The door opening between the burn room and the corridor was 0.9 m (3 ft) wide by 2.0 m (6 1/2 ft) high. The door between the animal room and the corridor was 0.9 m (3 ft) wide and extended to the ceiling. The door was arranged such that it could be opened or closed during the course of the test.



- T = Thermocouple tree—6-in. level; 1, 3, 5, 5.5, 6, 7, and 7.5-ft levels; and ceiling level
- W = Thermocouples on walls—3 and 6-ft levels
- A = Thermocouple tree—3, 5.5, and 7-ft levels
- 2 = Thermocouples on furniture—1-bed, 2-couch
- 3 = Thermocouples at animal location
- 4 = Thermocouple at sprinkler location
- Δ = Radiometer at floor and ceiling
- Δ_w = Radiometer in wall at 3 and 6-ft level
- Δ_H = Radiometer in wall at 4-ft level
- o = Vertical smoke cells/lights
- x = Gas analysis (CO, CO₂, O₂, NO_x, CH₄, HCN, HCl)—5.5 and 3-ft levels
- x_R = Gas analysis (CO, CO₂, O₂, NO_x, CH₄, HCN, HCl)—animal location
- x_V = Gas analysis (CO, CO₂, O₂, NO_x, CH₄, HCN, HCl)—5.5-ft level
- † = Sprinkler: S = standard; F = fast response; wall-mounted
- = Smoke detector, ionization, wall-mounted

FIG. 1—Full-scale test facility.

All interior walls and ceiling areas were constructed of 16-mm ($5/8$ -in.) Type X gypsum wallboard. The burn room contained a double thickness of wallboard for extra durability and ease of replacement. Three walls in the burn room (the two long walls and the wall with the door opening) were constructed of this wallboard with an interior facing of 0.25-mm (0.010-in.) vinyl wallcovering. All wallboard seams and instrumentation penetrations were sealed with a latex caulking compound. Ventilation for the test fires was provided only by the open end of the corridor, the dimensions of which were 1.5 m (5 ft) wide by 2.4 m (8 ft) high.

Gas Sampling and Analysis

Intermittent analyses at six different sampling sites were performed using continuous analyzers for carbon monoxide (CO), carbon dioxide (CO₂), oxygen (O₂), hydrocarbons (CH_x), and nitrogen oxides (NO_x). The sampling and analysis system for these gases is shown schematically in Fig. 2.

Heated sample lines [each 30 m (100 ft) long] were routed to six locations in the test facility. The gas sampling lines inside the rooms consisted of 6.35-mm (1/4-in.) stainless steel tubing connected to the heated lines above the ceiling. The lines were connected to a multivalve selector manifold with solenoid actuated two-way valves (see Fig. 2). Power was applied to one valve at a time by the stream selector, permitting flow through the sample pump, which then supplied a manifold line to the five gas analyzers. The selector valves and lines

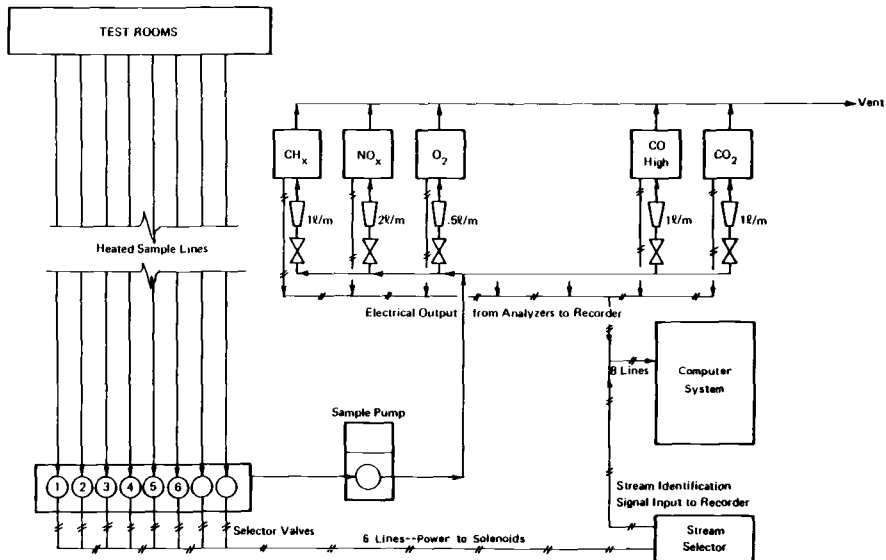


FIG. 2—Gas sampling and analysis system.

through the pump to the analyzers were heated, but at a lower temperature than the transfer lines so that the sample stream would be cooled to approximately 70°C before entering the analyzer cells. In-line filters with 40 to 60- μm pores were installed in the sample inlet line to each analyzer to minimize contamination of the sample cell by entrained solid particles. The holding time for each sampling line was 20 s. This was sufficient time for a new gas stream sample to reach the analyzer and for the analyzer to respond to any change from the previous gas composition. Since there were six gas streams, 2 min elapsed between readings at any given location.

Gas analyzers used are listed in Table 1 with the manufacturer, model number, type of detector, and range of each instrument. Nitrogen was used as a zero gas on all analyzers except NO_x , which used oxygen. Each analyzer was also connected through a manual selector valve to its own cylinder of calibration gas (for example, 10% CO_2 in nitrogen) for setting the upscale gain of the analyzer. An independent CO analyzer was used to monitor the remote room where the animals were placed for exposure. This analyzer provided continuous data on CO concentration in the animal room.

Hydrogen cyanide (HCN) and hydrogen chloride (HCl) were sampled with dry soda lime absorption tubes for subsequent wet-chemical analyses. Sampling was intermittent for fixed time intervals over the duration of each test at each of six locations (shown in Fig. 1). At each sampling location, eight tubes were grouped together in the arrangement illustrated in Fig. 3. At locations not subjected to excessive heat (that is, in the animal room, at the lower level in the corridor, and in the corridor vent), the tubes were located inside the test facility. At the other locations, the absorption tubes were located outside the test environment and connected to stainless steel tubes situated at the proper level inside the facility.

The rate of gas flow through the tubes was regulated and monitored at a central location, as near as possible to the sampling sites. A nitrogen back-purge system (shown in Fig. 3) provided a small backflow of nitrogen to prevent absorption of contaminant gases at all times except when that particular tube was being used for sampling. Samples were taken every 2 min (for 14 min) after ignition of the chair in these tests. In the animal room, additional samples were taken until the last animal was withdrawn from the room. Analytical procedures for HCN and HCl are given in the Appendix.

Heat and Smoke Measurements

Temperature measurements were made at 45 locations in the burn facility. Chromel-alumel thermocouples (20 gage) were placed as shown in Fig. 1. Thermocouple signals were recorded using a Kaye instruments scanner and Wang MVP computer. Prior to each test the thermocouples were examined and tested with heat to ensure proper operation and identification of their positions.

TABLE 1—*Gas analysis instruments.*^a

Gas	Manufacturer	Model	Type	Range	Response	Sensitivity	Accuracy
CO ₂	Beckman	864	NDIR ^b	0 to 10%	90% within 0.5 s	0.5% F.S.	±1%
CO	Beckman	865	NDIR ^b	0 to 10%	90% within 0.5 s	0.5% F.S.	±1%
NO _x	Beckman	951	chemiluminescence	0 to 1000 ppm	90% within 0.5 s	0.1 ppm on 10-ppm range	0.5% F.S. ^c
O ₂	Beckman	OM-11	polarographic	0 to 25%	90% within 1 s	...	±0.7% F.S.
CH ₄	Beckman	400	flame ionization	0 to 10000 ppm	90% within 0.5 s	...	±1% F.S.

^aData from manufacturers' instruction manuals.^bNDIR = nondispersive infrared.^cF.S. = full scale.

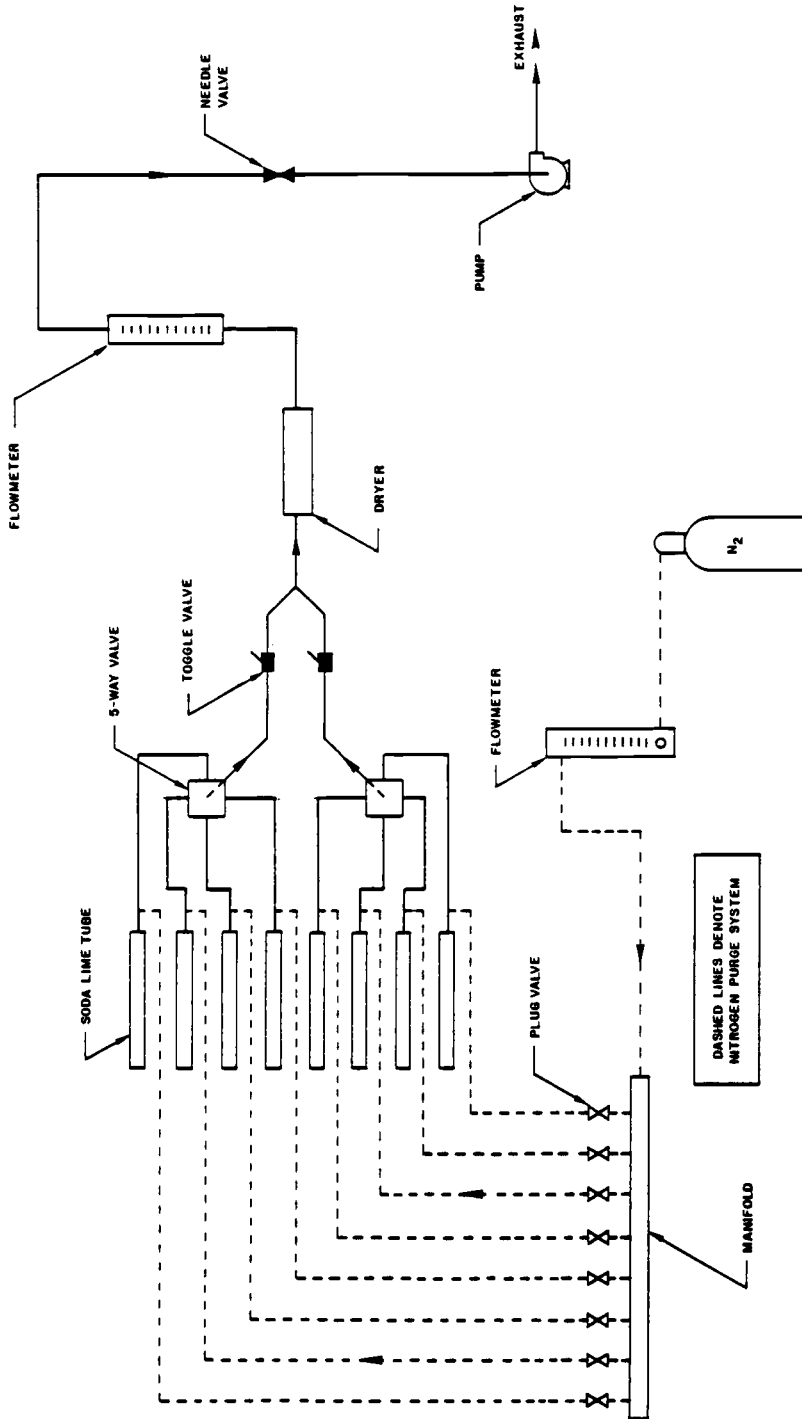


FIG. 3—Schematic drawing of soda lime absorption tubes for sampling of HCN and HCl. Shown is the arrangement for one group of eight tubes (that is, one location).

The reduction of light transmission resulting from smoke development was measured vertically across the upper 1 m of the rooms or corridor at four locations in the test facility (see Fig. 1). In each case, a light source on the ceiling was projected onto a 38-mm (1 1/2-in.) diameter Weston Instruments Model 856, Type I photocell. Light rays were focused on a collimating tube mounted above the photocell to minimize the effect of stray light. The photocell output was adjusted to 100% transmittance prior to each test. Calibration for linearity in photocell millivolt output versus optical density was accomplished through the use of Aminco optical density filters.

Total heat flux was measured at five locations in the test facility (also shown in Fig. 1). The heat flux gages were water-cooled Medtherm Gardon-type gages rated at 0 to 10 W/cm².

Miscellaneous Instrumentation

In the burn room, a wall-mounted ionization-type smoke detector was employed during all tests. It was mounted 15 cm (6 in.) below the ceiling in the center of the wall containing the doorway. Two sprinkler heads without water supply [one "standard" 71°C (160°F) head and one "fast response" 63°C (145°F) head] also were installed in each test at the same location as the smoke detector. The purpose of the sprinkler heads was to indicate when an actual sprinkler would have activated during the fire sequence. In one test, additional sets of sprinkler heads and smoke detectors were placed on the ceiling in the center of the burn room.

Tempered glass viewing ports were located in the wall of the burn room opposite the ignition source and in the corridor opposite the doorway to the burn room. The viewing port in the corridor was less than 0.9 m (3 ft) from the floor to permit continuous observation of the test from below the smoke level. Color photographs were taken of the experimental arrangement (Fig. 5a, shown later in this paper) and at intervals during each test (for example, Fig. 5b, shown later in this paper). A color video tape was made of each test to provide a record of the burning characteristics of the test materials.

Description of Test Materials

The furnishings and finish materials used are listed in Table 2. They were selected to be as representative as possible of a "typical" hotel/motel room. The accent chairs (Items 4 and 6) were Upholstered Furniture Action Council (UFAC) labeled and were qualified under the California 117-75 flammability test. The sofa (Item 7) was not UFAC labeled. The carpet passed the "pill" test [U.S. Department of Commerce (DOC-FF-1-70) or ASTM Test for Flammability of Finished Textile Floor Covering Materials (D 2859-76)]. The critical radiant flux (CRF) value [ASTM Test for Critical Radiant Flux of Floor Covering Systems Using a Radiant Heat Energy Source (E 648-78)] of the

carpet, when installed on an underlayment, was less than 0.1 (when the carpet was tested alone, without underlayment, the CRF was greater than 1.1). All test materials were placed in a conditioning room maintained at 21°C and 50% relative humidity for 48 h prior to testing.

The furniture was arranged in the test room as shown in Fig. 4. Ignition of the chair (Item 6) with subsequent flame spread to the sofa (Item 7) was considered to be a realistic scenario and one which allowed a reasonable spread in the time sequence of events of the fire. Preliminary tests had demonstrated that direct ignition of either the couch or bed led to flashover more quickly than desired. It also was found that the sofa would not ignite reproducibly unless the chair was touching it. Ignition of the chair was accomplished by imbedding an electrically operated "glocoil" in the foam upholstery. A period of smoldering preceeded ignition.

Exposure of Animals

Animals were exposed in the remote room to the combustion products of the fully furnished room fire in the same manner as they are often exposed in laboratory-scale experiments. The rats were placed in tubular restrainers (head-only mode) through one wall of the remote animal room. The location of the animals is shown in Fig. 1 and a photograph of that part of the facility is presented as Fig. 5c (shown later in this paper). The first group of animals, inserted into the room at the beginning of the test, was instrumented for measurement of incapacitation by the leg-flexion shock avoidance response [4]. A second group of animals was inserted when the door to the animal room was closed. The purpose of the second group of animals was to permit an assessment of the toxicity at a constant concentration of fire gases, rather than under the constantly changing atmosphere that existed prior to shutting the door. These animals were not instrumented for leg flexion.

A total of seven animals was used for the first group. Five were used for observation of incapacitation and lethality, while the other two were used to obtain blood samples upon incapacitation of the third (median) animal. Five animals constituted the second group for lethality measurements. Procedures used for determination of toxic signs and blood carboxyhemoglobin (COHb) analysis are given in Ref 5.

In these tests, the door to the remote animal room was open 25 mm (1 in.) at the start of the test and then closed 5 min after "flashover" as described in following paragraphs.

Results and Discussion

General Discussion

Four tests were conducted in this test series; however, only two (Tests 3 and 4) were exact replicates of the furniture setup described. Therefore, only these

TABLE 2—Description of furnishings for full-scale tests.^a

Item	Description	Material	Dimensions
No. 1 dresser	6 drawer, woodgrain laminate—top, front, and sides	particle board	64 in. wide by 17 1/2 in. deep by 31 1/2 in. high, 137 lb
No. 2 desk	woodgrain laminate—top, front, and sides	particle board	37 in. wide by 17 1/2 in. deep by 31 1/2 in. high, 81 lb, 2 oz
No. 3 desk chair	desk chair	wood—pine	19 1/4 in. wide by 16 5/8 in. deep by 34 3/4 in. high, 11 lb, 8 oz
No. 4 chair	wood frame chair with upholstered seat and back	95% polyurethane, 5% blended cotton batting; wood; fabric	25 3/16 in. wide by 22 in. deep by 30 1/2 in. high, 17 lb, 8 oz
No. 5 table	table—woodgrain laminate top, steel legs	100% acrylic particle board, steel legs	35 5/8 in. diameter by 28 in. high, 43 lb, 5 oz
No. 6 chair	See Item No. 4 beige urethane foam-cushioned sofa	wood frame; urethane foam, foam cushions; steel spring unit; fabric	58 in. wide by 34 in. deep by 24 in. high, 80 lb
No. 7 sofa		60% polyester, 40% nylon particle board	24 in. wide by 15 in. deep by 21 7/8 in. high, 41 lb
No. 8 nightstand	woodgrain laminate night stand	resin-treated, 80% shredded clipping pad, 20% polyether foam, steel spring unit	60 in. by 80 in. (queen-sized), 67 lb, 10 oz
No. 9 box springs	queen-sized box springs		

No. 9 mattress	queen-sized bed mattress	filling—100% Polyester; Top—50% polyester, 50% cotton; back—100% polypropylene; steel spring unit	60 in. by 80 in. (queen-sized), 46 lb
Bed base	bed frame	steel	adjustable—queen sized
Headboard	wooden bed headboard	wood	62 in. wide by 48 1/2 in. high, 44 lb, 8 oz
Bedding	mattress pad, rainbow printed sheet set with pillow cases	80% kodel polyester, 20% cotton	queen-sized, 1 lb, 14 oz
Pillows	standard pillows (2)	cotton cover, polyester fiber-filled	queen-sized, 3 lb, 7 oz
Blanket	queen-sized blanket	100% virgin acrylic nylon bound	22 oz (each)
Mirror	wall mirror (over dresser)	wood-framed mirror	102 in. by 90 in. (queen-sized), 3 lb, 5 oz
Pictures	decorative framed pictures (2)	wood frame, paper	46 3/8 in. high by 31 in. wide, 28 lb, 8 oz
Drapes	gold vinyl-backed cloth drapes	100% vinyl back, 60% rayon, 40% acetate	25 in. high by 29 1/2 in. high, 9 lb (each)
	fire rated 701 small-scale test		8 ft by 12 ft pleated, 8 ft by 24 ft flat, 18 lb, 8 oz
Carpet	tufted cut-pile heat set-piled	100% nylon pile, 30 oz/yd ² ; 100% synthetic backing	12 ft by 18 ft, 62 oz/yd ²
Cushion	coated hair/jute		
Walls	vinyl-covered gypsum board	5/8-in. Type X gypsum board, vinyl wallcovering	12 ft by 18 ft, 40 oz/yd ² (0.010 in. thick)
Ceiling	unfinished gypsum board	5/8-in. Type X gypsum board	

^aMetric conversion factors: 1 in. = 2.54 cm; 1 lb = 16 oz = 454 g; oz/yd² = 0.0339 kg/m².

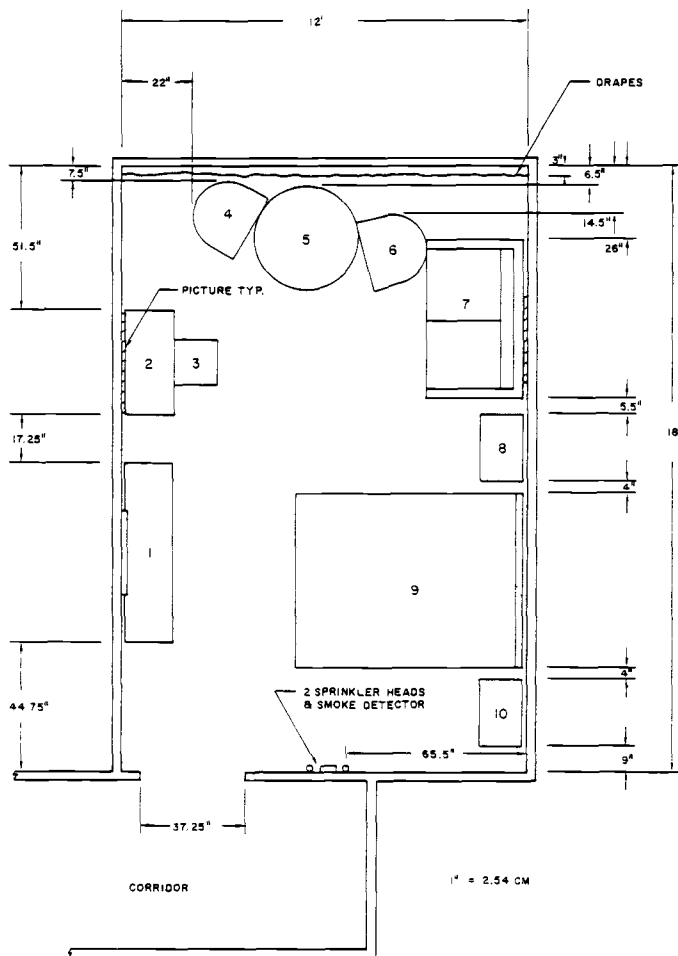


FIG. 4—Arrangement of furniture in burn room (description of furnishings is given in Table 2).

data are discussed. Observations and conclusions from the total test series are beyond the scope of this report. The fire tests were conducted on consecutive days. Summaries of times to significant events for the two tests and other pertinent data for the burn room are presented in Tables 3 and 4. Figure 5 shows photographs illustrating (a) the arrangement of furnishings in the burn room, (b) ignition of the upholstered chair, (c) rats exposed to smoke in the remote room, and (d) the burn room after the test.

Both tests were remarkably similar (for full-scale burns), with no significant differences in the sequence or times to events. Therefore, data in the text, for simplicity, will be presented for Test 3 only (please refer to the tables

TABLE 3—*Burn room. Summary of significant events.*

	Test 3		Test 4	
	Run Time, min:s	Time after Ignition of Chair, min:s	Run Time, min:s	Time after Ignition of Chair, min:s
Start	0:00	...	0:00	...
Smoke observed	4:30	...	4:29	...
Ignition of chair	18:55	...	19:02	...
Ignition of sofa	22:30	3:35	21:40	2:38
Smoke alarm (wall)	19:16	0:2	19:30	0:28
Smoke alarm (ceiling) ^a	...	...	19:20	0:18
Sprinkler head, fast response (wall)	22:30	3:35	21:20	2:18
Sprinkler head, fast response (ceiling) ^a	...	...	20:53	1:51
Sprinkler head, standard (wall)	23:01	4:06	24:35	5:33
Sprinkler head, standard (ceiling) ^a	...	...	24:20	5:18
650°C (1200°F) at 2.1 m (7 ft) height in burn room, 1st "flashover" ^b	25:43	6:48	25:05	6:03
Visual flashover	28:12	9:17	27:39	8:37
Door to animal room closed	30:43	...	30:05	...

^aCeiling alarms not installed in Test 3.^bFlashover was defined as given in the text.TABLE 4—*Burn room. Gas concentrations and other data.*

Gas Concentrations at 1.7 m (5 1/2 ft) Level	Test 3	Test 4
Maximum CO, ppm	70 700	62 900
Minimum O ₂ , %	0.2	0.2
Maximum CO ₂ , %	14.2	13.4
Maximum HCN, ppm	1160	1520
Maximum HCl, ppm	1170 ^a	280 ^a
Maximum NO _x , ppm	130	110
Maximum CH _x , ppm	4100	4290
Other Observations	Test 3	Test 4
Smoke Obscuration, ^b approximate time (min:s)	19:30	19:35
Maximum Temperature, °F	1520	1590
Maximum Temperature, °C	830	870

^aHydrogen chloride data in the burn room may be low (see text). Concentrations of HCl in corridor vent were 2000 to 3000 ppm.^bSmoke obscuration is defined here as 55% transmission or 0.25 optical density over 1 m.

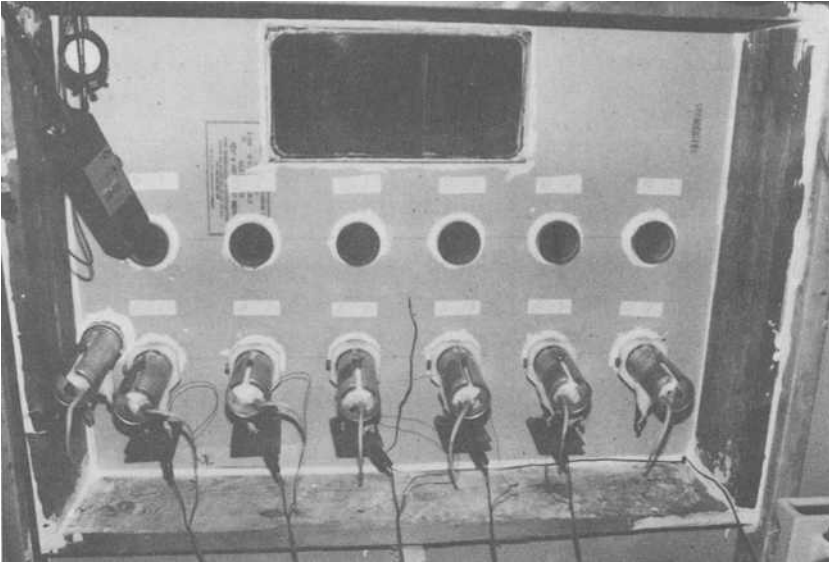


(a)—Arrangement of furnishings.



(b)—Ignition of chair.

FIG. 5—*Photographs of full-scale fire tests.*



(c)—Exposure of rats in remote room.



(d)—After the test.

FIG. 5—Continued.

for a comparison between the two tests). The data for Test 3 are plotted in Figs. 6 (burn room) and 7 (animal room).

The significant events in these tests may be listed as follows:

1. Start of test.
2. Appearance of smoke.
3. Flaming ignition.
4. Smoke alarm sounds.
5. Sprinkler activates.
6. 650°C (1200°F) "flashover" occurs.
7. Visual flashover occurs.
8. Animals in remote room are incapacitated.
9. Animals in remote room die.

Events relating to life safety in fires can be categorized into preflashover and postflashover occurrences. The definition of *flashover* used here is the attainment of approximately 650°C (1200°F) at the 2.1-m (7-ft) level in the

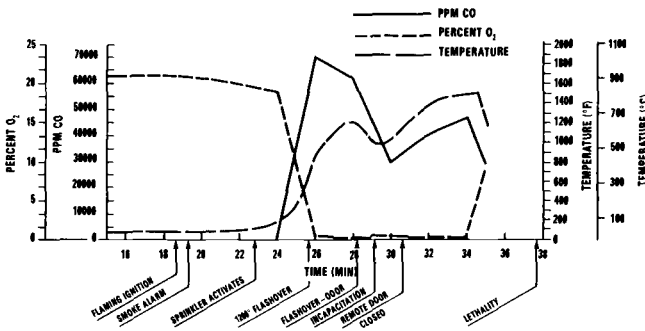


FIG. 6—Sequence of events and conditions in the burn room at the 1.7-m (5 1/2-ft) level.

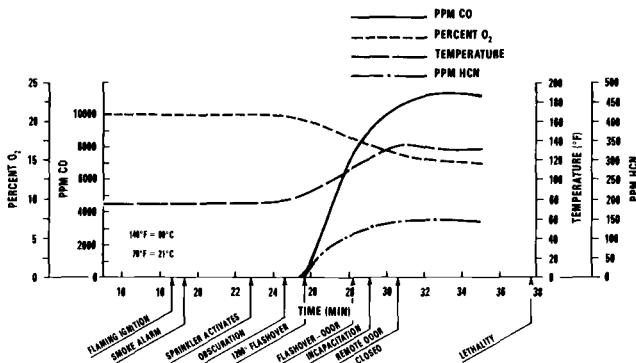


FIG. 7—Animal room conditions.

burn room. This event was monitored by instrumentation during the tests and is quite precise, whereas visual flashover is somewhat subjective on the part of an observer.

All hazardous events prior to flashover in these tests could have been potentially controlled by activation of a sprinkler and presented an environment from which safe escape was possible. Once flashover was reached, events were very quickly life-threatening, even in a remote location (for example, the animal room in these tests). After this point, potential for escape was limited to only a few minutes.

Events in the Burn Room

The tests were begun by activation of the glocoil in the foam upholstery of the chair. Smoke was not visible for about 5 min. Flaming ignition of the chair in Test 3 occurred at 18:55 (minutes:seconds). The approximately 19 min of smoldering from the start of the test to ignition of the chair did not create a toxic concentration of CO. The ionization smoke detector activated 21 s after flaming ignition. Escape from the fire in the burn room at this point, although being rapidly compromised by smoke, would still be possible from the standpoint of temperature and toxic gases (Fig. 6).

The fast-response sprinkler head (wall-mounted) activated at 3 min 35 s after flaming ignition (just about the time the sofa ignited). At this point, CO was less than 200 ppm, HCN was about 20 ppm, oxygen was greater than 19%, and temperature was less than 60°C (140°F). Thus, had the sprinkler been connected to a water supply, it would have controlled the fire at a time when heat and toxic combustion products would probably not have prevented escape. "Flashover," 650°C (1200°F) at the 2.1-m (7-ft) level in the burn room, occurred at 6 min 48 s after flaming ignition. Visual flashover, with flames emerging from the doorway, was observed 9 min 17 s after ignition.

All of the events just described are illustrated in the representation in Fig. 6 and summarized in Table 3. However, it appears in the figure that oxygen depletion and CO production precede the maximum temperature rise. This is an artifact caused by the gas sampling procedure; any given location [for example, at 1.7-m (5 1/2 ft), as shown in the figure] was only sampled every 2 min. The events were undoubtedly simultaneous, with maximum rates of CO production and oxygen depletion coinciding with one another and with the drastic temperature rise. Maximum measured CO levels in the burn room at 1.7 m (5 1/2 ft) reached 70 700 ppm for Test 3. Oxygen concentration dropped to nearly zero and CO₂ exceeded 10% by volume. Temperatures exceeded 800°C (1500°F). These data are shown in Fig. 6 and also in Table 4.

Hydrogen cyanide and hydrogen chloride were sampled using dry soda lime absorption tubes, as previously described. However, the high temperatures in the burn room and corridor precluded locating the tubes inside the facility; therefore, the tubes were outside and connected to lengths of stainless steel

tubing which extended to the sampling point. Experience has shown that HCl is readily lost to surfaces of sampling probes. Therefore, the concentrations given in Table 4 could be somewhat lower than actual concentrations in the burn room (especially HCl). Concentrations of HCl up to 3000 ppm were found in the hallway vent located approximately 13.7 m (45 ft) from the fire.

Heat flux measurements were made at several locations in the burn room (as shown in Fig. 1). One of the devices was located in the center of the room on the floor. In these tests, 3 W/cm^2 was reached at this location at 25:40 for Test 3 and 26:20 for Test 4. These times corresponded to the 650°C (1200°F) flashover for Test 3 (see Table 3), which preceded visual flashover by about $2 \frac{1}{2}$ min, and to midway between the two "flashover" points in Test 4. The attainment of approximately 3 W/cm^2 generally has been considered to be a measure of flashover. Maximum heat flux recorded in these tests was about 12 to 13 W/cm^2 (the gages were calibrated only to 10 W/cm^2) at 33 to 34 min (approximately 5 to 6 min after visual flashover). These data were recorded for the 1.8-m (6-ft) level on one wall.

Events in the Animal Room

Summaries of data relating to the animal room for the two experiments are given in Tables 5 and 6. The summary of data at this location, for Test 3, is portrayed in Fig. 7. The figure displays critical events, temperatures, and toxic gas concentrations in the remote room (HCN concentration is shown as a smooth curve even though the data were not taken continuously). Pre-

TABLE 5—*Animal room. Summary of significant events.*

Group A (Five Animals)	Test 3	Test 4
Begin exposure (min:s run time)	0:00	0:00
Mean time to incapacitation	29:04	28:06
% COHb at mean incapacitation (two animals)	19.3/38.9	20.6/37.8
CO (Ct, ppm-min)	16 610	11 960
HCN (Ct, ppm-min)	360	190
Mean time to death (no. of animals)	37:53 (5)	34:37 (5)
% COHb (average) at death	79.6	79.3
CO (Ct, ppm-min)	111 000	84 700
HCN (Ct, ppm-min)	1440	920
Group B (Five Animals)	Test 3	Test 4
Begin exposure (min:s run time)	30:48	28:49
Mean time to death (no. of animals)	37:10 (3)	34:44 (4)
% COHb (average) at death	81.7	80.2
CO (Ct, ppm-min)	69 300	64 600
HCN (Ct, ppm-min)	800	740

TABLE 6—*Animal room. Gas concentrations and other data.*

Gas Concentrations at Location of Animals	Test 3	Test 4
Maximum CO, ppm	11 300	11 700
Minimum O ₂ , %	14.5	13.9
Maximum CO ₂ , %	2.6	4.5
Maximum HCN, ppm	140	150
Maximum HCl, ppm	430	520
Maximum NO _x , ppm	50	50
Maximum CH _x , ppm	3640	3950
Other Data	Test 3	Test 4
Smoke obscuration, ^a approximate time, min:s	24:35	24:20
Maximum temperature, °F	136	140
Maximum temperature, °C	58	60

^aSmoke obscuration is defined here as 55 % transmission or 0.25 optical density over 1 m.

flashover events in the figure, except for smoke obscuration, refer to the burn room. No life-threatening conditions, except for visual obscuration by smoke, were present in the remote room until 650°C (1200°F) occurred in the burn room. Visual obscuration by smoke occurred after the sprinkler would have controlled the fire in the burn room.

Following attainment of flashover in the burn room, conditions in the remote room deteriorated rapidly (the door to the animal room was open 25 mm (1 in.) until 5 min after flashover). Mean time to incapacitation (leg-flexion shock avoidance) of five rats occurred at 3 min 21 s after the 650°C (1200°F) flashover, with death occurring approximately 9 min later (the number of animals that died during exposure is given in Table 5; only those animals pulled for postexposure observations survived). The door to the animal room was closed 5 min after the 650°C (1200°F) temperature was reached to avoid excessive heat and an excessive toxicological insult (that is, with the door open for the entire experiment, the CO levels might have been several times higher than they were in these tests).

The differences between the two tests in both mean times to incapacitation or mean times to death (see Table 5) were considered to be not substantial. The blood COHb saturations observed at death were all near 80%, which is normal for CO-induced lethality in rats. The CO concentration-time (Ct) product for Test 3 (Group A animals) was 111 000 ppm-min. This is what one might expect when CO is the sole toxicant; however, the Ct-product for Test 4 was lower at about 85 000 ppm-min. For the animals in Group B, the CO Ct-products were even lower (65 000–70 000 ppm-min), even though the COHb levels were also about 80%.

Although lethality appeared to correlate with COHb data, incapacitation

did not. Blood COHb saturations at incapacitation were less than 40%, which is somewhat low (60% or higher is normal in incapacitated rats if CO is the sole toxicant). Total dosages (Ct-products) also were low in Test 3 at about 16 000 ppm-min (30 000 to 40 000 ppm-min is more typical for incapacitation if CO is the sole toxicant). Hydrogen cyanide, at concentrations as high as 140 ppm in the remote room in Test 3, may have been a factor in incapacitation. These experiments do not confirm this, however, since the animals may have been affected by oxygen levels of 14 to 15% and temperatures of up to 60°C (140°F). Concentrations of HCl up to 500 ppm were also found in the animal room. Results of unpublished studies with pure HCl at Southwest Research Institute indicate that this concentration of HCl should be neither incapacitating nor lethal.

Conclusions

The following is a summary of conclusions made from this study:

1. This particular full-scale test procedure was useful in the assessment of the overall toxic hazard of a realistic fire scenario.

2. In these fully furnished room fires, significant toxic hazards from smoke did not occur until 650°C (1200°F) was reached at the 2.1-m (7-ft) level in the burn room. After that point, tenability decreased very rapidly in the burn room, in the corridor, and in the nearby animal room.

3. Carbon monoxide was the major cause of death in these experiments. Carbon monoxide and HCN both may have contributed to incapacitation of the animals. Hydrogen chloride also was present.

4. The smoke detector used in these tests activated prior to the development of acutely toxic concentrations of combustion products. In the fire scenario described, obscuration of vision by smoke preceded the threats from either heat or toxic gases.

5. A sprinkler system would have prevented any significant toxic threat from developing in these experiments, even in the room of fire origin. Data show that, at the time of activation of the fast response sprinkler heads, the conditions in the burn room were not acutely life-threatening. Carbon monoxide concentration was less than 200 ppm, HCN was 20 ppm or less, oxygen concentration was greater than 19%, and temperature was less than 60°C (140°F).

Acknowledgment

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Appendix

Methods for Analysis of HCN and HCl

A method of collection of gaseous HCN and HCl using tubes containing dry soda lime has proven to be reliable, easy to use, and with absorption efficiencies comparable to or better than solution impingers. This technique yields a time-integrated analysis (that is, the average concentration is determined over some time period). If the time increments can be made small enough (for example, 2 min), an intermittent plot of concentration versus time can be developed. The time-integrated analyses by this method enable the development of a reliable concentration-time (Ct) product, which is useful in combustion toxicity experiments, even though a true maximum concentration might not be obtained.

A summary of the method (including preparation of the tubes), use conditions, desorption, and analysis is presented in the following paragraphs. A major advantage in the use of these tubes is their ability to be located almost anywhere in a laboratory or full-scale fire test. The absorption tubes may be put at the very end of the gas sampling line, with connecting tubing, pumps, and rotameters far removed from the sampling tubes.

Approximately 0.5 g of 20 to 30 mesh soda lime (a mixture of sodium hydroxide and calcium oxide) in a glass tube is used for collection of HCN and HCl. The tubes are 3-mm (1/8-in.) inside diameter (I.D.) and 10 cm (4 in.) long. The ends of the tube are plugged with cotton. A suction pump, needle valve, and rotameter are employed to pull a specific volume of the combustion atmosphere through the sorption tubes. Samples are taken over fixed time intervals. Due to difficulty in maintaining a specified flow rate, the calibrated rotameter is read frequently over the sampling time interval, and the total volume of gas is then calculated. The rotameters are calibrated to 25°C. The equations that follow utilize this temperature for normalizing the concentrations of the gases. Since concentrations are calculated on the basis of volume/volume, only the equivalent flow of sample gas at the location of the rotameter need be determined. No conversion factor is necessary to calculate concentration at the sampling point. The ideal molar volumes are used in these calculations due to some discrepancies in the literature on the actual molar volumes of HCN and HCl.

HCN

The soda lime absorbent, including cotton wadding, is desorbed in 10-mL deionized water, and the solution is swirled briefly. The solution is allowed to stand for at least 30 min to permit the soda lime particulates to settle. An aliquot is extracted from each sample bottle (from 0.05 to 0.5 mL, depending on the cyanide concentration) for analysis of HCN by a standard method [6]. The aliquot is mixed in a 50-mL Erlenmeyer flask with enough 0.1 M potassium hydroxide (KOH) to make up a 5-mL solution. Two mL of freshly prepared Chloramine T/sodium dihydrogen phosphate buffer solution are added; the solution is swirled for a few seconds and allowed to stand for 2 min. Two and a half mL of 0.1 M KOH is then added, followed by 0.5 mL of the color-producing reagent (pyridine-barbituric acid). This total solution volume of 10 mL is left standing for 10 min to allow the color to develop. The absorbance of the solution is measured at 578 nm on a Coleman Junior III spectrophotometer. Cyanide standards are analyzed in the same manner.

Standard curves are prepared frequently, plotting absorbance at the particular wavelength versus concentration of Reagent Grade potassium cyanide (KCN). Un-

known solutions are read directly (as $\mu\text{g CN}^-$) from the standard curve. Calculations are performed as follows

$$\text{HCN, ppm} = \frac{A \cdot R \cdot 24.5}{V \cdot 26.02}$$

where

- HCN, ppm = concentration of HCN in the gas phase, ppm,
 A = $\mu\text{g CN}^-$ read from standard curve,
 R = ratio of volume of desorbent solution (10 mL) to that of aliquot (usually 0.1 mL), for example, 10/0.1,
 24.5 = molar volume of ideal gas, $\mu\text{L}/\mu\text{mol}$, at 25°C and 1 atm,
 V = total volume, in litres, of atmosphere sampled (volume rate of flow times time), and
 26.02 = formula weight of CN^- , $\mu\text{g}/\mu\text{mol}$.

HCl

The soda lime absorbent, including the cotton wadding, is desorbed in 10-mL de-ionized water, and the solution is swirled briefly. After at least 30 min of standing, a 3-mL aliquot is extracted for HCl determination. This sample is adjusted to pH 8 to 10 (phenolphthalein end point) with 0.1 N nitric acid (HNO_3). Titration of HCl is performed to a visual end point (*s*-diphenyl carbazone) with mercuric nitrate, in accordance with a standard procedure [7]. The titrant is 0.014 to 0.0014 N , depending on the expected concentration of chloride.

Concentration of HCl in the gas phase is calculated as shown below

$$\text{HCl, ppm} = S \cdot N \cdot R \cdot 24.5 \cdot (1/V) \cdot 1000$$

where

- HCl, ppm = concentration of HCl in the gas phase, ppm,
 S = mL titrant used for sample (corrected for that required for blank),
 N = normality of titrant,
 R = ratio of volume of desorbent solution (usually 10 mL) to that of aliquot (usually 3 mL), for example, 10/3,
 24.5 = molar volume of ideal gas, $\mu\text{L}/\mu\text{mol}$, at 25°C and 1 atm,
 $1/V$ = reciprocal of total volume, in litres, of atmosphere sampled (volume rate of flow times time), and
 1000 = conversion factor.

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Applications of Sprinkler Technology—Early Suppression of High-Challenge Fires with Fast-Response Sprinkler

REFERENCE: Yao, C., “Applications of Sprinkler Technology—Early Suppression of High-Challenge Fires with Fast-Response Sprinkler,” *Fire Safety: Science and Engineering*, ASTM STP 882, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 354–376.

ABSTRACT: Recent advances in sprinkler technology research, confirmed with large-scale fire tests, have demonstrated the feasibility of the early suppression of high-challenge fires with a few large-drop sprinklers when fast-response sprinkler links are used.

In principle, early fire suppression is determined by the proper balance of three important factors: (1) convective heat release rate at sprinkler response ($\dot{Q}_c$); (2) actual delivered density (ADD), the sprinkler discharge actually penetrating the fire plume; and (3) required delivered density (RDD), the quality of water required to suppress fire growth. At the instant of sprinkler operation, a successful early suppression system must be able to provide sufficient ADD with the first few sprinklers operated in excess of the RDD. The faster the sprinkler response, the lower the convective heat release rate at sprinkler operation, the higher the ADD, the lower the RDD, and the easier it will be to achieve early suppression.

Factory Mutual Research Corp. has initiated an intensive program to define and verify the early suppression criteria which can be used as a base for the preparation of approval and application standards, and the development of new ESFR (early suppression fast response) sprinklers by the sprinkler manufacturers. An overview of the ESFR program is presented in this paper.

KEY WORDS: sprinkler technology, high-challenge fire, early suppression fast response sprinklers, rack storage of plastics, convective heat release rate, response time index, required delivered density, actual delivered density

The automatic sprinkler system has been the most commonly used fire suppression system for the protection of industrial and commercial properties.

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There has been a gradual improvement and refinement of sprinkler devices since Henry Parmelee's development of the first practical automatic sprinkler in 1882. Fires in today's high-piled storage occupancies have challenged sprinklers of current designs to their limit of effectiveness. Recent advances in sprinkler technology research have resulted in a new approach to sprinkler protection which can radically change current sprinkler protection philosophies.

One hundred years ago, the automatic sprinkler was first developed for the protection of New England textile mills. Ceiling heights were low, and occupancies were not particularly combustible. The main function of a sprinkler system was to wet and protect the combustible ceiling structure. The standard sprinkler which was introduced by Factory Mutual in the early 1950s was designed to deliver water directly downward to suppress and control fire spread in the combustible contents in the building.

The hazard of storage warehouses has increased as a result of the introduction of new storage methods and new synthetic products, which have posed a severe challenge to the standard sprinkler system. The urgent need for improved fire protection in residential occupancies has raised doubt as to the suitability and effectiveness of an industrial sprinkler in residential buildings. To cope with these challenges, research in sprinkler technology was initiated to study the sprinkler protection principles which were used as a basis for development of new sprinklers to optimize sprinkler protection in both industrial and residential applications. A general review of Factory Mutual's Sprinkler Technology Research program and two sprinkler optimization programs was presented at the Society of Fire Protection Engineers/Center for Fire Research Engineering Application of Fire Technology Workshop of 1980 [1].

One of the optimization programs was the development of large-drop sprinklers, which were designed to provide: (1) maximum orifice size, 16.3 mm (0.64 in.), with a 12.7-mm (1/2-in.) pipe thread for the convenience of retrofit usage; and (2) large drops for the effective penetration of high-velocity fire plumes in high-challenge fires. The results of three fire tests are presented in Table 1 to illustrate the comparative performance of standard and large-drop sprinklers against fire in high-challenge storages, such as 6-m (20-ft)-high palletized storage of standard plastic commodities: polystyrene jars packaged in compartmentalized, corrugated paper cartons. The large-drop sprinkler is superior to the standard sprinkler in protecting high-challenge storage occupancies. Another optimization program was the development of "fast-response" residential sprinklers, which were designed to operate over a fire at a very early stage and suppress it with minimum fire damage and water demand. A new development is underway to combine the technology of fast-response sprinklers with the most advantageous characteristics of improved large-drop sprinklers to achieve early fire suppression, even of high-challenge storage fires. It is the intention of this paper to present an overview of our recent advances in the application of sprinkler technology.

TABLE 1—*Protection of 6-m-high palletized storage of Factory Mutual (FM) standard plastics commodities^a, 3-m clearance.*

Test No.	1	2	3
Sprinkler type	Standard Large-Orifice	Large-Drop	Large-Drop
Design density, mm/min (g/min/ft ²)	24.5 (0.6)	24.5 (0.6)	32 (0.78)
Pressure, kPa (psi)	248 (36)	193 (28)	345 (50)
Spacing, m by m (ft by ft)	3 by 2.4 (10 by 8)	3 by 3 (10 by 10)	3 by 3 (10 by 10)
Temperature rating, °C (°F)	140 (286)	140 (286)	140 (286)
No. sprinklers in operation	45+	18	4
% fuel consumption	> 20	1.5	< 1
Max ceiling temperature, °C (°F)	1110 (2030)	1008 (1846)	837 (1454)
Time above 816°C, min	6	1.5	0.05
Total water flow, dm ³ /min (g/min)	8177 (2160)	4088 (1080)	1181 (312)

^aFM Standard plastic commodities: polystyrene jars packaged in compartmentalized, corrugated paper cartons.

Early Suppression and Fire Control

Generally, there are two alternative concepts in sprinkler fire protection [2]: (1) early suppression and (2) fire control. The most effective mode of sprinkler fire protection is to achieve early fire suppression, that is, to suppress and extinguish a fire as quickly as possible with a minimum number of sprinklers in operation and minimum damage. However, when early suppression is not feasible, then sprinkler protection also can be designed primarily to achieve fire control, that is, to provide density-area demand needed by the operation of large numbers of sprinklers to wet and to soak the combustibles remote from the ignition stacks and thus control and confine the fire spread.

The “early suppression” protection system requires high performance and a large quantity of water discharged from the first four operated sprinklers. The payback is reduction in water demand and low fire and water damage. For ordinary-hazard occupancy such as low-ceiling warehouses storing less combustible products or machine shops, early suppression with a few standard sprinklers can be accomplished easily at a reasonable water pressure. This “early suppression” concept, though not specially designed as such, has been applied successfully over a wide range of variations and system designs. For high-challenge fires, as shown by Test 1 in Table 1, using standard large-orifice 13.5-mm (¹⁷/₃₂-in.) sprinklers at a discharge density of 24.5 mm/min (0.6 g/min/ft²), the fire plume velocity above the storage array was measured at 12 m/s (40 ft/s) when the first four sprinklers operated. However, the small drops discharged from these first four sprinklers could not deliver enough water through the high velocity fire plume to the seat of the fire to suppress the fire when it was still small. Instead, we withdrew to the “fire control” concept. In this case, the fire continued to spread, opening many more sprinklers. In the control concept, for the purpose of preventing an excessive num-

ber of sprinkler operations while keeping the fire under control, high-temperature rated sprinklers are recommended. Due to the excessive water demand, the long duration of high-ceiling temperature, and early fire jump into the target, simulating larger storage array, Test 1 is considered as a failure even from the point of view of fire control. However, in Test 2 the same density large-drop sprinklers provided adequate fire control and opened 18 sprinklers, with a great reduction in water demand. As shown in Test 3, by an increase in pressure to 345 kPa (50 psi), early suppression was accomplished with only four large-drop sprinklers. Table 1 presents typical examples of uncontrolled (Test 1), controlled (Test 2), and early suppression (Test 3) concepts in sprinkler fire protection. Thus, the problem is how to design the early suppression sprinkler system.

Three Factors for Early Suppression

In principle, a successful early suppression sprinkler system must be able to deliver, through the high-velocity fire plume, onto the top horizontal surface of the burning storage, a sufficient quantity of water drops at the instant of sprinkler operation in excess of what is required to suppress the fire growth. It is, therefore, necessary to define and determine the following three important factors:

1. Convective heat release rate ($\dot{Q}_c$) at sprinkler response, relating to fire growth rate of the storage hazard and speed of sprinkler response.
2. Actual delivered density (ADD), relating to sprinkler discharge pattern in the presence of a fire plume. It is the density of water actually penetrating the fire plume and delivered onto the top of the burning storage array.
3. Required delivered density (RDD), relating to extinguishment characteristics of the storage hazard. It is the density required to be delivered at the top of the burning storage array to achieve early suppression.

Of course, the faster the sprinkler response, the lower the $\dot{Q}_c$, the lower the RDD, the higher the ADD, and the easier it will be to achieve early suppression. This has been the working hypothesis of achieving early suppression with fast response sprinklers.

Convective Heat Release Rate at Sprinkler Operation

In full-scale palletized or rack storage fire tests, the first few sprinklers usually operate while the fire is still confined within the ignition stacks, represented by an array of two-pallet loads wide and two-pallet loads deep (2×2 array). A basic concern in early suppression sprinkler protection is to provide a method for the prediction of convective heat release of a given storage hazard in relation to the physical properties and geometrical locations of the heat-sensing element of the sprinklers.

In a real fire, the heat release rate actually released is always smaller than the theoretical heat release rate. The actual heat release rate, $\dot{Q}_A$, consists of two components: convective heat release rate, $\dot{Q}_C$, and radiative heat release, $\dot{Q}_R$. Two methods are used to measure the convective heat release rate during the early stage of a developing fire, for example, within the ignition stacks of a large storage array: (1) the fire products collector (FPC) method and (2) the temperature-velocity integration method.

The FPC method determines [3] the heat release rate according to a principle similar to that of the laboratory-scale "Factory Mutual (FM) flammability apparatus" [1], except that the FPC is about 1000 times larger in capacity. As shown in Fig. 1, the collector gathers fire products, together with ambient air, into an intake cone which allows the fire gases to flow at a known mass rate up to $33 \text{ m}^3/\text{s}$ ($70000 \text{ ft}^3/\text{min}$) into an expanded duct where extremely effective mixing occurs; the well-mixed flow allows downstream sampling at a single point for temperature and concentrations of fire gases. The temperature measurement leads to the convective heat release rate; the gas concentration measurements can be used to calculate the actual heat release rate.

In the temperature-velocity integration method [4,5], velocity probes and thermocouples are installed at three different levels above the storage array to measure the mass loss rate as well as the temperature and velocity profile of the fire plume. The convective heat release flux across a level of the instru-

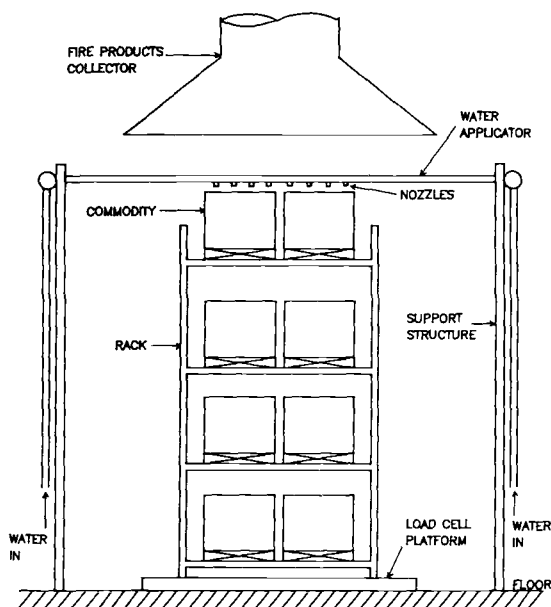


FIG. 1—Test setup for the determination of heat release rate and required delivered density of storage commodities within the ignition stacks of storage arrangement.

mentation station, located above the flame zone, can be computed by integration of the temperature and velocity profiles at that level.

A series of free-burn fire tests have been conducted with rack storage of two different classes of commodities arranged two pallet loads wide by two pallet loads deep and three different heights, two, three, and four tiers at 3, 4.5, and 6 m high, respectively. Figure 2 shows the arrangement of the four-tier (2 by 2 by 4) array of rack storage. As shown in Fig. 3, convective heat release rate measured with the FPC and integration method agrees very well. Mass loss rate during the growth stage of all the rack-storage plastic fires can be correlated with the following two "power law" relationships

$$\dot{M}_b = 3.82 \times 10^{-5} N(t - t_0)^2 \quad \text{when } 0 < (t - t_0) < 100 \text{ s} \quad (1)$$

$$\dot{M}_b = 2.41 \times 10^{-3} N(t - t_0) + 0.14 \quad \text{when } 100 \text{ s} < t - t_0 < 180 \text{ s} \quad (2)$$

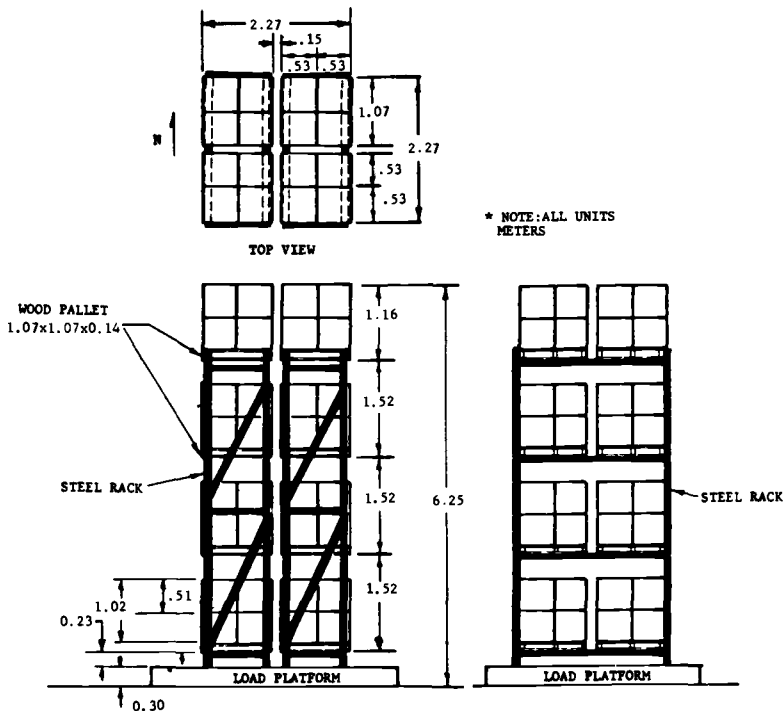


FIG. 2—Rack storage configuration of four-tier array (2 by 2 by 4) of standard plastic commodity.

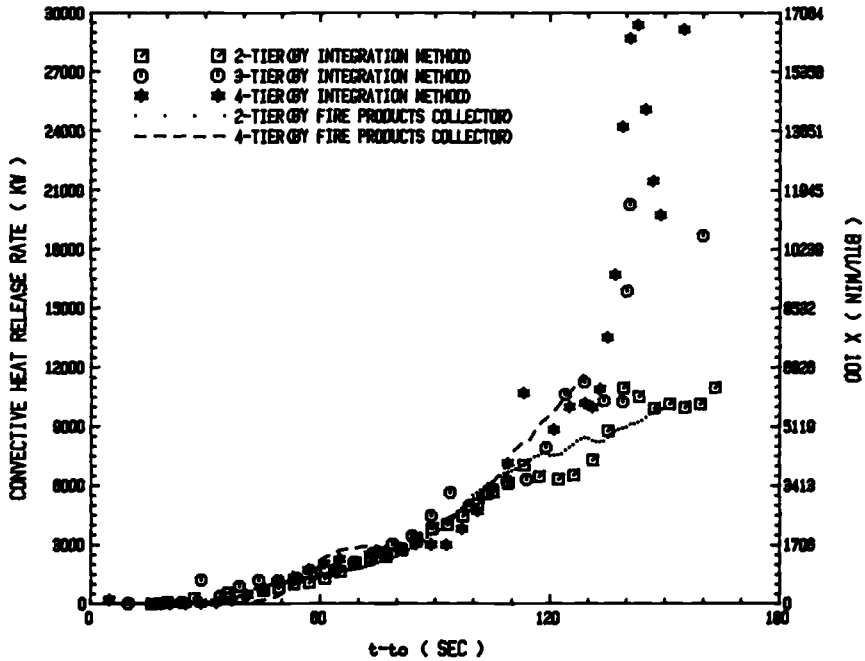


FIG. 3—The convective heat release rate in rack storage of standard plastic commodity.

where

- $\dot{M}_b$ = mass loss rate of the test fuel array, kg/s,
 t = time for ignition, s,
 t_0 = virtual time origin [1], s, and
 N = number of tiers in the storage array.

Convective heat release rate during the growth stage of rack-storage plastic fires can be correlated with the following equations

For 4.5-m-high (three tiers) storage

$$\dot{Q}_c = 1.28 \times 10^{-7}(t - t_0)^5 + 0.36(t - t_0)^2 \quad \text{when } 37 < (t - t_0) < 100 \quad (3)$$

$$\dot{Q}_c = 7.95 \times 10^{-4}(t - t_0)^3 + 0.14(t - t_0)^2 + 19.3(t - t_0) + 821 \quad \text{when } 100 < (t - t_0) < 116 \quad (4)$$

For 6-m-high (four tiers) storage

$$\dot{Q}_c = 2.83 \times 10^{-7}(t - t_0)^5 + 0.42(t - t_0)^2 \quad \text{when } 39 < (t - t_0) < 100 \quad (5)$$

$$\dot{Q}_c = 1.74 \times 10^{-3}(t - t_0)^3 + 0.3(t - t_0)^2 + 19.1(t - t_0) + 447 \quad \text{when } 100 < (t - t_0) < 122 \quad (6)$$

where

$\dot{Q}_c$ = convective heat release rate, kW.

Figures 4 and 5 show the correlation of the fire plume centerline temperature and velocity data measured for rack storage fires of two different commodities (plastic and class II²) at three different heights (two, three, and four tiers). In the log-log graph, the data fall on a simple curve. As $(z - z_0)/$

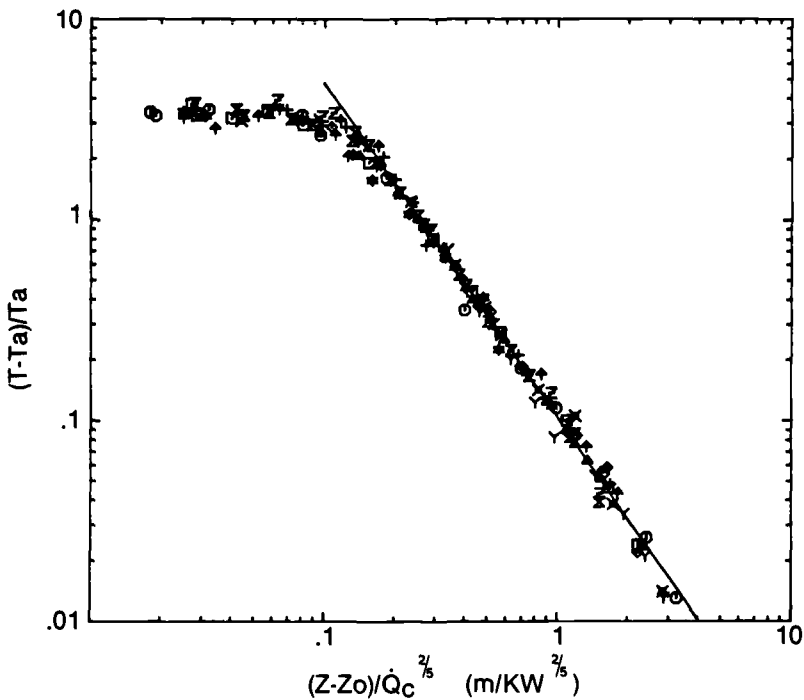


FIG. 4—Correlation of fire plume centerline temperature data measured for rack storage of two different commodities at three different heights.

²Class II test commodity: triwall corrugated paper carbon with a sheet-metal liner.

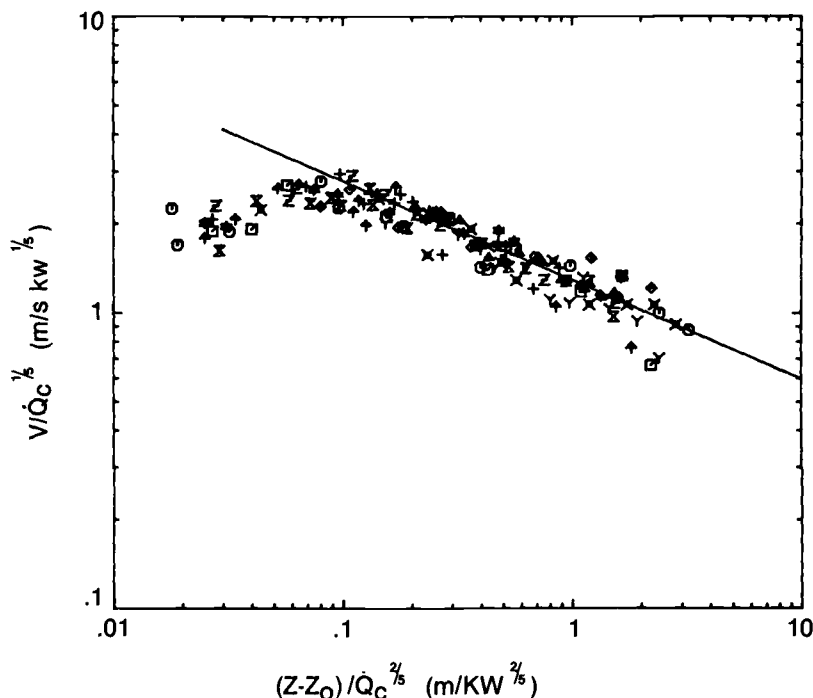


FIG. 5—Correlation of fire plume centerline velocity data measured for rack storage of two different commodities at three different heights.

$\dot{Q}_c^{2/5} \geq 0.14 \text{ MW}^{-2/5}$, the centerline plume temperature and velocity for high-challenge fires can be predicted from the following equations

$$\frac{T_c - T_a}{T_a} = 0.106 \dot{Q}_c^{2/3} (z - z_0)^{-5/3} \quad (7)$$

$$v_c = 1.3 \dot{Q}_c^{1/3} (z - z_0)^{-1/3} \quad (8)$$

where

T_c = plume centerline temperature, K,

T_a = ambient temperature, K,

v_c = plume centerline velocity, m/s,

z = plume height measured from the top of the storage array, m, and

z_0 = virtual source location measured from the top of the storage array, m.

The virtual source location of both three- and four-tier-high rack storage fires can be determined from

$$Z_0 = -1.6 + 0.094 \dot{Q}_c^{2/5} \quad (9)$$

For a given fire situation, sprinkler response time is determined by the thermal properties of the heat-sensing element of the sprinkler, such as temperature rating and sensitivity value. Sprinkler sensitivity is expressed by its time constant, defined as the time required for the link to reach a temperature (temperature above initial temperature of the link) which is a fraction, 0.632, of the excess gas temperature in a constant-velocity, constant-temperature gas stream. The time constant is independent of gas temperature and varies inversely with the square root of gas velocity. Hence, the product of the time constant and the square root of gas velocity is a constant value associated with the link sensitivity, which is called the "response time index" (RTI) [6].

$$RTI = \sqrt{u} \, t_r / \ln \left(1 - \frac{\Delta T_L}{\Delta T_g} \right) \quad (10)$$

where

RTI is in units of $s^{1/2} m^{1/2}$ (metric system) or $s^{1/2} ft^{1/2}$ (British system),

u = the approaching gas velocity at the heat-sensing element, m/s,

t_r = the response time of sprinkler, s,

ΔT_L = the activation temperature of the sensing element (temperature rating) minus the initial temperature of the sensing element, K or °C, and

ΔT_g = approaching gas temperature minus initial temperature of the sensing element, K or °C.

The RTI value can be measured by plunging the sprinkler into a tunnel containing air at known velocity, u , and temperature (ΔT_g). Measured values of RTI for 23 commercial sprinklers ranged from about 115 to 400 $m^{1/2} s^{1/2}$ (200 to 700 $ft^{1/2} s^{1/2}$).

After defining the set of variables, such as convective heat release rate, virtual source locations of the fire plume, sprinkler sensitivity value, temperature rating, spacing and clearance, the quasi-steady-state ceiling flow models [7], and the following unsteady heat transfer equation can be used to compute numerically the sprinkler response time, which, in turn, will be related to the convective heat release rate and fire plume strength at sprinkler operation.

$$\frac{d\Delta T_l}{dt} = \frac{u^{1/2}}{RTI} (\Delta T_g - \Delta T_l) \quad (11)$$

where

ΔT_l = excess temperature of the heat-sensing element above its initial temperature, K or °C.

Figure 6 illustrates the effect of sprinkler sensitivity and temperature rating on sprinkler response time, convective heat release rate, and plume velocity for a fast-growing 6-m (20-ft)-high rack storage fire of plastic commodities. A fast-response sprinkler, $RTI = 29 \text{ m}^{1/2}\text{s}^{1/2}$ ($50 \text{ ft}^{1/2}\text{s}^{1/2}$) and 71° (160°F) temperature rated, can reduce the fire challenge from about 4220 kW (240 000 Btu/min) for a conventional response sprinkler of $RTI = 174 \text{ m}^{1/2}\text{s}^{1/2}$ ($300 \text{ ft}^{1/2}\text{s}^{1/2}$) and 140°C (280°F) rated to 1320 kW (75 000 Btu/min); it also reduces the plume velocity from 12 to 7.2 m/s (40 to 24 ft/s).

Actual Delivered Density (ADD)

Present-day standard sprinklers are available in both upright and pendent designs, using deflectors to distribute water downward in a hemispherical pattern. Figure 7 shows a sprinkler discharge pattern in terms of local application density (D_l) versus radial distance measured at 1.5, 3, and 4.5 m (5, 10, and 15 ft) below a large-drop sprinkler. The figure represents an average distribution at a given radius because it is obtained with a rotating carriage supporting up to twelve 0.09-m^2 (1-ft^2) water collecting pans.

For the convenience of hydraulically designed sprinkler systems, the protection requirements of sprinkler systems have been expressed in the standards as area-density demands. The design density (D_a), which is commonly used in National Fire Protection Association 13, is defined as the total quantity of water discharged divided by the total area of operation per unit time. When four sprinklers overlap, the local application density measured over a cross-sectional area within the four sprinklers and at a distance of about 2 to 3 m below the sprinklers is equal to the design density. In the presence of fire,

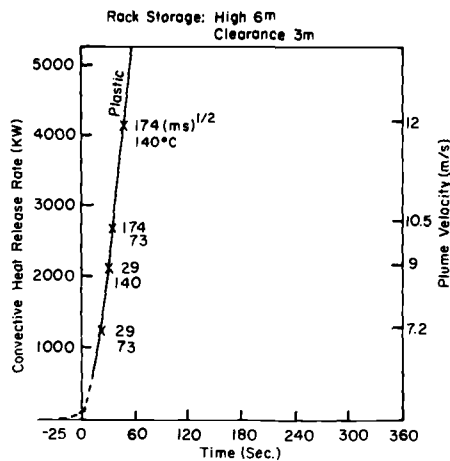


FIG. 6—Effect of sprinkler sensitivity and temperature rating on sprinkler response.

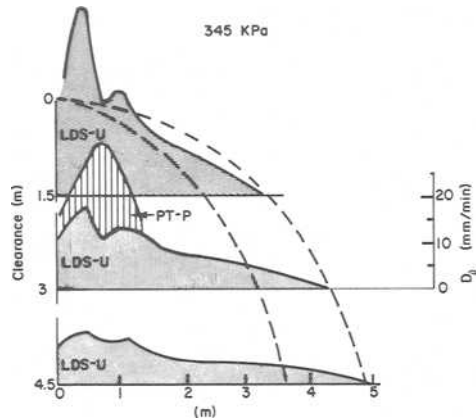


FIG. 7—Large-drop sprinkler discharge pattern—local application density distribution at different elevations. LDS-U represents large drop sprinkler—upright design. PT-P represents prototype 16.3-mm sprinkler—pendent design.

however, only a fraction of the sprinkler discharge can survive to penetrate the fire plume. The rate at which water is actually deposited from operating sprinklers onto the top horizontal surface of a burning combustible array is called the actual delivered density (ADD). It is a function of (1) the fire plume strength, that is, temperature and velocity distribution; (2) sprinkler discharge characteristics, that is, drop size distribution and downward momentum of the sprinkler discharge; and (3) the geometrical relationship between the sprinklers and the fire. In the early suppression concept, drops discharged from the first few sprinklers must penetrate the fire plume and deliver a sufficient quantity of water to suppress and extinguish the fire. The upward thrust of the fire plume presents a barrier to the penetration of drops. In principle, there are two separate modes of drop penetration: the gravitational penetration mode and the momentum mode. In the gravitational penetration mode, the terminal velocity of the drop must be greater than the gas velocity in the fire plume. The larger the drop diameter, the greater is the terminal velocity, and the more capable it is to penetrate the high velocity fire plume. In the momentum penetration mode, the total thrust of the sprinkler discharge must be greater than the upward thrust of the fire plume. The higher the discharge pressure, the greater is the spray thrust, and the closer it is to reaching the momentum penetration mode.

Figure 8 shows the measured terminal velocity as a function of drop diameter. The two higher curves pertain to fire plume at elevated temperature. For convective heat release rate in the range of 1320 to 4220 kW (75 000 to 240 000 Btu/min) and a maximum plume velocity from about 7.2 to 12 m/s (24 to 40 ft/s), drop sizes smaller than 1 mm may have difficulty in penetrating the fire plume, even if the fast response sprinkler is used. Drops larger

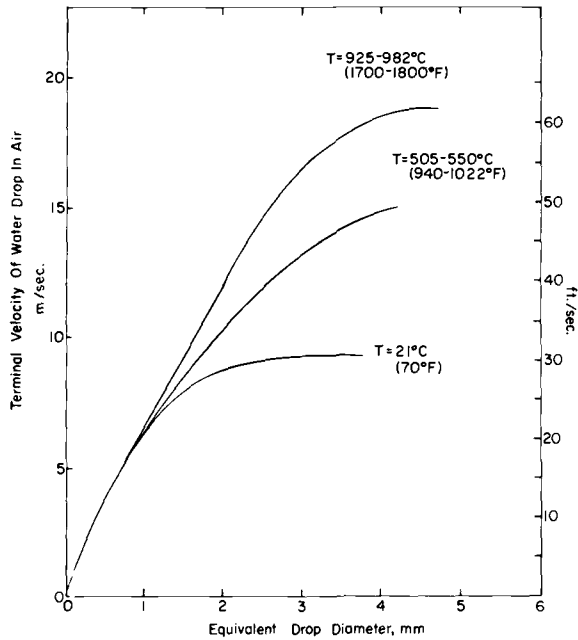


FIG. 8—Terminal velocity of water drops in air at various temperatures.

than 2 mm have terminal velocities in excess of 12 m/s (40 ft/s), which is common in the high-challenge fire when conventional sensitivity and high temperature sprinklers are used. Figure 9 shows the measured drop size distribution for a 13.5-mm ($17/32$ -in.) orifice diameter standard large orifice sprinkler and 16.3-mm (0.64-in.) orifice diameter large-drop sprinkler. There are a great many more large drops, 2 mm or larger, in the discharge of a large-drop sprinkler than a large-orifice sprinkler. For sprinklers of similar geometry, the mean drop size generated by these sprinklers is proportional to the two-thirds power of the orifice diameter and the minus one-third power of pressure [8]. In other words, an increase in orifice diameter, D , will increase the drop size, d , and an increase in pressure, p , will decrease the drop size slightly

$$d \propto D^{2/3} p^{-1/3} \quad (12)$$

Penetration performance of sprinkler discharge can be measured with a gas-fired plume simulator [1,8]. Drops penetrating the simulated fire plume are collected at the base of the plume with 1.2-m (4-ft)-diameter water collecting pans. The water collection rate measured without fire, $\dot{Q}_c = 0$, is the local application density, D_l . With the fire plume operating, the water collection rate is the ADD.

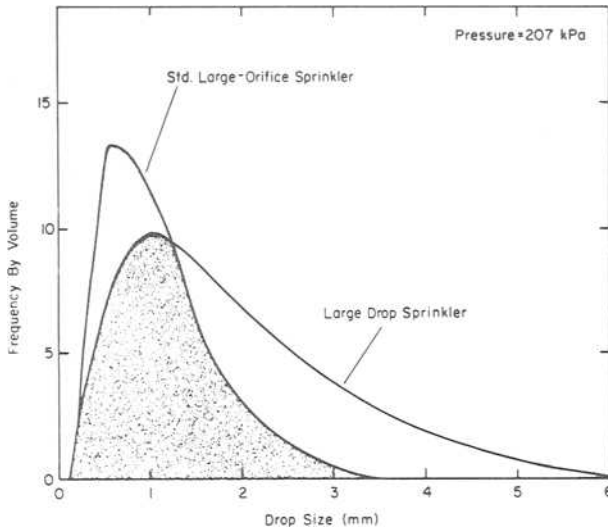


FIG. 9—Drop size distribution of standard large-orifice sprinkler and large-drop sprinkler.

Many sprinklers were tested at simulated convective heat release rates of 2290 and 4570 kW (130 000 and 260 000 Btu/min). Data measured for an approved large-drop sprinkler are presented in Fig. 10 as ADD versus sprinkler operating pressure for two convective heat release rates. In general, the figure shows that the lower the convective heat release rate, the higher will be the ADD. It also illustrates the transaction from gravitational penetration on the left side of the arrow mark ($\downarrow$) to momentum penetration on the right.

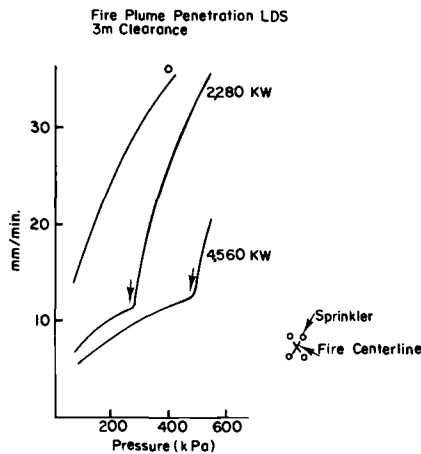


FIG. 10—Penetration performance of large-drop sprinkler.

For example, at a $\dot{Q}_c = 2.28$ MW (130 000 Btu/min) in the gravitational penetration regime, the ADD increases slowly with pressure. At a pressure of about 275 kPa (40 psi), the momentum penetration regime begins and the ADD increases rapidly with pressure. However, for a 4.56-MW (260 000-Btu/min) fire plume, critical pressure to initiate the momentum penetration is about 480 kPa (70 psi). Correlation of test results employing a large number of approved sprinklers [1,8], using the plume simulator at a test condition of four sprinklers centered around the fire plume and 3.6-m clearance, shows that in the gravitational regime

$$\text{ADD} \propto \frac{D_l}{D_d} \frac{K^{1.37}}{\dot{Q}_c^{0.22}} \frac{P^{0.13}}{S} \quad (13)$$

The critical condition for transition from the gravity regime to the momentum regime is

$$\frac{D_l}{D_d} \frac{1}{\dot{Q}_c^{2/5}} \frac{K p_r}{S^2} \geq 0.204 \quad (14)$$

where

- K = sprinkler K factor, $\text{cm}^3/\text{s Pa}^{1/2}$,
- p_r = critical pressure for transition, kPa,
- S = sprinkler spacing, m, and
- $\dot{Q}_c$ = convective heat release rate, MW.

In Eq 13, ADD is proportional to a strong power of K factor and spacing, S . In Eq 14, critical pressure for the initiation of momentum regime, p_r , is influenced strongly by the K factor, spacing, and convective heat release rate. The proportional constant in Eq 13 can vary among equations by a factor of three, depending on the sprinkler design. If we want to operate well in the momentum regime where ADD is as high as, sometimes even higher than, the design density, the sprinkler operating pressure should be about 1.5 times as high as the critical pressure, p_r . Methods to achieve reduction in the critical pressure to enter more easily into the momentum regime include: (1) use of fast-response sprinklers to reduce the convective heat release rate at sprinkler operation; (2) narrowing the sprinkler discharge pattern; (3) increase in the orifice size; (4) reduction in the spacing; and (5) reduced clearance.

To illustrate the point, limited ADD data presented in Fig. 10 are extrapolated and replotted in Fig. 11 as ADD performance of the large-drop sprinkler versus convective heat release rate at sprinkler operation at two operating pressures. The figure shows that the longer the delay in sprinkler operation, the larger the fire becomes and the lower is the ADD when sprinklers are operated.

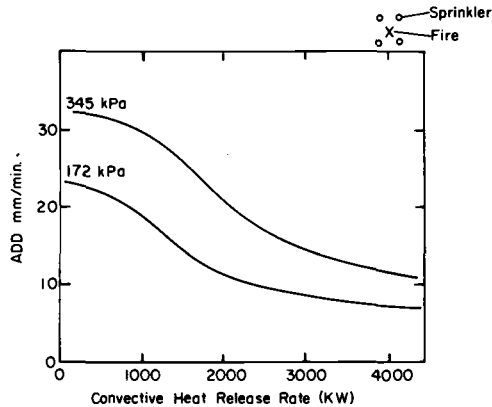


FIG. 11—ADD performance of large-drop sprinkler.

Required Delivered Density, RDD

Among the key data in the early suppression concept is the water density required at the top horizontal surface of a burning storage array, at which a specified early suppression criterion will be satisfied. The early suppression criterion can be defined in two different ways: (1) in terms of operating sprinklers (water demand) which can be set at no more than four sprinklers; and (2) in terms of fuel consumption (fire damage) which can be set at an acceptable level, for example, 10% of the ignition stacks. The first criterion depends on sprinkler variables such as ceiling clearance, sprinkler spacing, sprinkler sensitivity, and temperature, in addition to the fuel itself, while the second criterion is fuel-dependent only.

RDD is determined also under FPC. As shown in Fig. 1, water at a predetermined rate is applied directly and uniformly on the top horizontal surface of the ignition stacks by means of a special water applicator which is actuated manually to simulate a sprinkler operation at a predicted sprinkler response time or corresponding convective heat release rate. Data measured in this apparatus deal directly with the ease of suppression of the fire in the storage array, independent of sprinkler type and operating conditions.

Figure 12 is a plot of the convective heat release rate data collected by the FPC during experiments with 6-m (20-ft)-high rack storage of plastic commodities. The convective heat release rate increased rapidly to about 1760 kW (100 000 Btu/min). Water at a density of 16 mm/min (0.4 g/min/ft²) was delivered at the top of the test storage array to simulate the operation of four fast-response sprinklers. As shown by the lower curve, quick reduction in heat release rate exhibits the effectiveness of a delivered density of 16 mm/min. The upper curve moves upward when the delivered density is reduced to 14 mm/min (0.35 g/min/ft²). Calculation of sprinkler responses based on the

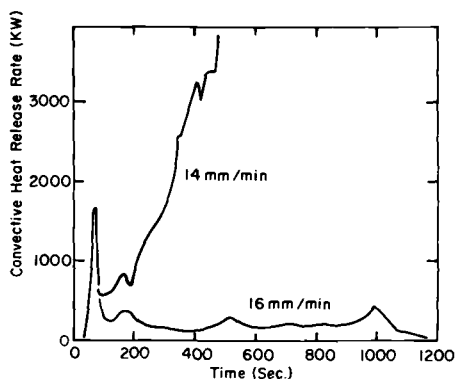


FIG. 12—Typical RDD test results: 6-m-high rack storage of plastic at two delivered densities.

convective heat release rate measured both before and after water application can be used to determine the RDD, satisfying the early suppression criterion, based on no more than four sprinkler operations. Figure 13 shows the RDD on the basis of the four-sprinkler operation versus convective heat release rate at sprinkler operation for a 6-m-high rack storage of plastic commodities. This figure shows that the higher the RTI, the larger the fire becomes and the greater the RDD for early suppression.

Early Suppression Sprinkler System

An early suppression sprinkler system can be defined as an automatic sprinkler system which is designed to deliver an ADD in excess of the RDD at

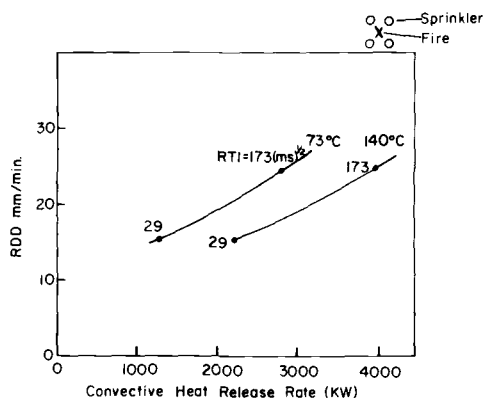


FIG. 13—RDD of 6-m-high rack storage of plastic commodities.

a convective heat release rate corresponding to a time when the first group of sprinklers are operated.

Figure 14 is a cross plot of Figs. 11 and 13 and shows the interrelationship of three factors for early suppression. This is obviously related to a specific application, that is, use of large-drop sprinklers at 3-m by 3-m (10-ft by 10-ft) spacing to protect a 6-m (20-ft)-high rack storage of plastics in a 9-m (30-ft)-high warehouse, assuming ignition at the center below four sprinklers, and they are all operated as a group at about the same time. The RDD curves, which increase with the $\dot{Q}_c$, intersect always with the ADD curves, which decrease with the $\dot{Q}_c$. It is merely a case of supply and demand. If sprinklers actuated prior to this intersect, the operated sprinklers would supply more water at the top horizontal surface of the burning storage array than is demanded, and early suppression would be expected to occur. The figure also shows that, in this case, early suppression with less than four sprinklers should be accomplished with large-drop sprinklers at a discharge pressure of 345 kPa (50 psi), even as low as 172 kPa (25 psi) when a low-temperature-rated (74°C) and high-sensitivity ($RTI = 29 \text{ m}^{1/2}\text{s}^{1/2}$) link is employed. A series of four fire tests were conducted to verify these relationships under identical test conditions, except that the temperature rating and sensitivity of the sprinkler links were the only test variables. In Table 2, Test 4 represents the baseline test using the approved large-drop sprinkler, which has a conventional sensitivity of $RTI = 173 \text{ m}^{1/2}\text{s}^{1/2}$ (300 $\text{ft}^{1/2}\text{s}^{1/2}$) and a temperature rating of 140°C (286°F). Eighteen sprinklers operated to control the fire with a total water demand of 5300 dm^3/min and fuel consumption of about 27 pallet loads. When high sensitivity and low temperature rating were used, in Test 7,

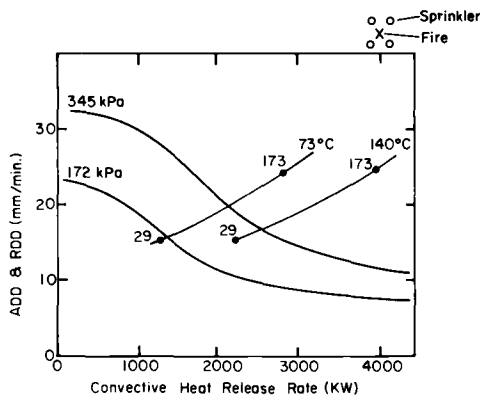


FIG. 14—Cross plot of ADD and RDD data.

³The figure applies only with the condition that the first four sprinklers operated within a reasonable time.

TABLE 2—*Protection of 6-m-high rack storage of FM standard plastic commodities, with large-drop sprinklers of different temperature ratings and sensitivity values.*

Test No.	4	5	6	7
Aisle width, m (ft)	2.4 (8)	2.4 (8)	2.4 (8)	2.4 (8)
Clearance, m (ft)	3 (10)	3 (10)	3 (10)	3 (10)
Temperature, °C (°F)	140 (286)	140 (286)	73 (164)	73 (164)
RTI, $m^{1/2}s^{1/2}$ (ft $^{1/2}s^{1/2}$)	173 (300)	29 (50)	173 (300)	29 (50)
Pressure, kPa (psi)	345 (50)	345 (50)	345 (50)	345 (50)
No. sprinklers	18	14	12	3
Water demand, dm ³ /min (g/min)	5300 (1400)	4260 (1125)	2880 (1025)	946 (250)

early suppression was accomplished as expected with only three sprinklers, low water demand, and four pallet loads in fuel consumption. The only surprise is Test 5, which needs more explanation. In Test 5, the first of the four sprinklers surrounding the ignition location operated at about a convective heat release rate of 2200 kW; the fourth sprinkler operated about 84 s later. During this period, the fire was still growing. The true ADD curve for Test 5 is much lower than that shown in Fig. 14³ because it was measured with four overlapping sprinklers. Density actually delivered at the top surface of the burning storage array could have been much lower than the RDD value required, which was increasing with time throughout the entire period up to the operation of the fourth sprinkler. In view of the possibility of sprinklers being shielded from obstacles, such as duct work or lighting fixtures, or failure of one of the first four sprinklers to function, the ADD value measured with only two or three sprinklers should be provided for early suppression sprinkler system design.

A summary of several key fire tests with fast-response, large-drop sprinklers is presented in Table 3. Test 8 was a repeat of Test 7, but two of the first four sprinklers were plugged to simulate a failure mode; two large-drop sprinklers at 345 kPa (50 psi) provided successful early suppression without opening any additional sprinklers. Test 9 was another repeat of Test 7, but with the discharge pressure reduced to 172 kPa (25 psi); in this test, four sprinklers provided early fire suppression. Similarly, at 345 kPa, with an increase of the storage height to 7.5 m (25 ft) high under a 9-m (30-ft)-high ceiling, again three sprinklers provided early suppression in Test 11.

Effect of Ignition Location

Full-scale fire tests have been conducted traditionally by assuming that the worst possible condition is to ignite the fire at the center beneath four sprinklers. Although ignition location has not been found to be a strong variable in our test configuration as far as the fire control protection concept is concerned, repeat of some of the crucial fire tests with fast-response sprinklers

TABLE 3—Protection of rack storage of plastic commodities with fast response ($RTI = 29 \text{ m}^{1/2}\text{s}^{1/2}$, temp. rating = 73°C) 16.3-mm (0.64-in.) orifice size sprinklers.

Test No.	7	8	9	10	11	12	13
Ignition location ^a	O	O	O	O	⊗	⊗	⊗
Storage height, m (ft)	X	X	X	X			
Clearance, m (ft)	O	O	O	O			
Sprinkler type ^b	6 (20)	6 (20)	6 (20)	7.5 (25)	6 (20)	7.5 (25)	6 (20)
Water pressure, kPa (psi)	3 (10)	3 (10)	3 (10)	1.5 (5)	3 (10)	1.5 (5)	3 (10)
$D\dot{V}$, mm/min (g/min/ft ²)	LDS-u	LDS-u	LDS-u	LDS-u	LDS-u	LDS-u	PT-P
Early suppression	34S (50)	34S (50)	172 (25)	34S (50)	34S (50)	34S (50)	34S (50)
	25.3 (0.62)	18.7 (0.46)	24 (0.59)	24 (0.59)	13 (0.32)	24 (0.59)	24 (0.59)
	yes	yes	yes	yes	no	yes	yes

^aO = operating sprinkler; X = ignition location.^bLDS-u = FM-approved 16.3-mm (0.64-in.) large-drop sprinkler—upright; PT-P = prototype 16.3-mm (0.64-in.) sprinkler—pendent.^cLocal applied density is defined as the density delivered over the top surface area of the ignition stacks (2 by 2 array) under no-fire conditions. It was measured with the same sprinkler arrangement and test condition which existed during the fire test.

has shown that ignition location has become a dominant variable in the early suppression protection concept. To illustrate this point, test results, using only the ignition stacks of the storage arrangement, are presented as Tests 11 to 14 in Table 3. Test 11 is a repeat of Test 7, but the ignition location is moved directly under a sprinkler. The first sprinkler opened quickly. The fire underneath caused the spray pattern to open up, which provided additional cooling and wetting of the adjacent sprinklers, causing a long delay in the operation of the second sprinkler. In other words, during the period of 48 to 90 s after ignition, only one sprinkler operated. The fully overlapping sprinkler discharge was not established until 200 s after ignition. As shown in Fig. 5, the average local applied density with a large-drop sprinkler, without the presence of fire, over the 2.4 m by 2.4 m ignition stack cross-sectional area is only 13 mm/min (0.32 g/min/ft²). It is obvious that the ADD value under a fire situation is far below the RDD value of 16.3 mm/min (0.4 g/min/ft²). Consequently, fire spread out the boundary of the 2 by 2 by 4 storage array and opened 13 sprinklers. In Test 12, when the storage height was increased to 7.5 m (25 ft) at a clearance of 1.5 m (5 ft), one large-drop sprinkler at 345 kPa (50 psi) provided early suppression because the local applied density at 1.5-m clearance (as shown in Fig. 5) was increased to about 24 mm/min (0.59 g/min/ft²). Furthermore, a repeat of Test 11 with a prototype pendant 16-mm (0.64-in.) sprinkler designed to provide a narrow spray with a local application density of 24 mm/min (0.59 g/min/ft²) at 3-m clearance when discharged at 345 kPa is shown by the hatched curve in Fig. 7; again, one sprinkler provided early suppression in Test 13.

It is obvious that one of the problems we are facing at present is the difficulty in ensuring early suppression with existing large-drop sprinklers when the ignition point is located directly below a sprinkler. Table 3 also shows that early suppression with a one-sprinkler operation can be achieved when the local applied density is close to 24.5 mm/min (0.6 g/min/ft²). However, when single sprinkler local applied density is below 16.3 mm/min (0.4 g/min/ft²), early suppression is not achieved. It is possible that a high performance ESFR sprinkler may provide early suppression with a local density somewhat less than 24.5 mm/min (0.6 g/min/ft²), but it must exceed the RDD.

Early Suppression Fast Response (ESFR) Program

Preliminary fire test data summarized in Table 3 have demonstrated the feasibility of early suppression of high-challenge fires with fast-response sprinklers. Proper balance of two important factors, ADD and RDD, has been identified as the necessary condition for the early suppression concept. Apparatus and procedures have to be developed to quantify reliably and economically the RDD of different storage hazards and the ADD performance of candidate ESFR sprinklers under various realistic operating conditions, especially in regard to the location of the ignition point. In order to bring this

ESFR concept into practice, the next step is to develop ESFR sprinklers and approval and application standards for such a sprinkler system. An extensive ESFR program has been initiated, with a limited goal initially, to provide reliable early suppression for high hazard storage occupancies, up to 7.5-m (25-ft)-high, double-row, rack storage of plastics in a 9-m (30-ft)-high building.

The first objective of this program is to measure the RDD for rack storage of plastic commodities of 4.5, 6, and 7.5 m (15, 20, and 25 ft) high in a 9-m-high building using fast-response sprinklers. These RDD values will be determined on the basis of both no more than four sprinkler operations and no more than a predetermined amount of fire damage or fuel consumption. Figure 15 shows the test results for 6-m-high rack storage of plastic commodities. Although a delivered density for fire suppression is about 16.3 mm/min (0.4 g/min/ft²), a conservative value for RDD for 6-m-high storage might be set at 18.4 mm/min (0.45 g/min/ft²). Beyond this value the fuel consumption might be less than 80 kg (7.5% of the ignition stacks), and no more than four sprinklers will operate, which is calculated with a conservative ceiling flow model, ignoring the cooling effect of the first four sprinklers. Preliminary analysis of test data for other storage heights shows that the RDD values for 4.5 and 7.5-m storage heights are about 14.3 and 24.5 mm/min (0.35 and 0.65 g/min/ft²), respectively.

The second objective is to measure the ADD of various candidate ESFR sprinklers under different realistic operating conditions, especially in regard

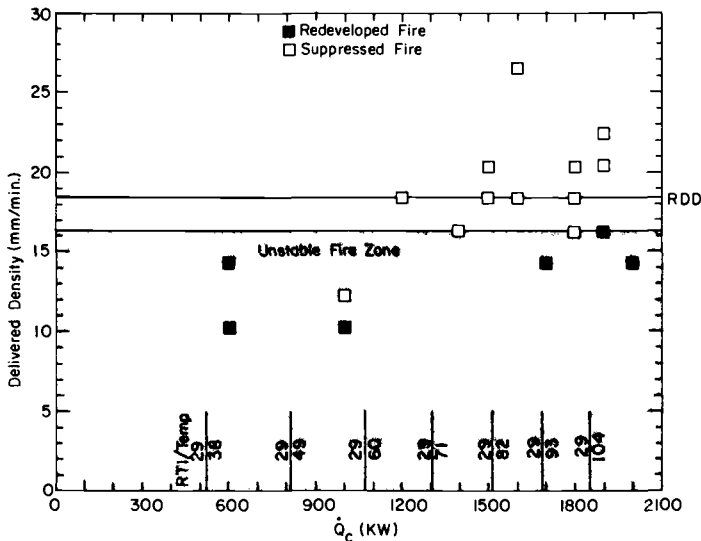


FIG. 15—RDD test results—2 by 2 by 4 rack storage of standard plastic commodity.

to the location of ignition point. To simulate the rack storage fire realistically, a new ADD apparatus is being developed. About 40 to 70% of the heat will be released beneath the simulated top tier storage commodities. Automated water-collecting devices will be located inside the simulated commodities to collect and measure continuously the rate of water deposited onto the top horizontal surface of the storage array. This apparatus can be adjusted to simulate the fire plume and ceiling flow characteristics of a real storage fire. It is almost nonreactive with sprinkler discharge and ideal for penetration measurement over a prolonged period. Actual ADD measurement will be conducted soon.

A third objective is to define and verify the proper RDD/ADD relationship or criteria for early suppression through a series of fire tests. These data will be used as a basis or guidelines for the preparation of approval and application standards.

Factory Mutual Research Corp. invites sprinkler manufacturers, the Underwriters Laboratories, the NFPA, and trade associations to participate in this most important event in sprinkler evolution. The method of approach adopted for the ESFR program is aimed at the application of scientifically based sprinkler technology and fire protection engineering to develop, as closely as possible, a performance-based standard for sprinkler protection.

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Definition of the Concentration Gradient Above Flammable Liquids³

REFERENCE: Munoz-Candelario, R. and Alvares, N. J., "Definition of the Concentration Gradient Above Flammable Liquids," *Fire Safety: Science and Engineering, ASTM STP 882*, T. Z. Harmathy, Ed., American Society for Testing and Materials, Philadelphia, 1985, pp. 377-385.

ABSTRACT: Areas where the potential for spill of flammable fluids is finite are defined as hazardous locations and are subject to code restrictions regarding location and protection from potential ignition sources. These locations require a specific separation distance between the potential spill plain and the ignition sources. This distance is universally accepted to be 18 in. for most U.S. codes. We found that acceptable separation distances have increased over the years, intuitively in the name of safety, although we found no data to support this. We surveyed the literature and performed experiments to find a quantitative basis for the increasing distances. The literature revealed a case for evaporating fluids with boundary conditions for the volatile vapor diffusing through a stagnant gas film. In containers with open tops, the calculated vapor-concentration gradients are monotonic and directly proportional to the vapor pressure of the fluid. We confirmed the validity of these calculated values by performing experiments to determine the elevations at which flammable mixtures formed above the surfaces of volatile, high-vapor-density liquids.

KEY WORDS: vapor concentration gradient, equations for calculating; heights for ignition sources, codes regulating; spilled liquids, determining the concentration gradient

When the vapor densities of flammable liquids and gases are greater than the air density, released vapors tend to collect in tight enclosures and in the depressions of horizontal surfaces. For spilled fluids, the concentration gradient above the liquid surface is driven by vapor pressure at the bulk fluid temperature. In a completely closed container, we expect that the partial pressure of flammable vapor in air will eventually equilibrate to the vapor pressure after the original spill, assuming enough fluid was spilled to provide this con-

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centration. In open containers or in normal, unsealed rooms, a concentration gradient is formed above the liquid surface. This gradient is dependent upon the rate of evaporation of the liquid and on the geometry and elevation of leak sites.

Regulations guiding the placement of appliances with ignition sources (open flame, sparking devices, glow coils, switches, etc.) require that the ignition source location be at least 45.7 cm (18 in.) above the potential spill surface [1]. In the past, this height varied from 15.2 to 61 cm (6 to 24 in.). The documentation for choosing regulation heights cannot be found. Therefore, we believe a quantitative investigation is necessary to establish a technical basis for defining a "safe" height for ignition sources, based on analytical and theoretical analyses of gradient formation.

A proper perspective of the wide range of practical situations that require an adequate understanding of this problem can be gained from a brief review of the literature. Baum and Rockett [2] developed equations to predict the vapor concentration of spilled volatile materials assumed to lie at the bottom of containership holds. Wing [3] did an equivalent study on accidental releases of toxic vapors in the control rooms of nuclear reactors. Foerster et al [4] analyzed the formation of explosive gasoline-air clouds after major spills of gasoline in road tunnels. Theoretical studies also have been made by Georgakis et al [5] on the development and decay of liquid fuel spills in large tankers and by Zeman [6] on the dynamics and modeling of heavier-than-air releases of cold gas. McKay and Matsugu [7] experimented with the evaporation rates of liquid hydrocarbon spills on land and water under wind action and developed equations to predict such rates.

The most fundamental and comprehensive study on fuel and hydrocarbon vaporization was reported by Gauss [8] in 1973. Starting from the basic diffusional equations for steady- and unsteady-state vaporization, and using experimental data on evaporation from flat disks under low-velocity winds, Gauss developed a semiempirical expression to predict evaporation rates of pure liquid hydrocarbons under a wide range of conditions. Analyses of the flammable envelopes surrounding pools of pure hydrocarbons and fuels were also performed.

The literature search revealed no experimental or theoretical study on the determination of the concentration of heavier-than-air vapors above the surfaces of low-volatility liquids in the absence of convection currents. Our work reported is aimed at defining the fundamental mechanisms governing such processes and estimating the concentration as related to hazardous conditions.

Applicable Theory

The rates at which pure liquids vaporize depend on factors such as air velocity across the liquid surface, air and liquid temperature, liquid vapor pres-

sure and mass diffusivity, and geometry factors. If the air above the surface of the liquid is stagnant, the transport of vapor leaving the surface of the liquid through air occurs solely by molecular diffusion. The driving force for this process is the difference between concentration of vapors at the gas-liquid interface and at other points in the gas column in the direction of transport. Assuming an isothermal environment, conditions at the interface correspond to equilibrium. The partial pressure of the substance undergoing evaporation is therefore equal to its vapor pressure at the temperature involved. The equilibrium vapor concentration at the liquid surface is then a function only of temperature, since this, in turn, fixes the vapor pressure of the substance.

The diffusional equations have been solved for the simple case of a liquid vaporizing into air in an enclosed container with an open top. The liquid surface is totally bound by the walls of the container, thereby allowing diffusion to occur only in the vertical direction. The liquid level is maintained at constant temperature and pressure, and the vapors and air are assumed to form an ideal gas mixture.

Bird, Steward, and Lightfoot [9] discuss this specific evaporation problem and present solutions to the applicable diffusional equations for both unsteady- and steady-state conditions. For the case of steady-state diffusion in the system shown in Fig. 1, concentration profiles in gas phase are determined by solution of the molar flux equation

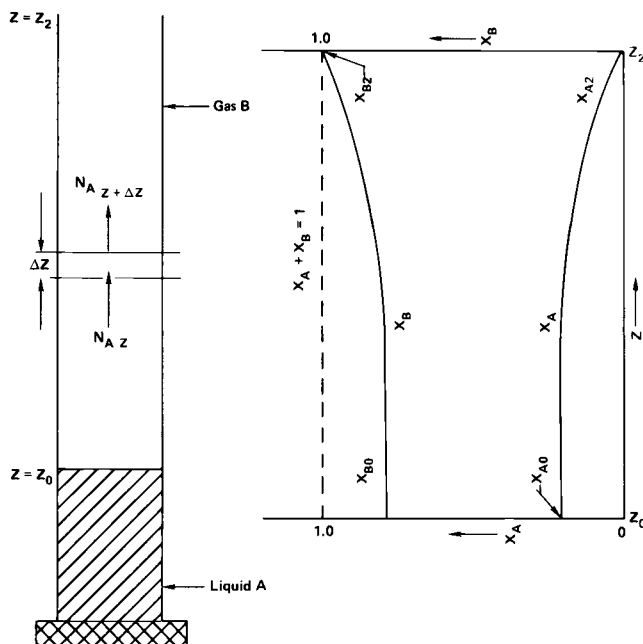


FIG. 1—Diffusion of A through B in steady state with B not in motion. Graph shows how concentration profile for B is distorted because of diffusion of A.

$$N_{AZ} = -cD_{AB} \frac{\partial X_A}{\partial Z} + X_A(N_{AZ} + N_{BZ}) \quad (1)$$

where

N_{AZ}, N_{BZ} = molar fluxes with respect to fixed axis,
 c = molar density,
 D_{AB} = diffusion coefficient of Gas A into Gas B , and
 X_A = mole fraction of A .

For A vaporizing into B , $N_{BZ} = 0$, and Eq 1 reduces to

$$N_{AZ} = \frac{cD_{AB}}{1 - X_A} \frac{\partial X_A}{\partial Z} \quad (2)$$

for the limit, $\Delta Z \rightarrow 0$, and steady-state conditions,

$$\frac{dN_{AZ}}{dZ} = 0 \quad (3)$$

Substituting Eq 2 into Eq 3 gives

$$\frac{d}{dZ} \left(\frac{cD_{AB}}{1 - X_A} \frac{dX_A}{dZ} \right) = 0 \quad (4)$$

For ideal gases and constant pressure and temperature, c is constant and D_{AB} is very close to constant, giving a second-order equation for concentration profiles in terms of X_A

$$\frac{d}{dZ} \left(\frac{1}{1 - X_A} \frac{dX_A}{dZ} \right) = 0 \quad (5)$$

The solution of Eq 5 for concentration profiles is

$$\frac{1 - X_A}{1 - X_{A0}} = \left(\frac{1 - X_{A2}}{1 - X_{A0}} \right)^{Z - Z_0 / Z_2 - Z_0} \quad (6)$$

The subscripts 0 and 2 in Eq 6 refer to the elevations at the surface of the liquid and at the top of the enclosure, respectively. If the stagnant air column is assumed to be infinitely high, the value of X_{A2} is zero. Taking the datum plane for the elevations right at the liquid surface makes the value for z_0 also zero, and the equation simplifies to the following form

$$\left(\frac{1 - X_A}{1 - X_{A0}}\right) = \left(\frac{1}{1 - X_{A0}}\right)^{z/Z_2} \quad (7)$$

This permits calculation of the gas-phase concentration as a function of fractional height above the surface of the liquid for long enclosures of known height. The equilibrium gas-phase concentration for Eq 7 can be calculated from the vapor pressure of the liquid at the temperature of the system.

Procedure and Results

The experimental work done in our study was aimed at confirming the applicability of Eq 7 for predicting the concentration of high-density vapors above the surfaces of low-volatility, flammable liquids. Rather than attempting to measure the concentrations at different heights, our approach was to determine only those heights corresponding to the lower flammable limit (LFL) of the vapors. The values obtained in this way were compared with the heights predicted by Eq 7 for the lower flammable concentrations in order to check agreement between the experimental findings and the theoretical model.

Long, open-ended glass tubes of different diameters were used as enclosure for the liquids under study. The lower portions of the tubes were immersed in a vessel of water and placed in a large, closed chamber to eliminate convection currents and to provide semi-isothermal conditions. The liquids tested were hydrocarbons immiscible with water. The hydrocarbons were then pipetted into the tubes to form a floating layer on the water's surface. We calculated the time when the steady-state concentration gradient was established in the tube [9]. After the predetermined time elapsed, we measured the LFL height using a very small capacitance spark probe inserted into the gas column from the top of the tube.

The experimental parameters used are tabulated below:

1. Tube inside diameters—1.05, 2.2, and 10.3 cm.
2. Tube lengths above the liquid surface from 25 to 35.7 cm.
3. Hydrocarbon tests—normal heptane and normal octane.
4. Temperatures of the liquids—room temperature (20 to 24°C).
5. Temperature difference between ambient air and tested liquid: $\pm 3^\circ\text{C}$.

Table 1 summarizes our experimental data and results. Values of fractional height above the liquid surface (corresponding to the LFL) are presented and compared with corresponding values predicted by theory. These were obtained from Figs. 2 and 3 for *n*-heptane and *n*-octane, respectively.

The example that follows illustrates the procedure used to construct Figs. 2 and 3:

1. Maximum height at which flaming occurred (LFL) = 18.6 cm.
2. Total height of the gas column above the liquid = 25 cm.

TABLE 1—Experimental data and results.

Run	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19
Tube i.d. (cm)	1.05	1.05	1.05	10.3	10.3	10.3	10.3	10.3	10.3	10.3	10.3	2.2	1.05	2.2	10.3	10.3	10.3	10.3	1.05
Hydrocarbon	normal heptane (nC ₇)	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	nC ₇	normal octane (nC ₈)	nC ₈	nC ₈	nC ₈	nC ₈	nC ₈	nC ₈	nC ₈
T _{liquid} (°C)	21	22	20.5	21	21	20	19	19	20	20	20	22	22	21	21	21	21	24	24
T _{vapor} (°C)	21	22	21	21	23	21	21	21	21	22	22	22	22	22	21	14	27	24	24
Tube height above liquid surface cm																			
(zz)	35.4	35.4	35.7	25	25	25	25	25	25	25	25	25	25	25	26.8	26.8	26.8	27.7	25
Height where flaming occurred (cm)																			
(z)	27.7	27.9	27.1	18.4	18.8	18.6	17.5	17.7	18.3	17.8	18.1	9.7	8.3	8.7	8.2	8.2	8.5	11.2	9.0
z/z ₂																			
(observed)	0.782	0.788	0.759	0.736	0.752	0.744	0.700	0.708	0.732	0.712	0.724	0.388	0.332	0.348	0.306	0.306	0.317	0.404	0.360
z/z ₂																			
(theoretical)	0.782	0.793	0.776	0.782	0.782	0.770	0.760	0.760	0.770	0.770	0.770	0.390	0.390	0.352	0.352	0.352	0.352	0.460	0.460
% deviation from theory	0	-0.6	-2.2	-5.9	-3.8	-3.4	-7.9	-6.8	-4.9	-7.5	-6.0	0	-14.9	-1.1	-13.0	-13.0	-9.9	-12.1	-21.8
Sparker position		center of tube				1/4 diameter from wall	1 mm from wall	3 mm from wall	7 mm from wall	1 cm from wall			center of tube				1 mm from wall		center of tube

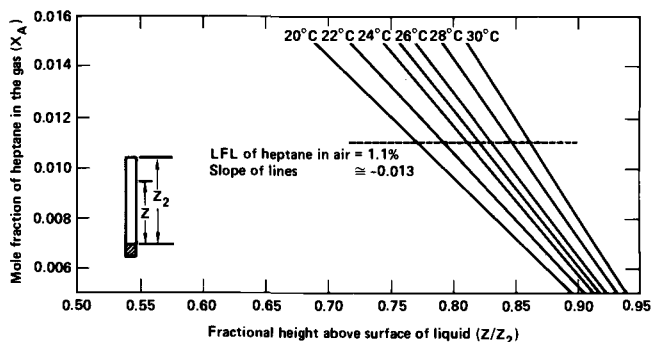


FIG. 2—Change in concentration of heptane vapors with fractional height above surface of liquid.

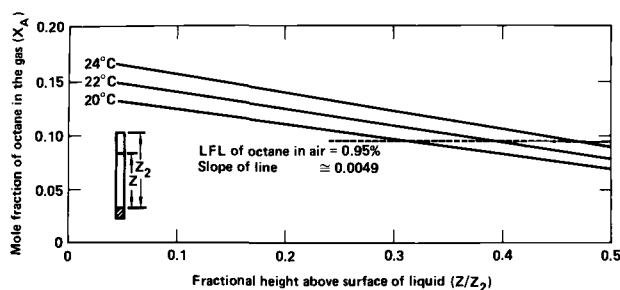


FIG. 3—Change in concentration of octane vapors with fractional height above surface of liquid.

3. Observed value of $z/z_2 = 18.6/25 = 0.744$.
4. Vapor pressure for *n*-heptane at 20°C = 35.8 mm Hg [10].
5. Lower flammability limit of *n*-heptane = 1.1% [10].
6. Theoretical values of $z/z_2 = 0.770$, from Fig. 2 corresponding to $X_A = 0.011$ at 20°C.
7. Percent deviation from theory = $(0.744 - 0.770)/0.770/100 = 3.4\%$.

Discussion

Our results show that the concentration profiles of vapors of low-volatility liquids vaporizing into stagnant atmospheres can be closely approximated from the basic diffusional equation for steady-state vaporization. The data presented in Table 1 show good agreement between the observed and the average-calculated concentrations of LFL. This agreement is within 4.4% for the *n*-heptane experiments and 10.7% in the *n*-octane experiment. This is remarkably good, considering that experimental deviations did not permit

attaining true equilibrium at the gas-liquid interface. Convection currents of small magnitude also may have been generated by temperature variations between the gas and liquid.

Some of the tests described in Table 1 involved determining the position of the lower flammable concentrations near the walls of the large diameter tube rather than at its center. This was done to establish if radial concentration gradients occurred in addition to the expected vertical ones. The possibility of radial gradients arises from the fact that, at the wall, the rate of evaporation is zero, and it increases toward the center. We were unable to provide conclusive evidence for either the presence or absence of radial gradients.

We note that the density gradient in the gas column above the surface of the liquids appears monotonic from bottom to top, as predicted by Eq 3 and shown in Figs. 3 and 4. However, stratification, or gravity-settling of the vapors heavier than air, may be indicated by the negative deviation between calculated and experimental data, especially apparent in the *n*-octane results.

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DISCUSSION

D. D. Drysdale¹ (written discussion)—As the flashpoint of *n*-nonane is 31°C, presumably above ambient laboratory temperatures, it would be important to maintain the entire length of the tube at least at the same temperature as the liquid, otherwise there would be some condensation of vapor at the wall above the liquid surface. As this could influence the results, what measures were taken to prevent it from happening?

R. Munoz-Candelario and N. J. Alvares (authors' closure)—The glove box enclosure in which tests were conducted was maintained at a constant temperature ($\pm 2^\circ\text{C}$) throughout the duration of the tests. For fluids with high flashpoints, the test section and fluids were maintained above the flashpoint temperature by heat lamps. Because of small enclosure volume, ($\sim 1\text{ m}^3$) internal temperatures were quite stable, and no condensation effects were observed. *N*-nonane does not appear in the paper as these results were obtained after the paper submission cutoff date. This information will be reported in a subsequent article.

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The Human Aspects of Fires in Buildings—A Review of Research in the United Kingdom

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ABSTRACT: Studies carried out by the Fire Research Station in the United Kingdom on human aspects of fires fall mainly into seven areas (indicated in the following paragraphs by italics).

Research on *perception and acceptability of risk* indicated that people have a fairly accurate perception of the relative severity of various hazards. They believe that most accidents are due to the faults of the victims, and they are unwilling to spend much money to reduce such hazards. On the other hand, many people do feel strongly about hazards which are outside their control. The assessment of *monetary value placed on human life* and the use of this value in fire protection economics are discussed with examples.

Research on *human behavior in fires* casts doubts on current behavioral assumptions and data which have implications for safe building evacuation. Information initially available to people regarding the possible existence of a fire and its size and location is often ambiguous and inadequate for taking appropriate action. People involved in fires rarely panic or behave entirely irrationally. This conclusion led to the current investigation on the nature and effective methods of presentation of appropriate information through microprocessor-controlled informative fire warning systems to ensure clear comprehension of alarm warnings and rapid and safe evacuation of the building. Research also has been carried out on methods of calculating the *evacuation times* of leaving buildings.

By *investigating actual (serious) fires* in domestic and group-residential buildings, Fire Research Station is studying people's environmental background, their actions prior to ignition, their involvement in ignition, their contribution to the fire's growth and spread, and their response to discovering the fire, fighting it, and escaping from it. A number of *social and housing conditions* affect the incidence of fires in dwellings: proportion of children in care, tenure (owner or tenant), and lack of amenities. *Severe weather conditions* during winter inflict greater loss of life among elderly females, especially those living alone.

KEY WORDS: fires, buildings, human aspects, perception of risk, acceptability of risk, value of life, human behavior, evacuation times, fire investigation, socioeconomic factors, severe weather conditions

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Centuries of experience have provided some knowledge of how to control the physical effects of fire through legislation and regulation about the provision of escape routes, fire-resistant materials, the structural design of buildings, and other engineering aspects. Such provisions are based only on traditional assumptions of human behavior in fire situations, response to fire alarms, use of extinguishers, travel through smoke, and use of escape routes. Moreover, the nature of fire has changed somewhat during recent years; in particular, the emergence of toxic smoke from foam-filled furniture has added a new dimension to coping with rapid and safe evacuation of occupants of a building in the event of a fire. There is, therefore, scope for scientific investigations aimed at making the provisions more appropriate. Statistics related to circumstances leading to fires in dwellings reveal that people are directly involved in the initiation of a majority of these fires. Human error and human agency also are responsible for enhancing the damaging effects of some fires.

The background ideas just mentioned led to the commencement in recent years of investigation into human aspects of the causes and development of fire. In the United Kingdom, the Fire Research Station (FRS) has been instrumental in initiating most of the research programs in this field. The projects carried out by or on behalf of FRS fall mainly into the following areas:

1. Perception and acceptability of risk.
2. Value of human life.
3. Behavior of people in fires.
4. Building evacuation.
5. Human response.
6. Socioeconomic factors.
7. Weather conditions.

Various studies pertaining to these subjects are reviewed in this paper. The review does not cover some of the recent studies carried out by Jonathan Sime of Portsmouth Polytechnic School of Architecture, Portsmouth, England.

The Perception and Acceptability of Risk

Introduction

Work in this area was carried out between 1976 and 1981 under contracts from the Fire Research Station. The researchers were C. H. Green and R. A. Brown of the School of Architecture at The Duncan of Jordanstone College of Art, University of Dundee, Dundee, Scotland (Green is now at Middlesex Polytechnic, Enfield, Middlesex, England). Their investigation was restricted to accidental hazards that were not the results of malevolent actions. The main objective of their study was to develop methods for measuring life safety and to determine the amount worth spending to achieve acceptable levels of

risk [1]. This required research into attitudes to and perception of risk, strength of preference for different levels of safety, and monetary valuation of safety.

Definitions

Green and Brown have used the term *hazard* to denote any situation, activity, or agent, exposure to which would leave an individual open to the possibility of harm. The term *risk* covers a variety of harmful outcomes and their associated probabilities. A *measure of risk* then is some specific combination of outcomes and probabilities. For example, fire is a hazard, and two measures of risk to life from it are risk of death per year and risk of injury per year.

Method of Study

Data were obtained through questionnaires completed by students and civil servants from 1976 to 1981. Generalization of the results to the rest of the population is therefore problematic. Much of the analysis has been by statistical regressions and correlations.

Phrases such as *threat to health*, *threat to society*, and *acceptable risk* used in the questionnaires were not defined, so that people could interpret them as they thought fit. It thus was hoped to get a better idea of how people saw risk than if their answers were restricted to some extent by predetermined definitions.

Comparison of Risks

The first objective here was to find out how people decide that the risk from Hazard A is greater than the risk from Hazard B. Assessment of any measure of risk would be based on value judgements, due to an element of choice in including or excluding a certain component of risk, for example, injury or multiple deaths.

Hence it was impossible to say that any one measure of risk was more appropriate than another. It was therefore decided to investigate whether agreement existed between people as to the bases for comparing different hazards.

For comparing the "personal safety" of various activities, respondents wanted to distinguish two types of hazards:

1. Those which are largely immediate-in-effect (IIE), where the possible harm occurs at the time of contact.
2. Those which are largely delayed-in-effect (DIE), where any bad effects occur or become apparent some time later.

For IIE hazards, two quite separate beliefs about risk were found:

1. Personal safety—how safe an individual would be in an activity.
2. Threat to society—the threat posed to society by an activity.

Personal safety can be explained in terms of beliefs as to:

1. The chance of an accident occurring.
2. The conditional chance of death should an accident occur.

Table 1 gives figures from an early sample [2].

The threat to society can be explained by beliefs as to:

1. The annual number of deaths or the annual number of accidents.
2. The maximum number of deaths that might result from any one accident.

Table 2 summarizes beliefs as to the threat to society, the frequency of deaths and accidents, and the maximum kill size. Beliefs as to the threat to society (*TS*) are accurately predicted by

$$TS = 16.3 (\text{annual deaths})^{0.30} \times (\text{max. kill size})^{0.33}$$

The beliefs as to personal safety and threat to society are independent to some extent (weakly correlated).

TABLE 1—*Beliefs about personal safety.*

	Present Level of Personal Safety, (Geometric Means)	Chance of an Accident, Chance of 1 in: (Geometric Means)	Conditional Chance of Death Should an Accident Occur, Chance of 1 in: (Geometric Means)
Staying at home (fire)	163	2500	620
Living within 5 miles of a major airport	203	15000	180
Swimming in a swimming pool	212	3500	360
Staying at a hotel (fire)	262	1800	230
Travelling by train	272	3500	210
Living within 5 miles of a nu- clear power station	335	4100	130
Crossing the road	361	400	100
Travelling by coach	368	700	170
Living within 5 miles of a major chemical plant	376	5700	410
Driving a car	406	180	140
Skiing	512	220	790
Travelling by plane	525	4000	10
Riding a motorcycle	808	60	30
Rock climbing	1056	100	30

TABLE 2—*Beliefs about threats to society and their determinants.*

	Present Level of Threat, Medians	Annual Number of Deaths in UK, Medians	Average Deaths per Fatal Accident, Medians	Max Deaths per Accident, Medians	Annual Number of Accidents in UK, Medians
Snakebite	19	1	1	1	10
Lightning	27	3	1	2	20
Swimming in a swim- ming pool	30	11	1	2	53
Skiing	40	6	1	2	60
Rock climbing	43	12	1	4	95
Travelling by train	125	44	18	150	80
Travelling by coach	157	50	20	52	100
Home (fire)	167	100	3	9	190
Hotel (fire)	178	38	10	100	100
Motorcycling	179	350	1	4	600
Travelling by plane	189	30	40	275	70
Travelling by car	232	500	3	10	600
Crossing the roads	250	385	1	4	500
Accidental release of dangerous chemicals	456	13	5	1000	55
Accidental release of nu- clear radiation	576	2	2	5000	16

Accuracy of Beliefs

People's perceptions of risks need to be compared with objective estimates. The term *perceived* describes people's beliefs or estimates of risks. The term *objective* refers to official statistics, such as for road accidents and fires, and scientific estimates, such as for risks of cancer. Psychological experiments have shown that perceived and objective values of risks are related by a power law [3]

$$\text{perceived} = a (\text{objective})^b$$

Objective estimates were obtained from official sources of annual deaths in the United Kingdom from various hazards. Respondents then were asked independently for their perceived estimates. Results are plotted in Fig. 1 on a log scale. (Similar results have been found in the United States by Lichtenstein et al [4].) The best fit to the points is given by the equation

$$\text{perceived} = 4.33 (\text{objective})^{0.53}$$

A high correlation (0.92) obtained in this analysis suggested that the perceived ranking of the hazards was similar to the objective ranking. The value of 0.53 for the coefficient b , which was much less than unity, indicated that

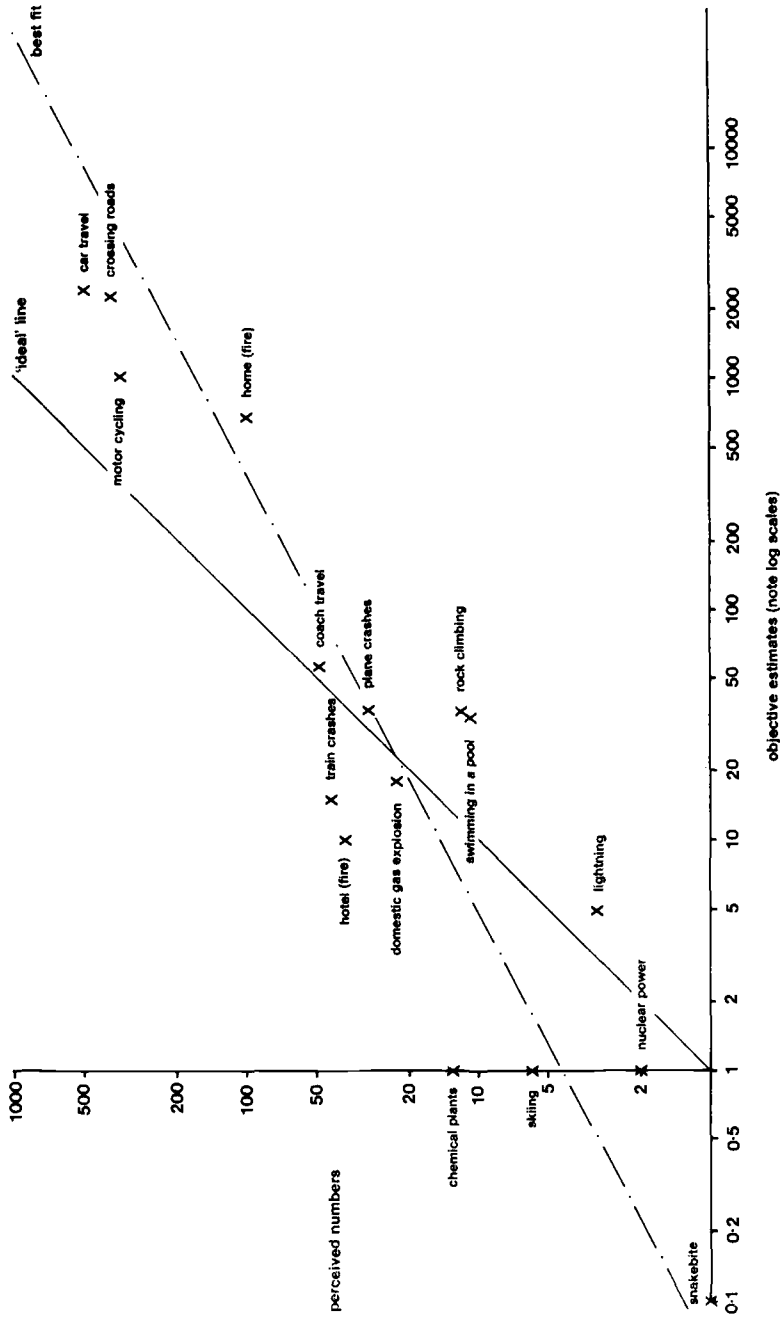


FIG. 1—Perceived and objective estimates of annual deaths (United Kingdom).

perceived estimates differed considerably from objective estimates. (If both the estimates were identical, the points in Fig. 1 would lie on the "ideal line," at 45° to both axes, for which b has the value unity. Respondents overestimated small risks and underestimated large risks.

The range of perceived estimates were much less than the range of objective estimates, indicating differences in scaling which to some extent could have caused the apparent discrepancies in the two estimates. It was likely that respondents differed considerably in their knowledge of objective risks, but their beliefs were, overall, accurate. But beliefs about risks from particular hazards were sometimes inaccurate due to special characteristics of the hazard or lack of information about the hazard.

Studies have revealed that awareness of a hazard and beliefs about the risk go together. People consider themselves most aware of those hazards from which they see the greatest risks. In one questionnaire, respondents were asked about a subjective feeling of confidence about their knowledge of real levels of threat from various hazards. In another questionnaire about levels of personal safety and the threat to society, they were asked to indicate the ranges within which they were very confident that the true levels fell. The range for an activity was called *uncertainty*. Results showed respondents to be not very confident and very uncertain. Also, confidence and certainty did not vary with beliefs as to the extent of risks. Even for commonplace hazards, respondents expressed a low level of confidence and high level of uncertainty as to their estimates of the risk. Using the Bayesian model,² it could be argued that the more certain individuals are as to the true level of risk from a particular hazard, the less effect any new information about the objective estimate of risk will have upon their beliefs. This result has an implication in planning publicity and safety campaigns concerned with the hazard.

Studies have shown that when people believe that risks are high, this leads to worry and anger—a rational response. Beliefs are not formed the other way around, with worry and anger at a hazard leading people to believe that the risks are high. Beliefs come from somewhere, and since we start, at some time in our lives, completely ignorant about the nature and potential of any given hazard, beliefs can evolve from experience. For some hazards like fire, learning will take place early in life. Beliefs about the extent and nature of risk from a particular type of hazard imply expectations as to the type and frequency of future accidents that will result from that hazard. For example, it was found that respondents believed that the person at risk has control over the extent of risk from dwelling fires [5]. This implies that those accidents that do occur should be expected to result from the carelessness of those involved [6].

²Brown, R. A. and Green, C. H., private communication, Building Research Establishment, Fire Research Station, Borehamwood, Hertfordshire, England, 1978.

Acceptability of Risks

Whether a risk is acceptable depends partly upon whether it is easily preventable and partly upon the types of accident to be expected. In a study concerned with fires in homes and hotels, the preferences of respondents between alternative preventive strategies suggested that the importance of preventing particular fires depended upon what can be done about them. To an individual, acceptable risk should imply a willingness to undertake the option carrying the additional risk; for example, to the driver of a car, overtaking has benefits which outweigh the likely risks. Thus, individuals will choose to undertake a given activity if they consider the risk acceptable, providing that they have a choice of whether or not to undertake that activity.

According to a theory proposed by Starr [7], for each hazard there is some acceptable level of risk, and this level varies between hazards according to some other factor such as the benefit or the voluntariness of the activity. Also, the acceptable level would be independent of the present level. But Green and Brown found that, for personal safety, what was stated as the acceptable level depended upon what was believed to be the present level. Hence, they considered three other models for determining the acceptability of risk:

1. The ignorable risk model: There is some level of risk below which any residual risk is ignored.
2. The acceptability ratio model: People specify a proportional change if the present level of risk is not acceptable.
3. The difference model: If the present level of risk is not acceptable, people specify a reduction depending on the present level.

The survey concerned with fires in homes and hotels showed that the acceptability ratio model was applicable for personal safety. Acceptable levels could be explained largely as adjustments to present levels with account being taken of proportion of population at risk. In standardized form, the regression equation for one study was

$$\text{Acceptable level} = 0.56 \text{ present level} - 0.48 \text{ percentage of population at risk}$$

No relationship was found between acceptable level and any measure of benefits. The inclusion of percentage of population in the equation suggests that judgements of acceptability are, even for personal safety, made in terms of social criteria. The negative sign for this factor implies that the more people who are at risk, the safer do respondents require the activity to be. The acceptability ratio model also was found to be applicable for *threat to society* and *threat to safety*.

Expenditure Preferences

Since some present levels of risk are unacceptable, the respondents were asked how they would divide between different hazards a sum of money available for reducing risks. Studies showed that, for personal safety, expenditure preferences, E , should be related to the difference, D , between present level and acceptable level. According to Melinek's analysis³ of the researchers' data, the best equation was

$$E = 0.35D + 0.17C - 0.12F + 3.05$$

where

C = the cost of prevention, and
 F = ease of prevention.

These results indicate that people are not acting as economic efficiency maximizers but are matching what they see as practical consequences with the costs of what they believe ought to be done.

For threat to society, expenditure preferences should be related to the acceptability ratio, AR , by an equation of the following form as determined for one study

$$E = 0.25 \text{ complexity} - 52AR + 50$$

The complexity variable differentiates those hazards in which accidents are believed to result from many complex events rather than from single simple events. However, the following equation was obtained in another study which included some additional questions

$$E = 0.16 \text{ concern} + 0.35 \text{ threat increasing} - 3.1$$

This result suggests that concern is the immediate response to high and unacceptable levels of threat, and that this, in turn, leads to decisions about the proportional change in threat required, the expenditure, and the willingness to pay.

Fires

The researchers wanted to explore whether, for achieving the maximum overall reduction in risk, people saw it as more important to reduce some accidents rather than others. For this purpose the researchers developed a series of questionnaires in which short descriptions of supposedly real fatal

³Melinek, S. J., personal communication, Fire Research Station, Borehamwood, Hertfordshire, England, 1979.

fires in houses and hotels were included. The respondents were asked how a fixed total of expenditure should be allocated between the fires in order to reduce the frequency and severity of each. It was found that a majority of the respondents did not take into account their beliefs about the relative frequency of each type of fire and the number of people who were described as having died in each type. Hence, most respondents were not acting as efficiency maximizers by allocating large sums of money to those areas where fires were most frequent or where kill sizes were the largest. Instead, the fires were clearly differentiated into groups: fires which were interpreted as due to the fault of those in the household with responsibility to prevent such events, and fires where blame and responsibility lay outside the household.

In cases where respondents blamed those involved, they tended to recommend that people should "take care," but in cases where they blamed others their recommendations were for technical "fix-its," either physical (for example, noninflammable wall linings) or procedural (for example, more tests). For dwelling fires, the respondents had a consistent preference for informational strategies like more television and newspaper advertisements and school classes rather than the "fix-it" strategies. For fires in upholstery due to careless disposal of cigarettes, the preference between options was in the following order: more publicity, warning labels on furniture, noninflammable furniture, a safety device in the heating system, and installation of smoke detectors.

Willingness to pay (WTP)

The respondents were asked to state what decreases or increases in their current weekly expenditure on risk reduction they wished to make for each hazard. The highest amounts were around 30 pence per week for safer nuclear and chemical plants. Respondents wanted less money spent on safety in some activities, for example, about 5 pence less per week to reduce the "threat to society" from rock climbing.

In the case of threat to society, the following relationships were estimated from two samples

$$\begin{aligned} \text{WTP} &= 0.18 \text{ concern} + 0.04 \text{ cost of prevention} - 7.5 \\ \text{WTP} &= 0.17 \text{ cost of prevention} - 51 \text{ acceptability ratio} - 44 \end{aligned}$$

Nuclear power stations and major chemical plants were included in the second sample but not in the first. For one study on personal safety, it was revealed that

$$\text{WTP} = 0.52D + 0.13\% \text{ population at risk} + 0.14 \text{ known hazard} - 13.4$$

where, as defined earlier, D denotes the difference between the present level and the acceptable level of risk.

Conclusions

People are fairly accurate in their perception of risks, in particular of the relative dangers of most hazards. People wish to divide hazards into two groups according to whether the effect of the harm caused is immediate or delayed. Acceptable levels of risk are related to present levels. A decision about the amount of money to be spent in reducing risks depends on what ought to be done and the cost or difficulty of carrying out prevention measures. People are unwilling to spend much money to reduce accidents for which the faults could be attributed to the victims. On the other hand, many do feel strongly about hazards which are outside their control. People believe that the best methods of prevention are education, publicity, and warning labels on furniture. People are quite reasonable in their beliefs about risk but seem less reasonable and consistent in their willingness to pay to reduce the risks from various hazards.

The studies do provide some indication of people's attitudes toward and perception of risk. But the results are based on samples of students and civil servants who had clear views about risk, which, hence, may or may not apply to a wider population. There is a need for large-scale surveys to find out if similar views are held more generally.

Value of Human Life

Introduction

An economic analysis of safety expenditure involves a consideration of the costs of various safety measures and the reduction in losses which can be expected by adopting these measures. The losses are of two kinds, property loss and life loss. To include both kinds in the analysis, a value must be placed on human life. In a recent paper [8], different methods of assessing the value of human life have been reviewed that describe the use of this value in fire protection economics.

Methods

The human capital approach is based on goods and services which a person can produce if he is not deprived by death of the opportunity to do so. The livelihood approach assigns valuations in direct proportion to income. Both methods allow deductions towards consumption and use discounted values. The value of life provided by these methods is generally small. Deduction for consumption is to some extent unethical. The third approach assumes that if an individual has a life insurance policy for x pounds, then he implicitly values his life at x pounds. A major drawback in this method is that a decision on sum assured is not necessarily made in a manner consistent with one's best

judgement of the value of one's life. The fourth method involves court awards to heirs of a deceased person as restitution from a party responsible for the fatality.

The fifth approach is the one widely adopted for valuing life; it is based on the money people are willing to spend to increase safety or reduce a particular mortality risk. The implied value of life revealed by WTP criterion would depend on factors such as acceptable expenditure per life saved, total level of risk, amount already being spent on safety, and earnings of the individuals. A major advantage in this method is that it would give a level of safety expenditure which people can be expected to accept or bear, thereby avoiding the disadvantages of compulsory regulations which are often complex and ineffective and which can destroy people's sense of responsibility. However, surveys carried out for collecting data under this method have provided variable and inconsistent results. Due to insufficient knowledge about the risk, most people find it difficult to quantify accurately the magnitude of a risk. Green has discussed in detail the rationality, uncertainty, and skepticism behind perceived risk [9].

Application

For the risk of being killed while crossing a road, studies on the use of a subway by pedestrians indicated a figure of £87,000 at 1973 prices for the implied value of life. At 1973 prices, estimated values of life based on mortality risks were £28,000 for smoking and £200,000 for industrial accidents. Based on all these studies, Melinek suggested a figure of £50,000 (at 1973 prices) for implied value of life for fire protection problems [10]. Fire fatalities tend to be the very young or the very old. Allowing for the subjective cost of injuries and ill health, Melinek obtained the following figures at 1973 prices: £74,000 for use of subways, £24,000 for smoking, and £161,000 for industrial accidents. He also estimated a figure of £14,740 for average discounted earnings, using average annual earnings of £1,920 for men and £1,060 for women; this estimate was considerably less than the value of £50,000 provided by the WTP method. Melinek also produced estimates for discounted earnings for different age groups and for males and females separately.

Cost-Benefit Analysis

The value of life was used in two studies concerned with cost-benefit analysis of fire protection measures. In the first study [11], the total cost of furniture fires was estimated and compared with a similar costing for all fires in dwellings (see Table 3). The cost associated with a particular type of fire represents the benefit if all fires of that kind were prevented. In practice, only a fraction of the loss would be eliminated, and the costs of protection should be balanced against this reduction in losses. Due to lack of data, the authors

TABLE 3—*Cost of furniture fires in dwellings and of all dwelling fires, 1970.*

Cost Component		All Dwelling Fires		Furniture Fires	
		Number of Fires	Cost, £m	Number of Fires	Cost, £m
Total cost (£ million)		...	54.5	...	18.8
Property damage	Total	45 305	19.9	10 530	4.4
Confined to the item first ignited		10 236	0.5	3 330	0.2
Spread beyond the item first ignited, but confined to the room of origin		29 818	8.9	5 991	1.8
Spread beyond the room of origin		5 251	10.5	1 209	2.4
Casualties: nonfatal		3 224	3.2	950	0.9
fatal		627	31.4	270	13.5

were unable to measure the effectiveness of all possible remedies, but they were able to draw the following conclusions by examining the maximum potential benefit. Action to reduce fatalities would have the greatest benefit, particularly deaths resulting from smokers' materials. For furniture already in use, the remedy suggested was to either reduce or eliminate the likelihood of ignition or to reduce the toxic hazard in the immediate vicinity of item ignited. This action would be preferable to the prevention of spread as most fatalities were found in the room of fire origin. It was clear that the expenditure on protection of individual items of furniture should be strictly limited, although selective spending may be worthwhile concentrating on those people most at risk, for example, the elderly.

The second study [12] was concerned with the use of foamed plastics as building material and the proposal that such materials be replaced by more traditional materials, for example, plasterboard. The following three remedial actions were investigated:

1. Replace foamed plastic by plasterboard.
2. Install plasterboard below foamed plastic.
3. Lay fiberglass above foamed plastic.

The results are reproduced in Table 4. The upper bounds of expected losses in the following two typical dwellings were calculated adding the corresponding total discounted losses (last column) shown in Table 4.

1. Top floor flat with two bedrooms: total discounted loss = £52.6 (1976 prices).
2. Large bungalow with three bedrooms: total discounted loss = £77.0 (1976 prices).

TABLE 4—1974 provisional data—houses, flats, maisonettes.

Room of Origin	Nu	Ns	Fs	Nc	Fc	R	AP	L	AL	AT	TDL
"Bedroom	38.2	829	65	9 428	194	3.4×10^{-5}	£0.51	0.078	£0.27	£0.78	£7.8
Kitchen	18.3	643	26	22 860	35	9.8×10^{-5}	£1.47	0.040	£0.39	£1.86	£18.6
"Living/dining	28.2	1065	163	9 984	177	5.5×10^{-5}	£0.83	0.153	£0.84	£1.67	£16.7
Bath/WC, cloakroom	28.2	59	2	561	1	3.1×10^{-6}	£0.05	0.034	£0.01	£0.06	£0.6
Hall, stair, landing	38.2	163	7	1 786	2	6.6×10^{-6}	£0.10	0.043	£0.03	£0.13	£1.3

NOTE—Nu = number of units (in million); Ns = number of fires that spread; Fs = number of fatalities in spreading fires; Nc = number of fires that were confined; Fc = number of fatalities in confined fires; R = risk of fire affecting = $Ns + 0.05 Nc/Nu$; AP = expected annual property damage = $R \times £15,000$; L = life loss per spreading fire = Fs/Ns ; AL = expected annual life loss = $R \times L \times £100,000$; AT = expected total annual loss = $AP + AL$; TDL = total discounted loss = $AT \times 10$.

"Including a proportion of "residential not stated."

In estimating the upper limit (most dangerous), it was assumed that a fire affecting a polyurethane ceiling will always spread, and that a spreading fire will cause the same number of fatalities whatever may be the ceiling material. For the two types of buildings, the average costs of remedial action per dwelling ranged from £130 to £930 at 1976 prices, which were considerably more than the discounted losses (maximum benefits) even when taking the most pessimistic view of the effect of foamed plastic ceilings. Hence, no remedial action was advocated on a cost-benefit basis.

The implied value of £100 000 for life used in this study was a subjective estimate. In order to test the sensitivity of this parameter, the calculations were repeated with the implied value of life as a variable. It was found that a decision to replace foamed plastic by plasterboard in a bungalow would imply the value of life to be £5m. Alternative propositions yielded implied values of £4.2m, £7.5m and £11m. Since such large values for life were unacceptable, the remedial actions were economically unjustifiable. Helzer, Buchbinder, and Offensend [13] have carried out a sensitivity analysis of value of life in evaluating alternative strategies for reducing upholstered furniture fire losses. It has been generally recognized that there is no such thing as a lump sum value of life—see, for example, Jones-Lee [14] and Mishan [15].

Human Behavior

Introduction

Compliance with the fire requirements of building regulations and codes would no doubt reduce the fire risk in a building and provide adequate means for safe evacuation. The success of this system, however, depends to a great extent on the behavior of building occupants at the time of a fire. Buildings contain a variety of people, some who will be able to escape in most circumstances, others who would have extreme difficulty, and those who will not attempt to escape. These facets of behavior are influenced by psychological, physiological, and circumstantial factors, as well as by the nature of the threat posed by the fire; also, they can be influenced by the building form and fire protection needed. Little was known about the behavior of people in fires, although considerable research has been carried out to quantify fire resistance, smoke spread, and fire spread. This gap in knowledge led the Fire Research Station to sponsor a series of studies of the behavior of people in fires. This section is a summary of these studies.

Pilot Studies—Loughborough

FRS research in this area commenced in 1969 [16] with an extramural project at Loughborough University on the behavior of people under stress. This work was initially concerned with a literature survey of studies falling broadly

into the categories of disaster research, psychological stress, physiological stress, and panic behavior. Interest was concentrated on three types of buildings: blocks of flats, hotels, and department stores. This study was followed by the collection of in-depth information from a small sample of fire incidents. A pilot study conducted in this connection, with the researcher (Wood) acting as interviewer, indicated a number of serious difficulties. Hence, a second pilot study was undertaken using a simple questionnaire and Fire Service personnel as the agents for data collection.

Large-scale study—Loughborough

As the technique adopted in the second pilot study proved to be feasible, a large-scale survey was carried out using a revised questionnaire; twelve fire brigades participated in this survey. Data collected were based on interviews with 2193 building occupants at the scene of 952 fire incidents. Behavior was examined at two levels: a general analysis of people's actions, and a more intensive study of two particular aspects, evacuation of the building and movement through smoke (see Wood [17] for details of this investigation). The main findings were as follows.

The most frequent course of action appeared to be directed solely to one end, either leaving the building or fighting the fire. There was little evidence of irrational, nonsocial behavior. The more serious a person considered a fire to be, the more likely that he would immediately leave the building, and the less likely that he would attempt to fight the fire. Familiarity with the layout of the building did not affect whether or not a person attempted to immediately leave the building. The more frequently people had received training or instruction on what to do in a fire, the more likely they were to raise the alarm or organize evacuation as a first action. Those who had been previously involved in a fire incident were no more likely to contact the fire brigade than those who had not. They were, however, more likely to fight the fire or minimize the risk in some way. They were less likely to leave the building immediately.

Differences in training only affected evacuation at lower levels of smoke spread; they had no effect upon either evacuation or reentry in night-time incidents. The presence of smoke increased the proportion of older people who left but did not affect the proportion who went back into the building. The time of occurrence of the incident did not affect whether or not people left the building. The more extensive the smoke spread, the more frequently exits other than normal were used.

Previous involvement in a fire incident by itself did not affect movement through smoke but increased the proportion of people moving through it in a work environment. Trained people were more likely to move through smoke when it spread less extensively and less likely to move through it when it

spread extensively. The combination of night-time and extensive smoke spread increased the proportion of people moving through smoke.

Wood made a special analysis of incidents involving casualties, drawing a comparison with incidents which did not involve injury. He found that the proportion of people who had never received training was significantly greater in casualty-producing incidents. Also, the proportion of people who knew a means of escape was significantly smaller, and the proportion who were completely familiar with the building was less in incidents where two or more people were injured. The time of the incident, the presence or absence of smoke, and the age of other people in the building appeared to have little effect upon whether or not casualties resulted. In casualty incidents, a smaller percentage of people contacted the fire brigade or fought the fire, while a larger percentage investigated, warned others, tried to save personal effects, and moved towards the exit.

Wood attempted to answer the criticism levelled at his questionnaire technique, which relied on what people said rather than what they did. He discussed how the validity of his data was checked. The sample used by him might be somewhat biased in that people interviewed tended to be those immediately involved with the fire, but in many incidents, particularly fires in houses, everyone in the building was interviewed. The least satisfactory aspect of the questionnaire was the unstructured questions relating to the courses of action.

Wood's "unexpected finding" included the popularity of fire fighting as a course of action. People were more likely to attempt to fight the fire if it was not thought to be very serious and when they thus judged that they had a good chance of extinguishing it. Frequency of fire fighting was not correlated with frequency of training, but little was known about the effectiveness of various types and frequencies of fire training. Women appeared to be more concerned with the safety of people, themselves included, in that they were more likely to warn others and evacuate family, in addition to being more likely to immediately leave themselves. In contrast, men seemed more situation-orientated, being more likely to attempt fire fighting or to minimize the risk. The frequency of going back into the building and moving into smoke also demonstrated sex differences, the proportion of men being significantly greater in both cases. Previous involvement in a fire incident appeared to reduce some of the stressful elements of the fire.

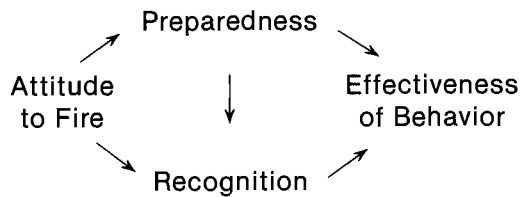
Wood carried out further analyses which he included in his Ph.D. thesis [18].

Pilot work—Surrey

Having gained experience of the pragmatic method adopted by Wood, the need for a structured and fundamental approach to the problem was seen. The FRS commissioned a study at Surrey University, Guildford, England, to

structure the problem in the light of modern psychological research, to review available methodology, and to suggest a strategy for future research on human behavior. The report [19] of this study reviewed existing sources of information and behavioral assumptions upon which fire regulations were designed, particularly in facilitating the unaided individual to escape from the building. It was argued that it was meaningful to think of escape behavior as one part of an overall response to fire. This would change the emphasis from an escape/no escape orientation to the possibility of multidimensional changes in behavior, some of which could eventually lead to escape. Some would lead to putting out the fire or other types of coping.

The approach just mentioned required the development of indices of effectiveness of behavior by giving weighted scores to particular actions. One of the first problems was to identify the dimensional structure of behavior in fire and, in so doing, set up methods for measuring it. It was considered valuable to build up descriptions of different patterns of action in fire and to organize these patterns onto scales indicating different degrees of underlying attitudes. The next problem was to relate measures of behavior effectiveness to psychological factors such as attitudes, preparedness and recognition, and physical factors concerned with the building form. With arrows representing possible correlations, not necessarily linear, a chart like this one was suggested:



Two problems were suggested as worth looking into: firstly, the relationship between various measures of building form and the effectiveness of behavior as mediated by the other variables; and secondly, the variations in the patterning of the correlations in various building forms. The just-shown linkage diagram was a suggestion as to what the key variables/dimensions might be.

The Appendix to the report just mentioned described first the difficulties encountered in studying human behavior in relation to fires. For example, the occurrence of fires is relatively rare and unpredictable. The crucial reactions occur over a very short space of time. Case studies, surveys, and field experiments (for example, fire drills) were the research procedures recommended to be carried out in a preexisting environment for studying the part played by real world contextual factors. Laboratory experiments and simulations also were suggested. Three broad types were identified for measuring human responses: verbal measures (for example, interviews), performance measures by giving people a task to perform, and actual observations of behavior.

The report just discussed formed the basis of a pilot study to determine patterns of behavior of people involved in buildings on fire. The overall objective was to develop a predictive model of behavior of people in fires based on their psychological characteristics. Central research questions were the extent to which behavior in fires can be explained in terms of (a) appropriate understanding of the fire situation by those affected; (b) accurate recognition of their role in the event; (c) effective understanding of the relevant rules for their roles; and (d) efficient performances of actions relating to their interpretation of the rules. In all four aspects, it was necessary to discover the contribution of the building and its fire protection to the outcome. In order to answer these questions, detailed case studies were carried out to obtain clear information on the sequence of actions which occur in a fire as well as details of the crucial early stages of fire development. Information was received from the fire brigades assisting in the study on the occurrence of fires. The study included fourteen domestic fires involving 41 people, eight multiple occupancy fires involving 96 people, and six hospital fires involving 61 people. In each of these cases, fire survivors were contacted who gave a detailed account of their actions in their own words. For a detailed summary of the results showing general trends, see Canter, Breaux, and Sime [20].

The technique adopted in analyzing the information collected was interesting and useful. The first stage of the analysis was to break down the data provided by the interviews with the fire survivors into single acts. A note was taken of the position in the sequence of events and the physical location of each act. These acts then were reduced for each fire to a standard "dictionary" of all acts carried out by each person in any given fire. Then each entry of the dictionary was related to the original breakdown of acts in order to ascertain which act had followed each of the other acts in the dictionary. A matrix was prepared with the same list of acts as both its rows and columns. Into the cells of this matrix were entered the frequency with which the acts in the columns followed those in the rows. This "transition matrix," which showed the likelihood of each act giving rise to every other act, was examined to determine those acts which were as likely to have followed as to have preceded each other at a point in the overall sequence. The acts were collected together into an "equivalence" group. It then was possible to determine the sequence in which the equivalence groups occurred. This process was described as "decomposing" the transition matrix. The resultant set of related act sequences was represented in "decomposition diagrams" which summarized a complex series of events in a visual form. This procedure was found useful for testing theoretical predictions and the statistical differences between frequencies of occurrence of various acts.

The report has demonstrated that a full and coherent account can be given of the sequence of actions which take place in fires without any need to refer to explanations drawing upon "panic" or "irrational" behavior models. It should be mentioned here that Sime [21] has discussed the concept of panic

and its use in different studies, newspapers, building regulations, and design literature. Panic is used as a description or evaluation of a state of anxiety or behavior on the part of an individual, group, or crowd. The anxiety or behavior is assumed to be irrational since its outcome is likely to be unfortunate. It is more important to consider under what circumstances behavior in fires is likely to be ineffective than to immediately attribute the behavior to panic. Behavioral sequences which are time-consuming and potentially dangerous appear to be a product of attempts to cope with ambiguous and rapidly changing information.

National Survey—Surrey

Following the general findings mentioned in the previous section, the Fire Research Unit of Surrey University carried out a national survey of behavior in fires. This survey utilized a questionnaire developed in a pilot study which was sent by officers in seven fire brigades to 1232 people involved in fires. Data provided by 585 completed questionnaires were analyzed on their own and in comparison with earlier surveys by Wood [17] and Bryan [22]. The results are given in an internal note⁴ (unpublished).

The structure of the questionnaire and its analysis concentrated on three main stages of behavior: recognition, communication, and action. The third stage included early actions, such as having a closer look at fire, calling the brigade, and warning others; it also included fighting the fire and escaping. Of the buildings included, 80% were domestic. Injury was reported only in 29 cases, and, of these, only 3 injuries were severe. Furthermore, only 40 people required help in leaving the building. Consequently, the results are generally applicable only to fires which are not serious. However, the results clearly indicate that the effective use of alarms, fire escapes, and first-aid fire fighting does relate to fires being less serious. The results also show that even in less serious fires there is some degree of ambiguity in the early stages of a fire and that people search out information about the fire. Problems of delay in recognizing that a fire is present also occur in less serious fires but not to such a large extent as in serious fires.

In regard to early action, about half the sample fought the fire and of these a quarter used a bucket (or container) of water and a further quarter a fire extinguisher, but one third used a variety of other means. Of those who knew that a fire extinguisher was available, 50% used some other means to fight the fire. Of those who did use an extinguisher, 11% were unable to get it to work. Sime, Canter, and Breau [23] have made a detailed analysis of the use of fire extinguishers in 13 fires. The results have been discussed in relation to the

⁴Canter, D., Breau, J. J., and Sime, J. D., private communication, Building Research Establishment, Fire Research Station, Borehamwood, Hertfordshire, England, 1983.

state of the fire upon discovery and three types of occupancies: domestic premises, multiple occupancies (for example, hotels), and hospitals.

The analysis just mentioned gave some indication that people have inadequate knowledge of the location of extinguishers. Current fire precautions suggest that the public's involvement in fire fighting is expected to be minimal in buildings where staff are employed. This, perhaps, accounts for the fact that people do not try to use extinguishers. Training of staff is important in occupancies which by their nature are less prepared to deal with emergencies. Significant improvements might be made in the ergonomic factors involved in lifting and operating different types of extinguishers and ensuring that they work when required to. However, the problems in extinguisher use cannot be resolved by modifying extinguisher design. There is some evidence that a preoccupation with fire fighting by members of staff can lead to a neglect of other priorities, notably calling the fire brigade and warning the public of a fire.

Chandler [24] suggests that extinguishers are possibly less likely to be used to tackle small fires. This might partly explain the lower success rate in extinguisher use than with other methods in hospitals. A detailed analysis of fire brigade statistics for 1969 revealed that occupants of dwellings were less successful with extinguishers than other first aid methods such as smothering, beating, buckets of water, and sand (see Ramachandran, Nash, and Benson [25]). If extinguishers are not used until a fire is relatively serious, then people need more idea of the capabilities of different types and sizes of extinguishers. There is a stage when a fire has become so serious that other actions inevitably take precedence.

Public Buildings

Commissioned by the Home Office, Canter investigated behavior of people escaping from fires in public buildings [26] (excluding institutions and hospitals) with particular emphasis on the behavior of those who are preoccupied or precommitted to certain courses of action at the time when a warning of fire is given. According to this study, delays in response to fire cues—which leads to a reluctance to evacuate—could occur due to a failure in actually perceiving the cues or due to a misinterpretation of the cues. Even recognition that a fire exists still may be interpreted as not a serious situation and not worthy of any evasive action. Delays in evacuation also could be due to individuals investigating the cause and state of a fire. In public buildings such as department stores, the causes of delay are likely to be associated with activities having a clear structure and lasting more than a few minutes (for example, a restaurant). A further increase in the probability of misinterpretation or in the ignoring of fire cues may be brought about by social processes in two ways: by being the member of a group, and by reliance of the public on the staff for information and of the junior staff on the senior staff. Of the “inef-

fective" actions recorded, more than a third were typically of the form of ignoring early warnings or misinterpreting those that were given. A number of victims did not have any knowledge of the appropriate action, including the appropriate way out of the building. Canter also discussed the implications of his findings for training and fire prevention.

Main Conclusions

1. The behavior of an individual can be modified according to the role in which he sees himself (that is, head of house, hotel guest, shopper, or staff member) and whether that role is part of a strong hierarchy. Confusion would result if the fire intelligence and action flow conflicted with the normal hierarchy.

2. The earliest clues to fires are often strange noises rather than flame and smoke. Early behavior is characterized by uncertainty, misinterpretation, indecisiveness, and seeking for confirmation.

3. Only act sequences which might be predictable are significant, since individual acts could occur at different stages of a fire.

4. Fire protection aids such as fire escapes and fire extinguishers are rarely used.

5. In the stress of a fire, people often act inappropriately and rarely panic or behave entirely irrationally. Such behavior, to a large extent, is due to the fact that information initially available to people regarding the possible existence of a fire and its size and location is often ambiguous or inadequate.

6. Lengthy delays can occur before people realize there is a fire. Even when people suspect that a fire exists, they are reluctant to do anything until they have checked, after which they will often try unsuccessfully to extinguish it rather than to raise the alarm or to escape.

The conclusions just mentioned have broad implications for policies concerned with building (fire) regulations and fire safety legislation that are designed to minimize the likelihood of "panic" and to reduce the possibility of people being trapped in the fire compartment. As Canter put it, it is possible that the same overall objectives of reducing loss of life and injury could be more effectively achieved with less disturbance to the building users and probably for the same or reduced capital costs if the regulations were reoriented to increase the likelihood of informed decisions being made by people in fires. The findings also have implications for fire prevention education and publicity, training and escape, and building design. Canter has prepared a report (under review by FRS) summarizing all these implications and outlining a model which sees human behavior in fires as a reasonable attempt to cope with rapidly changing ambiguous circumstances. The report also brings together an overview of studies of human behavior in fires carried out by Surrey University and others.

Current Research

It is extremely clear from the conclusions just mentioned (Nos. 5 and 6) and from earlier discussions that timely and convincing communication of the location and spread of fire to building occupants would be of great benefit in preventing those serious cases of unsuccessful behavior in fires, which, although rare, have led to most of the major fire legislation to date. It is therefore necessary to seek ways to modify human behavior through better communication in order to promote rapid, orderly, and safe evacuation of the building. Since there was no coherent research in this area, Surrey University, under contract from Fire Research Station, commenced in 1981 a study of improvement in fire alarm comprehension.

The first stage of the project just mentioned assessed, from existing literature and data, the order of effectiveness and likely success rates of all methods of raising fire alarms through automatic means. The study was concentrated on the evacuation of untrained personnel in emergency situations with particular attention to the accounts given of motivations and reasons for action taken. Particular account also was taken of the difficulty of comprehension of the existence of a fire. Reports based on this first stage work are currently being reviewed by the FRS for publication.

The first stage study just mentioned clearly indicated the need for automatic informative fire warning (IFW) systems such as BRESENS, which are being developed using microprocessing technology. Hence, under a contract of the FRS, the Fire Research Unit of Surrey University is carrying out an investigation to determine the nature of and effective methods of presentation of appropriate information through IFW systems, to ensure clear comprehension of alarm warning and rapid building evacuation. This research program includes simulation studies for message presentation (visual and verbal) in controlled laboratory conditions using microprocessor-controlled, systematically varied displays. In a fire situation, the amount of information that can be gathered is limited; the more time spent gathering information, the less time available for escape and other actions. Hence the message content is also being determined to assist message designers in producing messages that would achieve a correct balance between urgency and detail. As a complementary approach, case studies are being carried out to examine in detail IFW systems in use in existing organizations. For this purpose, the following techniques are being used: behavioral observations, questionnaire studies, and in-depth interviews.

As part of the project just mentioned, Tong [27] discussed the need for IFWs and investigated the psychological problems associated with the use of alarm systems. Research has demonstrated that in many cases inadequate behavioral responses were due to a failure of alarm systems to provide a clear and unambiguous fire warning. The credibility of alarms can be improved by providing the public with clear information about the existence of a fire, its size, and its location, which will motivate people to respond appropriately

and leave the building in an orderly and safe manner. A second difficulty derives from the long-standing philosophy that the public should not be given information about what is happening in a fire as this will provoke panic behavior. Panic behavior is not a direct consequence of receiving an alarm, but may occur if a late warning or no warning is given. In addition to the “technical level” (audibility and attention-capturing capacity), there is also a need to determine the “semantic level” of an alarm signal. The latter level denotes the precision with which transmitted symbols convey the desired meaning. There is also a need to assess the effectiveness of the received signal in guiding conduct in the desired way; research in this area must pay close attention to the psychological process involved in making decisions to fight the fire and to warn others.

Building Evacuation

Introduction

Traditionally, the concept behind means of escape provisions has been that individuals should by their own unaided effort be able to enter a place of safety or get out of a building by means of a staircase or other exit within a reasonable period of time. More recently, the nature of fires—particularly the growth of toxic smoke from fires—has suggested the need to rethink both the exit times and the measures to control the spread of smoke and fire. For large buildings, present British codes envisage evacuation of only part of the building, usually the fire floor and the floor above. It is necessary to assess the effects of making small changes in the time allowed for evacuation and the number of floors to be evacuated. It is also important to know how quickly people can be evacuated from a building or part of a building in an emergency. The FRS has investigated these two problems in-house as part of their contribution to behavioral studies.

Changes in Performance Standards

A decrease in overall evacuation time represents, potentially, a situation of greater safety, but this increase in safety would be balanced by an increased cost, either in construction or through loss of amenity. It was this cost which was the main concern of a paper by Melinek and Baldwin [28]. These authors derived an equation for the total staircase width required to provide an adequate safe area for evacuation purposes in multistory buildings. The staircase area was calculated as a function of the time allowed for evacuation to the staircase and the number of floors initially required to be evacuated. Estimates were made of the cost of the area occupied by the staircase.

The cost of the exits was found to be of the order of 1.5% of the rentable value of the building for a design evacuation time of 2.5 min, the time for

people to evacuate the fire compartment or the fire floor and the floor above. If these requirements were changed to provide enough standing space on the stairs for all building occupants, the cost was estimated to be about 2.5% of the rentable value or usable area of the building. This meant an additional loss of about 1% rentable value or usable floor space. The effect on the general evacuation of the building would be a reduction in time by just over 40%. However, for tall buildings, general evacuation times would still be long, and this decrease might therefore be of limited value. On the other hand, requirements might be relaxed by, say, requiring only one floor to be evacuated or by requiring evacuation of the fire compartment within 5 min. These requirements would reduce loss of rentable value to about 1%, but the general evacuation time would be about 30% higher if only one floor was to be evacuated or 70% higher if the design evacuation time was increased to 5 min. However, available data are insufficient to assess the value of regulations in saving life or whether more lives would be lost if the requirements were relaxed. Means of escape requirements are likely to affect staircase width only in multistory buildings with high population densities such as shops and offices.

Evacuation Times

Melinek and Booth [29] have carried out an analysis of evacuation times and of the movement of crowds in buildings. Using published data on crowd flows along corridors, the authors estimated that the normal capacity of corridors was about $1.5 \text{ people m}^{-1} \text{ s}^{-1}$; this value can be exceeded for a large rush of people. At low crowd densities (under one person per square metre), the speed of individuals is equal to their free walking velocity. The flow rate is a maximum when the crowd density is between one and five persons per square metre. For crowd densities greater than five persons per square metre, congestion exists and the flow rate decreases as the crowd density increases.

Also, for movement up or down staircases or both, as the crowd density increases, the flow reaches a maximum and then decreases. There is no reduction of flow for bends in the staircase. The capacity of a staircase decreases if a large distance has to be traversed and reduces markedly by two-way flow, which should be avoided where possible. Velocities of 1.3 m s^{-1} along corridors and 0.5 m s^{-1} on stairs (along the line of the stairs) can be assumed for unimpeded flow. Free flow at the head of the staircase can be assisted by a suitable system of barriers. At high densities, the flow of a crowd can become blocked by a series of "arches" formed at a doorway or at the head of a staircase. "Stable arches" can lead to a crowd trampling over people who have fallen. The frequency of arch formation is approximately inversely proportional to the square of the exit width.

The maximum population M which can be evacuated to a staircase, assuming a permitted evacuation time of 2.5 min, is given approximately by

$$M = 200b + (18b + 14b^2)(n - 1)$$

where

- b = the staircase width in metres, and
- n = the number of stories served by the staircase.

For staircase width of up to 2 m, this formula is in good agreement with the following equation represented by Table 2 of the Greater London Council code of practice for buildings not exceeding 30 m in height

$$M = 200b + 50(b - 0.3)(n - 1)$$

The recommended values of M are as follows:

Buildings without compartment floors between stories (Department of the Environment)	222 <i>b</i>
Buildings with compartment floors between all upper stories (Department of the Environment)	111 <i>bn</i>
Office buildings (Greater London Council)	(100 <i>b</i> + 10) <i>n</i>

Staircase widths calculated on the assumption of a population density in shops of 0.14 to 0.50 persons per square metre are likely to be adequate in normal circumstances. For a multistory building with n floors, Melinek and Booth derived the following formula for the minimum total evacuation time T_r for the population of floor r and above

$$T_r = \left(\sum_{i=r}^n Q_i \right) / (N^1 b_{r-1}) + rt_s$$

where

- Q_i = population of floor i ,
- b_r = staircase width between floor $r - 1$ and r ,
- N^1 = rate of flow of people per unit width down the stairs, and
- t_s = time for member of unimpeded crowd to descend one story.

The equation just mentioned gives n values of T_r ($r = 1$ to n). The minimum total evacuation time for the whole building is equal to the highest of these values of T_r . If there are several staircases, b_r will be the total width of the staircases considered. Using the formula just mentioned, evacuation times were calculated assuming that the populations and staircase widths were the same for all floors and $N^1 = 1.1$ persons $\text{m}^{-1} \text{s}^{-1}$. In most cases of the eleven office buildings considered in the study, the observed evacuation times were greater than the calculated values, the average difference being about 2 min. The calculated times ranged from 1.8 min for a six-floor building to 9.9 min for a 25-floor building.

Evacuation Model

Three carefully constructed evacuation exercises were carried out by the FRS at a London hospital in 1975 as part of an investigation into the provision of fire safety in hospitals by the Department of Health and Social Security (for details see Hall [30]). In an internal report, Appleton and Quiggin⁵ analyzed the information collected during these exercises and developed a model of the evacuation process and a method of estimating evacuation times. The results given by the method were compared with those observed during the evacuation exercises. The predictions were found to be generally, though not excessively, too low. This probably was due to the lack of any allowance for inefficiency, congestion, and resting times. However, both the exercises and the model showed that if the first exercise was a simple horizontal evacuation to the next ward, then the (estimated) evacuation time would have been just 7 min. For clearing the ward completely, it took 20 min during the actual exercise. It was concluded that the time scales of the hazards faced in a ward during a single bed fire were such that vertical evacuation (that is, to another floor level) of bed-bound patients would be completely inadequate to maintain safety.

Human Response

Introduction

The part played by legislation in the design and construction of buildings as well as in the control of their contents is widely recognized as a major influence in fire protection and safety of personnel.

Extensive investigation of actual fires by FRS has shown clearly that people's behavior and response before and during the incident often can be an overriding factor.

In recognition of the human factor, therefore, considerable attention is currently being paid to the study of people's environmental background, their actions prior to ignition, their involvement in it, their contribution to the fire's growth and spread, and their response to discovering the fire, fighting it, escaping from it, or aiding others to do so. These features are being analyzed as an integral part of the current FRS program of investigating serious fires in domestic and group-residential buildings such as hotels, hospitals, nursing homes, and residential care premises [31,32].

Environmental Background

The influences of socioeconomic factors and weather conditions on residential fires are being studied in order to identify sections of the population at

⁵Appleton, I. and Quiggin, P., private communication, Building Research Establishment, Fire Research Station, Borehamwood, Hertfordshire, England, 1978.

greater risk than the majority. Statistical analyses carried out in this respect are reviewed in the next section. Unpublished analyses of domestic fire fatalities nationwide over a recent year have substantiated a similar trend when expressed in terms of the dwellings' tenure, age, and type of accommodation and have even suggested a regional bias. Elderly people and, to a lesser extent, the very young have been shown to be more vulnerable to death from domestic fires. The correlation between disadvantageous socioeconomic factors and fires in domestic premises has not yet been confirmed by more rigorous statistical analysis, though residence within a hospital, nursing home, or similar institution implies another form of disadvantage.

Actions Prior to Ignition

Field investigations are revealing the influence of people's actions in causing or contributing to a fire's initiation or impeding subsequent escape from it. Actions so far encountered can be categorized by one or more of the following descriptions: thoughtless, disobedient, neglectful, ignorant, intemperate, careless, well-intentioned, and, of course, malicious.

Involvement with Ignition

The study of actual (not experimental) fires reveal that in cases where anyone is present at ignition and detection stage they are nearly always incapacitated in some way by alcohol, mental or physical retardation, sleep, or a combination of these conditions. Those present whose responses are not thus impaired are usually arsonists. The most common condition when the fire is detectable is that nobody is in the room of its origin, not even the person who might unwittingly have caused its ignition. Actions of people involved with ignition so far met can be considered thus: thoughtless, disobedient, neglectful, ignorant, intemperate, careless, and malicious.

Contribution to Growth and Spread After Ignition

In practice, fire growth and spread is almost always due to doors or windows being left open or opened at a crucial stage in the fire's development. Such occurrences are usually the result of escapes, fire-fighting, or rescues. As such they can be attributable to people's good intentions, carelessness, ignorance, and occasionally necessity. There have been cases of a left-open door to the room of fire-origin being propped open by falling debris or jammed shut by the occupant vainly trying to escape and collapsing behind it. The former allows spread, the latter prevents rescue.

Response to Learning About the Fire

1. *The Senses*—Initial response may be triggered by four of the five senses: sight, hearing, smell, and touch. Studies of actual fires suggest that sight is the most common, followed by sound, with smell and touch much less often. Visual detection of smoke and less frequently of flames predominates in domestic fires. Several group-residential buildings in which fires were detected by sight had audible alarm systems installed, and in others flames rather than smoke were seen. All group-residential buildings in which fires were heard had audible alarm systems, though in one case the sound of a bang preceded the sounding of the alarm bell. Sometimes, the alarm bell is ignored due to the “normality” of false alarm sounding. In a domestic example, an endangered couple were roused from sleep by the rattling of cans outside their flat by policemen. In another domestic fire detected by smell, the sleeping occupant was awakened by a choking sensation from smoke emanating from downstairs. In a group residential fire thus detected, the man who smelled smoke occupied a neighboring building, and his response preceded the raising of the alarm installed in the hotel of fire origin. A fire detected by touch resulted from a member of staff in an old persons’ home investigating the reason for the secondary lighting having flashed on. Approaching the fire door, she went to open the door, felt it was warm, and closed it again quickly before asking the warden to telephone the fire brigade.

Responses of those discovering the fire independently of the first person, simultaneously or after, are less well documented. The discovery is usually by sight or sound as it is if knowledge of the fire is received via the initial respondent. In one case a noise stemming from the fire was ignored when it was thought to be attributable to a slamming door.

2. *Nature of Response*—In general terms, occupants of residential buildings respond to real fire situations by one or more of the following: seeking it out to confirm its existence; fighting it; telling others about it; escaping from it; and assisting others to escape. Their choice of these reactions or the priority they place on each varies according to their perception of danger, responsibility, and location. In cases where an individual is incapacitated mentally, physically, by sleep, or by alcohol, the ability to respond normally is often dulled or frustrated as a consequence of impaired senses.

3. *Seeking Out the Fire*—Of those fires where the initial human response is to seek it out, hotel staff (particularly management) seem more likely to investigate (or delegate the job to others) than their guests. In domestic fires it is the man who confronts the fire; he usually beats a hasty retreat and delegates the job of phoning the FB to his wife or others. A nursing auxiliary’s response to seeing a glow in an occupied ward cubicle was to enter and attempt to remove a blazing chair. At a nurses’ home, it was the most fire-conscious young woman who became the fire’s only fatality when she apparently went to investigate the source.

4. *Fighting the Fire*—People do not often fight the fire immediately, perhaps because of more pressing priorities. Those whose initial response is to fight the fire are in the minority. The nursing auxiliary's attempt to remove the chair is an unusual example. Hotel staff appear to operate extinguishers only as secondary responses to ensuring the safety of their charges. Those endowed with similar responsibility in halls of residence also relegate the action to a low priority. Staff and students have joined in, enthusiastically emptying 20 water extinguishers from the relative safety of a yard outside the building on fire. Where the domestic fires evoked the fire-fighting response, it was an immediate one: common factors were an externally seated fire, a recognition that they had caused it, and easy access to a garden hose or water supply.

5. *Escaping from the Fire*—Escaping from the fire seems to be the most common response. Generally, those threatened by a fire elsewhere seem more willing to stay in their room than those assembled outside the building who often urge them to jump. Their willingness to leap from windows not only reflects the degree of perceived danger from the fire but also the height above ground and the athleticism or youth of the jumper. In one hotel fire, the only guest to leave his room in order to follow the recommended alternative escape route became the sole fatality. An old woman insisted on returning to her attic bedroom to retrieve cherished belongings before finally being overcome by smoke there. A mentally handicapped, though mobile, patient's response to a fire in his ward was to stay in bed and draw the covers over his head. Two mentally retarded young men chose to flee from their bungalow without giving assistance to their chronically sick mother who had to make her own way to safety. Two men on a drinking session were too inebriated to respond positively to a fire they started in their hotel room and so died there. A number of hotel guests following a signalled escape route faltered at a bolted emergency exit, unable to work out how to operate the lock. Some guests in the same fire, once their own safety was ensured, gave priority to moving their parked cars to keep them from being damaged by fire brigade operations.

6. *Letting Others Know*—Telling others about the fire can be for the purpose of warning potential victims or raising the fire brigade directly or by delegation. Warning potential victims can be from quite disinterested parties, such as the neighbor who came across to the residential home to knock on the door to tell them she'd seen smoke coming from a window. Similarly, a passerby out walking a dog knocked at the door of the house and shouted warnings to the sleeping occupants that the house was on fire. On the other hand, the sister of a woman whose house was on fire across the road chose to phone the fire brigade before crossing to check on the safety of her relatives. When a mentally handicapped patient saw the early stages of a fire in a bed, his response was to leave the ward, go downstairs, and tell the staff nurse on duty.

The tendency to delegate responsibility for phoning the fire brigade is quite

pronounced for hotel managers, senior officers, and even husbands, who then deal with what they see as more immediate tasks.

7. Assisting Others to Escape—Those with responsibility for people in normal day-to-day life seem to apply that role equally in fire situations by assisting them to escape. Thus, the duty nurse heroically enters and reenters a smoky ward to rescue patients, and the husband of a bed-bound woman carries her down the stairs and through smoke to safety. Those enjoying special relationships do likewise. The old man who is so fond of his granddaughter goes across the landing towards the fire with the intention of rescuing her, but in the end both die. The younger teachers in a residential block have a special respect for one of their number who is old and frail. Their immediate concern is for him and his safety. Sadly, it is the old frail man who dies in the fire, and one of those helping him is rendered unconscious by the smoke and toxic gases pervading their escape route. Finally, members of staff seem to respond well to their roles of leading guests on specific floors to safety in hotel fires where evacuation procedures are well understood or rehearsed.

Socioeconomic Factors

Introduction

Various studies carried out in the United States have demonstrated that fire incidence (with its consequences in terms of deaths and injuries) is related, though this does not necessarily imply causality, to a combination of factors (see, for example, Bertrand and McKenzie [33], Munson and Oates [34], and Gunther [35]). These factors reflected poor and substandard housing, overcrowding, social class, race, lack of family stability, and proportions of the young or elderly in the population. The only detailed analysis on these human aspects of fires in Britain related fires in London to data on housing and social conditions provided by the 1971 Census [36]. A recent paper has collated some of the more recent studies with reference to fires in Birmingham and Newcastle-upon-Tyne and updated results of the London analysis [37].

Metropolitan Counties—Fire Situation

Data clearly indicate that fire and casualties are a more serious problem in metropolitan counties than in nonmetropolitan areas. Metropolitan areas generally also have higher crime rates than elsewhere in England. A Scottish fatality study suggested that a causal relationship exists between fire fatalities and the blood alcohol content in their bodies.

Metropolitan counties could reduce the fire incidence in their areas through fire prevention campaigns and other measures. This can be achieved

by reducing the frequency of fires caused by children, malicious ignition, or smokers' materials—some of the types of fire which reflect carelessness or lack of vigilance or supervision. Such preventive measures could result in a potential saving of £17.5m in direct fire losses in metropolitan counties.

A strong correlation (0.74) exists between malicious fires and serious offences. Similar correlations exist between serious crime rate and fires due to both smokers' materials (0.67) and children (0.60). However, the real problem areas may be localized. Care also must be taken in interpreting these correlations since they do not imply causality.

Metropolitan Areas—Housing and Social Factors

The earlier London investigation conclusively demonstrated the existence of correlations between fire incidence and housing and social factors. Of the housing variables examined, tenancy (owner occupied, private rented, etc.) and the lacking of amenities were the most strongly correlated with fire incidence. If the underlying cause is carelessness, people who own their own homes might be expected to be more careful than those who live in rented properties. The social indicator most strongly correlated with fire incidence was proportion of children-in-care, which was thought to reflect family instability. Fire frequency appeared to be independent of rateable value per person and the age distribution of the population. However, the incidence of casualties was generally highest among the young and elderly.

The Birmingham and Newcastle analyses were based on small numbers of fires and hence could not confirm the existence of correlations similar to those shown in the London analysis. The housing and social factors used in the three surveys varied according to the availability of data; however, the overall conclusions in all three surveys were reasonably similar.

The observed correlations between fire incidence and socioeconomic parameters are reproduced in Table 5.

Conclusion

Stress, which is not easily quantifiable, could be a primary factor responsible for carelessness leading to increasing fire incidence due to this cause. Stress could result from socioeconomic factors such as poor housing conditions and low income, both of which are interrelated. Personal circumstances force people into sharing facilities or overcrowded households. Here, personal conflicts and tension are more likely, with an increased risk of carelessness and fatalistic attitudes reflected in high fire incidence due to children, smokers' materials, and malicious or doubtful sources. High fire and crime rates both could be functions of an unquantifiable lack of social identity and stability.

TABLE 5—*Correlation coefficients between domestic fire incidence and housing/social parameters (London, Birmingham, and Newcastle).*

Housing and Socio-economic Parameters	London		Birmingham	Newcastle	
	1971	1981	1978-1979 (2 Calendar Years' Data)	1979- 1980	1980- 1981
% of owner occupiers	-0.92	-0.92	-0.64	-0.22	-0.51
% of private rented housing	0.83	0.64	0.28	na ^a	na
Population density/hectare	0.86 ^b	0.84 ^b	0.22	0.02	0.02
% lacking at least one of three basic amenities ^c	0.82	0.70	0.26	0.05 ^d	0.01 ^d
% of overcrowded houses (more than 1.5 persons per room)	na	na	0.64	0.18 ^e	0.34 ^e
% in shared households	0.69	0.65	na	na	na
% expressing dissatisfaction with their housing	na	na	0.60	na	na
% in socioeconomic groups IV and V	0.71	0.62	0.66	0.34	0.59
% of people unemployed	na	0.89	0.47	0.40	0.61
Proportion under 15 years old	-0.43	-0.41	0.54	0.12	0.07
Proportion aged 65 and over	0.13	0.04	na		0.00 ^p
% of children in care	0.94	0.90 ^g	na	na	na
% of illegitimate births	0.92	na	0.54	na	na
Proportion of people of African or Caribbean origin	0.64	0.44	0.52	na	na
Proportion of people born in the Indian subcontinent	0.13	0.07	na	na	na

^ana = not available or not analyzed.^bPer "residential hectare" in London.^cExclusive use of fixed bath, hot water, and inside water closet.^dLacking a bath only.^eCorrelation with more than 1.0 persons per room.^pp = pensionable age (men 65, women 60).^gRelated to 1971 data, 1981 data not being available.

Future

Future research should be concentrated on identifying localized high fire risk areas for enabling publicity campaigns to be concentrated in such areas. There is a need to assess the effectiveness of such campaigns (for example, by television, house-to-house visits). There had been a number of campaigns carried out in collaboration with the Home Office with varying success in reducing domestic fire frequency (for example, in Leicester [38]).

In the analyses mentioned in this section, socioeconomic and fire data were aggregated for boroughs (London), fire station areas (Birmingham), and electoral wards (Newcastle). It was difficult to establish causality in these studies, although significant correlations with fire incidence were obtained for a number of socioeconomic factors. Hence, in order to confirm or sharpen

these findings, attempts are being made to compare the socioeconomic data for households that had fires with such data for other households. Such an approach depends on the availability of property-related data, which only a very few local authorities appear to have collected.

Weather Conditions

Introduction

The severe winter during December 1962 and the first quarter of 1963 resulted in an increased number of deaths in fires [39] and of multiple death fires [40]. A detailed statistical analysis [41] revealed a noticeable rise in fire incidence in London, Birmingham, and Manchester during the first quarter of 1963, particularly in the number of fires due to oil space heating, fires in grates, etc. The severe winter of 1978–1979 provided another opportunity to check earlier findings and assess changes since 1963. Chandler has discussed in detail the results of this investigation [42].

Data and Methodology

Data for fires in dwellings attended by fire brigades during the period 26 February 1978 to 28 April 1979 were tabulated on a weekly basis and classified according to Meteorological Office regions. Data were obtained particularly for major sources of ignition and sources such as space heating and electric blankets and for fires involving casualties, rescues, and escapes. For each region, weather data from one weather station were obtained and were assumed to reflect the average weather conditions prevailing in the region. For each weather station, weekly averages were determined for minimum daily temperature, rainfall, hours of sunshine, wind speed, vapor pressure, and relative humidity. Using these factors as independent variables, a multiple regression analysis was performed with each of a number of fire incidence variables as dependent variable.

Results

Since the 1963 survey, fires resulting from oil heaters and open coal fires have decreased. This reflects the fact that there has been nearly a ten-fold increase in usage of central heating in dwellings since that winter. However, there was a sharp increase in numbers of space heating fires at the start of the first cold spell (late November 1978) and also at New Year. A possible contributory factor was the use of portable appliances, some with electrical wiring, to supplement central heating at the onset of very cold weather.

The correlation coefficients, based on the full 61-week period, between ma-

TABLE 6—*Correlation coefficients between major sources of ignition and weather factors.*

Source of Ignition	Weather Factors					
	Temperature	Rainfall	Sunshine	Wind Speed	Vapor Pressure	Relative Humidity
Total fires	−0.87	−0.03	−0.29	0.03	−0.79	0.38
Fires due to:						
Space heating	−0.90	−0.19	−0.52	0.22	−0.92	0.52
Electric blanket	−0.86	−0.14	−0.48	0.20	−0.88	0.48
Wire and cable ^a	−0.84	−0.26	−0.45	0.16	−0.82	0.39
Smokers' materials	−0.30	−0.37	0.32	−0.07	−0.31	−0.32
Cooking appliances	−0.06	−0.26	0.05	−0.35	0.03	0.12
Life risk fires ^b						
All sources of ignition	−0.79	0.14	−0.39	0.03	−0.75	0.48
Space heating	−0.90	0.17	−0.44	0.03	−0.86	0.50
Smokers' materials	−0.36	−0.04	0.08	−0.11	−0.32	0.09
Cooking appliances	0.26	−0.001	0.25	0.11	0.25	0.02

^aIncluding leads to appliances.^bFires involving casualties, rescues, escapes, or evacuations.

major sources of ignition of fire and weather factors in England and Wales are reproduced in Table 6. Correlation coefficients of absolute values greater than 0.33 are statistically significant at the 1% level. Following preliminary examination of the data, logarithmic transformations were used on data relating to fire incidence but not life risk fires.

For England and Wales, temperature or vapor pressure was a very prominent factor in all the "best fit" regression equations obtained for the various sources of ignition given in Table 6. The addition of other weather factors correlated with fire did not improve these regression relationships. In all regions, including Scotland, temperature and vapor pressure were significantly correlated at 1% with total fires and fires due to space heating, electric blanket, and wire and cable including leads. The same was true of life risk fires due to space heating. Fires due to cooking appliances, the major cause of domestic fires, were not generally influenced by severe weather conditions.

The analysis suggested that in the temperature range 0°C down to −5°C there were an extra 30 fires per week in England and Wales for every degree drop in temperature. This result was in general agreement with the assessment based on fire frequencies for the 1962–1963 winter. Fires involving casualties, rescues, or escapes were dependent on temperature in most regions and even more so those fires due to space heating. The most vulnerable age group during the cold spell in early 1979 was those aged 65 and over, especially females, who were usually alone in the room of fire origin.

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Summary

The symposium on Fire Safety: Science and Engineering was organized with the intent of offering an up-to-date review of the results of fire research. The papers presented at the symposium cover a gamut of topics: fire risk, performance standards, fire modeling, fire safety of structures, flame spread, the smoke problem, sprinkler, and human aspects of fires.

The keynote paper by H. E. Nelson deals with a newly emerged branch of engineering: fire protection, the teachings of which are available now to all who care to use them. Not too long ago codes and standards were based largely on experience and judgment and allowed little flexibility in accommodating new designs. Basic research has yielded a pool of information sufficient to allow many important decisions on fire safety to be made on solid scientific grounds. The key to the success of this new branch lies in the bridging of the communication gap between the researcher and the user of his information.

E. W. Marchant lists 14 aspects of building performance. The fire safety performance interacts with all other aspects. There is a trend in building regulations whereby the conventional prescriptive approach is giving way to the performance concept, which allows flexibility in complex situations. The movement toward that approach is almost complete in England and Wales.

J. R. Beyreis and G. T. Castino argue that fire test methods and building codes and standards have been based mainly on experience. They claim that fire risk assessment is an orderly process relying on the integration of fire test methods and building codes and standards as a means of responding to experience.

The next five papers deal with preflashover fires. B. T. Lee presents experimental results on various interior finish materials obtained by using the proposed ASTM standard room fire test, entitled, "Proposed Standard Method for Room Fire Test of Wall and Ceiling Materials." Employing three different fire scenarios, he finds that, although all three are satisfactory for differentiating between the finishes, one allows a very comprehensive evaluation. The heat flux incident on the floor and the doorway air temperature appear to be the most consistent parameters for evaluating fire buildup and flashover.

S. E. Magnusson and B. Sundström present a computational technique for predicting, from the results of small-scale experiments, fire growth in a room

lined with combustible materials. The model regards the fire process essentially as a concurrent flame spread phenomenon. The results of seven full-scale tests are used to define some undetermined parameters. Further experimentation is needed to assess the validity of the technique.

The problem of fire development in a room and the subsequent spread of smoke and toxic gases is discussed by W. W. Jones. An improved numerical scheme is presented which is considerably faster and more "rugged" than previous techniques used to follow up the process.

A. M. Kanury presents a preliminary study on the scaling of compartment flashover. It provides a framework for synthesizing experimental observations on flashover.

A stochastic model of fire growth, developed by G. Ramachandran, regards the growth of fire as consisting of various states. The model, based on data furnished by fire brigades in the United Kingdom, is capable of predicting the probability distribution of fire duration in each of the various states and the probability of transition from one state to another.

Two papers deal with postflashover fires. That by U. Wickström proves that all fires can be described by a single characteristic curve, which takes account of the influence of ventilation and wall properties in terms of a modified time. This curve is considered an improvement over standard temperature-time curves, such as that of ISO 834.

T. Z. Harmathy and J. R. Mehaffey discuss the normalized heat load concept of calculating the destructive potential of postflashover fires with the aid of six input variables, two of which are random. The authors outline two methods of designing building elements for prescribed levels of fire safety. Using these methods, decisions on fire safety can be based on economic considerations: balancing the cost of fire protection against the value at risk.

The calculation of the fire resistance of building elements is discussed in three papers. T. T. Lie and T. D. Lin present a numerical scheme for determining the fire resistance of reinforced concrete columns. The calculation scheme has been based on a series of experiments in which the effect of load, column size, and type of aggregate proved to be the variables having the largest influence on the column performance.

The structural response and fireproofing requirements for steel frame structures under different fire exposures are discussed by B. Bresler and R. H. Iding. Using analytical techniques, they show that some realistic fires, such as long-duration, low-intensity fires or short-duration, high-intensity fires, may produce lower thermal and structural distress in steel beams than a standard fire exposure of ASTM Method for Fire Tests of Building Construction and Materials (E-119-83). They suggest that the fireproofing requirements be developed using appropriate fire categories.

D. C. Jeanes claims that the structural performance of steel-framed floor systems can be predicted more accurately with the FASBUS II computer than

with the traditional ASTM E 119 method. The computer code can simulate the fire conditions more realistically and will provide the designer with more information.

An experimental procedure for measuring some material properties related to ignition and flame spread is described by J. G. Quintiere and M. Harkleroad. The apparatus uses a radiant heat source. The results appear to yield phenomenological constants on the ignition and flame spread characteristics of the materials, rather than factors dependent on the apparatus.

V. Babrauskas and J. F. Krasny show that it is now possible to predict, from simple bench-scale tests, whether the ignition of a single upholstered item can lead to flashover. Experiments using the cone calorimeter can supply the information needed in the prediction.

The smoke problem is discussed from three different points of view. D. D. Drysdale and F. F. Abdul-Rahim show that the quantity of smoke released in a compartment fire depends not only on the nature of the material but also on the conditions of burning. The yield of "cold" smoke appears to be greater than the quantity predicted from the dynamic measurement of hot combustion products issuing from a test rig.

Talking about heating, ventilating, and air-conditioning (HVAC) smoke management systems, L. Milewski points out that today's HVAC systems have been optimized to do their specific tasks. If intended to play a role in smoke management, their design should reflect that added role.

L. Y. Cooper introduces the concept of smoke compartmentation and discusses the importance of cross-door pressure differential in the spread of smoke. He reviews existing and potential test methods for measuring the leakage characteristics of doors.

The toxic hazards from fires in furnished rooms are analyzed by A. F. Grand et al, based on the results of full-scale experiments. The toxic hazard appears to increase drastically a few minutes before flashover. The authors believe that fast-acting sprinklers would prevent any significant toxic threat.

The development of the early-suppression, fast-response sprinkler is dealt with in C. Yao's paper. In the design of sprinkler systems, a balance has to be struck between three important parameters: convective heat release rate, actual water delivery density, and required water delivery density. In an extensive program initiated in the author's laboratory, the criteria of early suppression will be defined and used in the development of fast-response sprinkler systems.

Some aspects of the flammability of liquids are discussed by R. Munoz-Candelario and N. J. Alvares. They present experimental data on vapor concentration gradients above liquids. They show that such information is necessary for defining safe separation between liquid spills and ignition sources.

Based on studies conducted in the United Kingdom, G. Ramachandran discusses some human aspects of fires in buildings. These aspects fall into

seven areas, covering, among others, such dissimilar factors as perception of risk and weather conditions.

It is hoped that the designers of fire safety features of buildings will find this volume beneficial in their work.

T. Z. Harmathy

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