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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_s	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	$A\sqrt{h}/A_s/\sqrt{k\rho c}$

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

$$I_W = A_i q_w \quad (4)$$

where

A_i = 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

$$C_1 = c\alpha_1,$$

$$C_2 = \alpha_1\alpha_2, \text{ and}$$

$$Y = (A\sqrt{h}/A_i)/\sqrt{(k\rho c)}.$$

$\Theta_f'(t - \xi)$ is the time derivative of the fire temperature at the time $(t - \xi)$. The numerator, $A\sqrt{h}/A_i$, 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].

<i>j</i>	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 $h = 25 \text{ W} \cdot \text{m}^{-2} \text{K}^{-1}$.

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. If 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 $u_x = \gamma_a t/x^2$.

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 $u_x = \gamma_a t/x^2$. 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 $x = 4$ cm and $y = 6$ 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.

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