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Heat Transfer Principles in Thermal Calculation of Structures in Fire

Chao Zhang^{a,*} and Asif Usmani^b

^aNational Institute of Standards and Technology, 100 Bureau Drive, Gaithersburg, MD 20899-8666, USA

^bSchool of Engineering, Edinburgh University, Edinburgh EH9 3JN, UK

Abstract

Structural fire engineering (SFE) is a relatively new interdisciplinary subject, which requires a comprehensive knowledge of heat transfer, fire dynamics and structural analysis. It is predominantly the community of structural engineers who currently carry out most of the structural fire engineering research and design work. The structural engineering curriculum in universities and colleges do not usually include courses in heat transfer and fire dynamics. In some institutions of higher education, there are graduate courses for fire resistant design which focus on the design approaches in codes. As a result, structural engineers who are responsible for structural fire safety and are competent to do their jobs by following the rules specified in prescriptive codes may find it difficult to move toward performance-based fire safety design which requires a deep understanding of both fire and heat. Fire safety engineers, on the other hand, are usually focused on fire development and smoke control, and may not be familiar with the heat transfer principles used in structural fire analysis, or structural failure analysis. This paper discusses the fundamental heat transfer principles in thermal calculation of structures in fire, which might serve as an educational guide for students, engineers and researchers. Insights on problems which are commonly ignored in performance based fire safety design are also presented.

Keywords

Structural fire analysis; Heat transfer; Fire resistance; Steel member; Thermal calculation; Flashover; Lumped heat capacity method; Flame radiation; Participating medium; Thermal resistance; Section factor; Localized fire; Large enclosure

1. Introduction

This paper presents theoretical descriptions of the key heat transfer principles that govern the thermal behavior of structures in fire. In particular,

- Section 2 introduces the theory of heat radiation through a participating medium.
- Section 3 discusses thermal calculation in a post-flashover fire environment. The applicability of design formulae for predicting the temperature of bare and

*chao.zhang@nist.gov(C. Zhang).

insulated steel members in fire is investigated by the theory of lumped heat capacity method. Theory of thermal radiation in participating medium is used to explain the variation of measured temperatures of steel members with the same cross section but at different locations in a fire compartment. A modified one zone model is discussed and used to investigate the heat sink effect of the steel members in a fire compartment.

- Section 4 discusses thermal calculation in a pre-flashover fire environment. Localized fire model is discussed and developed to calculate the heat fluxes to structural members in large enclosure. The applicability of design formulae (for post-flashover fires) for temperature calculation in localized fires is discussed. The usage of localized fire model to determine the safe distance from a unprotected steel column to a localized fire source is presented.

Section 5 gives the conclusion of this study; and Appendix A gives the correlations for calculation in localized fires.

2. Heat radiation through participating medium

The basic heat transfer principles, including conduction, convection, and radiation, are well documented and can be easily found in heat transfer textbooks like [1, 2, 3]. Below, the theory of heat radiation through a participating medium is presented, which is essential to understand the heating mechanism under fire conditions and is not commonly introduced in SFE textbooks.

For participating media like gases, the intensity of the incoming radiation will reduce with penetration distance by either the absorbing or the scattering effects of the medium. As shown in Fig. 1, consider a beam of radiation with intensity $E_{ir}(0)$ that passes through a participating medium of thickness L . By Beer's law the intensity of the radiation beam at point x is given by [1]

$$E_{ir}(x) = E_{ir}(0)e^{-\rho\kappa x} \quad (1)$$

where κ is called the extinction coefficient, which is generally the sum of the absorption coefficient and the scattering coefficient; ρ is density of the medium; and x is the penetration distance. Correspondingly, for the participating medium of thickness L , the absorptance $\alpha(L)$ is

$$\alpha(L) = \frac{E_{ir}(0) - E_{ir}(L)}{E_{ir}(0)} = 1 - e^{-\rho\kappa L} \quad (2)$$

By Kirchoff's law [1] we get the emissivity for the participating medium of thickness L , $\varepsilon(L)$, as

$$\varepsilon(L) = \alpha(L) = 1 - e^{-\rho\kappa L} \quad (3)$$

where $\rho\kappa L$ is called the optical path length or opacity.

The outgoing spectral radiation at L , E_{tot} in Fig. 1, is the sum of the reduced penetrating radiation and the emitted radiation by the participating medium [4],

$$E_{tot}(L) = [1 - \varepsilon(L)]E_{ir}(0) + \varepsilon(L)E_b \quad (4)$$

where E_b is black body radiation

3. Thermal calculation in a post-flashover fire environment

3.1. Assumptions and simplifications

The following assumptions and simplifications are usually adopted in the thermal calculation in a post-flashover fire environment [5]

- The gas properties are homogeneous in the fire compartment.
- Both hot gases and building components are assumed to be gray. The surfaces of building components are assumed to be opaque.
- In radiation calculation, the fire and the exposed surface are represented as two infinitely parallel gray planes that the view factor is taken as unit.

Correspondingly, the net heat flux transferred to an exposed surface is given by

$$\dot{q}'' = \dot{q}_c'' + \dot{q}_r'' = h[T_g(t) - T(0, t)] \quad (5)$$

where $\dot{q}_c''$ and $\dot{q}_r''$ are convective and radiative heat fluxes, respectively; $T_g(t)$ and $T(0, t)$ are temperatures of the surrounding gas and the exposed surface, respectively; and $h = h_c + h_r$ is the heat transfer coefficient. h_c is the convective heat transfer coefficient or film coefficient with values typically taken in the range 5–50 W/m²K [5]; and h_r is the radiative heat transfer coefficient

$$h_r = \frac{\varepsilon_f \varepsilon_s}{\varepsilon_f + \varepsilon_s - \varepsilon_f \varepsilon_s} \sigma [T_g^2(t) + T^2(0, t)] [T_g(t) + T(0, t)] \quad (6)$$

where ε_f , ε_s are the emissivity of the fire and the exposed surface, respectively. Fig. 2 shows the calculated radiative heat transfer coefficient at different values of T_g . The convective heat transfer coefficient values of 5, 25 and 50 W/m²K are also plotted for reference. Convection dominates at low temperatures, but above 400 °C (673 K) radiation becomes increasingly dominant.

3.2. Lumped heat capacity method for steel temperature calculation

3.2.1. The lumped heat capacity method—The expression for Fourier's law is similar to that for Ohm's law in electric-circuit theory. As a result, an electrical analogy can be used to solve heat conduction problems. Fig. 3 illustrates an analogous circuit composed of two thermal resistances in series, which represents a 1D heat transfer model. R_i is thermal resistance. For conduction, $R_i = \delta_i/k_i$, in which δ_i , k_i are the thickness and conductivity of material i , respectively. In steady state, if thermal resistance R_1 is much greater than R_2 ($R_1 \gg R_2$), the temperature difference ratio $(T_2 - T_3)/(T_1 - T_2) \approx 0$ and if R_2 is also small, $T_2 \approx$

T_3 . The *Biot* number is used to determine the applicability of the lumped capacity method [1]

$$Bi = \frac{R_2}{R_1} = \frac{\delta/k}{1/h} = \frac{h(V/A)}{k} \quad (7)$$

where $\delta = V/A$ is the characteristic thickness of the solid which is subjected to a convection like boundary condition with heat transfer coefficient h . In practice, the lumped heat capacity method which assumes a uniform temperature distribution in a solid can be used provided $Bi < 0.1$ [6].

3.2.2. Biot number for commonly used steel sections—Fig. 4 shows the calculated *Biot* number Bi for bare steel members with various section factors A/V . The section factor for commonly used steel sections ranges from 30 m^{-1} to 320 m^{-1} [7]. Bi decreases with temperature due to the increase of h_r . For steel members with small section factors (e.g. 30 m^{-1} , 50 m^{-1}), at low gas temperatures (e.g. below 600°C), Bi is less than 0.1, so that the lumped heat capacity method is valid; but at high temperatures, Bi becomes greater than 0.1, so that the lumped heat capacity method becomes invalid. For steel members with section factors greater than about 135 m^{-1} , Bi is always less than 0.1 for typical fire temperatures.

3.2.3. Temperature of bare steel members—In current structural fire design codes, the average temperature of bare steel members is calculated by using the lumped heat capacity method. In the Eurocode [8]

$$\Delta T_s = \frac{A/V}{c_s \rho_s} \dot{q}'' \Delta t \quad (8)$$

where $\dot{q}''$ is calculated using Eq. 5 by taking the surface temperature $T(0, t)$ as the average steel temperature T_s . c_s , ρ_s are the specific heat and density of the steel, respectively. Here, shield effect is ignored.

Fig. 5 compares the Eq. 8 prediction against the finite element method (FEM) calculated average temperatures of bare steel I sections when all sides are exposed to the standard ISO 834 fire. In previous work [9], the FEM was successfully used to predict the temperature of steel members in a fire test. Table 1 lists the investigated sections and their section factors. The Eurocode equation (Eq. 8) is conservative and over-predicts the steel temperatures, but the over-prediction decreases with increasing section factor as shown in Fig. 6. The maximum over-prediction for a section factor of 32 m^{-1} is about 100°C and the maximum over-prediction for section factor of 208 m^{-1} is about 65°C . Fig. 7 shows the FEM predicted temperature distributions in different steel sections. The temperature gradient increases with decreasing section factor. Taking 550°C as a limiting temperature² [10], Table 1 also lists the fire resistances (*FR*) for different sections predicted by Eq. 8 and FEM. The equation is conservative in terms of fire resistance. The *FR* difference decreases with increase of the section factor.

²The limiting temperature of a steel member is related to the applied load or utilization ratio. 550°C is often taken as the limiting temperature for steel columns in simple calculations.

3.2.4. Requirement for uniformly insulated steel members—In current codes, when calculating the temperature of uniformly insulated steel sections in fire, a 1D heat transfer model based on the lumped heat capacity method that assumes a uniform temperature within the steel section is used [11]. At the fire exposed surface, a Dirichlet boundary condition ($T(0, t) = T_g(t)$) is usually assumed for simplicity [11].

Consider Fig. 3, by interpreting T_1 as the fire temperature (Dirichlet boundary condition), R_1 as the thermal resistance of the insulation (R_{in}) and R_2 as the thermal resistance of the steel, from Eq. 7 we get the requirement for applying the lumped heat capacity method for uniformly insulated steel members,

$$R_{in}=R_1 > \frac{R_2}{0.1} = \frac{10}{k_s(A/V)} = \frac{2}{9(A/V)} \quad (9)$$

3.2.5. Temperature of uniformly insulated steel members—In the Eurocode [8], the average steel temperature of uniformly insulated steel sections is calculated from

$$\Delta T_s = \frac{A/V}{c_s \rho_s R_{in}} \frac{T_g - T_s}{1 + \mu/3} \Delta t - (e^{-\mu/10} - 1) \Delta T_g \quad (10)$$

with

$$\mu = \frac{c_i \rho_i d_i}{c_s \rho_s} (A/V) \quad (11)$$

where c_i , ρ_i are the specific heat and density of the insulation, respectively; and d_i is the thickness of the insulation.

Fig. 8 compares Eq. 10 and FEM predicted average steel temperatures for a steel section uniformly insulated by various thicknesses of a spray-applied fire resistive material (SFRM). The section factor is taken as 32 m^{-1} and the thermal conductivity of the SFRM is taken as a typical value of 0.12 W/mK [10]. Accordingly, the required thermal resistance calculated by Eq. 9 is $2/(9 \times 32) = 0.007 \text{ m}^2\text{K/W}$. Therefore, if the thickness of the insulation is greater than $0.12 \times 0.007 \times 1000 = 0.84 \text{ mm}$, the temperature distribution in the steel section can be approximated as uniform. Fig. 9 shows that the temperature gradients in the insulated steel section are indeed very small and the gradients decrease with increasing insulation thickness. Correspondingly, Eq. 10, which is based on the lumped heat capacity method, gives consistent results with FEM, as shown in Fig. 8.

It should be noted that with increasing insulation thickness, although the temperature distributions in the steel section approaches being more and more uniform, the average temperature predicted by Eq. 10 diverges from the temperature given by FEM when the steel section becomes smaller in comparison to the insulation layer, as shown in Fig. 10. The divergence is caused by two reasons: on the one hand, when the insulation layer becomes comparatively large, the heat transfer process cannot be simplified as one dimensional; and on the other hand, Eq. 10 cannot accurately account for the heat sink effect of the insulation layer and only produces acceptable results when that effect is not important [11].

3.3. Effect of location on steel temperature

From Eq. 3, the emissivity of a participating medium depends on the thickness of the medium. Therefore, the radiative heat flux transferred from the fire environment to the surface of a component is relative to the location of the component in the fire compartment (because of the different emissivities). Correspondingly, the temperature of a component in a fire compartment is affected by its location.

Consider a fire compartment shown in Fig. 11, the compartment is filled with flames produced by combustion and the temperature of the flames follows the standard ISO 834 fire curve. Fig. 12 compares the calculated average temperatures of a bare steel section located at different positions in Fig. 11. In calculating the radiative heat flux transferred to each surface of the section, the assumptions given above (see Section 3.1) were adopted and the flame emissivity was calculated by Eq. 3 (as illustrated by the red arrow in Fig. 11) in which the effective emission coefficient $\rho\kappa$ was taken as 1.13 m^{-1} representative of assorted furniture [5]. The irradiations from the compartment walls were not considered. Fig. 12 shows that the steel temperatures are indeed different at different locations. The section has the maximum temperature when located at the center of the compartment (Location 3 in Fig. 11), and has the lowest temperature when located near the corner of the compartment (Location 1 in Fig. 11). The maximum temperature difference is 93°C .

Taking 550°C as a limiting temperature, Table 2 compares the calculated fire resistances for the above and some other steel sections at different locations shown in Fig. 11. The effective emission coefficients $\rho\kappa$ taken as 0.51 m^{-1} and 0.8 m^{-1} are for wood cribs [12]. Wood cribs are commonly used in fire tests and are traditionally used to characterize the fire loads for structural design [10]. The maximum difference among the calculated fire resistance at different locations reaches 5.2 min. The fire resistances predicted by the Eurocode with fire emissivity taken as 1 are also provided in the table. Obviously, the Eurocode gives conservative results.

It should be noted that the non-uniformity in gas temperatures and the radiation from the compartment boundaries also contribute to the difference of steel temperatures at various locations. The effect of gas layer emissivity discussed here, however, is not well acknowledged in practice.

3.4. Heat sink effect of building components

It is obvious that the building components in a fire compartment absorb a portion of the energy released by combustion of the fuel in the compartment. The fire or gas temperature decreases because of the loss of that energy, while the temperature of the components increases because of the absorption of that energy. As a result, the temperature of a component depends on the heating mechanism of the compartment (and also on the location of the component). However, as shown in Fig. 13, in the current approach to calculate the temperature of a component in a fire compartment, a fire curve is first derived from the one zone model, and then the curve is used to calculate the temperature of the component [9]. Since the heat sink effect of the component is not considered in the one zone model, the current approach over-predicts the temperature of the component [13].

Eq. 12 gives the heat balance equation for a modified one zone model which considers the heat sink effect of the components in a fire compartment [13],

$$HRR = \dot{q}_g'' + \dot{q}_w'' + \dot{q}_o'' + \dot{q}_m'' \quad (12)$$

HRR is the heat released by combustion; $\dot{q}_g''$ is the heat storage in the gas volume; $\dot{q}_w''$ is the heat loss through the compartment boundaries (e.g. walls, ceiling, floor); $\dot{q}_o''$ is the heat loss through openings; and $\dot{q}_m''$ is the heat stored in the components. The heat balance equation for the traditional one zone model does not include the term $\dot{q}_m''$.

Fig. 14 shows a FE model which was verified in previous work [13] to predict the temperature of steel components (bare or insulated) in a fire compartment by the modified one zone model. To illustrate the heat sink effect of the building components, we consider a fire compartment with four steel columns located at different corners of the compartment. The compartment is 5.0 m wide, 4.0 m deep and 3.0 m high and is made of gypsum board of thickness 200 mm. The compartment has a window 4.8 m wide and 1.5 m high and it also has 2 fire doors. The steel sections are UC203×203×71. The floor fire load density is 600 MJ/m² not unlike office buildings [14]. Assuming that the window glass is immediately broken when fire breaks out and the fire doors remain closed over the whole fire duration, and taking the column failure temperature as 550 °C, the required minimum fire protection is 6.7 mm thick SFRM, using the current prescriptive approach, and 5.8 mm using the modified one zone model.

Fig. 15 compares the predicted fire and steel temperatures using the current approach and the modified one zone model. The current approach gives higher steel temperatures because it does not consider the heat sink effect of the steel members. The fire temperature for the compartment without steel columns (calculated using the one zone model) is higher than that of the compartment with the bare steel columns (calculated by the modified one zone model), and is lower than that for the compartment with insulated steel members (calculated by the modified one zone model). Clearly, the high thermal conductivity and large heat capacity of the steel captures a portion of the heat to be stored in the columns (heat sink effect). Furthermore, the heat stored in the gas volume in a compartment with the bare steel columns is smaller than the heat stored in the gas volume in the compartment without the columns, therefore, the predicted fire temperature for the compartment with bare steel columns is lower. However, when the steel columns are insulated, the insulation layer reduces heat transfer to the steel and, also, the presence of the columns reduces the volume of the gas for the same fuel load which leads to a higher fire temperature.

4. Thermal calculation in a pre-flashover fire environment

4.1. Localized fire model and two zone model

Fig. 16 illustrates a localized fire model to represent the fire environment in large enclosures [15]. The fire plume is vertically divided into the lower combustion (flame) region and the upper non-combustion (plume) region. When there is a ceiling, the hot gases will be deflected as a horizontal ceiling jet. The temperature distribution in a localized fire is highly non-uniform, as shown in Fig. 17. In practice, correlations derived from classic plume

theory are used to calculate the non-uniform gas temperatures in a localized fire [15]. It should be noted that the correlation derived from the top-hat assumption which assumes uniform distributions of plume temperature and velocity in the horizontal plane is widely used in smoke control codes such as NFPA 92B and TM 119, but this is not applicable for structural fire design.

As shown in Fig. 18, two zone model which divides the gas volume in the compartment into the upper hot uniform zone and the lower cool uniform zone is widely used to represent the pre-flashover fire environment in a small/medium sized compartment [16, 17]. In practice, the equation derived by McCaffrey, Quintiere and Harkleroad [18] is usually adopted to calculate the temperature rise above ambient temperature in the upper hot zone, provided that the upper gas layer does not exceed between 500 °C to 600 °C. Further temperature rise in the hot zone will cause flashover and the two zones will be converted into one zone [17], but only if the enclosure is not too large, when there can only be local flashover (e.g. Bradford City Stadium fire in United Kingdom). Since the (average) hot gas temperature calculated by the two zone model is not high, it is usually thought that pre-flashover fires are not hazardous to structures. However, the local high temperature in a pre-flashover fire (as shown for the case with a low ceiling in Fig 19) can indeed damage structural components near the fire source especially when there is flame impingement, as found in [19, 20, 21, 22, 23]. Therefore, a localized fire model instead of a two zone model is recommended for structural fire design.

4.2. Thermal radiation to horizontal components

Fig. 20 illustrates a theoretical model to calculate the thermal radiation to a horizontal surface above a fire source. The fire plume volume is represented by a cylinder. The diameter of the cylinder is taken as the equivalent diameter of the fire source (calculated by appendix Eq. A.5) and the height of the cylinder is taken as the ceiling height. Flame and smoke are assumed to be gray and have the same emission coefficients $\rho\kappa$. The temperatures in the horizontal sections of the cylinder are uniform and taken as the centerline temperature of the fire plume (calculated by Eq. A.1).

The thermal radiation received by the section at a height H is calculated by

$$E(H) = \int_0^H E_z dz \quad (13)$$

with

$$E_z(dz) = (1 - \alpha) F \varepsilon \sigma \times (T_g + 273)^4 \quad (14)$$

where

$$\alpha(H - z) = 1 - e^{-\rho\kappa(H-z)} \quad (15)$$

and

$$\varepsilon(dz) = 1 - e^{-\alpha(H-z)} \quad (16)$$

where $\alpha(H-z)$ is the absorptivity of the cylinder volume with height $H-z$; $\varepsilon(dz)$ is the emissivity of the cylinder volume with height dz ; F is the view factor between two parallel circular surfaces calculated by the equations in Fig. 20.

Figs. 21 and 22 show the radiative heat fluxes to the horizontal surface at various heights H calculated by using Eq. 13. The black body radiation calculated using the centerline temperature of the fire plume at the height, and the convective heat flux calculated by $h_c T_g$ are also presented for comparison. The centerline temperatures are also presented. With increase in height, the centerline temperature and view factor decrease while the emissivity increases, and therefore, the radiative heat flux calculated by the theoretical model is at first much less, then greater, and finally less than the black body radiation calculated using the centerline temperature. Therefore, for calculating the temperature of a horizontal component located just above a fire source and components far from the flame, using the Eurocode approaches for post-flashover fire environments (e.g. Eq. 8 and Eq. 10), which directly use the gas temperature near the component as effectively the black body temperature, might yield very conservative results.

4.3. Thermal radiation to vertical components

For steel columns fully engulfed in (or surrounded by) a localized fire, the Eurocode equations for post-flashover fire environments (e.g. Eq. 8 and Eq. 10) may be used to predict the maximum temperature. Taking the surrounding gas temperatures as the centerline temperatures of the fire plume (calculated by Eq. A.1), Fig. 23 shows the results for the maximum temperatures of a steel column surrounded by a localized fire predicted by Eq. 8 and FEM. The fire temperature was taken as the maximum centerline temperature when using Eq. 8. In the FEM calculation, 3D heat transfer considering gas temperature variation along the column length was analyzed. Fig. 24 shows the temperature distribution in the steel column predicted by the FEM analysis. There is a strong temperature gradient along the column length because of the varying gas temperature. Due to thermal conduction along the column length, the maximum section temperature predicted by the FEM analysis is lower than that predicted by Eq. 8 until the 3D heat transfer process becomes steady, as shown in Fig. 23. Therefore, depending on the duration of the fire (and also the type of the fire), the maximum section temperatures predicted by Eq. 8 may be too conservative.

For calculating flame radiation to adjacent targets, solid flame models are commonly used [24, 25, 12]. The solid flame models represent the flame by a solid geometry such as a cylinder or cone, and assume that thermal radiation is emitted from the side surfaces. Correspondingly, the incident radiative flux to a target is given by [25]

$$\dot{q}_{in}'' = F_{1-2} E_f \quad (17)$$

where E_f is the emissive power of the flame, and F_{1-2} is the view factor between the target and the flame. From Eq 17, we may define an effective black body temperature:

$$T_{beff} = (\dot{q}_{in}''/\sigma)^{1/4} - 273 \quad (18)$$

The temperature of the adjacent targets cannot exceed the calculated effective black body temperature. Fig. 25 shows the calculated effective black body temperatures for a vertical target located adjacent to a flame. The flame is modeled by a cylinder with an emissive power of $E = 58(10^{0.00823D_f})$ kW [25]. The cylinder (flame) height is calculated by Heskestad's correlation (Appendix Eq. A.6). The target is located at half the height of the cylinder, where the view factor between the cylinder and the target is the maximum. The horizontal distance between the cylinder edge and the target is X . The black body temperature decreases with increasing X . For $X > 0.5D_f$ (D_f is the equivalent diameter of the fire source calculated by Eq. A.5), the black body temperatures are less than 550 °C. Therefore, taking 550 °C as the limiting temperature, if the horizontal distance from the fire source to an adjacent steel column is greater than the fire diameter, the bare steel column might maintain its stability without applying any fire protection³. We are aware that there are many uncertainties in the use of buildings, however, the proposed model provides a feasible way to ensure the safety of steel columns by isolating the columns from potential fire sources for a particular distance, which may be used in performance based fire safety design.

5. Conclusion

This paper presents a theoretical investigation on the fundamental principles of heat transfer to structural members in a fire. Based on the results of this investigation, the following conclusions can be drawn:

- The current approaches to calculate the temperature of bare steel members in fire (e.g. the formula given in the Eurocode, such as Eq. 8) are based upon the lumped heat capacity method, and give conservative results compared with more rigorous analysis. The magnitude of over-prediction of the current approaches decreases with increasing the section factor. The fire resistance of commonly used steel sections predicted by the current approaches are about 13% lower than those obtained from more rigorous approaches, such as FEM. Here, the fire resistance is simply evaluated by a critical temperature of 550 °C.
- The lumped heat capacity method is valid for calculating the steel temperature of uniformly insulated steel members provided that the thermal resistance of the insulation layer is greater than $\frac{2}{9(A/V)}$. Here A/V is the section factor.
- The temperature of a component in a uniform fire compartment is affected by its location. Ignoring irradiation from the compartment walls, a component located at the center of a uniform fire compartment has the highest temperature, whereas a component located at the corner has the lowest temperature.

³Readers should be aware that this statement is based on very crude simplifications, applying the statement for design might yield unsafe results. Further investigations by advanced calculation approaches or fire tests are required to verify the statement.

- The current code based models used to predict the temperature of steel members in a fire compartment ignore the heat sink effect from the mass, even though this effect is important as demonstrated by a modified one zone model which considers the heat sink effect. For the conditions considered in this study, without considering this effect yields a design of fire protection about 16% thicker than with considering it.
- A localized fire model instead of the two zone model is recommended for structural fire safety design in large enclosures, where flashover is unlikely to happen.
- A theoretical model is developed to calculate the thermal radiation from a fire plume to the horizontal surfaces located above the fire source. It shows that the current approaches in post-flashover fire environments (e.g. the equation in the Eurocode) for calculating temperatures of horizontal components that are situated close to the fire source and far away from it yield conservative results.
- Using code based approaches (e.g. the equations in the Eurocode) to calculate the temperature of steel members engulfed in a localized fire may be very conservative.
- A theoretical model is developed to calculate the safe distance of vertical components adjacent to a localized fire. If the horizontal distance from an adjacent steel column to the fire source is greater than the calculated safe distance, the steel column might be left unprotected.

Readers should be aware that the calculations in this paper are based on simplified approaches that fire tests or sophisticated calculations are needed to verify some of the conclusions.

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Appendix A

Correlations in localized fire

The centerline gas temperature in a localized fire is calculated by [15]

$$T_g = 20 + \min(\Delta T_{plume}, \Delta T_{flame}) \quad (A.1)$$

with

$$\Delta T_{plume} = 0.25(1000\dot{Q}_c)^{2/3}(z - z_0)^{-5/3} \quad (A.2)$$

and ΔT_{flame} is the flame centerline temperature increment, as calculated by Quintiere and Grove [26]. For simple calculation [15]

$$\Delta T_{flame} = 1500(1 - \chi_r) \quad (A.3)$$

where $\dot{Q}_c = (1 - \chi_r)\dot{Q}$ is the convective part of the heat release rate (*HRR*) of the fire source, in which $\dot{Q}$ is the heat release rate of the fire source (labeled as *HRR* in the main text such as Eq. 12) and χ_r is the radiative fraction; and z_0 is the height of the virtual fire source, calculated by

$$z_0 = -1.02D_f + 0.083\dot{Q}^{2/5} \quad (A.4)$$

where D_f is the equivalent diameter of the fire source, calculated by

$$D_f = \sqrt{4A_f/\pi} \quad (A.5)$$

in which A_f is the area of the fire source.

The flame height is calculated by [27]

$$L_f = -1.02D_f + 0.235\dot{Q}^{2/5} \quad (A.6)$$

The ceiling jet temperatures is calculated according to SFPE handbook [28].

Figure 1.
Energy out from a participating medium.

Figure 2.

Radiative heat transfer coefficient calculated by using Eq. 6 with $\varepsilon_f = 1$ and $\varepsilon_s = 0.8$.

Figure 3.
An electrical analogy for 1D heat conduction.

Figure 4.

Biot number for bare steel members engulfed in gases with constant temperatures. (h_r is calculated by Eq. 6 with $\varepsilon_f = 1$, $\varepsilon_s = 0.8$, $T(0, t) = T_g$; h_c is taken as $25 \text{ W/m}^2\text{K}$; and k_s is taken as 45 W/mK).

Figure 5.
Comparison between Eq. 8 (marked as "EC3") and FEM predicted average steel temperatures of bare steel sections subjected to the standard ISO834 fire.

Figure 6. Maximum differences between Eq. 8 and FEM predicted average steel temperatures of bare steel sections subjected to the standard ISO834 fire.

Figure 7.

Temperature distribution in bare steel sections subjected to the standard ISO834 fire. The maximum temperatures in different sections are equal and taken as 550 °C. The plotted temperatures are taken from the flange and web center lines.

Figure 8.

Comparison between Eq. 10 (marked as “EC3”) and FEM predicted average steel temperatures of an insulated steel section subjected to the standard ISO834 fire ($A/V = 32 \text{ m}^{-1}$, $k_i = 0.12 \text{ W/mK}$) for various SFRM thicknesses.

Figure 9.

Temperature distribution in a steel section uniformly insulated by various thickness subjected to the standard ISO834 fire ($A/V = 32 \text{ m}^{-1}$, $k_i = 0.12 \text{ W/mK}$). The maximum temperatures for different insulation thicknesses are equal and taken as 550°C . The plotted temperatures are taken from the flange and web center lines.

(a) average temperature

(b) temperature gradient

Figure 10.

Predicted Average temperature and temperature gradient for a steel section with $A/V = 137$ insulated by different thickness of SFRM subjected to the standard ISO834 fire. In subfigure (b) the maximum temperatures for different insulation thicknesses are equal and taken as 550 °C, and the plotted temperatures are taken from the flange and web center lines.

Figure 11.
Steel sections located at different positions in an enclosure

Figure 12.

Calculated steel temperatures (by Eq. 8) at different locations for the case with $A/V = 53$ m^{-1} , $\rho\kappa = 1.13$ m^{-1} .

Figure 13.

Current model for predicting the temperature of uniformly insulated steel members in post-flashover fires

Figure 14.

A FE model to simulate the modified one zone model for a fire compartment with steel components

(a) fire temperature

(b) steel temperature

Figure 15.

Comparison between the temperatures obtained from the current approach by the one zone model and the proposed approach by the modified one zone model

Figure 16.
Illustration of a localized fire model

Figure 17.
Numerical results for gas temperature in a localized fire [15].

Figure 18.
Illustration of a two zone model

Figure 19.
Calculated gas temperature rise beneath an unconfined fire at ceiling with various heights (H_c) by correlations given in the Appendix. The fire source is a 1 m^2 square, 600 kW steady fire.

Figure 20.

A theoretical model to calculate the radiation to an horizontal components in a localized fire

Figure 21.

Comparison between the radiative heat fluxes to a horizontal surface above a 1 m^2 , 600 kW steady fire source by the cylinder model (Eq. 13) and by black body radiation using the fire plume centerline temperature at the surface. The calculated flame length is about 1.9 m . The convective heat fluxes are calculated by $h_c T_g$ with $h_c = 9 \text{ W/m}^2\text{K}$ [27]. The inset figure at the right top corner is the zoom-in of a portion of the plot.

Figure 22.

Comparison between the radiative heat fluxes to a horizontal surface above a 2 m^2 , 1.2 MW steady fire source by the cylinder model (Eq. 13) and by black body radiation using the fire plume centerline temperature at the surface. The calculated flame length is about 2.4 m . The convective heat fluxes are calculated by $h_c T_g$ with $h_c = 9 \text{ W/m}^2\text{K}$ [27]. The inset figure at the right top corner is the zoom-in of a portion of the plot.

Figure 23.

Comparison between the Eq. 8 and FEM predicted maximum (average) steel temperatures in the sections of a steel column fully engulfed in a localized fire. The steel column is 3 m height and has a cross section of UC203×203×71. The fire source is a 1 m², 600 kW steady fire. The convective heat transfer coefficient h_c is taken as 9 W/m²K

Figure 24.
FEM predicted temperature distribution in a steel column surrounded in a localized fire.

Figure 25.
Effective black body temperatures for a target adjacent to a flame.

Table 1

Fire resistances for bare steel members

Section	A/V (m^{-1})	FR_{Eq} (min)	FR_{FEM} (min)	$FR_{Eq}-FR_{FEM}$ (min)	$FR_{Eq}-FR_{FEM}$ $\frac{FR_{FEM}}{\%}$
UC356×406×634	32	27.6	32.5	-4.9	15.1
UC305×305×283	55	19.7	23.0	-3.3	14.3
UC254×254×167	76	16.3	18.9	-2.6	13.8
UC203×203×71	137	11.5	13.3	-1.8	13.5
UC203×203×46	208	9.3	10.7	-1.4	13.1

Table 2

Location effect on fire resistance of steel component

HSS section $h_s \times b_s \times t_s$	A/V (m^{-1})	$Q\kappa$ (m^{-1})	$FR_{Loc\ 1}$ (min)	$FR_{Loc\ 2}$ (min)	$FR_{Loc\ 3}$ (min)	$FR_{Loc\ 4}$ (min)	FR_{EC3} (min)
500×300×20	53	0.510	27.9	24.8	23.7	26.3	20.2
500×300×20	53	0.800	26.8	23.1	21.8	24.8	20.2
500×300×20	53	1.13	26.1	22.3	20.9	23.9	20.2
500×300×10	103	1.13	17.3	15.0	14.1	15.9	13.6
500×300×7.5	136	1.13	14.6	12.7	12.0	13.5	11.6