Fire Safety of External Building Elements—The Design Approach

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The adoption of a design approach to fire safety in buildings has gained increasing acceptance as it has become clear that the traditional rules are not always sufficiently flexible or may not be adequate to cope with modern developments in the use and construction of buildings. For elements of structure, building codes have a performance requirement based on a specified exposure period of the element to the standard fire resistance test of ASTM E119, and while it has been recognized that the building fire exposure may not be the same as the standard fire exposure, the tendency has been more to criticize the required period of fire resistance than to question the standard test conditions themselves. However, a major difficulty with the standard test has been encountered when buildings are designed with external structural steel elements. These elements are required by the codes to have the same minimum periods of fire resistance and hence the same cladding as internal elements, even though the external fire exposure conditions are known to be less severe.

Internal elements exposed to fire are surrounded by flames and by the heated surfaces (walls, ceiling, floor) of the enclosing room or compartment. These heating conditions are similar to those of the standard fire resistance test, where the element is enclosed in a furnace. External elements are exposed to radiation from the windows in the facade, the value of the intensity of radiation received varying with the position of the element in relation to the windows. They are exposed to radiation and convection from the outflowing flames and hot gases, the heat flux again varying with position, and they also lose heat to the surroundings at normal ambient temperature. Depending on their size and position and on the behavior of the fully developed fire, external steel elements may be designed so that they do not need any cladding.

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Initially, attempts were made in research programs to simulate external exposure by inserting a window in the standard test furnace wall, but this was not really satisfactory, because no matter how long the test is run, the flame exposure is likely to be less severe than that from a real building fire.[1] Accordingly, *ad hoc* experiments have been carried out from time to time with external elements exposed to flames and radiation from "real" fires, the results of these experiments leading to a relaxation of code requirements for individual specific buildings.[2,3] If, instead, a design approach is adopted, it not only obviates the need for these *ad hoc* tests, but also extends to sizes of fire well beyond the limits of size of practical fire tests. The approach is to analyze the external heat transfer to structural elements and to calculate the amount of protection, if any, which would be needed.

For structural steel elements, critical conditions can be defined in terms of a critical steel temperature and, given the heat transfer conditions, calculation of the steel temperature is relatively straightforward. The main problem, then, in adopting the design approach, is to define the external heat transfer.

A large body of data on building fire and flame behavior exists, and the objective of this paper is to show how it may be analyzed and used to estimate external heat transfer for practical designs of buildings. In particular, correlations have been derived not only from model-scale experiments, but also from measurements for a wide range of experimental fires in large-scale building compartments.

Since this work forms the basis of a design guide[4] for the use of bare exterior structural steel, it has been considered necessary to meet the following requirements:

1. The correlations of fire and flame behaviour should be based on parameters which can be readily identified by the designer.
2. The correlations should be explained by relatively simple equations to facilitate calculations.
3. The geometrical model of flame projection should also be simple, to facilitate calculations.

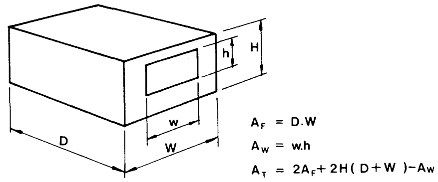

Fig. 1. Simple fire compartment

The parameters which have been adopted are the dimensions of the compartment and its windows, and the fire load per unit floor area, L/A_F. The compartment in its simplest form is illustrated in Fig. 1; in practice, the correlations can be used for a variety of window sizes on one or more walls. The value of L/A_F can be estimated for the particular building under consideration, or obtained from survey data for the type of occupancy. A simple pocket calculator is adequate for the calculations.

This paper has been arranged to give, first, a summary of the main experimental data describing fire and external flame behavior. (The main features of the large-scale experiments are listed in the Appendix.) Correlations based on these data are then derived. The mode of heat transfer to external steel elements is outlined and a heat transfer model has been developed, so that steel temperatures may be estimated.

INTERNAL FIRE BEHAVIOR

A building fire which is allowed to continue until the fuel is exhausted, without intervention of the fire brigade, can be considered to go through three main phases: growth, full development, and decay. Most flaming and most structural damage occur during the fully developed period, and this phase has been studied by a number of workers; a summary is given by Thomas.[5] An analysis of fully developed fire behavior is complex, and the models which have been developed are based on a number of simplifying assumptions, notably that the temperature distribution is uniform throughout the fire compartment and that the fuel burns in a uniform way. Nevertheless, important parameters have been defined, and a substantial data bank exists from which it is possible to show how these parameters interact.

The pioneer worker in this area was Ingberg,[6] whose relationships between fire load density (fire load per unit flow area) and fire severity have formed the basis of fire resistance requirements for elements of structure. The importance of ventilation was quantified by Fujita[7] in terms of area and height of the ventilation opening (usually the window). Later work, carried out in a cooperative research program under the auspices of the Conseil International du Batiment (CIB),[8] has shown how the Fujita relationship is modified by the size and shape of the fire compartment. By using models, it was possible in the CIB program to cover a wide range of these and other factors. On large-scale, an early systematic study of some of the factors was carried out at Borehamwood[9] and later at Maisieres-les-Metz,[10] and there are a number of other large-scale experiments which yield some measurements to compare with the model-scale data.

In experiments, the fire load has usually been cribs of wood sticks, an easily reproducible fuel, or waste wood, but some data are available for furniture. Strictly speaking, the information derived is only applicable to fires involving mainly wood fuel, but it may be assumed to give reasonable correlation with domestic, office, and similar types of fire load. Most experiments have been carried out in still air or light wind conditions, that is, the air flow has been controlled by the fire behavior and compartment dimensions, and this may be termed "natural draft". Some experiments have included an extra air supply and this may be termed "forced draft". The windows have usually been unglazed, the most likely condition once the fire becomes fully developed.

Where external fire exposure is concerned, two of the most important features of the fully developed fire are the rate of burning, which affects flame size and fire duration, and fire temperature, which affects the radiation from the window.

Natural Draft—Continuous weighing of the fire load during actual tests has shown[11] that the rate of weight loss is approximately steady over the fully developed period when the weight falls from 80 to 30% of its initial value. This rate is defined as the average rate of burning, R, and the effective fire duration, τ, is defined by:

$$R = \frac{L}{\tau}$$

where L is the total mass of the fire load.

With ample ventilation—the free burning condition—the value of τ is τ_F, determined by the characteristics of the fire load, thin fuels with large surface areas giving smaller values of τ_F. Thus, for any given type of fuel, R is directly proportional to L and is given by:

$$R = \frac{L}{\tau_F} \tag{1}$$

Where ventilation is restricted, there is an upper limit to the value of R, however large the fire load, and it has been shown, by considering air flow and a heat balance, that important parameters are the area A_w and height h of the windows, the area A_T of the enclosing surfaces to which heat is lost (excluding the windows), and the ratio of the depth D to width W of the compartment. Thomas[5] gives the correlation of the CIB data shown in Fig. 2, for a range of these parameters, in compartments 1.6, 3.3, and 4.9 ft high. He modifies the Fujita equation[7] to:

$$R = 0.6\, A_w h^{1/2}$$

to allow for the compartment dimensions D, W, and A_T. The measurements of R which have been reported for large-scale ventilation-restricted fires,[9,10,12] have been

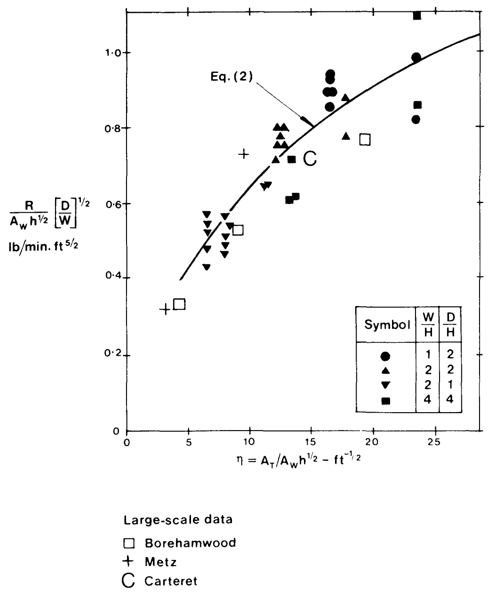

Fig. 2. Variation of $R/A_w h^{1/2}$ with compartment size and ventilation, as given by Thomas for CIB data

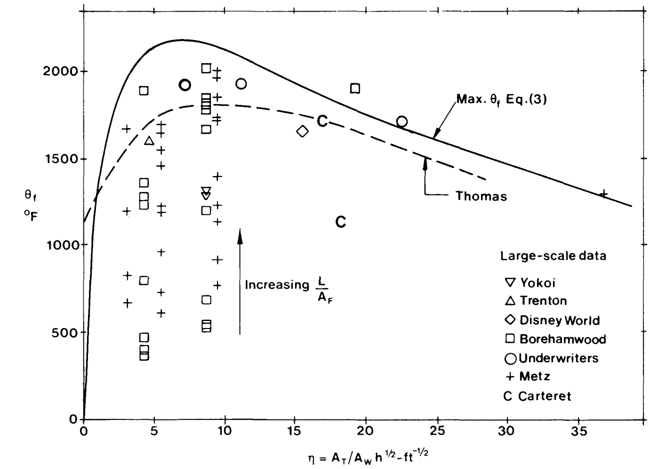

Fig. 3. Variation of average fire temperature rise with compartment size and window area, natural draft

plotted in Fig. 2 and are in reasonable agreement with the CIB data. The line drawn by Thomas is the best one through the points, and can be represented by the following equation:

$$R = \frac{1.22\,(1 - e^{-0.065\eta})}{(D/W)^{1/2}}\,(A_w h^{1/2}) \tag{2}$$

where

$$\eta = \frac{A_T}{A_w h^{1/2}}$$

For a particular fire load and compartment size, R should be calculated from both Eqs. (1) and (2). If Eq. (2) gives a lower value, there is a ventilation controlled condition.

There is an upper limit to the temperature attained within the fire compartment, depending on the fire load and the compartment dimensions. Thomas[5] gives the correlation of the CIB measurements of average fire temperature rise, θ_f, over the fully developed fire period, as a function of η, as shown in Fig. 3. The major point of interest is that θ_f rises to a maximum for $\eta = 5$ to 10 and then declines. The value of θ_f also depends on the fire load, and this is clearly demonstrated in Fig. 3 where the results for large-scale tests[9,10,12,13–17] with low fire load densities, fall well below the Thomas curve. It is reasonable to assume that there is an "upper limit" or maximum to the value of θ_f for a given value of η; the following equation is proposed:

$$\theta_{f(max)} = 8025\,\frac{(1 - e^{-0.18\eta})}{\eta^{1/2}} \tag{3}$$

Equation (3) is shown in Fig. 3.

For low values of fire load, the upper limit is not attained and examination of the data indicates that the effect is not simply one of fire load density, but of fire load in relation to ventilation and compartment dimensions. Earlier analysis[18] of both the CIB and large-scale data showed that $\psi = L/(A_w A_T)^{1/2}$ is an important parameter which is related to an equivalent fire resistance. It has been used to modify Eq. (3) for the upper limit, as follows:

$$\frac{\theta_f}{\theta_{f(max)}} = 1 - e^{-0.25\psi} \tag{4}$$

Figure 4. indicates that this correlates the large-scale data. Combining Eqs. (3) and (4) gives:

$$\theta_f = 8025\,\frac{(1 - e^{-0.18\eta})}{\eta^{1/2}}\,(1 - e^{-0.25\psi}) \tag{5}$$

Forced Draft—There is not much information available on the effects of forced draft on rate of burning, but observations made during the Underwriters' Laboratories tests[16]

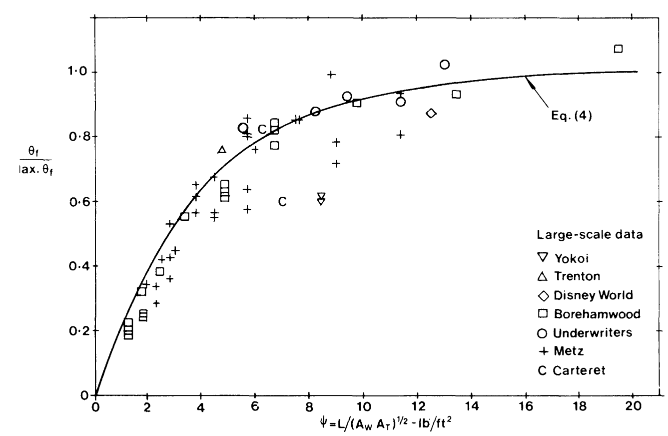

Fig. 4. Variation of average fire temperature rise with fire load, compartment size, and window area, natural draft

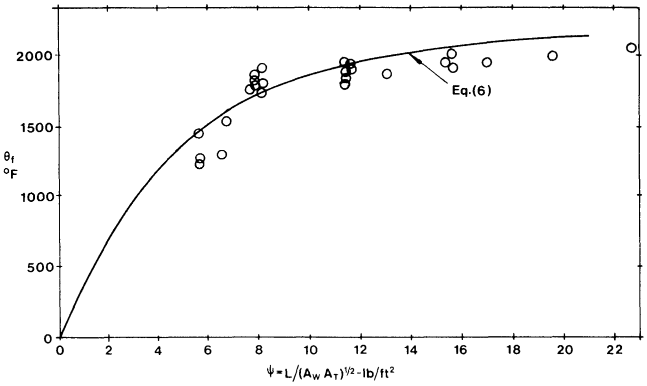

Fig. 5. Variation of average fire temperature rise with fire load, compartment size, and window area, forced draft, Underwriters' Laboratories data

indicate that its maximum effect would be to give the free-burning condition of Eq. (1), i.e., $R = L/\tau_F$.

The data obtained at the Underwriters' Laboratories show no significant variation of temperature with η or air supply, but θ_f can be related to ψ as shown in Fig. 5. The curve has the equation:

$$\theta_f = 2160\,(1 - e^{-0.20\psi}) \qquad (6)$$

It would be expected that θ_f would decrease with increasing air supply, and Eq. (6) may overestimate the fire temperature where there is a strong wind.

EXPERIMENTAL STUDIES OF EXTERNAL FLAME PROJECTION

The first comprehensive study of flame projection from windows was made by Yokoi,[13] who wished to estimate the risk of vertical fire spread. He first derived correlations for temperature and velocity distribution in the plume of hot gases rising above alcohol fires burning in rectangular trays. By treating the upper half of a window as the rectangular heat source, he then derived similar correlations for the plumes rising from various size and shape windows in a 1.3 ft x 1.3 ft x 0.65 ft high model room containing alcohol fires. He demonstrated the effect of a wall above the window and the shape of the window on the temperature distribution and trajectory of the plume. The wall absorbs heat from the plume, but restricts the air entering from the wall side, and the wider the window the closer the plume is to the wall. Yokoi denoted the shape of the window by $n = w/{}^{1}\!/_{2}h$, the ratio of the width to the height of the upper half of the window, and derived a series of plume shapes for different values of n. He obtained good agreement between the results of his model tests and four experiments using wood fuel in large-scale concrete buildings, even though, as he points out, theoretically it is necessary to make adjustments for the emissivity of wood flames and for the thermal properties of the wall above the window. He also comments that, where there is restricted ventilation in the room, the outflowing gas will continue to burn after it leaves the window and this will affect the correlation.

Webster et al.[19–21] carried out a series of tests, mostly on model scale, with cubical rooms open on one side, containing wood crib fires. Visual records of flame height were made. Thomas [22] correlated these results by a dimensional analysis essentially the same as that used by Yokoi, derived from the dominant role of buoyancy and considerations of turbulent mixing. By assigning a flame tip temperature of about 1000°F, reasonable agreement could be obtained between these data and those of Yokoi.

Seigel[23] reported some findings derived from the tests at the underwriters' Laboratories[16] in a large-scale room with various sizes and shapes of window containing wood crib fires. An air supply was connected to this room for most of the experiments, which had the effect of increasing the rate of burning of the cribs to the "well-ventilated" (free-burning) condition.[24] Visual records of flame height and projection were made and the temperature distribution in the emergent plume was recorded. Seigel's correlation treats flames as forced horizontal jets and the projection is defined by a temperature of 1000°F at the flame tip.

In a series of tests[9] at Borehamwood, in a large-scale room with natural ventilation, containing wood crib fires, visual flame heights were recorded, but only as a by-product of the main experiment; thus, these values are rather approximate.

Most of the large-scale experiments[3,12,14,25,26] specifically designed to study flame projection, have been *ad hoc*. The information derived can be generalized by relating them to the correlations described above. This process has at least two important aspects: first, it demonstrates the validity of the correlations obtained by the use of models; secondly, it illustrates the difference between the idealized laboratory environment and "natural" conditions. For example, it has been observed that in practice the fire load may burn unevenly or that flame projection from windows may be asymmetrical.

Figure 6 illustrates the main features of interest for flame projection.

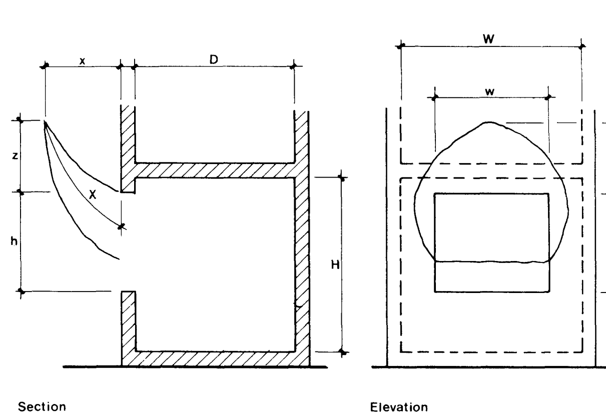

Fig. 6. Dimensions used in calculations of flame projection

EXTERNAL FLAME DIMENSIONS

Natural Draft—Yokoi correlated y/r_o, where y is height above the top of the window and r_o is the effective radius of the upper half of the window, with Θ, a dimensionless term involving temperature rise and rate of heat supply, and obtained a family of curves for different values of n, the ratio of width to height of the upper half of the window. Thomas[22] showed that these curves could be brought together by correlating $n^{1/3}y$ with Θ. Thomas and Law,[27] analyzing the data of Yokoi, Webster et al. and Seigel, proposed the following correlation:

$$\frac{n^{1/3}(z+h)}{r_o} = \frac{2}{\Theta} \tag{7}$$

where $(z + h)$ denotes the height of the flame tip above the base of the window of height h, assuming a temperature at the tip of 1000°F.

Equation (7) may be rearranged to give:

$$\frac{z+h}{h} = 2\pi^{1/3}\left[\frac{R}{A_w\rho_z(gh)^{1/2}}\right]^{2/3}\left[\frac{C^2T_a}{c_z{}^2\theta_z{}^3}\right]^{1/3}$$

or

$$\frac{z+h}{h} = 23.5\left[\frac{R}{A_w\rho_z(gh)^{1/2}}\right]^{2/3} \tag{8}$$

where $C = 6.9 \times 10^3$ Btu/lb, $c_z = 0.24$ Btu/(lb °F), $T_a =$ 520°F, ρ_z is density of hot gas, and g is acceleration due to gravity.

Data for large scale fires[3,9,12—16,25,26] are plotted in Fig. 7, which indicates that Eq. (8) overestimates flame height on large scale and that other factors appear to affect the flame behaviour. A regression analysis of the log values shows that $R/[A_w\rho_z(gh)^{1/2}]$ is nevertheless highly significant, n is significant at the 5% level, and the following equation is obtained:

$$\frac{z+h}{h} = 8.9\left[\frac{R}{A_w\rho_z(gh)^{1/2}}\right]^{0.51} n^{0.12}$$

Much of the scatter of the data is probably random and the power of n is small; accordingly, there seems no strong reason to depart from the general form of Eq. (8), but to use an adjusted coefficient.

The recommended correlation for flame height is:

$$\frac{z+h}{h} = 16\left[\frac{R}{A_w\rho_z(gh)^{1/2}}\right]^{2/3} \tag{9}$$

This may be written:

$$z + h = 3.55\left[\frac{R}{w}\right]^{2/3} \tag{10}$$

where $\rho_z = 0.028$ lb/ft^3 at 1000°F.

A similar regression analysis for the projection x of the flame tip gives:

$$\frac{x}{h} = \frac{0.454}{n^{0.53}} \tag{11}$$

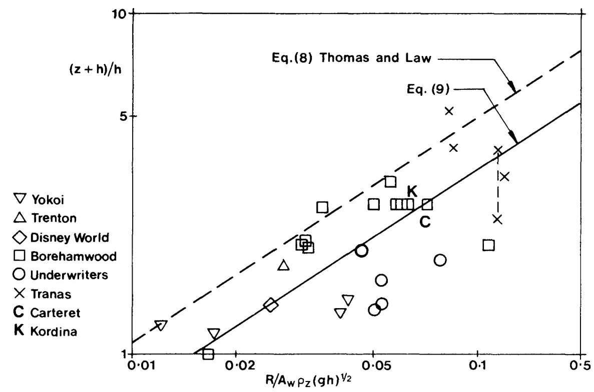

Fig. 7. Flame heights for large-scale tests with natural draft (Note: Range of n: 0.5-18.7)

with n significant at nearly the 0.1% level and the term $R/[A_w\rho_z(gh)^{1/2}]$ not significant. The data are plotted in Fig. 8. Equation (11) indicates that projection of the flame tip decreases with n, as shown by Yokoi, and is less than half the window height for values of n exceeding unity, which includes most situations. Equation (11) is the recommended correlation for x, provided there is a wall above the window. Yokoi showed that without a wall, the value of x would be independent of n. In the absence of other data, Yokoi's no-wall relationship is recommended. It may be represented by the following equation:

$$\frac{x}{h} = 0.60\left[\frac{z}{h}\right]^{1/3} \tag{12}$$

The measurements at the Underwriters' Laboratories suggest that the maximum width of the emerging flame will be little different from the window width.

Forced Draft—Seigel,[23] treating the flame as a jet,[28] proposed an equation of the form:

$$l \propto \frac{R}{A_w{}^{1/2}\theta_z}$$

where l is the distance along the center line at which the temperature rise is θ_z. For the data obtained by the Un-

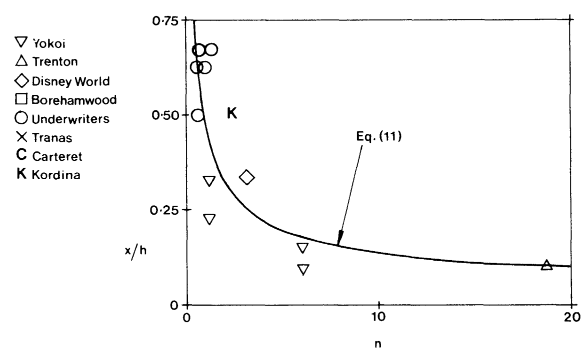

Fig. 8. Horizontal projection of flame tip for large-scale tests with natural draft, wall above

derwriter's Laboratories, he showed that at the flame tip, where $l = X$:

$$X = 0.051 \frac{L}{A_w^{1/2}} - 1.28 \tag{13}$$

for fires with "normal" burning. Normal buring was defined as sufficient ventilation for the cribs used as fire load to burn at their maximum rate, that is, as if they were free-burning.

For these cribs, τ_F was 26 min so that Eq. (13) may be written:

$$X = 1.33 \frac{R}{A_w^{1/2}} - 1.28 \tag{14}$$

Seigel's correlation is based on the reasonable assumption that the main effect of a forced draught is to increase the rate of burning of a ventilation controlled fire. The effect on "free-burning" fires was insignificant for the range of forced ventilation used. However, for a given rate of burning, a wind may also affect the flame size and direction. For this reason, regression analyses of the data, similar to the ones for natural ventilation, but including a Froude number (u^2/gh) have been carried out, and the following have been obtained, where u is wind velocity:

$$\frac{z+h}{h} = 6.99 \left[\frac{R}{A_w \rho_z (gh)^{1/2}}\right]^{0.784} n^{0.434} \left[\frac{u^2}{gh}\right]^{-0.216} \tag{15}$$

$$\frac{x}{h} = 6.85 \left[\frac{R}{A_w \rho_z (gh)^{1/2}}\right]^{0.760} \times n^{0.444} \left(\frac{u^2}{gh} \text{ not significant}\right) \tag{16}$$

From this it can be deduced that:

$$x \simeq \left[\frac{u^2}{gh}\right]^{0.216} (z + h) \tag{17}$$

Equation (15) may be written:

$$u^{0.432} (z + h) = 20.0 \left[\frac{R}{A_w^{1/2}}\right]^{0.784} \tag{18}$$

The data for flame height are plotted this way in Fig. 9 and indicate that a correlation of the form proposed by Seigel, with $R/A_w^{1/2}$ raised to the power of unity, would be reasonable provided the wind effect is included. The following correlation is proposed:

$$u^{0.43}(z + h) = 12.5 \left[\frac{R}{A_w^{1/2}}\right] \tag{19}$$

Note that Eq. (19) agrees with Eq. (14) for $u = 180$ ft/min, which is the mean value for these experiments.

The data for x are plotted in Fig. 10, showing the following correlation, derived from Eq. (17):

$$x = 0.077 \left[\frac{u^2}{h}\right]^{0.22} (z + h) \tag{20}$$

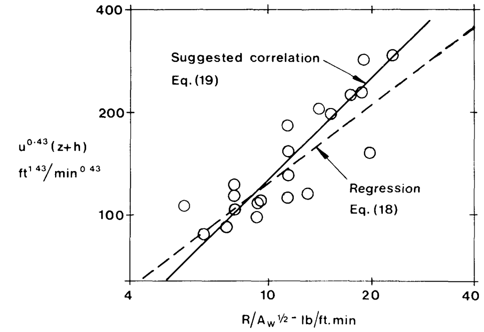

Fig. 9. Flame heights for large-scale tests with forced draft, Underwriters' Laboratories data (Note: Range of f: 100-367 ft/min)

The maximum width, w_z, of the emerging flames usually exceeded the window width. The angle made by the emerging flame, as shown in Fig. 11, does not correlate with any of the dimensionless parameters considered above. The average value of the angle is 11°, giving:

$$\frac{w_z - w}{2x} = 0.194$$

or

$$w_z \simeq w + 0.4x \tag{21}$$

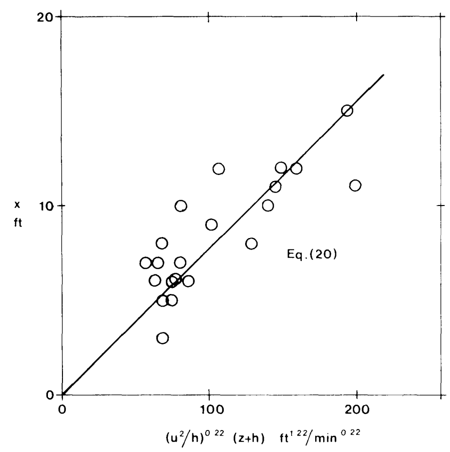

Fig. 10. Horizontal projection of flame tip for large-scale tests with forced draft, Underwriters' Laboratories data (Note: Range of u^2/h: 1000-22,500 ft/min^2)

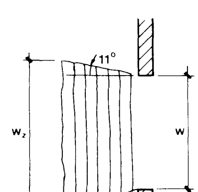

Fig. 11. Plan view of emerging flames with forced draft

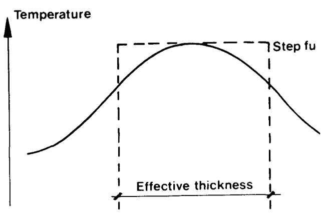

Fig. 12. Temperature distribution across flame section

Effective Flame Boundary—In order to estimate heat transfer to steel structures, the flame boundaries need to be defined. In cross section, the temperature distribution would be as illustrated in Fig. 12, and one approach would be to define the flame boundary by the 1000°F contour (which would be consistent with the definition of the flame tip). However, since radiative transfer will be an integrated effect, which can be made equivalent to a uniform effective temperature, an equivalent step function distribution is proposed. Since radiant transfer is so sensitive to the value of temperature, it is prudent to adopt the axial temperature (maximum) for the step function with a defined effective thickness. The problem is to define this effective thickness, bearing in mind as assumption of maximum temperature throughout.

With natural draft, the flame emerges above the neutral plane from the upper two-thirds of the window. Since flame width varies little with distance from the window, it seems reasonable to assume that the step function remains the same size throughout the trajectory, that is, $w \times 2h/3$. This is illustrated in Fig. 13. As shown later, this assumption is consistent with estimated values of emissivity. A wind could deflect this flame sideways and it will be assumed that, as an average throughout the fire, the deflection would not exceed 45°, as shown in Fig. 13. This seems reasonable, because on average the wind speed will be of the same order as the speed of the outflowing hot gases.

With forced draft, the flame can emerge from the whole window. The width does increase with distance and it is reasonable to assume that the vertical dimension also increases. However, the upper vertical increase is already contained in the value for z. It is therefore proposed that the size should be $h \times w$ at the window, increasing to $h \times (w + 0.4x)$ at the flame tip, as shown in Fig. 13.

TEMPERATURE AT FLAME AXIS

Seigel[24] has analyzed the temperature distribution in flames for the forced draft data and obtained the following correlation, where θ_o is measured at the window:

$$\text{For } \frac{lA_w^{1/2}}{R} > 0.52: \quad \frac{\theta_z}{\theta_o} = 0.62\left[\frac{lA_w^{1/2}}{R}\right]^{-3/4} \tag{22}$$

$$\text{For } \frac{lA_w^{1/2}}{R} < 0.52: \quad \frac{\theta_z}{\theta_o} = 1.0$$

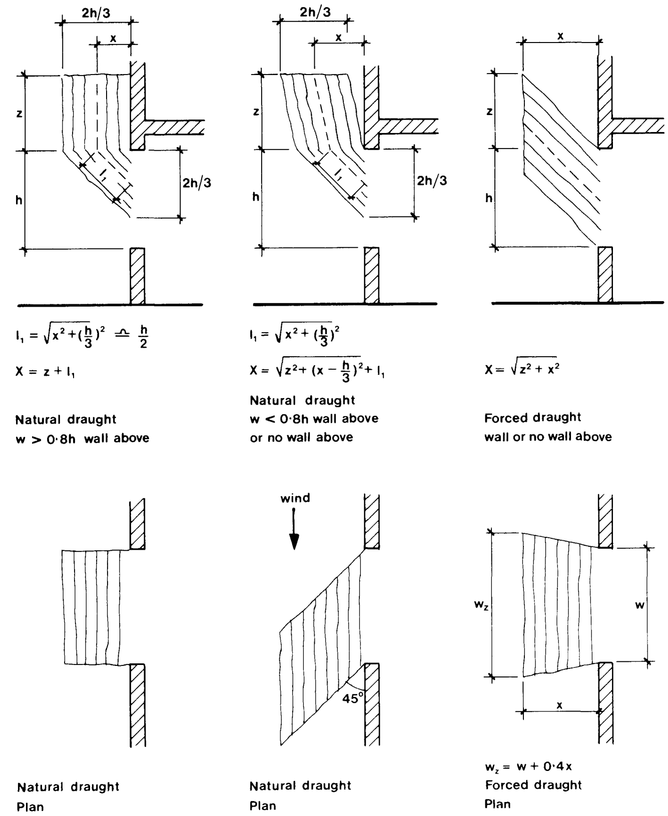

Fig. 13. Assumed trajectories of emerging flames

This follows the temperature distribution pattern found for jets.[28] The data are plotted in Fig. 14. Note that combining Eqs. (14) and (22) gives $(\theta_z/\theta_o) \simeq 0.5$, which is correct at the flame tip.

A similar approach has been adopted to correlate the temperature data for natural draft. These are better correlated in terms of lw/R and the data are shown in Fig. 15. The line has the equation:

$$\frac{\theta_z}{\theta_o} = 1 - 0.33\frac{lw}{R} \tag{23}$$

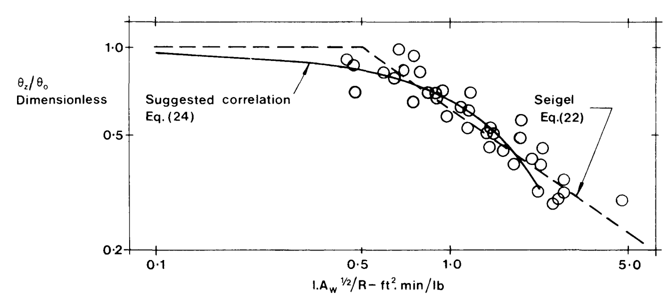

Fig. 14. Flame temperature distribution for large-scale tests with forced draft, Underwriters' Laboratories data

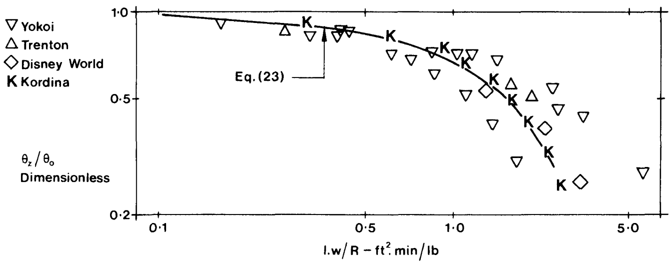

Fig. 15. Flame temperature distribution for large-scale tests with natural draft

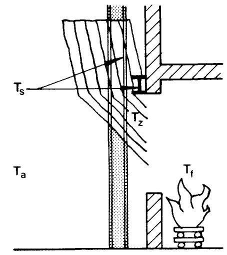

Fig. 16. Location of temperatures

A similar correlation for the forced draft data is shown in Fig. 14, and has the advantage, from an analytical point of view, of avoiding a discontinuity. The correlation has the equation:

$$\frac{\theta_z}{\theta_o} = 1 - 0.33\frac{lA_w{}^{1/2}}{R} \tag{24}$$

The significance of Eqs. (23) and (24) is that the decrease in flame temperature is directly proportional to the distance along the center line of the flame. By substituting θ_z = 940°F (assuming an ambient temperature of 60°F) and $l = X$ in Eqs. (23) and (24), the value of θ_o may be derived. For fires with natural draft, this may give values of θ_o which are greater than the fire temperature θ_f. This result is not unexpected, since substantial amounts of unburnt gas can be emitted from the compartment. For forced draft fires, the values of θ_o may be less than θ_f.

MODEL OF HEAT TRANSFER TO EXTERNAL STEEL SURFACE

In the following equations, the temperatures, T, are on the absolute scale °R, since a major portion of the heat transfer is by radiation, and intensity of radiation is proportional to the fourth power of the absolute temperature. Thus, $T_z = \theta_z + Ta$, $T_s = \theta_s + T_a$, and $T_f = \theta_f + T_a$, where the suffix z refers to the external flames, s to the external steel, f to the fire within the building, and a to the ambient external air. Temperatures at these positions are illustrated in Fig. 16. In the following equations, T_s denotes an average temperature across the section.

If an external steel surface is engulfed by flame and is heated by radiation and convection from flames, and by radiation from the openings of a building on fire, the heat balance for a unit surface area is given by the following equation:

$$\begin{aligned}\alpha_z(T_z - T_s) + \epsilon_z\epsilon_s\sigma(T_z{}^4 - T_s{}^4) \\ + \epsilon_f(1 - \epsilon_z)\epsilon_s\phi_f\sigma(T_f{}^4 - T_s{}^4) \\ + (1 - \epsilon_z)\epsilon_s(1 - \phi_f)\sigma(T_a{}^4 - T_s{}^4) \\ = \frac{Mc_s}{A_s}\frac{dT_s}{dt} + k\end{aligned} \tag{25}$$

In Eq. (25), $\alpha_z(T_z - T_s)$ represents the net rate of heat transfer by convection from the flame. The value of α_z, the heat transfer coefficient, will depend on the temperature and velocity of the flame and on the geometry of the steel surface. Provided the velocity is known, α_z can be obtained from standard textbooks; α_z may not be known with great accuracy, but since most of the heat is transferred by radiation, the error will be small.

$\epsilon_z\epsilon_s\sigma(T_z{}^4 - T_s{}^4)$ represents the net rate of heat transfer by radiation from the flame. The flame emissivity, ϵ_z, will depend on the flame thickness. The emissivity of the surface of the stanchion, ϵ_s, will be high, of the order of 0.9 and for the purposes of these calculations could be taken as unity (a conservative assumption). The Stefan-Boltzmann constant is denoted by σ. Provided ϵ_z is known, the heat transfer can be estimated.

$\epsilon_f(1 - \epsilon_z)\epsilon_s\phi_f\sigma(T_f{}^4 - T_s{}^4)$ represents the net rate of heat transfer by radiation from the windows and other openings of the building on fire. The emissivity of the fire within the building, ϵ_f, is high and may be taken as unity.[29] Some of the radiation will be absorbed by the flame, the fraction which is transmitted being given by $(1 - \epsilon_z)$. The configuration factor, ϕ_f, of the windows in relation to the surface will depend on the size and shape of the windows and the position of the surface; its calculation is given in standard textbooks. (The factor is the ratio of the intensity of radiation received at the surface to the intensity of radiation emitted; it is inversely proportional to the square of the distance between source and receiver and varies with their relative orientations and areas.) Provided ϵ_z is known, the heat transfer can be estimated.

$(1 - \epsilon_z)\epsilon_s(1 - \phi_f)\sigma(T_a{}^4 - T_s{}^4)$ represents the net rate of heat transfer by radiation from the surroundings at ambient air temperature and, since T_a will be less than T_s, it will be negative.

$(Mc_s/A_s)dT_s/dt$ represents the rate of heat gain per unit surface area. The mass per unit length, M, divided by the perimeter, A_s, gives the mass per unit surface area, and c_s is the specific heat of steel.

k represents the rate of heat loss by conduction away from the heated area but, where engulfed by flame, the temperature gradient is likely to be small and for the purposes of these calculations it will be neglected (a conservative assumption).

$\sigma T_a{}^4$ is small and may be neglected.

Taking account of the assumptions described above, Eq. (25) may be simplified to give:

$$\alpha_z(T_z - T_s) + \epsilon_z \sigma T_z{}^4 + (1 - \epsilon_z)\phi_f \sigma T_f{}^4 - \sigma T_s{}^4 = \frac{Mc_s dT_s}{A_s dt} \quad (26)$$

For steady state conditions the right hand side of Eq. (26) is zero ($dT_s/dt = 0$).

If the steel surface is outside the convective stream of flame and hot gases, the heat balance is similarly given by:

$$\epsilon_z \phi_z \sigma T_z{}^4 + \phi_f \sigma T_f{}^4 - \sigma T_s{}^4 - \alpha_s(T_s - T_a) = \frac{Mc_s\, dT_s}{A_s dt} \quad (27)$$

$\epsilon_z \phi_z \sigma T_z{}^4$ represents the heat transfer by radiation from the flame. The configuration factor, ϕ_z, of the flame in relation to the surface will depend on the size and shape of the flame front and the position of the flame in relation to the surface. Since the value of T_z varies along the flame front, an effective value must be estimated.

$\alpha_s(T_s - T_a)$ represents the heat loss by convection to the surroundings and can be taken as $\alpha_z(T_s - T_a)$ for most situations of practical interest.

As before, for steady state conditions, the right hand side of Eq. (27) is zero. In practice, steady-state conditions may not be attained, but if they are assumed, the maximum steel temperature will be calculated and a conservative solution will result. However, when the duration of flaming is short and/or the value of M/A_s is high, there may be transient conditions, and T_s can then be calculated by iterative methods, using Eq. (26) or (27) as appropriate.

Before calculations can be undertaken, it is necessary to estimate the flame velocity to determine α_z for convective transfer, and the emissivity and effective temperature of the flames, to calculate radiative transfer. Measured values of heat transfer from flames in large scale experiments will be examined to help to establish a realistic model.

Convection from Flames and Hot Gases—The convective heat transfer coefficient, α_z, depends on the mass flow per unit area, $u_z \rho_z$, of the hot gases and the size and orientation of the receiving surface; it can be obtained from relationships between the Nusselt number, Nu, and the Reynolds number, Re, where:

$$Nu = \frac{\alpha_z d}{K_z}$$

$$Re = \frac{u_z \rho_z d}{\mu_z}$$

d = a characteristic length of the surface
K_z = gas thermal conductivity
ρ_z = gas density
μ_z = gas viscosity

The thermal properties of the gas are taken at the "film" temperature, the mean of the temperature of the hot gas and the surface. For flow perpendicular to a tube[30] of diameter d:

$$Nu = 0.24\, Re^{0.6} \quad (28)$$

For flow at angle of 45° the value of the constant in Eq. (28) is about 0.18, and for parallel flow about 0.12. Equation (28) will normally be appropriate for both columns and beams.

When there is natural draft, a column or beam will only be exposed to convective heat transfer if it is close to the building where the mass flow per unit area can be assumed to be nearly the same as at the window. The mass flow leaving the window depends on the processes by which air is drawn into the fire.[31] For a ventilation controlled fire it is approximately $6.4R$. Since the neutral plane, above which the flames and hot gases leave the compartment, is about $2h/3$ below the top of the window, the mass flow per unit area is given by:

$$u_z \rho_z \simeq 9.6 \frac{R}{A_w}$$

and the Reynolds number is:

$$Re = \frac{9.6Rd}{A_w \mu_z} \quad (29)$$

Equations (28) and (29) can be combined to give:

$$\alpha_z = 0.93\, K_z \left[\frac{R}{\mu_z A_w}\right]^{0.6} \left[\frac{1}{d}\right]^{0.4} \quad (30)$$

The value of α_z is not very sensitive to film temperature and a representative temperature of 1350°F may be adopted. Equation (30) then becomes:

$$\alpha_z = 0.027 \left[\frac{R}{A_w}\right]^{0.6} \left[\frac{1}{d}\right]^{0.4} \quad (31)$$

For a free-burning fire, Eq. (31) overestimates α_z and its use gives a slightly conservative solution.

Where there is a significant forced draft, the mass flow till include the supplied air and will emerge from the whole window area. Thus Eq. (28) becomes:

$$\alpha_z = 0.24\, K_z \left[\frac{R}{\mu_z A_w} + \frac{u_z \rho_z}{\mu_z}\right]^{0.6} \left[\frac{1}{d}\right]^{0.4}$$

or

$$\alpha_z = 0.0068 \left[\frac{R}{A_w} + \frac{u}{13}\right]^{0.6} \left[\frac{1}{d}\right]^{0.4} \quad (32)$$

When the steel structure is remote from the flame, it loses heat by natural convection:

For columns:

$$\alpha_s = 0.0040 \left[\frac{\theta_s}{d}\right]^{0.25} \quad (33)$$

For beams the coefficient is similar, 0.0037.

When d exceeds 1 ft:

$$\alpha_s = 0.0050\,(\theta_s)^{0.25} \tag{34}$$

Radiation from Flames—The value of flame emissivity ϵ_z depends on flame thickness λ and may be assumed to follow a relationship of the following form:

$$\epsilon_z = 1 - e^{-b\lambda} \tag{35}$$

A value for b of 0.158 ft^{-1} was obtained by Beyreis et al.[32] for flames above wood cribs burning inside an enclosure. This value is somewhat higher than that assumed by Seigel[24] for flames outside the enclosure or by Heselden,[33] who suggests a value of about 0.09 ft^{-1}. Direct measurements of heat flow from emerging flames were made in the large scale experiments at Borehamwood,[9] where a heat flow meter was placed in the wall at a height of 1.8 ft above the top of the window. Denoting the measured value of heat flux by I_z and neglecting convective transfer (small):

$$I_z \simeq \epsilon_z \sigma T_z^4$$

Values of I_z at 1.8 ft above the window have been calculated according to the methods described and σT_z^4 is plotted against the measurements of I_z in Fig. 17. The slope gives ϵ_z and the value of 0.3 adopted by Seigel is shown to be realistic for these data. To generalize the result, $\lambda = 2h/3$ is substituted in Eq. (35) and gives a value for b of about 0.09 ft, the value suggested by Heselden. Using the value suggested by Beyreis gives $\epsilon_z = 0.47$, which is rather high. Therefore, within the assumptions made here, it is recommended, for flames emerging from windows, that

$$\epsilon_z = 1 - e^{-0.09\lambda} \tag{36}$$

When a surface is engulfed in flame the effective radiating temperature may either be the local flame temperature or an average of a range of flame temperatures according to the circumstances. For example, one surface of a column may "see" a thickness of flame varying from the local temperature T_l to the value T_o at the window, and another surface may see a thickness varying from T_l to T_x at the flame tip. As a conservative assumption, the value of T_z should be taken as the local value or the far value, whichever is the larger.

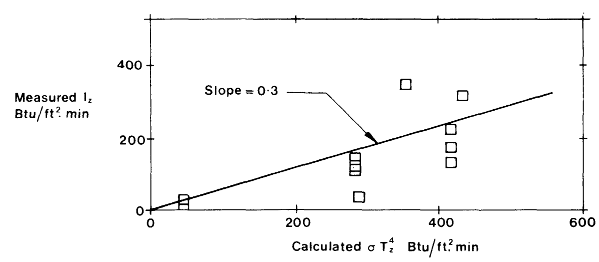

Fig. 17. Heat flow from flames above window in large-scale experiments at Borehamwood compared with calculated heat flux for fully emissive flames

When a surface is not engulfed in flame it "sees" a flame front varying in temperature from T_o at the base to T_x at the tip. At a large distance, the effective radiating temperature T_z would be given by:

$$T_z^4 \simeq \frac{T_o^4 + T_x^4}{2}$$

Close to the flame front, the effective radiating temperature for the point on the surface receiving the maximum rate of heating would be given by:

$$T_z^4 \simeq T_o^4 \tag{37}$$

Equation (37) is a reasonable approximation for most situations of practical interest for forced draft fires.

For fires with natural draft, the radiation is mainly received from the portion of the flame above the window, the portion below being thinner. Then, at a large distance:

$$T_z^4 \simeq \frac{T_w^4 + T_x^4}{2}$$

and close,

$$T_z^4 \simeq T_w^4 \tag{38}$$

where T_w is the flame temperature opposite the top of the window.

Equation (38) is a reasonable approximation for most practical situations of interest for fires with natural draft.

Measured values of the sum of fire and flame radiation from fires with natural draft[9] are compared in Fig. 18, with values calculated according to the methods described. The comparison shows that the calculations tend to overestimate the intensity of radiation received, but not by an unacceptable amount.

MEASURED TEMPERATURES OF EXTERNAL ELEMENTS

Using the correlations and heat transfer models described in the previous sections, temperatures of external steel elements have been calculated and compared with measurements made in the large-scale tests, the maximum measured values being selected. In the following graphs, if there were perfect agreement the points would fall on the line. Where points fall above the line, the calculation errs on the safe side.

Elements Engulfed in Flame—Figure 19 shows a comparison for columns engulfed in flame from fires with natural draft. For unshielded columns, Fig. 19(a), the calculated temperatures tend to exceed the measured values except for one of the readings at the Underwriters' Laboratories.* Fig. 19(b) shows results for columns with insulated shields on the flange facing the fire, as illustrated in

* *See later discussion.*

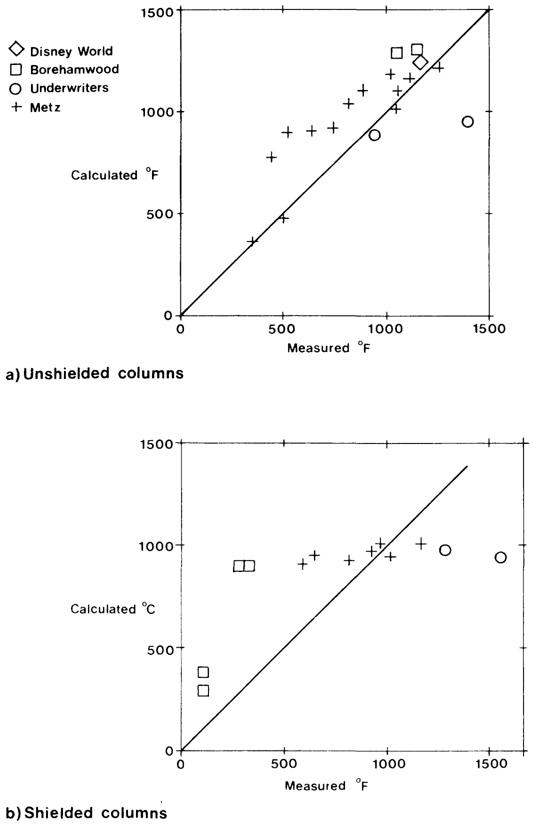

Fig. 18. Sum of window and flame radiation received by radiometer at 15 or 20 ft in large-scale experiments at Borehamwood

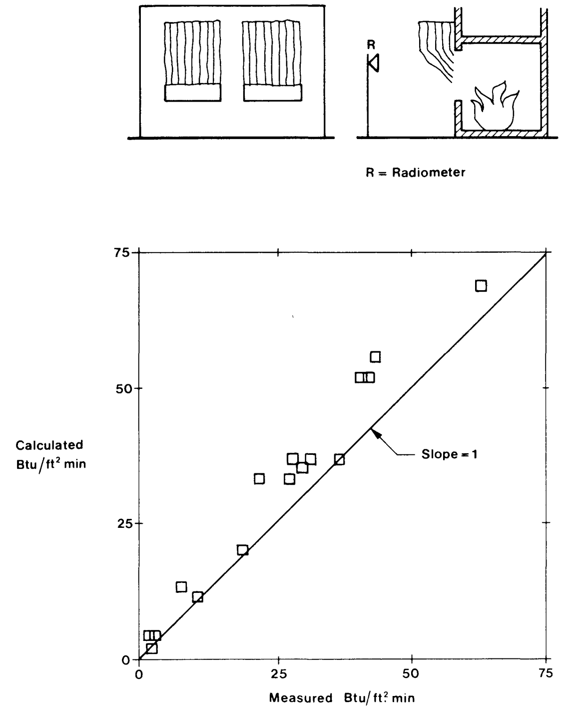

Fig. 19. Measured and calculated temperatures of steel columns engulfed in flame, from large-scale tests with natural draft

Fig. 20(a) or (b). (In the calculations it has been assumed that there is no heat transfer on the shielded face.) For these columns, the calculation method appears rather insensitive to the effect of shields on temperature attained, but errs on the safe side, except for the readings at the Underwriters' Laboratories.

Comparisons of calculated and measured temperatures of unshielded columns in large-scale fires with forced draft are shown in Fig. 21. The calculated temperatures are in general lower than the measured ones. The significance of this result will be discussed later.

Comparisons of calculated and measured temperatures of spandrel beams in the large scale tests at Borehamwood and Trenton are given in Fig. 22 and show satisfactory agreement. At Trenton the beam was shielded as shown in Fig. 20(c), and in calculations it has been assumed that there is no heat transfer to or from the flanges.

Elements Not Engulfed in Flame—For columns not engulfed in flame, Fig. 23 shows a comparison for fires with natural draft and Fig. 24 for fires with forced draft. There is reasonable agreement, although calculated temperatures tend to be slightly lower than measured ones for the forced draft tests.

For a spandrel beam not engulfed in flame, forced draft fires, Fig. 25 shows a comparison for the Underwriter's tests, in two of which there was an awning which shielded the lower range flange. There is reasonable agreement for both the shielded and unshielded results.

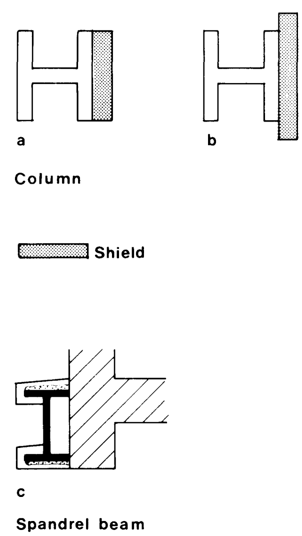

Fig. 20. Shielded elements

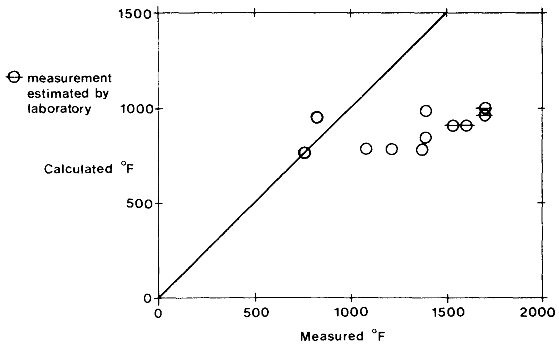

Fig. 21. Measured and calculated temperatures of steel columns engulfed in flame, from large-scale tests with forced draft, Underwriters' Laboratories

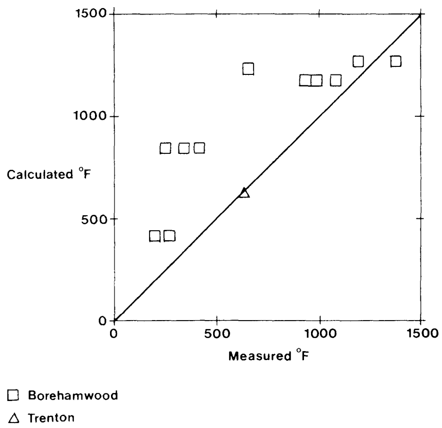

Fig. 22. Measured and calculated temperatures of spandrel beams engulfed in flame, from large-scale tests with natural draft

Discussion of Steel Temperatures—Although the agreement between calculated and measured steel temperatures is in general satisfactory, there are certain results which need further consideration. Some of the calculated temperatures for the Metz columns and the Borehamwood columns are rather high (Fig. 19). For these tests the fire loads were low and it is possible that the duration of external flaming was less than the fire duration. Some of the calculated column temperatures for the Underwriters' Laboratories tests are low (Figs. 19 and 21) and the reasons

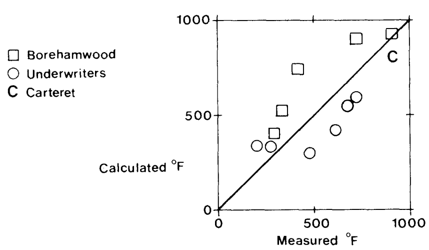

Fig. 23. Measured and calculated temperatures of columns not engulfed in flames, from large-scale tests with natural draft

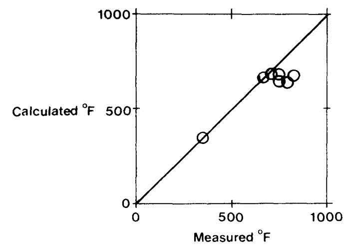

Fig. 24. Measured and calculated temperatures of columns not engulfed in flame, from large-scale tests with forced draft, Underwriters' Laboratories

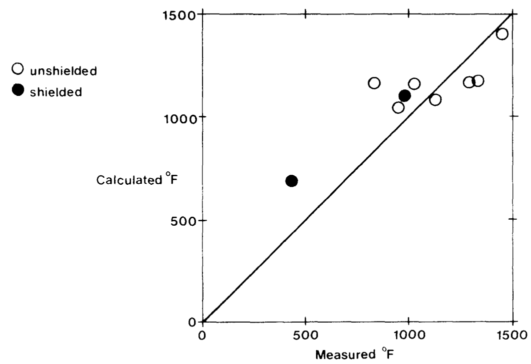

Fig. 25. Measured and calculated temperatures of spandrel beam, from large-scale tests with forced draft, Underwriters' Laboratories (Note: Shield is 2-ft awning)

for this are not clear. The calculated flame temperatures for the engulfed columns in Fig. 21 (forced draft) are certainly not lower than the measured ones. They are high enough to compensate for any error in measurement of the kind discussed in the Underwriters' Report. A very large increase in emissivity would be needed if the heat transfer calculations were to give some of the steel temperatures reported. The satisfactory agreement of the calculation method with the other sets of data, and indeed with some of the Underwriters' data, suggests that the method is not in serious error.

It will be noted that the comparisons given are for steel temperatures up to values of about 1000°F. The critical temperatures normally adopted in standard fire resistance tests for carbon grades of structural steel are 1000°F for columns and 1100°F for beams. Temperatures much in excess of these values are thus of little interest.

PRACTICAL APPLICATION OF THE CALCULATIONS

Fire and Flame Behavior for Larger Compartments—The correlations for flame and fire behavior show satisfactory agreement between calculation and measurement for the large-scale data, but it will be noted that most of these data are for room size compartments, the largest experiment being at Trenton, where the compartment was 56 ft wide by 24 ft deep. In the calculations, it is assumed that the whole compartment, however large, is involved in fire simultaneously, whereas for very large compartments the fire is likely to be progressive; if so, flame size and fire temperature could be overestimated. On the other hand a progressive fire could last longer, but this is allowed for by assuming steady state heating conditions. It is reasonable, therefore, to assume that the correlations may be used for a large compartment and that errors for large sizes will tend to be on the safe side.

The effective fire duration in the free-burning condition depends on the type, thickness, and spacing of the fire load. Experimental fire loads are usually of wood cribs—an easily reproducible fuel—and there has been considerable study of the effects of crib design on the rate of burning. Less information is available for furniture, but it can be assumed that a fire involving domestic furniture would give similar results to a fire in wood cribs with a free-burning time of 20 min.[34] It is unlikely that the amounts of plastics usually found in furnishings would significantly alter this value. For the type of fire load normally found in residential, office, educational, and hospital buildings, a free burning time of 20 min is recommended. For fire loads which are significantly different, it would be prudent to carry out a supplementary measurement. If significant amounts of combustible materials were used as linings for the walls and ceiling, the time could be very much shorter, (as found in Test S of the Borehamwood experiments), but these are not normally permitted by building codes and should not be used.

Compartment Parameters—In the calculation of R for ventilation controlled fires with natural draft, using Eq. (2), (D/W) represents the ratio of depth of the compartment to the width of the wall which contains the window or windows. If, however, there are windows on more than one wall, (D/W) is effectively reduced. In general, if the wall having the maximum window area is of width W:

$$\left(\frac{D}{W}\right)_{effective} = \frac{(A_w)_1}{A_w}\left(\frac{D}{W}\right)$$

where $(A_w)_1$ = window area on wall of width W
A_w = total window area, all walls

When there is more than one window, the value of A_w used in Eqs. (2), (5), and (30), for fires with natural draft, is the sum of the individual areas:

$$A_w = A_1 + A_2 + A_3 + \ldots \text{ etc.}$$

For forced draught, the value of A_w used in Eqs. (6), (19), and (24) is the sum of the individual areas of the windows through which the flames emerge, or approximately half the sum of all windows when there is a through wind.

The value of w used in Eqs. (10) and (23), for fires with natural draft, is the sum of the individual widths:

$$w = w_1 + w_2 + w_3 + \ldots \text{ etc.}$$

When the windows have different heights, the value of h used in Eqs. (2) and (5), and in n, for fires with natural draft, is as follows:

$$h = \frac{A_1h_1 + A_2h_2 + \ldots}{A_1 + A_2 + \ldots} \text{ etc.}$$

There is one exception to this, in the calculation of n for widely spaced windows. If the space between windows exceeds two window widths, according to Yokoi, the flames from the windows may be assumed to be separate and n should be calculated for the individual window.

Temperature Gradient in Steel—The measurements of column temperatures show that, in general, the maximum value is attained at the level opposite the top of the window, a typical difference between top and bottom window levels being 400°F. The way in which the temperature falls off above the window level will vary with the flame height. There can also be a temperature gradient across the section such that the inner flange facing the fire could be 400°F more than the outer flange, particularly when there is shielding. The calculations described here are not sensitive enough to estimate the size of such gradients, but it should be noted that in practice there can also be large gradients for internal members. Thus, it is reasonable to adopt an average temperature as the criterion, but to reduce the stress in an external structure which the engineer estimates to be particularly sensitive to differential heating.

Relationships Used in Calculations—For convenience, the main relationships used for calculations are summarized in Table 1, given in both U.S. and S.I. units.

CONCLUSIONS

The fire exposure of external structural elements cannot be simulated by the standard fire resistance test, but it can be calculated. The heat transfer to these elements depends on the flame trajectory and temperature, the temperature in the fire compartment, the position of the element, and the cooling of the element to the surroundings. As for internal elements, the structural performance depends on the steel temperature, and a calculation method for estimating the temperature rise can be established. The calculation method is based on comprehensive studies with models and on a number of large-scale experimental fire tests in realistic buildings. The calculated steel temperatures are in satisfactory agreement with values measured in the large-scale experimental fire tests.

External flame and internal fire behavior can be calculated, given the amount and type of fire load, the dimensions of the compartment or room assumed to be on fire, and the dimensions of the windows. The effects of a forced draft, such as a through wind, can also be allowed for. Equations describing the various types of behavior are summarized in Table 1. They can be applied to the sizes of rooms and compartments normally found in buildings.

Table 1. Summarized Relationships

		U.S. units lb, ft, min, Btu, °R	S.I. units kg, m, s, kJ, °K
Natural Draft			
Flame	$z + h$	$3.55\left(\frac{R}{w}\right)^{2/3}$	$12.8\left(\frac{R}{w}\right)^{2/3}$
	x	$0.45h\left(\frac{1}{n}\right)^{0.53}$	$0.45h\left(\frac{1}{n}\right)^{0.53}$
"No-wall"	x	$0.60h\left(\frac{z}{h}\right)^{1/3}$	$0.60h\left(\frac{z}{h}\right)^{1/3}$
	w_z	w	w
	ϵ_z	$1 - e^{-0.09\lambda}$	$1 - e^{-0.30\lambda}$
	$\frac{\theta_z}{\theta_\rho}$	$1 - 0.33\frac{l_w}{R}$	$1 - 0.027\frac{l_w}{R}$
	θ_o	$\frac{940}{(1 - 0.33Xw/R)}$	$\frac{520}{(1 - 0.027Xw/R)}$
	α_z	$0.027\left(\frac{R}{A_w}\right)^{0.6}\left(\frac{1}{d}\right)^{0.4}$	$0.026\left(\frac{R}{A_w}\right)^{0.6}\left(\frac{1}{d}\right)^{0.4}$
Fire	R	$\frac{L}{\tau_F}$ or	$\frac{L}{\tau_F}$ or
		$1.22\frac{(1 - e^{-0.065\eta})A_w h^{1/2}}{(D/W)^{1/2}}$	$0.18\frac{(1 - e^{-0.036\eta})A_w h^{1/2}}{(D/W)^{1/2}}$
	θ_f	$8025\frac{(1 - e^{-0.18\eta})(1 - e^{-0.25\psi})}{\eta^{1/2}}$	$6000\frac{(1 - e^{-0.10\eta})}{\eta^{1/2}}(1 - e^{-0.05\psi})$
Forced Draft[a]			
Flame	$z + h$	$12.5\left(\frac{1}{u}\right)^{0.43}\left(\frac{R}{A_w^{1/2}}\right)$	$16.9\left(\frac{1}{u}\right)^{0.43}\left(\frac{R}{A_w^{1/2}}\right)$
	x	$0.077\left(\frac{u^2}{h}\right)^{0.22}(z + h)$	$0.61\left(\frac{u^2}{h}\right)^{0.22}(z + h)$
	w_z	$w + 0.4x$	$w + 0.4x$
	$\frac{\theta_z}{\theta_o}$	$1 - 0.33\frac{lA_w^{1/2}}{R}$	$1 - 0.027\frac{lA_w^{1/2}}{R}$
	θ_o	$\frac{940}{(1 - 0.33XA_w^{1/2}/R)}$	$\frac{520}{(1 - 0.027XA_w^{1/2}/R)}$
	α_z	$0.0068\left(\frac{R}{A_w} + \frac{u}{13}\right)^{0.6}\left(\frac{1}{d}\right)^{0.4}$	$0.0065\left(\frac{R}{A_w} + 1.3u\right)^{0.6}\left(\frac{1}{d}\right)^{0.4}$
Fire	R	$\frac{L}{\tau_F}$	$\frac{L}{\tau_F}$
	θ_f	$2160\,(1 - e^{-0.20\psi})$	$1200\,(1 - e^{-0.04\psi})$
Surroundings			
	T_a	520	293
	α_s	$0.0040\left(\frac{\theta_s}{d}\right)^{1/4}$ or	$0.0014\left(\frac{\theta_s}{d}\right)^{1/4}$ or
		$0.005\,(\theta_s)^{1/4}$ for $d > 1$	$0.002\,(\theta_s)^{1/4}$ for $d > 0.3$

[a] A_w denotes window(s) through which flames emerge.

The heat transfer model which has been developed employs simplified flame shapes and effective temperatures, derived from the equations describing flame and fire behavior. It has been used for shielded and unshielded elements, engulfed in flame or not, attaining temperatures up to about 1100°F.

NOMENCLATURE

A_F = floor area ($D \times W$), ft^2
A_s = perimeter of steel, ft
A_T = $2A_F = 2H(D + W) - A_w$, ft^2
A_w = window area, ft^2
b = extinction coefficient, ft^{-1}
C = calorific value, Btu/lb
c = specific heat, Btu/(lb °F)
D = depth of compartment, ft
d = diameter, side of steel, ft
g = acceleration due to gravity, ft/min^2
H = height of compartment, ft
h = height of window, ft
I = heat transfer per unit area, Btu/(ft^2·min)
K = thermal conductivity, Btu/(ft·min·°F)
k = heat loss by conduction, Btu/(ft^2·min)
L = fire load, lbs
l = distance along flame center line from window, ft
M = mass of steel per unit run, lbs/ft
n = $2w/h$, dimensionless
R = rate of burning, lbs/min
r_o = $(A_w 2\pi)^{1/2}$, ft
T = absolute temperature, °R
t = time, min.
u = velocity, ft/min
W = width of compartment, ft
w = width of window, ft
X = center line distance of flame tip from window, ft
x = horizontal distance of flame tip from window, ft
y = vertical distance above top of window, ft
z = vertical distance of flame tip above top of window, ft
α = convective heat transfer coefficient, Btu/(ft^2·min·°F)
ϵ = emissivity, dimensionless
ϕ = configuration factor, dimensionless
μ = viscosity, lb/(ft·min)
Θ = dimensionless flame temperature

$$= \Theta_z \left[\frac{r_0{}^5 c_z{}^2 \alpha^2{}_z y}{R^2 C^2 T_a}\right]^{1/3}$$

θ = $T - T_a$, °F
θ_f = fire temperature rise, °F
ρ = density, lbs/ft^3
σ = Stefan Boltzmann constant, 2.861×10^{-11} Btu/(ft^2·min·°R^4)
τ = fire duration, min
T_F = free-burning fire duration, min
λ = flame thickness, ft

$$\eta = \frac{A_T}{A_w h^{1/2}}, \text{ ft}^{-1/2}$$

$$\psi = \frac{L}{(A_w A_T)^{1/2}}, \text{ lb/ft}^2$$

The subscripts $a, f, l, o, s, z,$ denote ambient, fire, local, window plane, steel, and flame respectively. Subscripts w and x used with the temperature denote flame temperatures level with the top of the window ($y = 0$) and at the flame tip, respectively.

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APPENDIX A
DIMENSIONS OF COMPARTMENTS AND TYPES OF FIRE LOAD IN LARGE SCALE EXPERIMENTAL FIRE TESTS

Test	D, ft	W, ft	H, ft	Type of fire load
Yokoi[13]				
1	31.8	43.8	11.5	Timber
2	11.4	14.1	8.1	Timber
3	8.2	16.4	5.5	Timber
4	8.2	16.4	5.5	Timber, plywood linings on walls and ceiling
Trenton[3]	24	56	9	Wood cribs of sticks 1½ in. thick
Disney World[14]	28	14	8.5	Wood cribs of sticks 3½ in. thick with 1 in. spacing
Borehamwood[9,15]	12	25	10.0	Wood cribs of sticks 1¾ in. thick with 1¾ in. spacing Fibre insulating board on walls and ceiling Test S
Tranas[25]				
I[a]	20.7 × 2	21.6	8.4	Mixed furniture
II[a]	20.7 × 2	21.6	8.9	Mixed furniture
Carteret[12]	12	10	8	Wood cribs
Kordina[26]	16.7	11.9	9.0	Office furniture
Webster[21]	8	8	8	Wood cribs of sticks 1 in. thick
Underwriters[16]	12	10	10	Wood cribs of sticks 1½ in. thick
Metz[10,17]	12	11	10	Wood cribs of sticks 2¾ in. × 1¾ in. thick with 1¾ in. spacing

[a] Windows on two opposing (shorter) walls.