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TECHNICAL NOTE ON

FIRE RESISTANT DESIGN OF OFFSHORE TOPSIDE STRUCTURES

FIRE AND BLAST INFORMATION GROUP

TECHNICAL NOTES AND WORKED EXAMPLES TO COMPLIMENT THE IGN'S

Technical Note on Fire Resistant Design Of Offshore Topside Structures

- This document is a deliverable of the Fire And Blast Information Group (FABIG) for the year March 1992 - February 1993.

- We wish to acknowledge and thank those FABIG members who reviewed and commented on the draft version of this technical note.

- FABIG would like to encourage comment and feedback from its membership. If you have any comments on this Technical Note or any other FABIG activities please address them to Mr Hugh Bowerman, FABIG Project Manager at The Steel Construction Institute.

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1. INTRODUCTION

Section 4 of the Interim Guidance Notes for the Design and Protection of Topside Structures against Explosion and Fire (IGN) gave design guidance for fire resistance. This technical note is intended to compliment that section as follows:

- in a number of subject areas more information is given;
- more commentary is included, particularly with regard to the analysis methods;
- worked examples are included.

It is intended that this document should not duplicate the IGN, although for clarity it is inevitable that parts will.

The scope of this document is only intended to cover the following sections of the IGN:

- section 4.4 - Determination of Component Temperatures
- section 4.6 - Response to Fire Effects

IGN Section 4.4 provides a number of equations, but comparatively little guidance on how to apply them. Through the worked examples this technical note attempts to illustrate how the equations are used.

IGN Section 4.6 gives the main criteria to consider when trying to determine the fire resistance of a structure, but is considered to be over concise. In particular it is short on methodology and inadequately covers the range of techniques that are available.

2. BACKGROUND

The topsides of a platform is a generic term for the upper sections of a jacket, support frames, modules, bridges, accommodation and plant; all of which are vulnerable to the effects of fire. Current guidelines and regulations covering fire safety have been derived from experience with ships and onshore petrochemical plants. However these items experience different hazards from offshore structures and the validity of applying current guidelines is questionable. Careful process, fire protection and layout design can all contribute to reducing the size, probability of occurrence, intensity and duration of possible fires. Nevertheless, in order to quantify the reduction in risk, characterisation of the particular fire scenario is required.

Fire protection on onshore structures is generally designed to ensure the structure survives a conflagration. Offshore, the fire requirements are substantially concerned with personnel evacuation, long term damage to the structure being of lesser importance. The Health & Safety Executive now implement a goal-setting safety philosophy, based on Lord Cullen's recommendations stemming from the Piper Alpha inquiry [1]. Central to this philosophy is the requirement that each installation must have a designated Temporary Refuge (TR). Goal-setting regulations will replace existing regulations in areas of construction, plant, equipment, fire and explosion protection and evacuation escape and rescue. The TR must be designed to survive major hazards such as fire, for the time which is required to evacuate the platform.

Considerable research effort has been expended by the onshore sector of industry into quantifying the development of fires, the generation and movement of combustion products and inherent fire resistance of unprotected or partially protected structure. The need to simulate the effects of varying fire characteristics in real fires onshore has been largely overcome by the use of a widely adopted standard furnace test fire. This is possible because most fires are in buildings and burn cellulosic based fuels. The resulting compartment fires have similar thermal characteristics.

Most research work has been calibrated against cellulosic fires, whereas topsides structures are most likely to be subject to hydrocarbon fires. There are major differences between these fire types. For example, offshore fires are larger, generally more intense and reach their maximum burning rates much faster than onshore cellulosic fires. Thus, a hydrocarbon fire will attain its peak temperature of 1100°C in a matter of minutes whilst it may take about an hour for a cellulosic fire to reach 950°C. Furthermore, the furnace tests used to simulate onshore fires are normally capable only of reproducing the effects of an engulfing fire whereas on open or partially confined platforms there is a need to know the effect of a fire on members outside the flame.

In contrast to the approach for serviceability loads, fire is an accidental load. Design for accidental loads allows permanent deformations providing these are not so excessive as to threaten TR integrity, passability of escape routes, lifeboat embarkation points, damage the control room or lead to escalation of the accidental event. Design for accidental events thus differs from normal design and is based on specific scenarios. In this approach each scenario is simulated as accurately as possible to determine the loading (e.g., fire heat fluxes) and the consequences (e.g., temperature of the steel rising with time and the progressive structural collapse). The loads in the analysis would normally vary through time with acceptance of a design solution based on the endurance simulated by the analysis.

Finally, designing a topside structure for fire under a goal-setting regime requires an integrated multi-discipline approach. Thus the engineer should be aware of the assumptions and logic behind the identified scenarios and any identified criteria (such as endurance time from the escape and evacuation assessment, or deflection criteria to avoid overstressing hydrocarbon pipework / firewater mains), to ensure integration of the structural aspects within a coherent and consistent safety case.

3. ACCEPTANCE CRITERIA

3.1 Purpose of acceptance criteria

An acceptance criterion defines an agreed limit beyond which the structure or part of the structure is deemed to have failed. In building codes, the deflection of a member under a loading condition is a check of structural acceptance. This is a serviceability criterion to protect finishes etc. In contrast to the approach for service loads, design for accidental loads, such as fire, allows much larger and permanent deformations providing that these are not so excessive that they threaten TR integrity, etc.

For offshore structures subject to fire loading, acceptance criteria are typically associated with the protection of the:

- primary structure
- safety systems
- Temporary Refuge (TR)
- escape routes.

It is necessary to demonstrate that no part of the structure impinges on critical equipment and that deformations do not cause collapse of any part of the structure which supports the TR, escape routes and embarkation points within the required endurance period. Nor should collapse lead to an event which, through escalation, might violate the above criteria (see section 8.4)

The required TR endurance depends on the size, activity, complexity and number of persons aboard the installation. Endurance time must take account of the time for response to an accident, travel to the TR, muster, decision and safe controlled evacuation. This technical note is not concerned with the determination of what endurance times are required, but rather the determination of the length of time that the structure can withstand a given scenario.

The remainder of this section discusses in more detail three typical acceptance criteria adopted in fire resistant design for topsides structures. The criteria are presented in order of increasingly accurate methodology with correspondingly decreasing levels of conservatism. The appropriate acceptance criteria to use will depend on the level of complexity of the structural response analysis, for example, a full non-linear structural response analysis will enable a structural collapse criterion to be used. **It should be noted that more advanced analysis does not necessarily enable a safer structure to be designed, but instead enables the designer to justify using some of the reserve inherent in the simpler forms of analysis. A structure designed using advanced techniques is likely to have less reserve than one designed using simpler techniques due to the reduction in conservatism in the analysis methods.**

3.2 Acceptance criterion (1) - Strength

The most important criterion is strength; the structure or parts of the structure must retain their strength during the fire for an adequate length of time. This criterion can be specified either in terms of defining a limiting temperature, or, more accurately, in terms of a limiting strain or deflection.

Limiting temperature

This acceptance criterion requires the temperature of the structural steel to be limited to a given value, typically 400-500°C [2]. This is the temperature at which steel exhibits an approximately 50% reduction in yield stress. The yield stress then corresponds approximately to the likely working stress level [2]. This assumes that the effect of stresses induced by thermal restraint can be ignored (see section 8.3).

Limiting strain

The following criteria determine the strain limit to be used in design:

- The type of structural member (beam, column, tie)
- Member cross-sectional geometry and proportions
- The deformation capacity of any protection material present

In fire tests on loaded bare steel beams, high strains are developed. At a deflection of span/30, strains well in excess of 3% have been measured [3]. However, it should be noted that this deflection also includes a component arising from thermally-induced curvature. Consequently, the load-induced or 'mechanical' strains are smaller, but typically of the order of 2 to 3%. A limit of 1.5% is therefore recommended in BS 5950:Part 8 as the design strain of bare steel beams. This limit is lower than the strains measured in tests and takes account of the fact that beams of differing geometries from those tested are often used.

Fire protected beams behave in a similar manner, except that at large deformations (and hence strains) there is a possibility that cracks may open up in the protection or, in extreme cases, the protection may become detached. One criterion imposed in the certification of fire-protection materials is a loaded beam test to assess 'stickability' [3]. The test is normally terminated at a deflection of between span/40 and span/30. At a deflection of span/40 it would be reasonable to expect that extreme fibre strains exceeding 1.5% had been experienced in a beam of normal proportions. Therefore, provided that the fire protective materials have demonstrated their 'stickability' by remaining intact up to the above order of beam deformations, then the 1.5% strain limit may be used in assessing the strength of the steel section.

Failure to meet this stickability criterion implies that the deformation capacity of the protective system is relatively low, leading to lower strain limits in the steel. A strain limit of 0.5% is considered appropriate in such cases.

Manufacturers are encouraged to carry out the above beam tests to a deflection of span/30 in order to justify the use of the higher strain limit of 1.5% (rather than 0.5%). This leads to higher elevated temperature steel properties in the important temperature range of 400-600°C and results in a smaller thickness of fire protection for the same fire resistance. Tests should be carried out on a representative beam section for the span under consideration.

Load tests on columns behave rather differently to beams in that relatively low strains are experienced at failure. In this case, a strain limit of 0.5% is considered appropriate for all forms of fire protection.

A similar strain limit is used for tension members which are subject to uniform axial strain. Higher strains could lead to excessive movements and could affect the overall performance of the structure where the tension members are used as ties.

The strain limit specified should also be such that the section under consideration can sustain these values of strain without any premature local instabilities developing and compromising the overall performance of the member. A problem may also arise with unusual beam proportions if only a deflection limit is specified; for example, a beam with a small span to depth ratio will undergo higher strains to reach a given deflection than a beam with a higher span depth ratio.

3.3 Acceptance criterion (2) - Deformation

Acceptance criteria can also be expressed in terms of limiting the deflection of certain members in order to ensure that:

- no part of the structure in a fire impinges on critical equipment
- deformations do not cause collapse of any critical part of the structure
- fire wall integrity is not compromised as a result of excessive deflections of their supporting structure.

The difference between deformation and limiting strain (ref. section 3.2) is that strain limits are imposed in order to define the strength of a member. Deformation limits are applied in order to ensure that the consequences of a member deforming are acceptable. A deformation limit may permit greater or less deflection than the strain limit.

The large deformation of members will result in second order effects becoming significant. For example, axial restraint due to membrane action and increased moments due to P-δ effects. If large deformations form the basis of an acceptance criteria, the method of analysis must be able to model these phenomena. A non-linear structural analysis may be required.

3.4 Acceptance criterion (3) - Collapse

Collapse is an available acceptance criterion if a non-linear structural response analysis is being carried out. The survivability of the TR (or other critical structure) can be based on an assessment of deflections varying with time.

There are a number of differences between a deformation and collapse criterion:

- deformation criteria are applied on a member by member basis. Collapse criteria are applied to a structural system. This permits load redistribution and hence the mobilisation of strength reserves elsewhere in a structure;
- a collapse criterion permits individual members to fail completely.

The residual strength of the structure at the required endurance time may need to be assessed. This can be achieved by continuing the analysis to collapse. This will give two parameters:

- the time at which collapse occurs;
- deflection at the point immediately prior to collapse.

An example of a collapse criterion would be a limit on the movement of a TR support point, or perhaps a combination of movements and rotations of the TR itself. Such limits may be based on serviceability type criteria or a consideration of human factors. The analysis should proceed past the acceptance criteria to the point of collapse in order to determine how close the specified acceptance criteria is to loss of the TR. It can be argued that some margin of residual strength is required in order to cater for analysis inaccuracy - unlike other acceptance criteria there is no inherent reserve .

4. PROPERTIES OF STEEL AND OTHER MATERIALS IN FIRE

4.1 Strength retention factors for structural steels

An important parameter defining the strength of steel at a particular temperature is the strength retention factor. This gives the elevated temperature strength as a proportion of the room temperature strength. The determination of appropriate retention factors at elevated temperatures has been the subject of considerable debate in recent years. This is complicated by two key factors; firstly the method of test and the heating rate used, and secondly the strain limit at which the steel strength is determined.

Isothermal [4] or steady state tests are tests where the tensile specimen is subject to constant temperature and strain is applied at a constant rate. The stress-strain curve is therefore appropriate for a given constant temperature. Isothermal tests are carried out at a relatively rapid strain rate and appear to provide more beneficial results than anisothermal tests.

Anisothermal [4] or transient tests are ones where the specimen is subject to constant load and the rate of heating is set at a pre-determined amount. The resulting strains are measured. The effect of thermal strains are deducted by using 'dummy' unloaded specimens subject to the same temperature conditions. Stress-strain curves at a particular temperature are obtained by interpolation from a family of curves at different stresses. There is a slight dependence in anisothermal tests on the rate of heating. The reference heating rate is taken as 10°C/minute (ie. 600°C rise in 60 minutes [3]). The faster the rate of heating in anisothermal tests, the lower the resulting strains in the steel for a given temperature and applied stress. This means that for a given strain, higher strengths are recorded at a given temperature for faster rates of heating.

In general anisothermal tests result in lower strengths than isothermal tests [3]. However, they can be claimed to be more realistic. This difference between the two methods of test is smaller, but nevertheless significant, at higher strains (> 1%) than at lower strains. The difference between the methods is apparently less for grade 50 than grade 43 steel (Note: BS 4360 grade 43 and grade 50 steels are now termed by their European designation, Fe430 and Fe510, reference BS EN 10029:1991).

The value of strain at which the strength of the steel is measured is also of importance. The yield strain is traditionally defined as the value consistent with a yield plateau for mild steels. However, at elevated temperatures there is a continuous change in strength with increasing strain (or strain-hardening) and the concept of a yield plateau is no longer valid. In fire tests on beams and columns very high strains are experienced and this suggests that strengths greater than those at the 'effective' yield point are developed. The selection of the appropriate strain limit is therefore important if the performance of steel in fire is to be predicted accurately and high temperature data is frequently presented both in terms of the 0.2% and 1.0% proof strengths (i.e. the strength at which the permanent strain is either 0.2 or 1%). Other common references involve defining the strength at 0.5%, 1.5% or 2% absolute strain.

The strength retention factor defines the strength of steel at a particular temperature and 'mechanical' strain relative to its room temperature yield strength. The strength retention factors for grade 43 and 50 steel are presented in Table 4.1. The relative importance of the strain limit is apparent from this data. Note that IGN Table 4.7 gives the fraction of yield stress at which the elastic limit occurs. This is less than but close to the 0.2% proof stress, however, for fire design neither of these limits is recommended. Table 4.2 gives the corresponding reduction in Young's Modulus. This is based on the tangent modulus at zero strain.

Such differences in the interpretation of the methods of test and the selection of the strain limit are generally unimportant for insulated sections because they contribute to a relatively small difference in the required thickness of fire protection. The differences can, however, be significant for unprotected sections where short fire resistance periods may be sought.

It is important to note that the strength retention factors for steels other than normal carbon steels vary from one type of steel to another (*eg* RQT steels, stainless steels, *etc*) and are product specific. They depend on the chemical composition and the production process and care should be exercised to ensure that the strength retention factors used in the design are applicable to the steel specified and used. It is also important to ascertain the details of the test used in determining the strength retention factors, the strain limit on which the values are based and whether "average" or "minimum guaranteed" values are given. When comparing the elevated temperature properties of different materials great care is needed to ensure that comparisons are based on similar criteria.

4.2 *Behaviour of other steels and materials in fire*

Although most of the structure on an offshore platform will be made of carbon steel, there are a number of other materials which are finding increasing use or which may be found on some types of platform. This section briefly reviews the high temperature properties of four other materials that may be found:

- stainless steel
- glass reinforced plastic
- aluminium
- concrete

It should be noted that each of these materials can be supplied in a number of different grades, forms etc. The information provided below is thus of a generic nature only, except where specific grades are quoted.

As discussed in the previous section, it is very difficult to compare the high temperature properties of different materials. This difficulty is compounded by problems in comparing measured properties with minimum specified data presented in British Standards.

A number of tables are provided to assist comparison between the different materials. Blanks in the table indicate that no representative data could be found. Table 4.1 presents the strength retention factors of these materials against those of carbon steel and Table 4.2 gives the strength retention & elastic modulus retention factors for carbon and stainless steel. Table 4.3 compares the thermal properties of all the materials discussed in this section.

GRP

GRPs are used because of their low density and high corrosion resistance combined with a high strength to weight ratio. Fire performance of GRP depends on the type of resin, the proportion of fibre and the laminate thickness. High fibre content and thick laminate improve fire performance. Fabrication technology restricts the field to thermosetting resins which do not melt but will ultimately decompose. Good fire performance can be achieved by composite construction where a low conductivity core is sandwiched between two relatively thin GRP laminates.

Phenolic based composites have better strength retention characteristics than most other resins. Unlike many other thermosets, phenolics form an intractable char, which protects the composite to some extent from heat penetration. They have low initial flammability and, when involved in a fire, they contribute little further heat, producing only low levels of smoke and toxic products.

The burning of GRP has created concern about the generation of toxic gases. Where such gases may threaten personnel, the use of GRP may need to be avoided. However, on the fire side of a GRP barrier the level of toxic substances originating from the barrier is likely to be insignificant relative to those produced by the fire.

Further guidance on the use of GRP can be found in IGN Section 5.6.2 and IGN ref. 6.

Aluminium

The mechanical properties of aluminium vary depending on the specific alloy under consideration. Its fire endurance properties are inferior to carbon steel. Some limited data is given in Tables 4.1 and 4.3.

Further guidance on the use of aluminium can be found in IGN section 5.6.3 and IGN ref. 6.

Stainless steel

Stainless steels are available in a great variety of grades and forms, depending on their chemical composition, production process and micro-structure. Their fire performance varies from one grade to another, with many grades having better strength retention characteristics at elevated temperature than carbon steel. However, because the elevated temperature strength of stainless steel is largely product specific, it is important to ascertain the test method used to generate strength retention factors, the strain limit at which the strength was measured, the sample size and the statistical basis of the data.

Elevated temperature properties and strength retention factors for stainless steel can also be found in Standards (see Tables 4.1 to 4.3). In general, such data consist of 'minimum guaranteed' values and have been produced for the design of stainless steel components for continuous use at high temperature. Such data is not suitable for fire engineering calculations, where elevated temperature must be treated as an 'accidental load' applied to the structure in addition to its operational/service loads.

Further guidance on the use of elevated temperature strength data in the design of stainless steel for accidental fire loading is in progress. In the meantime, the producers' advice should be sought on specific grades.

Concrete

Concrete is frequently used above the waterline in the form of gravity platform legs which are hollow and may contain risers, drilling equipment etc. In an intense fire, although the surface of concrete may spall (often associated with the expansion of the underlying steel reinforcement), in general it maintains its load bearing characteristics.

Further guidance on the use of concrete can be found in IGN ref. 6.

Table 4.1
Comparison of strength retention factors applicable at elevated temperature.

Material	Temperature (°C)									
	20	50	100	200	300	400	500	600	700	800
Carbon steel* grade 43 [3]	1.00	0.97	0.97	0.95	0.85	0.80	0.62	0.38	0.19	0.07
Carbon steel* grade 50 [3]	1.00	0.97	0.97	0.95	0.85	0.80	0.62	0.38	0.19	0.07
Stainless steel† grade 316L	1.00	1.00	0.99	0.96	0.92	0.87	0.82	0.65	0.51	0.35
Aluminium† grade 6061 [5]	1.00	0.98	0.94	0.78	0.29	0.09				
Concrete‡ [6] normalweight	1.00	1.00	1.00	1.00	1.00	0.91	0.73	0.56	0.38	0.20
Concrete‡ [6] lightweight	1.00	1.00	1.00	1.00	1.00	1.00	1.00	0.80	0.60	0.40
GRP (phenolic - wr)	1.00	0.98	0.95	0.90	0.1					

* 0.5% absolute strain † 0.2% proof stress ‡ cube strength

Table 4.2
Stiffness and strength of carbon and stainless steels at elevated temperatures

Temperature in°C	Carbon Steel - from anisothermal test data						Stainless Steel (from isothermal test data)
	EC3:Part 10			BS 5950:Part 8			
	Slope of linear elastic range (relative to E_a)	Effective yield strength (relative to f_y)	Proportional limit (relative to f_y)	Strength reduction factors at a strain (in %) of:			
	$k_{E(\theta)} = E_{a(\theta)}/E_a$	$k_{y(\theta)} = f_{y(\theta)}/f_y$	$k_{p(\theta)} = f_{p(\theta)}/f_y$	0.5	1.5	2.0	Elastic modulus
20	1.000	1.000	1.000	1.000	1.000	1.000	1.00
100	1.000	1.000	1.000	0.970	1.000	1.000	0.99
200	0.900	1.000	0.807	0.946	1.000	1.000	0.95
300	0.800	1.000	0.613	0.854	1.000	1.000	0.91
400	0.700	1.000	0.420	0.798	0.956	0.971	0.87
500	0.600	0.780	0.360	0.622	0.756	0.776	0.82
600	0.310	0.470	0.180	0.378	0.460	0.474	0.78
700	0.130	0.230	0.075	0.186	0.223	0.232	0.73
800	0.090	0.110	0.050	0.071	0.108	0.115	0.68
900	0.0675	0.060	0.0375	0.030	0.059	0.062	
1000	0.0450	0.040	0.0250	0.0206	0.0394	0.0446	
1100	0.0225	0.020	0.0125	0.0137	0.0263	0.0297	
1200	0.0000	0.0000	0.0000	0.0069	0.0131	0.0149	
1300	0.0000	0.0000	0.0000	0.0000	0.0000	0.0000	

Note: Only isothermal test data is available for stainless steels. Therefore, the values shown are not directly comaparable with the anisothermal data for carbon steels.

Table 4.3
Comparative approximate thermal properties

Material	Property								
	0.2% proof strength (N/mm^2) ‡	1.0% proof strength (N/mm^2)	Young's Modulus (kN/mm^2)	Specific heat (J/kg°C)	Thermal conductivity (W/m°C)	Emissivity	Coefficient of linear expansion ($x10^{-6}/°C$)	Melting range (°C)	Maximum useful working temperature* (°C)
Carbon steel - grade 43 [3]	275	275	205	520	37.5	0.2 - 0.9†	12	1450-1540 [5]	650
Carbon steel - grade 50 [3]	355	355	205	520	37.5	0.2 - 0.9†	12	1450-1540	650
Stainless steel grade 316L [7]	190	225	193	500	13.5	0.75 [5]	16.5	1375-1450 [5]	950
Aluminium - grade 6061-T6	270		69	896	197	0.1-0.2 [5]	23.5	570	200-250 [5]
Aluminium - grade 5454-O	110		69.6	900	134	0.1-0.2 [5]	23.7	600-640	
Concrete - normalweight	Cube Strength 30		Static Modulus 23 - 33		1.4 - 1.8	.85 - .95	7-12		500 - 600
Concrete - lightweight	Comp. 28 days 2 -62		6.9 - 20.7		.24 - .93	.85 - .95	8-12		
GRP - CSM $	30	80	8	1400	0.25		13-35	N/A	170
GRP - WR $	70	200	20	1000	0.3		10-16	N/A	250

* approx. temperature at which strength retention factor is 0.2
† see text
$ properties vary with glass contents and manufacturing process
‡ debonding strength for GRP

Note: Values apply to temperature range 0-100°C. Many of the properties vary with temperature. Further information, including parametric formulae, can be found in references [19] and [21].

5. HEAT FLUX LOADINGS

5.1 *What is heat flux?*

Heat flux is the rate at which energy is transferred per unit area. Heat may be transferred by radiation, convection and/or conduction. In practice radiation will dominate heat transfer in large fires.

Thermal radiation involves heat transfer by electromagnetic waves confined to a relatively narrow region of the electromagnetic spectrum. Like visible light, it can be absorbed, transmitted or reflected at a surface and will cast shadows if partially blocked by an opaque object.

Conduction is the transfer of heat through a solid body. Adjacent molecules transfer energy to one another. For a member to heat up requires that the energy is conducted into the material. In practice, relative to radiative heat transfer, the heat moved from one place to another by conduction is low, even for metals which are regarded as good conductors. However, where the heated surface is large and the conductive heat flow path short (e.g., a heated plate), then for a metal conduction will be sufficient for there not to be a significant temperature gradient through the thickness of the material. In contrast, insulators have much lower rates of conduction than metals and can support high temperature gradients through even a thin layer. Example 3 shows how little heat is transferred along the length of a member (Appendix A.3).

Convection describes heat transfer associated with fluid movement around a body. A warm fluid will transfer heat to a colder body whilst a hot body will transfer heat to a colder fluid. It is a characteristic of convection that the heated or cooled fluid changes density relative to the surrounding fluid. This causes the fluid to move, providing a new source of hot or cool fluid. Natural convection occurs where only the fluid buoyancy forces act to circulate the fluid. Forced convection occurs where the fluid is forced past the surface. The heat flux transferred by convection is much higher for forced convection than natural convection. It is also higher in turbulent flow conditions than in laminar flow conditions. In a fire, convection to objects within the fire is likely to be forced.

Re-radiation is the term used to describe radiation that is emitted from a hot surface as a result of the temperature of that surface. Insulating materials can have high re-radiation characteristics due to high surface temperature. A surface may receive radiation both from the fire and from other re-radiating surfaces. When a member is engulfed by a fire, the incident radiant heat flux can be assumed to originate entirely from fire radiation. If the member is not engulfed, the incident radiation on a surface will include additional components of radiation from adjacent hot surfaces. Such components may be significant in compartment fires where the temperature of heat affected surfaces may rise significantly.

In order to study the true structural response to a fire, it is necessary to know its duration, intensity and variability with time. To determine cooling and flame location the local direction and intensity of the wind may be required. The nett heat flux into a member will also be determined by its position relative to the fire, although it should be noted that the area of influence of the fire may be difficult to determine (e.g., a jet fire or sea pool fire). Heat flux loadings are also subject to a wide variation depending on the hydrocarbon type (e.g., its mass burning rate) and the size and nature of release (e.g. spill, blowout or gas jet).

Fire modelling is an extremely complex subject due to the number of uncertainties in determining the heat flux loading from a realistic fire. Integrated fire protection/scenario based safety design not only depends on accurate characterisation of a particular fire in terms of size, heat output, temperature, duration etc., but also the interaction of active and passive fire protection systems with the fire. Historically this has been achieved by conducting full-scale fire test programmes. Computer modelling is now reducing the reliance on expensive fire testing. However, where clear guidance can not be found from the literature the advice of fire scientists in relevant research organisations should be sought.

5.2 *Defining credible fire scenarios*

It is much more realistic to develop a collection of fire scenarios for each platform than to assume a standard fire curve is applicable. For example, a condensate pool fire in a vented area may be less severe or a wellbay jet fire more severe than the standard fire curve.

In order to assess the integrity of a structure under fire conditions, it is first necessary to identify the credible fires which may occur. These may be pool or jet fires and should be quantified in terms of their heat flux, fire diameter, flame length and duration.

Heat flux, fire diameter and flame length can best be determined using either empirically derived fire models or numerically based CFD computer codes. IGN section 4.3 gives further details on available methods. It also gives characteristic heat fluxes by fire type. However, it should be noted that the ability to characterise fires burning in the presence of confinement and congestion remains very limited.

Determining the duration of the heat flux is a complex task involving tabulating each pipe and vessel containing flammable substances as part of a hydrocarbon and hazardous materials inventory study. ESDVs are provided at strategic locations, normally adjacent to large vessels, to reduce the available fuel and thus control the size and/or duration of fires. The inventories are determined by calculating the working volume of each element of a line between its ESDV and summing them. Consideration is also given to the fluid content of the line, its phase, pressure and temperature plus the size of any leakage holes and the blowdown time.

Scenario definition must also consider whether firewater deluge systems will be activated, what the probability of ignition is, and where and when ignition will occur (the timing will determine whether an explosion, pool or jet fire ensues). Using the results of these releases and ignition probabilities, credible fires may be qualitatively assessed. Non-hydrocarbon fires also need to be considered (typically accommodation module or switchboards). The result is a series of credible fire situations which show how heat flux varies at different points in the vicinity of the fire.

5.3 *Effect of fuel type*

When hydrocarbons burn, the amount of heat released by combustion is virtually the same for all likely fuels. The basic heat of combustion is between 40,000 and 50,000kJ/kg depending on the length of the carbon chain [8,9]. However, each fuel type has a specific burning rate (kg/m^2/s). This is the mass of fuel supplied to the flame per second per unit area of the pool. Table 5.1 gives indicative values for various fuels based on experimental work.

Table 5.1
Indicative mass burning rates for different fuels

Fuel	Mass burning rate (kg/m^2/s)
Liquid propane gas on land	0.13
LNG on land	0.11
Heptane	0.069
Crude on water	0.055
Methanol	0.024
JP-5	0.069

Another parameter varying with fuel type is flame emissivity. This determines the amount of heat generated that is released as radiation. It is not possible to give precise figures since fire type may influence this parameter. However, in general the more sooty a flame (yellow) the higher will be its emissivity (and the lower its temperature). Thus, a natural gas fire will emit a lower proportion of the released energy as radiation than a crude oil fire (note: although a natural gas fire may appear relatively transparent, significant levels of radiation is emitted by hot water vapour and CO_2) [8].

A common way of defining the amount of radiation released by a fire in lieu of defining fire emissivities is to use the F-factor. This gives the amount of radiation emanating from a fire as a proportion of the total heat released. Further details and characteristic values are given in IGN ref. 12.

5.4 *Effect of ventilation*

Fires are generally either fuel-controlled or ventilation controlled. Fires in open, well-ventilated areas are controlled by the mass release rate of the fuel, whereas those in confined areas, which are more likely to occur on offshore platforms, may be controlled by ventilation. In such circumstances the heat released by the fire within the compartment will be reduced, however, a large fire may burn outside the compartment. Such an external fire would need to be assessed.

It is necessary to check for cladding failure since this will affect the ventilation and thus the characteristics of the fire and hence will critically affect the rate of energy release from the fire. In the event of a cladding failure, it is also advisable to calculate the exit flame lengths and heat fluxes in an escalation assessment.

At present there is still inadequate knowledge to give precise guidance on how to treat large compartment fires. IGN Tables 4.1 and 4.2 gives some suggestions, but these are largely based on the premise that a compartment fire can be considered similar in intensity to a fire in the open.

5.5 *Effect of position of member in relation to the flame*

Members and surfaces (e.g. cladding and floor plates) need to be treated differently depending on which zone of the module they occupy relative to the fire. For example, when a member is *engulfed* by a fire, the incident heat flux can be assumed to originate entirely from fire radiation since the incident convection component is small compared with the total incident heat flux. However, for members which are *remote from the flame* and are surrounded by gases which are considerably cooler than the surface, significant heat may be lost by convection from the surface. For a member which is not engulfed but in the *hot plume*, the incident heat flux due to convection may be significant compared to that due to radiation.

The intensity of radiant energy falling on members remote from the flame can be found by using the appropriate 'configuration factor' which takes into account the geometrical relationship between the body and the fire. Section 7 gives more information on configuration factors. Examples 1-5 (Appendices A.1-A.5) help illustrate how the location of a member affects the type of heat flux that could control member temperature.

5.6 *Heat flux from pool fires*

Pool fires receive thermal feedback from the flames to vaporise the liquid and this is the most significant feature in controlling their mass burning rates. As general guidance to determining fire characteristics, the designer is directed to IGN Tables 4.1 and 4.2. Typical values of the heat flux to an engulfed object from a pool fire on the open deck are 100-160kW/m^2. As the molecular weight of the fuel decreases, the gases and soot particles in the flame become much hotter. Thus typical heat fluxes in a very large-scale (greater than 40m in diameter say) flame above a subsea natural gas release could lie between 250-300kW/m^2 [8].

5.7 *Heat flux from jet fires*

Important factors for jet fires include the type, conditions, pressure and availability of the fuel, the geometry of rupture and the environment. Different structural geometries also affect the resistance of a passive fire protection material exposed to a jet fire. The confined jet fire length is based on the release rate.

In a jet fire scenario, it is possible that the flame length may be in excess of 100m and have a heat flux of 350kW/m^2 although more typical values for a target engulfed by a jet fire in the open with a gaseous release, lie between 50-300kW/m^2 for natural gas. With the inventory isolated by ESDV closure and vented to flare via the blowdown system, the flame length may, depending on the leak size, rapidly reduce such that for a full bore rupture the total fire duration would be in the order of minutes. A small leak, however, may have a duration of hours but have a shorter flame length with high, localised heat fluxes. With the blowdown system fully operational it is expected that large jet fires would rapidly reduce in size to tolerable levels.

5.8 *Fire models*

The first stage in a thermal response analysis is to determine the level of heat flux loading received by a surface. Sections 5.6 and 5.7 above referenced IGN Tables 4.1 and 4.2 as sources of characteristic heat fluxes. These may be used for engulfed members. However, outside of the flame the heat flux received will reduce.

The reduction in heat flux can be calculated using the configuration factor, ref. section 7.4. However, this requires certain information about the size and radiation characteristics of a fire. These properties are frequently described by fire models. Depending on the type of model (point source, surface emitter, ref. IGN section 4.3), it may not be necessary to solve the complex surface integral that is inherent in configuration factors applied to surface emitter models.

Examples 1 and 2 (Appendices A.1 and A.2) include the determination of heat received by a surface. A multi-point model is used, though in the second example it is necessary to resort to a surface emitter concept close to the flame surface.

6. FIRE PROTECTION SYSTEMS

6.1 *Passive fire protection systems*

Passive fire protection (PFP) systems are either coating or barrier insulators which delay or limit the effects of fire on structural and segregating elements or vital equipment. Fire protection delays the rise in temperature to critical levels, reduces the risk of escalation and buys time in which evacuation of personnel, blowdown of inventories or control and fire fighting measures can be brought into operation. Fire walls, process areas, structural members, pressure vessels, risers and ESDVs may all be protected by passive coatings.

In general PFP materials are of a porous or semi-porous nature - hence taking advantage of the good thermal insulating properties of gas. Their performance is based around their thermal conductivity, heat capacity, density state changes and moisture content. There are a variety of different forms of fire protection materials:

- *cementitious materials* - use a hydraulically setting cement as a binder with a filler of good insulation properties. They are usually sprayed or trowelled directly onto the surface; a wire mesh is required to ensure adherence to the surface.
- *intumescent coatings* - have an organic base which expands to produce a stable 'char' with good thermal insulation properties when subjected to fire. They are applied by spraying several layers, usually with reinforcement between, and may intumesce to a total thickness of a few tens of times the dry thickness.
- *refractory fibres* - are fibrous materials with a high melting point which form fire resistant boards and mats.

IGN Section 4.5.3 discusses PFP materials in greater depth.

Most fire protection materials contain moisture. When temperatures within the materials approach 100°C, further heat input does not increase the temperature of the material but vaporizes any free moisture. This causes a delay or 'dwell' in the temperature-time response of the protected steel section.

The performance of fire protection materials is currently assessed in standard fire tests [10,11]. The fire tests are furnace based, derived from onshore practice. However the prescribed furnace conditions do not relate via thermal and/or aerodynamic effects to those of real fires which could impose severe thermal shock loading, fluctuating heat loading and erosive forces (from jet fires). Tests involving direct flame impingement may offer more of the characteristics of real fires. New, more representative tests are being developed [12].

Smoke and toxic gas emissions from PFP materials are currently considered in isolation and there is uncertainty about the overall life-threatening significance of these. Compared to those from the primary fire they are not considered significant when on the exposed side, however, care must be taken to ensure that toxic gasses are not released on the non-fire side (e.g., in the TR).

The current approach to specifying fire protection appears to concentrate unduly on the most severe fire loading requirements in an attempt to define a minimum required thickness. This is then applied to all steelwork that requires protection, which results in much of the PFP being over-specified. Scenario-based design demands a more detailed consideration of both fire intensity, of where the fire occurs and of which structural members are affected. It also permits the structure to be analysed in order to determine which members are most susceptible to temperature loading. Combining these features permits a far more rational distribution of PFP. For example, scenario-based design may show that whilst the initial fire is very severe, the fire reduces in magnitude rapidly and before failure can occur.

Passive systems provide a cost-effective method of fire protection for both onshore and offshore structures. The main drawbacks of passive systems are the lengthy process of application with stringent QA requirements and possible condensation and corrosion of structural steel beneath coatings. Also, once PFP is in place, maintenance and inspection of the steel under the coating is difficult. Another important feature to consider is the weathering and long-term performance of the coating; it must be resistant to the severe environment to which it will be exposed and must be resistant to water, frost, light, etc.

From a cost, weight and time perspective, it is clearly desirable to minimise PFP. However, a rigorous structural response analysis may indicate the required design thickness is less than the minimum practical thickness which can be applied. This suggests there is scope for improving the design of pfp systems to permit thinner layers to be applied.

Example 5 (Appendix A.5) shows a comparison between unprotected steel and several different types of insulation (low conductivity, reflective). The benefit of even a small layer of pfp is clearly shown.

6.2 *Active fire protection systems*

An active fire protection (AFP) system is one which requires activation - switching on, directing, injection or expulsion and a means of sustaining delivery in order to combat smoke, flame or thermal loading. One of the main drawbacks of AFP systems is that it is impossible to quantify the ability of a certain spray system to remove heat from an offshore hydrocarbon fire and reduce the fire load to construction and process equipment. The apparent arbitrary fixing of the 12.2 litres/min/m^2 deluge rate and the accompanying regulations from SI 611 (1978) appear to have stifled any development in this area. It is now recognised that the blanket application of water at this rate is probably inefficient in most areas and ineffective in others. There has recently been a trend to concentrate the available water to those areas of greatest hazard.

Active water spray (or deluge) systems are widely used for fire protection. These usually consist of a network of small bore pipework and spray nozzles connected to the firewater main which is capable of delivering the design water spray to the protected area. It should, however, be noted that an active fire protection system may be damaged in the early stages of an incident leading to impaired performance. Unquantifiable benefits which may arise from a general deluge system are:

- spray may knock-down high-level fire plumes from pool fires, reducing ceiling temperatures. This effect is dependent on the fuel, air supply, obstructions and turbulence of the updraught.
- the even application of water to a surface of a vaporising pool of hydrocarbons will reduce the rate of vaporisation.
- a fire spray will absorb some smoke and gases, reducing the hazard these cause.
- high water application rates to dead crude or non-vaporising oils can cool and may even extinguish a pool fire. They will also flush the oil into the drainage system, so disposing of part of the release.

Gas fires can be extinguished by water sprays. The most probable mechanism of the extinguishment is inerting of the fire zone with water vapour, combined with cooling of the reactants. The critical water vapour concentration seems to be about 30% locally to dilute oxygen to a concentration where combustion becomes impossible [13]. Note that if a gas fire is extinguished, then there remains the possibility of explosive reignition. It may therefore be desirable not to completely extinguish gas fires.

The main factors affecting the interaction of a water spray and a fire plume are:

- the fire size and the time that the fire has been burning before the spray is activated
- the discharge rate of water
- the mean water droplet size (a spray with smaller droplets needs less water to cool the gases and surfaces than one with larger droplets, however, droplets that are too small will be unable to penetrate the fire by gravity)
- ventilation rate
- location of the fuel versus the spray nozzle.

Water is more dense than most hydrocarbon fuels, and also immiscible. This means that water will not provide an effective cover for burning hydrocarbons, or mix with them to dilute them to the point of not sustaining combustion. Instead the hydrocarbon will float on top of the water, continuing to burn and possibly spread. To combat such fires, foam solutions can be introduced into the water to provide an effective cover and smother the fire.

Fire water piping systems are notoriously unreliable as they are exposed to the worst conditions for corrosion, i.e. a combination of stagnant seawater, saline moisture and atmospheric oxygen. Inadequate performance of carbon steel and copper-nickel alloys has resulted in a marked increase in the use of 'super' alloy stainless steels for sea and fire water piping systems in the UK offshore sector. More recently, favourable comparisons have been made for using GRP against high alloy stainless steels for fire water piping systems on offshore platforms. Weight savings by using GRP piping instead of carbon steels can be up to 60% [14].

At present there is virtually no information on how the presence of an AFP system will reduce the requirements of PFP. Clearly this will depend on the nature, position and reliability of the AFP system as well as the fire scenario. However, a number of mechanisms can be identified which will come into play:

- water droplets act to cool the fire. Since radiatior released by a fire is proportional to the fire temperature to the power four, any reduction in temperature may be significant;
- water droplets intercept fire radiation, either absorbing or reflecting it. This concept is used by fire fighters who use water curtains to protect themselves from fires;
- water droplets impinge onto surfaces resulting in cooling of the surface.

A recent study assumed that a deluge system would reduce the incident heat flux received by structural members by 50% [15]. This was based on the assumption that the member was outside the flame. However, whilst observations from water spray tests indicate that 50% may be a reasonable figure, further experimental and theoretical investigations are required to check the validity of this assumption and to determine how it varies at different locations relative to the fire and for different types of water spray.

Recent studies have indicated that, as far as protection of the structure is concerned, PFP costs only about a third of the cost of an equivalently effective AFP system. This is due to the high capital and operating costs associated with AFP, especially on existing installations. In fact, future work may show that AFP is not cost-effective for protecting structural steel at all, and instead should be designed to:

- reduce the temperature of the fire and its burning rate;
- reduce the extent of the flames;
- protect specific items of plant, escape routes of personnel;
- provide a water-laden atmosphere which will absorb a significant proportion of the fire radiation being transmitted through it;
- remove smoke and toxic gases from the atmosphere.

6.3 Firewalls

Firewalls provide barriers and prevent passage of smoke and flame by containing or excluding fire from areas or compartments. Fire rated blast walls are usually placed between utilities, drilling and process areas.

The design of firewalls is carried out by their suppliers. It is essential that the structure supporting these firewalls satisfies any specific performance criteria laid down by the firewall supplier.

7. DETERMINATION OF COMPONENT TEMPERATURES

7.1 Introduction

The aim of this section is to explain how the time-varying heat flux data described in Section 5 can be converted to a time-varying steel temperature.

In practice, the heat received at the surface of a member is a complex function of the member geometry and orientation, as well as its location relative to the fire. In addition, the amount of heat used in raising the temperature of the steel depends on the insulation properties, insulation thickness and the mechanisms available for dissipating the heat received; the role of both AFP and PFP must be considered.

It is usually preferable to carry out a screening process to determine those members which play a critical role in fulfilling the specified acceptance criteria. It is only necessary to investigate the temperatures of these critical elements in any detail.

7.2 General Heat Balance Equation

The process of heat transfer between a surface and its surroundings can be described in terms of the balance between the input heat and the various ways in which the input heat is dissipated from that surface. It should be noted that this heat balance equation is applied at the surface of the component being considered and may be used to calculate the surface temperature.

The heat balance equation is:

$$\epsilon\, q_{ir} + q_{ic} = q_{rad} + q_{conv} + q_{cond}$$

where:

- q_{ir} = the incident radiant heat flux, generally given by the fire loading models.
- ϵ = surface emissivity at surface reference temperature (non-dimensional).
- q_{ic} = the incident convective heat flux.
- q_{rad} = the heat flux re-radiated from the surface.
- q_{conv} = the heat flux convected away from the surface.
- q_{cond} = the heat flux conducted away from the surface. (i.e. into the material)

In the above equation, the terms on the left hand side represent the heat received at the surface while the terms on the right hand side represent the heat removed from the surface. The terms are described in more detail in Section 4.4.1 of the Interim Guidance Notes.

Following a general discussion of the main difficulties encountered in solving the heat balance equation, two methods of solution are outlined. The first approach is relatively simple, based on the H_p/A ratio (Sections 7.5-7.7). Secondly, the use of finite element and finite difference computer programs to develop more rigorous solutions is discussed. In both cases the number of repetitive calculations required indicates that methods will rely on the use of computer programs. Worked examples 1-5 (Appendices A.1-A.5) illustrate applications of the heat balance equations.

Example 5 includes the listing of a BASIC program which can be run to effect a numerical solution. This has all terms of the heat balance equation, including convection.

7.3 Difficulties in solving the heat balance equation

One of the main difficulties is determining the incident heat flux on a given structural member. In most fires. the incident radiant heat flux dominates and can be approximated by:

$$q_{ir} = \phi\, \tau\, e_f\, \sigma\, T_f^4$$

where:

- τ = atmospheric transmissivity
- ϕ = configuration factor which takes into account the geometrical relationship between the emitter and receiver (see Section 7.4)
- ϵ_f = emissivity of flame
- σ = Stefan-Boltzmann Constant = 5.67×10^{-8} W/m^{2}K^4
- T_f = temperature of flame

If there are a number of fire sources, or re-radiation from surrounding surfaces is significant, then q_{ir} will be the sum of the individual elements.

i.e., $$q_{ir} = \Sigma\ (\ \phi_i\ \tau_i\ \varepsilon_{f,i}\ \sigma\ T_f^4\ )$$

As well as depending on the magnitude of the heat flux at the fire, the emissivity of the fire, the distance and orientation of the member in relation to the fire, q_{ir} is also related to the mitigating effect of the AFP system (see Section 6.2).

From inspection, it is clear that the theoretical radiative heat transfer is extremely sensitive to the value of temperature as it is derived from a fourth power law.

The emissivity of a fire is a function of the size of the flame and varies between fuels. It is difficult to estimate a realistic value; unity is often used, i.e. the fire is assumed to be optically thick and to act as a black body.

Incident radiation is also a function of the atmospheric transmissivity of the medium between fire and receptor. Selective absorption by water vapour reduces the incident heat flux. This phenomena is, thus, a function of atmospheric humidity and the distance between the fire and receptor.

In certain circumstances, such as compartment fires, the incident radiation may need to be increased to allow for reflection and re-radiation effects. This increase is very difficult to quantify and until more refined methods are available, an increase of 25% is tentatively recommended for non-engulfed members in a compartment fire.

The choice of emissivity for the surface of a member is also very difficult to measure. The emissivity of polished steel is around 0.1, clean mild steel, 0.2-0.3 and steel with a rough oxide layer, 0.8-0.9. A mild steel UB as delivered from manufacture would typically have an emissivity of 0.8. Table 4.3 recommends values for a variety of structural materials. The emissivity of common PFP materials such as ceramic fibre, vermiculite cement, intumescent epoxy and board is around 0.9.

In selecting an appropriate emissivity, it should be noted that the value may change during the fire. For example, aluminium has a low factor of less than 0.1, however, if the surface becomes covered in soot a value of circa 0.9 is applicable. On sensitive items such as pressure vessels, or where low emissivities are being used, it may be necessary to carry out a sensitivity analysis to determine the effect of varying the emissivity.

It should be noted that steel without a passive fire protection coating will invariably be coated with some kind of paint system. This may have a lower emissivity than bare steel, but the paint will burn off to form a carbonaceous char with an emissivity approaching unity. The char, however, may provide some nominal insulation which could compensate for the increase in emissivity.

The effect of conduction between and along members is usually ignored. Example 3 (Appendix A.3) demonstrates that the rate of conduction along a typical steel member is low and that the amount of heat transferred by this mechanism is small compared to the radiative heat transfer.

Certain terms may be eliminated in the heat balance equation depending on the position of the member in relation to the fire. For example, engulfed objects will not lose any net heat by convection and so the term q_{conv} can be removed. Table 7.1 illustrates how the terms vary for different positions relative to the fire. The table also indicates limiting values for the configuration factor.

Examples 2-5 (Appendices A.2 - A.5) show various applications of the heat balance equations.

7.4 *Configuration factors*

In order to calculate the radiant intensity at a point distant from the radiator, a geometrical 'configuration' or 'view' factor must be used. For engulfed objects, the configuration factor can be assumed to be unity. The configuration factor for a surface that does not face the source, or is shielded is zero.

Figure 7.1 gives a general expression for the configuration factor for two surfaces. For a point source, the equation for the configuration factor for the receptor simplifies to the form:

$$\phi = \cos\beta / 4\pi r^2$$

where:

r = the distance between the emitter and receiver
β = angle between the normal to the surface and a line connecting the surface with the point

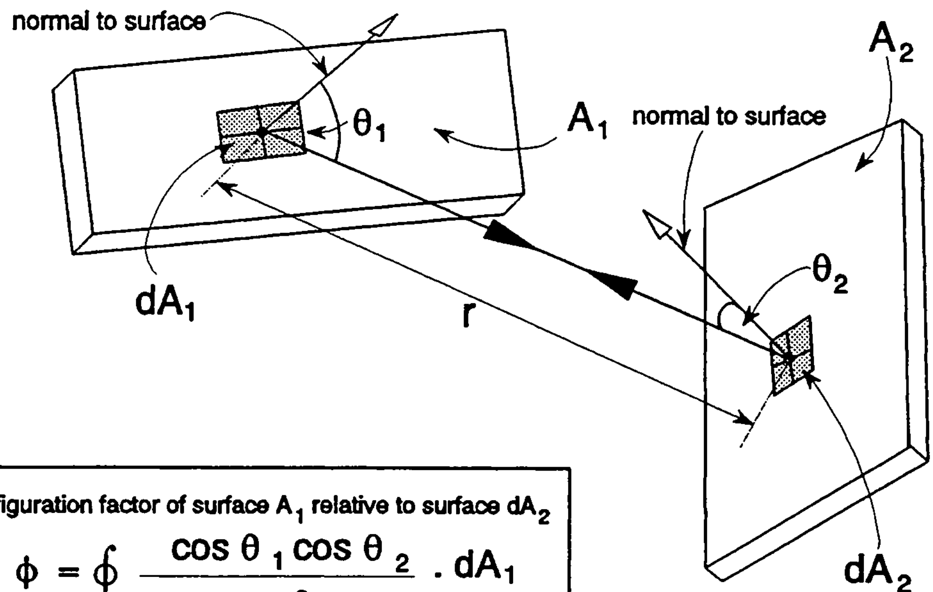
Values may be derived for various shapes and geometries from table and charts in the literature (see section 2.4.1 in Drysdale [16]). Examples 1 and 2 (Appendices A.1 and A.2) show the application of the above equation to typical problems.

Configuration factors can be complex to calculate, and will vary for different surfaces of a member. However, the concept is an important one as correct application enables significant reductions in received radiation to be justified for non-engulfed members.

Further information on configuration factors and the heating of steel outside the flame can be found in the SCI publication 'Fire safety of bare external structural steel' [17].

Table 7.1
Changes in significance of heat balance equation terms as a function of surface location relative to fire

Location relative to fire	Characteristic movement of heat relative to surface				Comments
	q_{ir} = fire radiation	q_{rad} = re-radiation	q_{ic} or q_{conv} = convection	ϕ = configuration factor	
Engulfed	• dominant heat source • large in magnitude • assume acts equally on all surfaces (conservative)	• significant at high surface temperatures (e.g., with insulation) • can be nearly equal to q_{ir}	• $q_{conv} = 0$ • q_{ic} is generally small relative to q_{ir} unless surface temperatures are low and gas velocities high (eg. jet fire)	• normally = 1 • may be < 1 if flame is not optically thick	It is normal to assume that the flame is optically thick. This can lead to apparent discontinuities in heat flux as the flame is entered.
Hot Plume	• magnitude depends on distance from flame • may be very low if surface shielded from fire	• significant at high surface temperatures • can be greater than q_{ir}	• usually $q_{conv} = 0$ • q_{ic} may be the dominant heat transfer process • magnitude of q_{ic} will vary with local gas velocities	• $\phi < 1$ • value important for determining q_{ir} • $\phi = 0$ if shielding occurs	Hot plume conditions may exist at locations remote from the fire. Ambient temperature in the hot plume is same as local gas temperature
Non-engulfed	• dominant heat source • varies approx. with square of distance from fire • much less than engulfed unless very close to fire	• unlikely to be significant unless surface is insulated	• $q_{ic} = 0$ • q_{conv} is dominant method of cooling • occurs from all member surfaces	• important in determining level of q_{ir} • $\phi = 0$ for shielding • may need to consider re-radiation from other surfaces	Conservative to ignore convective cooling (may be excessively so if local air velocity is relatively high).



Configuration factor of surface A_1 relative to surface dA_2

$$= \phi = \oint_{A1} \frac{\cos\theta_1 \cos\theta_2}{\pi r^2} \, . \, dA_1$$

Figure 7.1
The Configuration Factor

7.5 *Simple H_p/A method - assumptions and section factors*

To reduce the complexity of the problem for design, the following assumptions are made:

(a) The heat passing through the surface insulation to the steel is only a function of the surface area, insulation thickness and incident heat flux.

(b) All exposed surfaces receive the same incident heat flux and have the same thickness and properties of insulation.

(c) The rate at which the temperature of the steel rises is a function of the ratio of the exposed heated perimeter to the cross-sectional area of the member. This is more commonly known as the H_p/A ratio and is explained in more detail below.

As section size increases so does thermal capacity and surface area; these are the two most important factors affecting fire endurance. The combined effect of these two parameters may be expressed as the ratio of the exposed perimeter H_p to the cross-sectional area A of the member. This ratio H_p/A is normally presented in units of m^{-1} and is termed the 'section factor'. Sections with low H_p/A factors respond more slowly to heat and therefore achieve higher periods of fire resistance than sections with high H_p/A factors. Some sections with very low section factors heat up so slowly that they can survive a fire unprotected.

The definition of the heated perimeter of an unprotected member is relatively straightforward.

For a fully exposed I section:

$$H_p = (4B + 2D - 2t)$$

and for a fully exposed rectangular hollow section:

$$H_p = 2B + 2D$$

where B and D are the overall breadth and depth of the section and t is the web thickness.

Where a column acts in conjunction with a wall, or a beam in conjunction with a floor, and it is assumed that the wall or floor material is of such low conductivity that heat does not pass through into the surface of the flange, H_p for an I section reduces to $(3B + 2D - 2t)$ and for a rectangular section to $(B + 2D)$. Formulae for heated perimeters of various protected members are given in BS 5950: Part 8 [18].

For protected sections, there are two main forms of fire protection that should be considered in determining the H_p/A value of sections (Figure 7.2):

- *Profile protection* is where the fire protection follows the surface profile of the member. Therefore the section factor relates to the proportions of the steel member.

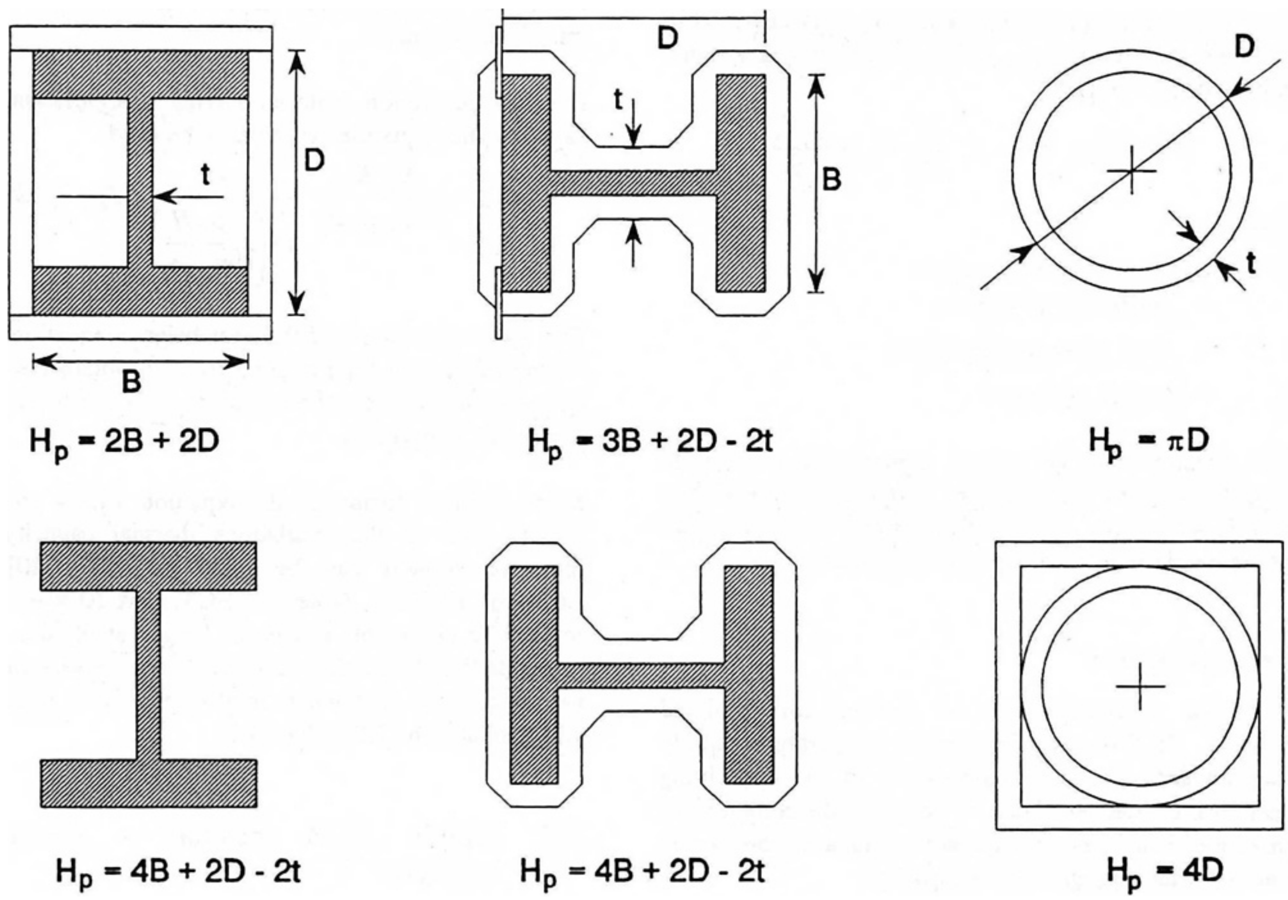


Figure 7.2
Profile and box protection to structural members

- *Box protection* is where there is an outer casing around the member. The heated perimeter is defined as the sum of the inside dimensions of the smallest possible rectangle around the section, neglecting air gaps etc. The cross-sectional area, A, is that of the steel section. The thermal conductivity of the protection material is assumed to be much lower than that of steel and therefore, the temperature conditions within the area bounded by the box protection are assumed to be uniform.

Adjusting the H_p/A factor is a logical method of correcting for fire exposure. Thus, if radiation is predominantly from one direction, then the Hp term should simply be equal to the projected area of the member that sees the radiation. The factor can thus be adjusted so that realistic thermal loads are used. However, if a member were engulfed then radiation and convection should be assumed from all sides and the H_p/A values should be the maximum calculated for the section. Adjustment of the H_p/A factor is thus of main benefit to non-engulfed members. Coupled with correct determination of the configuration factor, it is therefore possible to justify significant reductions in thermal load for non-engulfed members. The problem is to define whether a member is engulfed or not.

The program given in example 5 (Appendix A.5) defines the rate of heating in terms of the H_p/A factor.

7.6 *Simple H_p/A method - Calculation of the Temperature Rise of the Steel Section*

The heat balance equation can be solved to calculate the temperature rise of the steel with time based on the simplifying assumptions described above and the H_p/A concept. The solution varies depending on whether the steel is insulated or uninsulated. It is also dependent on the thermal properties and thermal mass of any insulation present.

Uninsulated sections

The solution for an uninsulated section fully engulfed in the flame, neglecting q_{conv} and q_{ic} and assuming a flame emissivity of 1 is [19]:

$$\sigma \, \epsilon \, (T_f^4 - T_s^4) = \frac{A}{H_p} \, C_s \, \rho_s \, \frac{dT_s}{dt}$$

where:

T_s = temperature of surface
ϵ = emissivity of surface
A = steel cross-sectional area
H_p = heated perimeter

This equation can be solved numerically on a time stepping basis to calculate the temperature rise of the steel T_s with time. It is used in various forms to obtain the temperature rise of bare steel sections.

Insulated sections

A similar expression can be derived for insulated sections. In this case the surface temperature rapidly rises towards T_f. The temperature of the underlying steelwork is then calculated by adopting the equation for one-dimensional passage of heat through a fire protection material with negligible heat capacity:

$$dT_{ss} = \frac{1}{C_{ss} \, \rho_{ss}} \, \frac{H_p}{A} \, \frac{K_i}{d_i} \, (T_s - T_{ss}) \, dt$$

where:

C_{ss} = specific heat of steel section in J/kg°C
ρ_{ss} = density of steel section, in kg/m^3
H_p/A = section factor (m^{-1})
T_{ss} = temperature of steel section in °C
K_i = thermal conductivity of the protection material (W/m°C)
d_i = thickness of the protection material (m).

Temperature dependent properties K_i and C_{ss} can be introduced in an incremental integration of T_{ss} knowing the variation of the temperature T_s with time, t.

The above equation ignores certain beneficial factors. Firstly, thicker heavier insulation materials have some thermal capacity (they store heat). Secondly, some protective materials have some natural moisture content and a certain amount of heat is required to vaporise this moisture. This causes a dwell in the rise of temperature at approximately 100°C.

The equation also assumes that steady state conditions exist at each moment in time, and that there is consequently a linear variation of temperature through thickness. During the initial stages of heating a non-linear variation may exist. However, the error introduced is small.

For fire protection material having a significant heat capacity, the equation below may be used:

$$dT_{ss} = \frac{H_p \, K_i}{A \, C_{ss} \rho_{ss} d_i} \, \frac{1}{1+\left(d_i \dfrac{\rho_i}{\rho_{ss}} \dfrac{H_p}{A}\right)} \, (T_s - T_{ss}) dt$$

The above equation and the heat balance equation must be solved at each time step in order to obtain the time-temperature curve for the member. This can be done by numerical integration.

More detailed forms of the equation which consider factors such as the insulations thermal capacity and moisture content can be found in FR2 [20] and EC3:Part 10 [21]. Note that EC3: Part 10 was issued for public comment, but is no longer available. It is probable that the section on protection materials will not be revised and that when finally issued the document will probably be EC3: Part 1.2.

7.7 Simple H_p/A method - Numerical examples

The information which can be obtained using the method described above takes the form of time-temperature curves, for both the insulation surface and the underlying steelwork. In order to illustrate this, a circular hollow section with an outside diameter of 323 mm and a wall thickness of 20 mm was considered. The section was first assumed to be unprotected and then was re-analysed with protection consisting of 25 mm of ceramic fibre. In both cases, the section was assumed to be receiving a heat flux of 100 Kw/m^2.

The results of the analysis are shown in Figure 7.3. It can be seen that the temperature of the uninsulated section rises to around 550°C in just 7 minutes and reaches a peak temperature of 940°C in little over 20 minutes. In the case of the insulated section, it can be seen that the surface of the protection rises very rapidly to its maximum temperature of 938°C whereas the steel temperature rises to about 300°C after 2 hours.

Example 4 (Appendix A.4) is a manual example of the H_p/A method. It shows how the H_p/A concept and heat balance equations can be combined to create a time-temperature curve for the underlying steel member. It can be seen that the procedure is ideally suited to being solved on the computer. Example 5 (Appendix A.5) includes a suitable program, and illustrates the H_p/A method for a number of different situations.

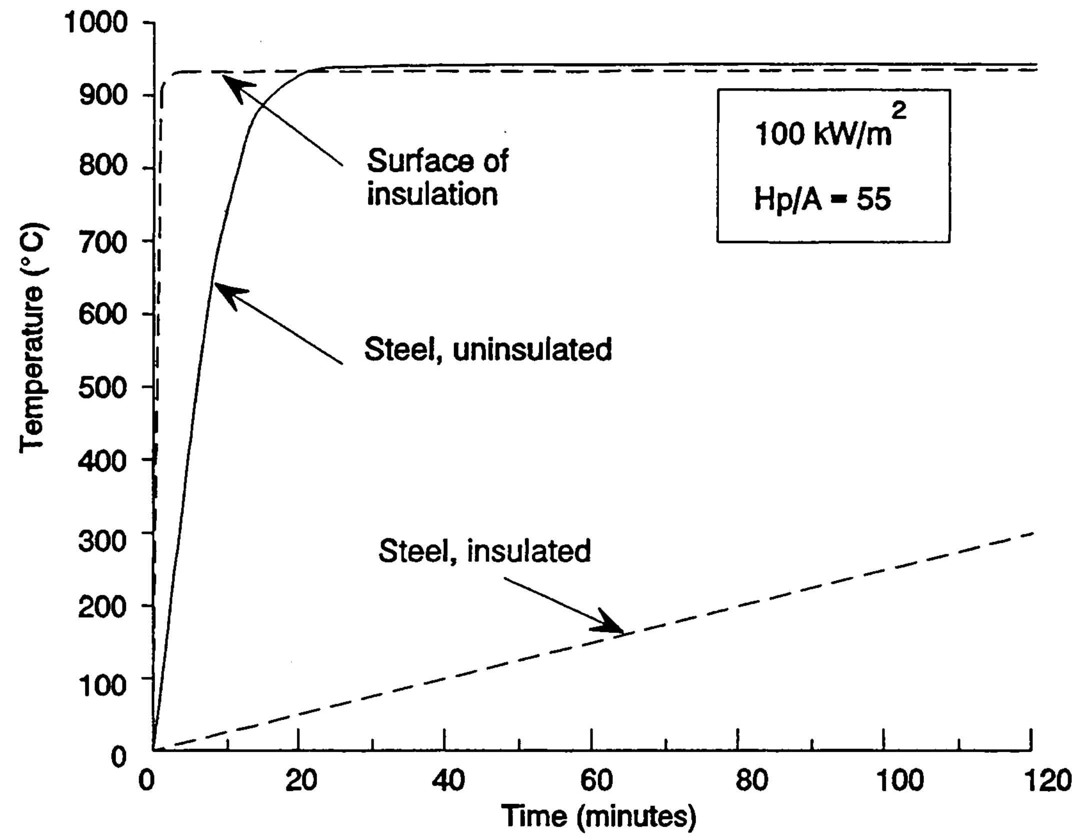


Figure 7.3
Thermal analysis of protected and unprotected steel sections

7.8 More rigorous methods

Experience shows that the transient temperature distributions in structural steel vary both along a member's longitudinal axis and also across its cross-section. More rigorous thermal analysis methods can take these effects into account. For example, although engulfed members are subject to a relatively uniform temperature distribution, the distribution around members subject to heat influx from a remote source will result in non-uniform longitudinal expansion and hence a curvature. This thermally induced imperfection will reduce the buckling load capacity of a compression member and would almost certainly not be detected in a simple thermal analysis.

When considering the use of more rigorous methods, some consideration should be given to the accuracy of the fire prediction. It is necessary to satisfy oneself that the fire loading is defined with sufficient confidence to justify the use of rigorous response analysis.

Finite difference methods

The finite difference technique is used extensively to compute heat transfer into steelwork by calculating the temperature of adjacent squares in the mesh. It suits one and two dimensional thermal analysis and may be carried out on small computers [22].

Finite element methods

Assuming that the flux absorbed by the steel member per unit length for a given incident flux is known, it is possible to use the thermal analysis capabilities of finite element (FE) programs to determine the temperature rise of the member against time. A thermal finite element analysis is a completely different problem to the structural response analysis. For example, the cross-section of the beam may be modelled by several hundred elements in order to permit the heat flow into the beam due to convection and radiation to be accurately modelled. A beam can also be divided into a number of elements lengthwise in order to model temperature variations along its length. (However, this level of complexity is not usually necessary for all members.) In contrast, for the structural analysis it is desirable to model the beam as just one or two elements.

Most thermal FE packages permit radiation, convection and conduction to be modelled. The problem of using such packages relates to the difficulty in accurately modelling the fire and passive fire protection, pfp having highly non-linear characteristics, for example:

- boundary and phase changes in intumescents
- movement and evaporation of water through cementitious and porous media
- the effects of internal voids giving rise to internal convection effects.

It is generally desirable for the thermal model to have the same geometry as the structural model, thus permitting the temperature data to be directly transferred at each time step into the subsequent structural response analysis. However, in such circumstances it is probable that the thermal modelling will be very crude in the FE analysis and there may be limited benefits gained. The advantage such methods offer is an automated manner in which to handle the temperature data. Even if the thermal loading model used in the FE analysis is crude, the fact that assumptions are not needed to reduce the volume of the data is likely to result in a more realistic analysis.

8. STRUCTURAL RESPONSE

8.1 The nature of failures

Yield

Members subject to loads which do not cause buckling effects will yield when the stress reaches a limiting value, depending on the pattern of stresses as well as their absolute values. The effect of temperature rise is to reduce the yield strength as discussed in section 4.

Member Buckling

A buckling failure is associated with geometric non-linearity (eg. imperfections) which are acted on by axial stresses to further increase the deflections, which results in buckling failure if the member stiffness is inadequate. Buckling may affect members or parts of members subject to compression or shear. Buckling may occur in a column under compressive loads ('flexural' buckling) or a flange of an I-section under compression induced by beam bending ('lateral torsional' buckling). The web of a girder may buckle under the action of shear stresses which have a diagonal compressive component ('shear buckling'). The essential factor leading to buckling is that the static equilibrium of the member is modified by the deflection of the member and is significantly different in the deformed and undeformed states.

The effect of temperature on member buckling is to reduce the value of E. This promotes larger deflections for a given load, and hence greater buckling problems. It is thus necessary to model the change in material properties with temperature.

Global Collapse

The failure of an individual member, whether by yielding or buckling, does not necessarily result in failure of a multi-member structural system. Global collapse occurs when member's progressively fail until some overall collapse criterion is violated.

8.2 The effects of fire

Steel structures have an inherent fire resistance which is influenced by a number of structural characteristics that are within the control of the designer. Significant benefits can therefore be derived by considering the fire limit state from conceptual design. These characteristics can be summarised as follows:

Capacity: the capacity of a steel member will be greater than the load it will be supporting at the start of a fire. The greater the excess capacity, the greater will be the members inherent fire resistance.

Ductility: the ability to deform plastically beyond yield. Non-ductile members result in local and overall instabilities leading to a sudden load shedding which can instigate premature collapse; ductile members have a better ability to support loads in fire conditions.

Redundancy: a redundant structure which can offer alternative load paths when member failure occurs will have better fire endurance characteristics than a non-redundant or a less redundant structure.

All of the above characteristics can be controlled in the design process by the following parameters:

- section width to thickness ratio's (have a direct influence on ductility)
- member size (has a direct influence on capacity)
- member surface area (has a direct influence on the rate of temperature rise and hence on the time taken to reach a specific elevated temperature)
- structural configuration (has a direct influence on redundancy)

8.3 Thermal restraint

A fire affecting one part of the structure will cause the members in or near the fire to heat up more than other members and will also create temperature gradients within other members. This is at variance with the uniform heating usually applied in standard fire tests and assumed for simplicity in design. Non-uniform heating of the structure causes additional stresses in members which are not free to expand due to the restraint afforded by the remainder of the structure. The restraining effect will be further magnified since the hottest members will have a lower modulus of elasticity, E, than cooler members. This phenomenon, known as thermal restraint, may have a detrimental effect on the behaviour of structural members and could lead to premature failure by comparison with the predictions of the 'traditional approach' (see Section 8.5).

In a recent incident onshore [23], a few beams and columns in a steel framed building buckled as temperatures well below the design temperatures as a result of the additional forces caused by thermal restraint. In this instance, ground floor columns restrained by a stiff structure at an upper level failed by local buckling at the column ends and compression diagonals of trussed beams buckled as a result of the additional forces caused by the restraint from other truss members. The incident clearly demonstrated the detrimental effect that such restraint can have on the structural behaviour of individual members in fire. However, the fact that the building did not collapse also demonstrated the benefits of redundancy and ductility.

Offshore structures tend to comprise members with low slenderness. The quality of materials and design of connections are generally such that ductile response can be assumed. The result is that, whilst the structure may be particularly prone to developing high forces as a result of thermal restraint, it is also ideally conditioned to redistribute the forces to other parts of the structure. Studies have shown that for a typical offshore structure forces generated by differential heating can be ignored. However, this assumes no slender members that may bow or buckle prematurely. The recommendation of this note is that a structure should be screened for slender members (e.g., compression flanges) and the criticality of these members assessed. An illustration of how thermal restraint can lead to failure is given as example 6 (Appendix A.6) [24].

8.4 Determination of critical structural members

A topsides structure generally possesses a high degree of redundancy. A structural redundancy analysis which examines the response of the structure with respect to its in-place loadings will determine which members are redundant and can be removed without catastrophic consequences. This will also enable essential members to be identified. The results from the fire hazard studies will then be used to determine which of these essential members are at risk from fire.

A suggested procedure for determining critical structural members is outlined below:

(1) by inspection eliminate all structure:

   (a) not critical to overall structural integrity (e.g., supporting non-critical items)

   (b) not supporting escape routes

   (c) not supporting critical safety equipment

   When eliminating structure it will be necessary to make a qualitative assessment of the escalation potential were the eliminated structure to fail. For example, structure supporting a non-critical but heavy piece of equipment may not itself be critical, but failure could result in the equipment item falling through the platform causing damage. Other members may indirectly contribute to the strength of critical structural members by, for example, providing restraint to compression elements.

(2) modify structural stick model as follows:

   (a) remove members identified in (1)

   (b) modify loading so that it represents the probable load at the time of fire. IGN page 4.29 recommends combining best estimate dead and live loads with 0.33 x operating environmental load.

   (c) remove safety factors in the code check

   section 8.5 gives further advice on appropriate loading and how the safety factors can be removed from the code.

(3) run structural analysis. Those members with the highest unity checks will in general have the lowest fire resistance. Look particularly for those members where the unity check is high as a result of member capacity being reduced due to stability criteria. This will indicate members that are most prone to thermal restraint problems.

(4) a high unity check does not necessarily mean that failure of that member would be a problem. The analysis can be re-run with some of the high unity check members removed and replaced with compensating moments and forces where appropriate. This will indicate the ability of the structure to redistribute loads since some members may be removed with little effect on surrounding structure whilst the removal of other members will cause a large number of members to overstress.

(5) repeating step (4) and assessing at each stage as in (3) will result a list of members that can be defined as critical. Further fire response analysis should concentrate on these members.

Note that the procedure described above may be integrated with the analyses used for linear elastic methods. These are described in the next section.

8.5 Linear elastic methods

Basis for linear elastic methods

Linear elastic methods consider the structure as a complete entity in order to determine member forces and moments. For members subjected to a temperature rise the properties (yield strength and Young's Modulus) are reduced to reflect the elevated temperature using information such as given in Tables 4.1 and 4.2. A linear analysis is then performed and the resulting stresses or unity checks are interpreted as discussed below. The temperature at which these analyses are done is used to estimate endurance via the time vs. temperature relationship established in Section 7. Whilst such methods are attractively simple, they do not reflect the actual behaviour of the complete structure, particularly with respect to member end conditions. However, in general the approach is over-conservative, although it may occasionally be unsafe.

For example, in a module support frame, individual members benefit from continuity at their connections with other elements leading to increased member resistance. Furthermore, at a global structural level, failed members which are engulfed by fire will be able to shed their load to cooler and less severely stressed elements of the structure; consequently, although member collapse may occur in the fire zone, stress redistribution in a redundant structure can prevent overall structural collapse. Thus the temperature at ultimate structural collapse can be significantly higher than the temperature at first element failure. Such aspects are important when assessing the integrity of TR support structures and establishing structural support durations for comparison with potential evacuation times.

Limitations of linear elastic methods:

(1) These analyses assume the structure heats up uniformly or that any thermal loads generated by differential heating of members can be redistributed within the structure. In slender compression members, a combination of thermal stresses with the applied loads may lead to a premature buckling failure. In such instances, linear elastic methods can be unconservative. Example 6 (Appendix A.6) illustrates how restraint can lead to member failure at a lower temperature.

(2) Temperature gradients through the thickness of a member can result in thermal bowing with possible P-δ effects. These are not accounted for.

(3) These methods do not allow for any redistribution of stresses; each member is individually checked and is made to satisfy the code check.

(4) As the structural solution is based on a linear analysis, the actual behaviour of the structure as the steel heats up cannot be accounted for. The behaviour can be traced in a step-wise manner using non-linear analysis.

(5) Imposed finite imperfections e.g. due to a prior explosion cannot be accounted for.

(6) Other failure criteria e.g. insulation failure cannot be accounted for.

Allowable stresses and safety factors for fire resistant design

Fires are rare occurrences and for calculation purposes are treated as a form of 'accidental' loading. In allowable stress codes, the allowable stress term may be increased for certain extreme loading events with a long return period. API and AISC codes recommend values of 1.333 for extreme environmental loads (return period equal to 100 years) and 1.7 for earthquake conditions (return period about 2000 years). Similarly, in limit state codes it is normal to apply a comparatively high load factor to an in-service limit state and a lower factor to a serviceability or collapse limit state. This is because the probability of overload and inaccuracies in the method of calculation coinciding are considered to be small and of less significance than those under normal loading conditions.

Except for BS 5950: Part 8, no guidance is given on increasing allowable stresses or decreasing load factors for fire design. The key to establishing appropriate design values is to determine the point when the stresses throughout the section will be equal to yield. The manner in which this is achieved differs slightly for allowable stress design and limit state design.

Recommendations for allowable stress design

It has been shown [19] that an increase in allowable stresses of 1.7 can be used to estimate the ultimate behaviour of a steel-framed structure under fire loadings, given that:

- design is to AISC
- linear elastic methods are used
- the loadings are the expected loads at the time of the fire
- the elevated temperature stress and elasticity terms relate to a 0.2% strain
- lateral and torsional buckling of elements is not triggered by the failure of secondary attachments and other out-of-plane restraints.

It should be emphasised that using an allowable stress factor of 1.7 only gives an estimate. A more accurate method is to modify the allowable stress equations in order to remove, where practical, the built in safety factors.

Recommendations for limit state design

Partial factors on loads (γ_f) are taken as unity for permanent dead loads and storage loads. BS 5950:Part 8 suggests load factors on non-permanent imposed loads be reduced to 0.8 except on escape routes and lobbies where a factor of 1.0 should be maintained. In the development of the Interim Guidance Notes discussions were held with oil industry experts and it was considered that a factor of 0.8 on imposed loads was too high since, although offshore modules are designed to sustain high imposed loads, in practice the majority of an area will not see this. This is in contrast to buildings. For this reason, a value of 0.33 is suggested, though if possible the imposed load should be assessed by a site survey. The partial factor for wind load is reduced to 0.33 for structures greater than 8m in height. The effect of wind loading may be ignored for smaller structures. Snow loads on roofs may also be ignored.

The partial factors on material strength (γ_m) at the fire limit state are taken as unity for structural steel. On average the actual strength will be greater than the characteristic values used in normal design.

Further information on load and resistance factors is given in IGN Section 4.6.6.

8.6 Member based methods of fire design

Application of these methods is demonstrated in worked example 7 (Appendix A.7).

Limiting Temperature Method

This is the traditional approach to fire design. It assumes that structural failure occurs when the steel reaches a critical temperature, usually about 400°C. At this temperature the steel exhibits an approximately 40% reduction in yield stress. This corresponds to the likely working stress level in the member. Note that allowing an overstress of 1.7 in allowable stress design is directly equivalent to reducing the yield stress by 42%.

All steelwork requiring a pre-defined fire resistant period (say one or two hours) is uniformly protected with PFP such that its temperature does not rise above the specified temperature limit of 400°C during this time. No account is taken of the load level in the member at the time of the fire.

This method can lead to unsafe design because it is unable to detect problems of thermal restraint (see Section 8.3 and Example 6). However in stocky structures like offshore platforms this is unlikely. This was illustrated by a recent Shell study [24] which concluded that the effects of thermal expansion on member loads should not be superimposed on the topside load effects. Member loads resulting from thermal expansion did not appear to influence the ultimate strength of the final global failure. Their inclusion in a linear elastic analysis would give rise to significant underestimation of the temperature at which structural collapse would occur. However, it should be noted that the Shell analysis was based on a structure where members were insufficiently slender for premature buckling to be a problem.

The main criticism of the method is that it is likely to be overconservative. With fast computers and up-to-date structural programs it is probable that a significantly less conservative analysis can be carried out for almost no extra effort. The following sections describe some of these.

Code check methods

In a computerised structural analysis, which is typically used in offshore structural design, code checks are performed as post-processor routines. The aim is to compare the acting stress (or factored load) with the allowable stress (or member capacity) and to express the result as a ratio, often termed the unity check.

Code check methods based on the use of existing ambient temperature structural design codes can be used to determine if hot steel structural components satisfy the specified code unity check. The procedure is as follows:

(1) Carry out a room temperature linear elastic analysis to determine member forces and unity checks for each member.

(2) Incorporate the modified (reduced) safety factors into the code check. This will decrease the unity check. This procedure will be different for allowable stress design compared to limit state design:

   allowable stresses: two methods are available. The first is to increase the allowable stresses to an appropriate value for fire loading, for example by increasing the denominator of the unity check.

   With AISC a factor of 1.7 on the denominator is suggested. The problem with this is that it does not reflect that different clauses within the code have different factors of safety. However, it can be argued that it is sufficiently close for practical purposes. This can be shown by considering an I-section with an assumed shape factor of 1.12 and using this to determine the net safety factor for the various load conditions using the 1.7 allowable stress factor.

bending	(0.66/1.12)x1.7	= 1.00
tension	0.6x1.7	= 1.02
compression (stocky)	0.6x1.7	= 1.02
compression (slender)	(12/23)x1.7	= 0.89

Each gives a net factor close to unity, except for the slender compression member which gives a conservative 0.89. Since such members have a degrading post-buckling curve (ie. they shed load rapidly) it can be argued that it is good practice to maintain a small safety margin for slender compression elements.

limit state: in limit state design the safety factors are applied directly to the loads and material properties. It is therefore a straightforward process to reduce these to the values applicable for the fire limit state (i.e. reduce the numerator of the unity check).

(3) Adjust the yield strength and Young's Modulus to correspond to the properties at the anticipated temperature of each member. This will lead to a reduction in the denominator of the unity check and hence increase its value.

(4) Assess whether the final modified unity check is satisfactory (less than 1)

This method will generally predict higher failure temperatures than the limiting temperature method, which means that either less PFP is required or enhanced endurance times are possible. However, a number of limitations still exist with this approach:

(1) Slender member problems cannot be identified.

(2) It can be difficult to modify allowable stress codes.

(3) Most codes are not validated at elevated temperatures, except BS 5950: Part 8 (see below). However, comparisons between BS 5950:Part 8 and methods based on other codes result in similar critical temperatures.

The application of code check methods shall be illustrated by reference to BS5950:Part 8.

BS 5950: Part 8

This is the first Code or Standard in the UK dealing specifically with the fire resistance of steel structures. The Code provides methods of calculation whereby the designer can establish appropriate thicknesses of fire protection. However, the clauses in the Code relating to fire resistance periods and to the calculation of PFP thickness have been calibrated against standard cellulosic fire curves and are not applicable to hydrocarbon fires.

The code check method outlined in the previous section assumes a member temperature and proceeds to determine whether the member fails at that temperature or not. Whilst the method is as rigorous as the limiting temperature method, it is not actually providing the information that is necessary to efficiently design the fire protection system. For that it is desirable to know the temperature at which the member fails. This then enables the methods of section 7 to be applied to determine what fire protection (if any) is required to prevent the member failing for the specified design duration.

The method of the previous section could be repeated for a number of different member temperatures, and graphs plotted for each member of unity check against temperature. The limiting temperature could then be obtained from the graph (i.e., temperature at which unity check is unity). It may be expected that similar types of member having similar room temperature unity checks would have similar limiting temperatures. This turns out to be the case. Given the type of member (beam, column, tension) it thus becomes possible to define the limiting temperature direct from the room temperature unity check (obtained using load and material factors applicable to the fire limit state). This is the method used in BS 5950:part 8. The Code is only applicable to hot finished steels complying with BS EN 10029:1991 (replacement to BS 4360) and cold finished steels complying with BS 2989.

The limiting temperature of a member in a given situation depends on the load that the member carries. A detailed analysis of fire test results and the use of computer models has demonstrated that in virtually every situation the limiting temperature is dependent on the fraction of the ultimate load capacity that a member supports at the time of the fire. It can therefore be assumed that a 250 x 50 x 10 SHS loaded to 50% of its ultimate bending capacity will fail at the same temperature as a 150 x 100 x 8 SHS loaded to 50% of its ultimate bending capacity. Based on the fact that fully loaded members, designed in accordance with the

Code, fail in fire resistance tests at approximately the same temperature, BS 5950: Part 8 has extended this observation to all levels of load so that fire protection can be more accurately and economically specified. Note that BS 5950:Part 8 is based on a theoretical approach, but that this is backed up by fire test results. This test justification is important, particularly in relation to applying the method to members that may fail by buckling rather than yielding.

BS 5950: Part 8 gives limiting temperatures for different types of member for a range of load ratios (unity checks). It is important to understand the limitations of this data and how to calculate the load ratios in each situation. The data relevant to offshore structures is reproduced in Table 8.1 and is applicable to I sections and SHS. A full description of the derivation of Table 8.1 is given in reference [3].

The load ratio R is the applied force multiplied by the appropriate fire load factors divided by the member capacity (at room temperature) calculated according to BS 5950: Part 1. It is equivalent to the unity check obtained in allowable stress design.

For a member in bending heated on 3 or all 4 sides and designed in accordance with BS 5950: Part 1, the load ratio is given by the greater of:

$$R = \frac{m M_f}{M_b} \quad \text{or} \quad R = \frac{M_f}{M_c}$$

where:

M_f = applied moment at the fire limit state

M_b = lateral torsional buckling moment

M_c = the moment capcity M_{cx} or M_{cy} as appropriate to the axis of bending

For columns in simple construction the load ratio R, is given by the interaction formula of Clause 4.8.3.3.1 in BS 5950: Part 1.

$$R = \frac{F_f}{A_g\, p_c} + \frac{m\, M_{fx}}{M_b} + \frac{m\, M_{fy}}{p_y\, Z_y}$$

where:

F_f = axial load during fire

M_{fx} = maximum moment about x axis during fire

M_{fy} = maximum moment above y axis during fire

A_g = gross cross-sectional area

p_c = compressive strength of member

m = 1.0 (Clause 4.7.7 - Part 1)

M_b = buckling resistance moment capacity about major axis

p_y = steel strength

Z_y = elastic section modulus about minor axis

Table 8.1
Limiting Temperatures for Design of Protected and Unprotected Members

Case No.		Load Ratio (R)					
		0.7	0.6	0.5	0.4	0.3	0.2
	Members in compression						
(1)	Slenderness ratio ≤ 70	510	540	580	615	655	710
(2)	Slenderness ratio ≤ 180	460	510	545	590	635	635
	Members in bending						
(3)	Unprotected members, or protected members complying with Clause 2.3(a) or (b)	520	555	585	620	660	715
(4)	Other protected members	460	510	545	590	635	690
	Members in tension						
(5)	All cases	460	510	545	590	635	690

This approach may be used for compression members for which Part 1 allows the use of the simplified approach.

Where a member is subject to both compression and bending, it is appropriate to use the load ratio for compression members. Providing slenderness is ≤ 70, this will give a virtually identical limiting temperature to treating the member as a bending element. If slenderness > 70 then the limiting temperature will be reduced.

In summary, the design procedure in BS 5950: Part 8 is as follows:

(1) Carry out a room temperature linear elastic analysis to determine member forces, but using load factors applicable to the fire limit state.

(2) Calculate the load ratio for each member.

(3) Determine the limiting temperature for the member type at the calculated load ratio from Table 8.1.

BS 5950: Part 8 represents a more rigorous code check than those based on modifications to API or AISC because it takes into account the temperature gradient within the member, the stress profile through the cross-section and the dimensions of the cross-section. Potential instability failures for slender columns will also be detected. However, comparative studies show that the modified AISC and BS 5950: Part 8 give similar results.

8.7 Non-linear methods

Simple non-linear analysis

There are a variety of methods which are based on repeated linear analysis, changing the model as members fail. Modified code checks are used to determine member utilisation. Differential heating can be introduced if the computer program is able to model thermal expansion, however, internal member forces can generally be equilibrated within the structure.

This approach is best limited to situations where linear elastic analysis with member based code checks indicates that only a few members will fail. It enables a more detailed assessment of consequence without resorting to full non-linear analysis and may indicate higher member temperatures can be endured before failure. Sub-models can be used to investigate the effect of differential heating on slender members. However, great care is required in such an analysis to ensure members fail in the correct order and the introduction of hinges can be very laborious. Slender members are not treated rigorously by this method.

Given the increasing availability and sophistication of non-linear tools, carrying out a non-linear analysis using a repeatedly modified linear elastic model cannot be recommended in the general case.

Non-linear analysis

Non-linear analysis permits the fire duration of a structure to be based on the resistance of the overall structure rather than just the resistance of each member. A progressive collapse study can be carried out via either a non-linear incremental load or incremental temperature analysis. In the context of determining fire response it is appropriate to increment the member temperatures.

The sophistication of a non-linear analysis can vary widely, for example, temperature effects may be treated in the following ways:

- member to member temperature variation
- variations in temperature along member length
- temperature variation across member section.

A non-linear analysis can study failure and load redistribution characteristics. Both geometrical and material non-linearity can be included as well as material variability with temperature.

Geometric non-linearity is important when large deformations of the nodes in a structure begin to influence the result of the structural analysis. Such behaviour can only be analysed when the solution technique allows the feedback of structural deflection into the computation of element loads.

Because the temperature can vary around the structure it is usually important for the software to assign different material properties to individual elements so that such variation can be conveniently modelled. Time dependent simulation enables conduction between members and changes in thermal loading to be modelled. If the structure has been previously damaged by an explosion, it is possible to model initial conditions.

Typical requirements of a non-linear software package capable of analysing a structure subject to fire loads include:

- ability to generate or import temperature loads
- non-linear beam elements with thermal gradient capability

- material models including multi-linear curves, temperature dependence and creep
- large deflection capability
- buckling capabilities

If the thermal model has the same geometry as the structural model, the temperature data can be directly transferred at each time-step into subsequent structural response analysis. The accuracy of results depends on the number of elements used to model each physical member with members which are anticipated to undergo large plastic deformation requiring more elements.

Non-linear analyses are useful for studying the sensitivity of structural response to different fire scenarios and geometry and restraint effects. These methods are likely to become more common, especially in the assessment of existing installations.

Advantages of non-linear analysis over linear analysis:

(1) It is the most accurate analytical way of describing structural behaviour up to collapse, particularly under fire conditions where large deformations and non-linear material behaviour are present. The analysis should detect members that fail prematurely by buckling and correctly account for the post-buckling resistance.

(2) The true collapse load can be estimated by allowing stress redistribution from the failed members to the less severely stressed members and by tracing the behaviour of the structure in a stepwise member.

(3) It can lead to a more economical design than methods based on simplifying assumptions.

Disadvantages of non-linear analysis over linear analysis:

(1) User competence can affect the accuracy of the result.

(2) Because the analysis traces the behaviour of the structure step by step as the load or the temperature increases, the analysis can be time consuming for any realistic size of structure.

(3) Non-linear analysis is costly by comparison with linear analysis.

(4) Where premature shedding of member loads as a result of buckling is not a problem, non-linear analysis can justify savings in pfp. However, such savings result from eating into the "reserve" inherent in the general conservatism of the more simple methods.

(5) Whilst structural response is accurately modelled, the method can only be as good as the fire prediction.

8.8 Reliability-based methods

It is apparent from the preceeding sections that the prediction of the outcome of a given fire scenario is not always certain because of a) **physical variability**, eg. in material properties and b) **modelling uncertainty**, eg. insufficient knowledge of heat fluxes in pool and jet fires in partially confined spaces.

Structural Reliability Analysis has already been used successfully by the offshore industry in quantifying the extreme storm risk, allowing for explicit modelling of all important **physical variables**, like long-term wave climate, short-term variability within a storm, directional spreading of waves and variability in material properties of steel and soils.

In order to obtain realistic results it is necessary to minimise the modelling uncertainty by using accurate models and carry only the physical uncertainty.

Following the traditional terminology of Structural Reliability Analysis (SRA) the variabilities and uncertainties may be split into **loading uncertainty** and **resistance uncertainty**. For a fire problem the **loading uncertainty** arises from:

- definition of type of fire, eg. pool or jet fire, fuel type, etc.;
- definition of size of fire, eg. cone radius and length for jet fires;
- definition of duration of fire (how long does the inventory last ?);
- uncertainty on effectiveness of deluge in pool or jet fires;
- uncertainty in heat fluxes on structural members.

Similarly the **resistance uncertainty** arises from:

- uncertainty in time-temperature relationship for a given heat flux input;
- mean reduction of yield strength with temperature and variability in this parameter;
- mean reduction of Young's Modulus with temperature and variability in this parameter;
- material elongation characteristics;
- fabrication imperfections;

- modelling uncertainties, for instance if linear elastic analyses are performed the modelling uncertainty may be significant;

A Structural Reliability Analysis may, in principle, be carried out to quantify the Fire Loading risk using the above uncertainties as a basis. However, a number of parameters on the loading side are still not understood sufficiently to enable quantification of the loading uncertainty. Following the analogy with the extreme storm modelling, the reason why we can synthesise the probability of failure due to extreme storms is because we understand the long-term climate, the short-term variability within a storm and because we have good wave loading models and accurate models for the ultimate strength of a structure. For the fire loading problem these key parameters are still not understood. Therefore the application of reliability methods to this problem seems rather premature.

What is more reasonable at present is to recognise the uncertainty in the outcome and perform sensitivity studies for those cases that contribute most to the overall risk. Based on the results of these sensitivity studies the outcome of the fire scenario may be stated in probabilistic terms. For instance, instead of concluding that the endurance of the structure under a given scenario is 20 minutes the sensitivity studies may help us to say that:

- probability (collapse) in less than 15 minutes = 0
- probability (collapse) after 35 minutes = 1
- probability (collapse) between 15 and 35 minutes increases linearly from 0 to 1.

The QRA Framework can easily cope with outcomes stated in probabilistic terms as above. With experience, gained from sensitivity studies, the uncertainty range on endurance may be stated as a function of fire type (pool fire vs. jet fire) and structure type (unprotected truss, plate girder, floor, ceiling, etc.). The advantage of providing the outcome as a range of possible endurances with associated probabilities is that it provides a better appreciation of the risk picture. This has implications for the estimation of fatalities (because evacuation is also a function of time) and for the effectiveness of some upgrade measures e.g. effectiveness of blowdown.

9. CONCLUSION

This technical note has presented a number of methods which can be used to determine the rate at which a structure heats and the consequent structural response. The methods vary both in terms of simplicity and refinement.

When determining the rate at which a member heats it should be noted that the methods presented, particularly the more simple ones used in the worked examples, should be regarded as estimates. There are many parts of the procedure where the input data is of an approximate nature (e.g., the heat flux from the fire; the properties of the insulation). The methods do enable the designer to determine with reasonable confidence the magnitude of the fire loading problem, however, where these simple methods indicate severe loading to a critical part of the structure then more advanced procedures should be considered. In general this will necessitate contacting a fire loading expert. Even if such an expert is unable to refine the loading, it is good practice to have the most vulnerable parts of the structure independently checked.

A number of different methods of determining the structural resistance at elevated temperature have been presented. The more simple of these (limiting temperature & code check methods) consider the response of individual members. In general these are either comparatively accurate or conservative. However, they fail to consider additional loads in members due to thermal restraint and thermal bowing and can therefore be unconservative for slender members where buckling is the mode of failure. Since offshore structures comprise mainly of stocky members, this is not a major problem although design should screen for "vulnerable" members.

The more advanced elevated temperature resistance methods consider the response of the whole structure. These methods allow members to shed load and may include buckling. In general higher critical temperatures will be computed. The methods can be regarded as rigorous from the structural response viewpoint (assuming an appropriate model), however, if the thermal loading analysis is not of similar rigour (including hazard determination and fire modelling) then it may be questioned whether such a detailed response analysis is justified.

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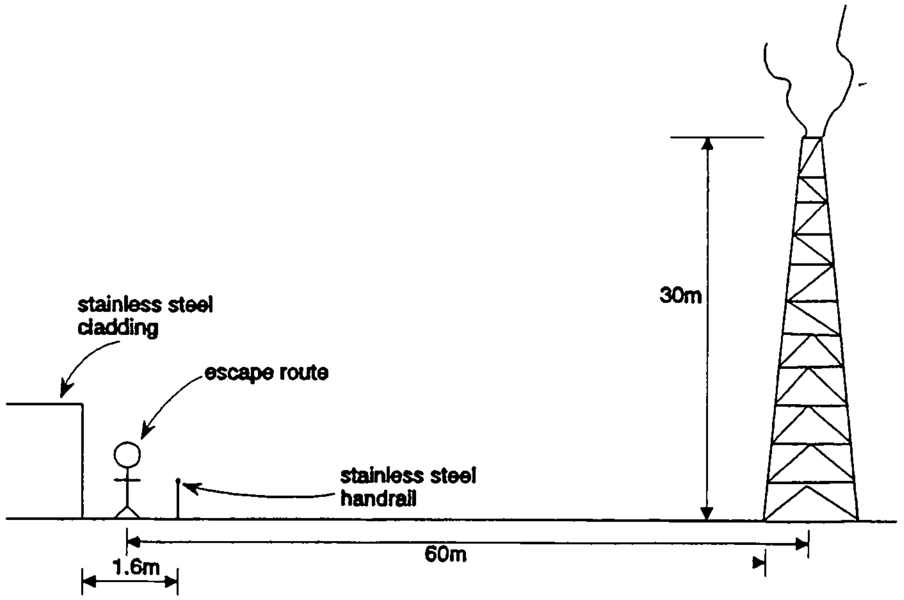
EXAMPLE 1: HEATING OF STEEL REMOTE FROM FIRE SOURCE

An escape route from a temporary refuge is exposed to radiation from a flare. A quick assessment is required to establish:

(1) whether personnel can use the escape route, or whether radiation shielding is necessary;

(2) the temperature of decking, walls and handrails after 1 hour.

The base of the flare is 60 m horizontally and 30 m elevated from the walkway. During blowdown it will burn 20 kg/s of mixed hydrocarbon. It is estimated that personnel would be exposed on the walkway for a period of 15 seconds.



The air temperature at the walkway can be assumed to be 30°C. Due to convective flows, air velocity can be taken as 4 ms^{-1}.

__Size of and heat generated from flare__

Since mixed hydrocarbon, assume heat of combustion is 48 kJ/gm — *Drysdale Table 1.13*

At 20 kg/s, total heat release = 960 MJ/s = Q

Only a proportion of this heat will be released as radiation. Based on large scale tests, and assuming mixed hydrocarbon results a sooty flame, 30% of the heat is released as radiation.

i.e. F-factor = 0.3 — *FL1 P 123, API R521*

The reduced heat will be assumed to emanate from 5 points located along the flame centreline. The API model will be used to determine flame length.

$$L = 2.76\, Q^{0.452}$$ *L in metres, Q in MW* — *API R421, FL1 P92*

$$= 61.5, \text{ say } \underline{60\ m}$$

Locate points at 6, 18, 30, 42 and 54 metres above flare tip.

$$\text{Radiation from each point} = \frac{960 \times 0.3}{5} = 57.6 MW$$

say 60 MW

The heat released will be further reduced by the transmissivity of the atmosphere. Reductions of 20% have been noted over 20 m distances. Greater reductions may occur over longer distances, but this is not proven. — *FL1 P 105*

$$\therefore \text{ net heat from each point} = 60 \times 0.8 = 48 MW$$

say 50 MW

__Radiation received at a surface__

The maximum heat flux to a receiving surface outside the flare is given by:

$$q = \frac{F\,Q\,\tau}{4\pi\, r^2}$$

FL1, eq 7.15 P 123

The term $F\,Q\,\tau$ is the 50 MW value calculated from the previous page.

$$\therefore q \approx \frac{4000}{r^2}$$

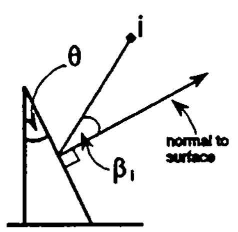
where q is in kw/m^2

r is in m

However, there are 5 points. Also, the surface may not be normal to the line connecting the point with that surface.

Assuming an isotropic emitter:

$$\therefore q = \sum_{i=1}^{5} \left[\frac{4000}{r_i^2} . \cos \beta_i \right]$$



where r_i = distance from point to surface, metres

β_i = angle between normal to surface and the line connecting that surface to point i.

Use the above equation to calculate the variation in radiation for differently oriented surfaces. The orientation of the surface to the vertical is denoted by θ.

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CALCULATION SHEET

Job No.	Sheet	Rev.
OFF 3197	Sheet *4* of *10*	

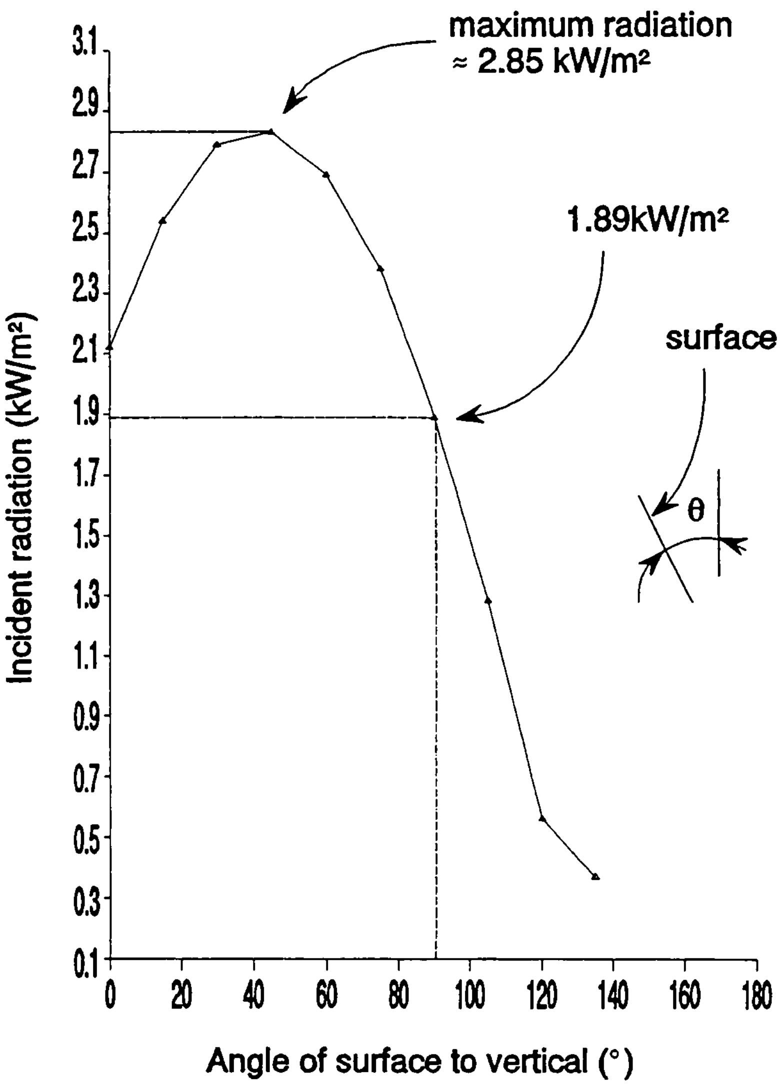
Job Title: *WORKED EXAMPLE 1*

Subject: *Heating Of Steel Remote From Fire Source*

Client	Made by	Date
FABIG	*HGB*	*Jan 1993*
	Checked by: *CAS*	Date: *Jan 1993*

Angle of surface to vertical θ	Point 1 $r_1 = 70$		Point 2 $r_2 = 77$		Point 3 $r_3 = 85$		Point 4 $r_4 = 94$		Point 5 $r_5 = 103$		Total Production Σq_i (kW/m^2)
	β_1	q_1	β_2	q_2	β_3	q_3	β_4	q_4	β_5	q_5	
0	31	.70	39	.52	45	.39	50	.29	54	.22	2.12
15	16	.78	24	.62	30	.48	35	.37	39	.29	2.54
30	1	.82	9	.67	15	.53	20	.43	24	.34	2.79
45	14	.79	6	.67	0	.55	5	.45	9	.37	2.83
60	29	.71	21	.63	15	.53	10	.45	6	.37	2.69
75	44	.59	36	.55	30	.48	25	.41	21	.35	2.38
90	59	.42	51	.42	45	.39	40	.35	36	.31	1.89
105	74	.23	66	.27	60	.28	55	.26	51	.24	1.28
120	89	.01	81	.11	75	.14	70	.15	66	.15	.56
135	104	.20	96	.07	90	0	85	.04	81	.06	.37

The Steel Construction Institute	Job No. OFF 3197		Sheet 5 of 10	Rev.
Silwood Park Ascot Berks SL5 7QN Telephone:(0344) 23345 Fax:(0344) 22944	Job Title WORKED EXAMPLE 1			
	Subject Heating Of Steel Remote From Fire Source			
	Client FABIG	Made by HGB	Date Jan 1993	
CALCULATION SHEET		Checked by CAS	Date Jan 1993	



__Endurance time for personnel__

The upper limit for continuous exposure is 2.5 kW/m^2. The received radiation is estimated to be slightly in excess of this value at 2.85 kW/m^2. There is, however, no danger to personnel exposed for only 15 seconds.

IGN, Table 2.2 Pg 2.15

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CALCULATION SHEET

Job No. *OFF 3197*		Sheet *6* of *10*	Rev.
Job Title *WORKED EXAMPLE 1*			
Subject *Heating Of Steel Remote From Fire Source*			
Client *FABIG*	Made by *HGB*	Date *Jan 1993*	
	Checked by *CAS*	Date *Jan 1993*	

Temperature of decking

Assume 8 mm plate, insulated underneath.

Emissivity of surface = *0.8 (assumed)*

Local ambient temperature = *30°C (303°K)*

Determine maximum surface temperature possible by allowing for convective cooling and re-radiation.

Heat gain = $0.8 \times 1.89 = 1.512\ kw/m^2$

Heat loss = *heat gain assuming steady state conditions are achieved*

convective loss = $q_c = h_c\ (\theta_s - \theta_a)$ — *BS 5970: 1992*

where h_c = *convection coefficient*
θ_s = *surface temperature*
θ_a = *ambient temperature* = $303°\ K$

laminar airflow outdoors, $h_c = 3.96 \sqrt{\left(\frac{V}{D}\right)}$ set $V = 4 m/s$, $D = 1.6 m$

$= 6.3\ W/m^2K$

radiative loss = $\epsilon\ \sigma\ \theta_s^4$

where $\sigma = 5.67 \times 10^{-8}$
$\epsilon = 0.8$

total loss = $6.3 \times (\theta_s - 303) + 4.54 \times 10^8\ \theta_s^4$
= 1.512

solving equation gives $\theta_s \approx 385°\ K$

Determine heat required to raise temperature of plate from 303°K to 385°K (82°)

Mass of L/m² $= 0.008 \times \rho_s = 63$ *kg (where* $\rho_s = 7850$ *kg/m³)*

Heat $= 520 \times 82 \times 63 = 2.686$ *MJ* — *IGN Table 4.6*

Determine length of time to heat plate assuming no losses

$$time = \frac{2.686 \times 10^6}{1512} = 1777 \text{ seconds}$$

It is therefore conceivable that with heat losses the plate may heat to 385° K in an hour. However, step through in 600 second interval in order to better estimate temperature after 1 hour.

Time (s)	Heat in		Heat loss (radiation)		Heat loss (convection)		Net heat gain (kJ)	End temp. (°K)
	rate (w)	total (kJ)	rate (w)	total (kJ)	rate (w)	total (kJ)		
0-600	1512	907.2	383	230	-	-	677	324
600-1200	1512	907.2	500	300	132	79	528	340
1200-1800	1512	907.2	605	363	233	140	404	352
1800-2400	1512	907.2	697	418	309	185	304	361
2400-3000	1512	907.2	771	463	365	219	225	368
3000-3600	1512	907.2	833	500	410	246	161	373

From the above table, estimate the temperature of the plate to be 100°C after 1 hour.

i.e. contact between human flesh and the plate will result in burns.

<u>Error</u> assumes no losses from underside of plate. In practice radiative losses may occur, and will certainly slow the rate of temperature rise.

Temperature of wall

Assume from 3 mm thick stainless steel plate.

Emissivity of surface = 0.75 (Assumed)

Assume negligible heat transfer from back of plate.

Local ambient temperature as previously (303°K)

Use same method as previously in order to determine the temperature rise.

For vertical surface $\Sigma q_i = 2.12$ *kW/m*^{2}

Heat gain $= 0.75 \times 2.12 = 1590$ *W/m*^{2}

Radiative and convective losses will be similar to previous example, ∴ end temperature will be similar (slightly higher).

Since material only 3 mm thick, temperature rise will be more rapid than for 8 mm thick steel.

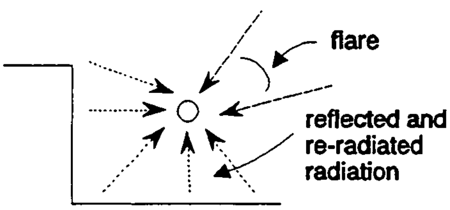
Estimate temperature after 1 hour ≈ 110°C.

Temperature of handrail

Assume 75 mm × 3 mm w.t. pipe forms handrail

Emissivity of surface = 0.75 (Assumed)

Local ambient temperature = 303°K



The handrail is a more complex problem than may at first appear. In addition to receiving direct radiation from the flare, it is also, receiving reflected and reradiated radiation. The magnitude of *each component will vary in time depending on its heating rate. Whilst it is possible to consider all the individual radiation components as they vary against time, the approach adopted here will be to assume:*

(1) heating from the flare only, but over all the circumference (conservative)

(2) re-radiation from the handrail over all its circumference.

The convection losses for the handrail requires the use of a different convection coefficient than for flat surfaces.

$q = h_c(\theta_s - \theta_a)$ $\qquad \theta_s$ = temp. of surface, $\theta_a = 303°K$

BS 5970: 1992 Section 33.5.6

$$h_c = 8.9\left[\frac{V^{0.9}}{[d_n \times 10^{-3}]^{0.1}}\right]$$

$V = 4\ ms^{-1}$

$d_n = 75\ mm$

$h_c = 40$

$\therefore q = 40(\theta_s - \theta_a)$ *in* W/m^2

Radiation losses $= \epsilon\,\sigma\,\theta_2^4$
$= 4.25 \times 10^{-8}\,\theta_s^4$

Effective thickness of pipe = 3 mm

Use tabular method to determine heating against time

$520 \times 0.003 \times 7850 = 12.25$

Note: temp rise $= \dfrac{\text{cumul. heat (kJ)}}{12.25}$

End temp. = *temp. rise* + 303

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CALCULATION SHEET

Job No. OFF 3197	Sheet 10 of 10	Rev.
Job Title WORKED EXAMPLE 1		
Subject Heating Of Steel Remote From Fire Source		
Client FABIG	Made by HGB	Date Jan 1993
	Checked by CAS	Date Jan 1993

Time (s)	*Heat in (W/m^2)*	*Heat loss (radiation)*		*Heat loss (convection)*		*Net heat gain*	*Cumul. heat gain*	*End temp. (°K)*
		rate (W/m^2)	*total (kJ/m^2)*	*rate (W/m^2)*	*total (kJ/m^2)*	*(kJ/m^2)*	*(kJ/m^2)*	
0-100	*214*	*358*	*36*	*-*	*-*	*178*	*178*	*318*
100-200	*214*	*434*	*43*	*600*	*60*	*111*	*289*	*327*
200-300	*214*	*486*	*49*	*960*	*96*	*69*	*358*	*332*
300-400	*214*	*516*	*52*	*1160*	*116*	*46*	*404*	*336*
400-500	*214*	*542*	*54*	*1320*	*132*	*28*	*432*	*338*
500-600	*214*	*555*	*56*	*1400*	*140*	*18*	*450*	*340*

From the table it can be seen that the final temperature is dominated by convective cooling, and in this example will be circa 340°K. If the ambient conditions were, say, 10°C, then this temperature would reduce to about 325°K, ≈ 55°C. This temperature will be reached in about 10 minutes.
If a highly reflecting handrail were used (e.g. out of aluminium) then most radiation would be reflected and it is probable that handrail temperature will be able to be held for short periods by an unprotected hand (estimate temperature rise ≈ 10°C). (Note: this assumes clean aluminium, a layer of fire protection or soot from the fire could greatly reduce the amount of radiation reflected.)

Conclusions

(1) Personnel can use the escape route without being harmed by radiation from the fire.

(2) The escape route floor and vertical boundaries will be too hot to touch without burning occurring.

(3) The handrail temperature is expected to be 340°K if the ambient temperature is 303°K, contact with this handrail for for than a couple of seconds would probably result in a burn. However, if the ambient temperature is 283°K then the handrail temperature will be about 325°K and the handrail could be held for a long duration without causing burns. (See IGN Figure 2.7)

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CALCULATION SHEET

Job No.	OFF 3197
Sheet	1 of 6
Rev.	
Job Title	WORKED EXAMPLE 2
Subject	Heat Flux Variation with Location Relative to Fire
Client	FABIG
Made by	HGB
Date	Jan 1993
Checked by	CAS
Date	Jan 1993

EXAMPLE 2: HEAT FLUX VARIATION WITH LOCATION RELATIVE TO FIRE

This example illustrates how the heat balance equations given in Section 4.4.1 of the Interim Guidance Notes may be applied. The example also shows the relative importance of being able to define the extent of the fire in that the location of a member is more important than the precise magnitude of the received heat flux.

This example assumes a horizontal jet fire. It should be emphasised that comparatively little is known about large scale, horizontal jet fires (ref. FL1). The characteristics of the jet are chosen more to illustrate the application of the heat balance equations than to recommend a method of treating jet fires. The more advanced fire models could generate a more accurate representation of the fire if required.

Fire Size

[FL1, p100, eq 7.14].

The fire is assumed to provide 2000 MW. An F-factor of 0.25 is assumed. Flame length is to be sized using the equation developed by Cook et al.

$$L = 1.555\, Q^{0.467} \qquad \text{where } Q = 2000 \text{ MW}$$

L is in metres.

Note that this equation strictly applies to flames inclined at 45°. From the equation obtain:

$$L = 54 \text{ metres, say } 60 \text{ metres.}$$

Due to buoyancy, this will be curvi-linear. This analysis shall assume a straight line.

The flame will be modelled as an arbitrary constant diameter cylinder. An initial estimate of 10 metre diameter will be used to calculate the surface emissive power. It is more common to model the flame as a solid cone, however, for mathematical simplicity this example will use a cylinder. For this analysis it will be assumed that a member lying inside this cylinder is fully engulfed, and therefore receiving the calculated amount of radiation on all sides. This is conservative since surfaces facing out of the fire may receive considerably less radiation.

Based on the diameter of the cylinder, the surface Emmisive Power (SEP) of the flame is:

$$SEP = \frac{\text{total radiative heat}}{\text{surface area}} = \frac{2000 \times 10^3 \times 0.25}{60\ \pi\ 10 + \dfrac{2\pi 10^2}{4}} = 245\ kW/m^2$$

SEP's of this magnitude have been measured for large jet fires, but normally as local maxima. This implies that the F-factor may be too high, or the size of fire assumed too small. However, for the purpose of numerical simplicity a diameter of 10m shall be assumed acceptable.

The problem could be solved using a proprietary SEP model such as SHELL THORNTON RESEARCH CONE FRUSTRUM MODEL OF RADIATION FROM FLARES. However, for this example the fire shall be considered as a multi-point model. It will be noted from the results that the multi-point source model breaks down close to the fire.

Divide the fire into 5 discrete points. Radiation from each point is:

$$Q_i = \frac{2000}{5} = 400\ MW$$

Radiation received at a surface from each point is given by:

$$q_i = \frac{F\ Q_i\ \tau\ \cos\beta_i}{4\pi r_i^2}$$

where:

r_i = *distance of surface from point*

τ = *transmissivity*

β_i = *angle between the normal to the surface and line connecting point on surface with radiation source.*

For this analysis τ shall conservatively be taken as 1.0.

Total radiation received at a surface from all points:

$$q = \sum_{i=1}^{5} \left[\frac{F\ Q_i\ \tau\ \cos\beta_i}{4\pi r_i^2} \right]$$

Radiation shall be modelled as emanating from 5 points located 6, 18, 30, 42 and 54 metres from one end of the cylinder.
Convection shall be based on an assumption of ambient (20°C) temperatures outside the cylinder and 1000°C temperatures inside it.

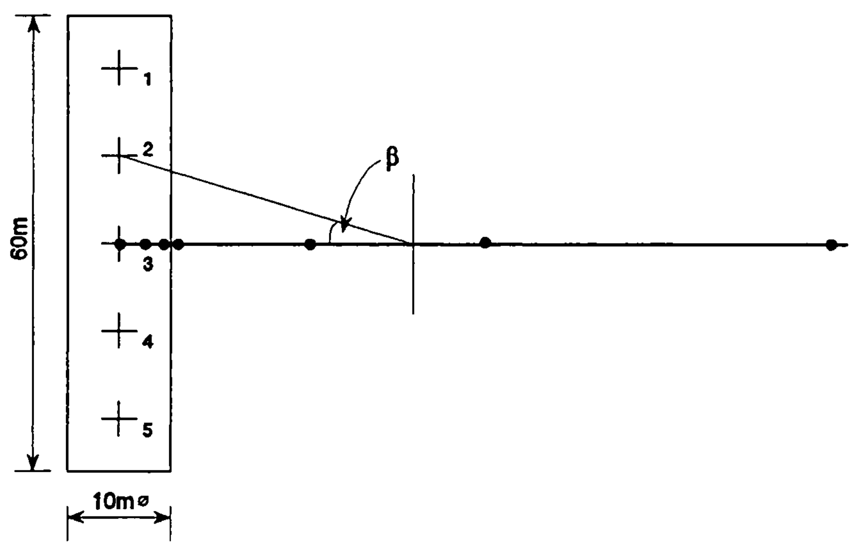
The heat balance equations shall be applied to a 5 mm thick, 1 m^2 flat plate. Convective heating and cooling shall be assumed as appropriate. The temperature and heat fluxes shall be determined for the plate at a number of distances from the centreline of the jet fire.

In order to determine appropriate convection coefficients it shall be assumed that the local gas velocity (i.e. of the air or combustion products) varies from 30 ms^{-1} at the jet centreline to 5 ms^{-1} at a distance of 100 m. Linear variation shall be assumed.

The steady state temperatures and heat flux transfers shall be calculated at the following distances from the centreline:

0 m, 2.5, 4.9, 5.1, 10, 25, 50, 100

The steady state temperature shall be determined according to the balance that heat in = heat out. q_{cond} is assumed zero since steady state conditions.



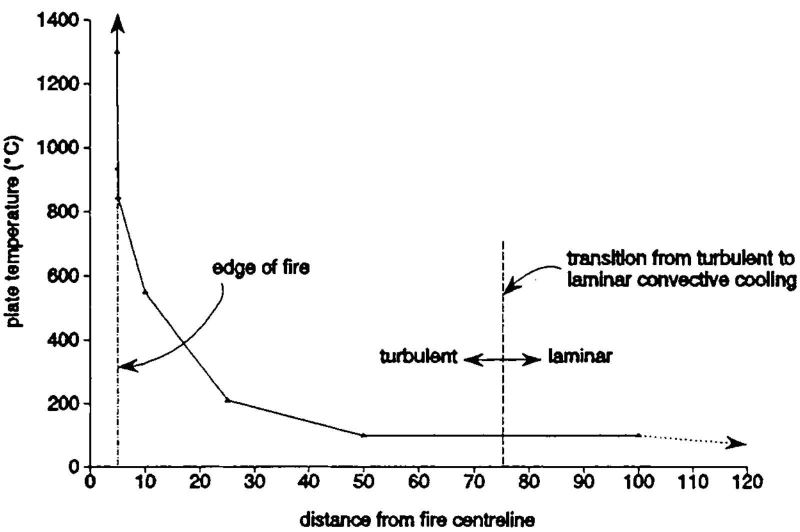
When applying the heat balance equations the emissivity of the surface shall be assumed to be 0.8 (ϵ = 0.8)

Equations are solved by trial and error (guess temperature of plate, check heat balance equations).

cL distance (m)	conv. velocity (ms^{-1})	θpL (°C)	θ_{amb} (°C)	conv. coefficient	$\beta_{2,4}$ (°)	$\epsilon q_{ir\,2,4}$ (kW/m^2)	$\beta_{1,5}$ (°)	$\epsilon q_{ir\,1,5}$ (kW/m^2)	$\epsilon_{qir\,3}$ (kW/m^2)	$\Sigma\ \epsilon q_{ir}$	q_{ic}	q_{rad}	q_{conv}	out of balance	Notes
0	30		1000		90.0	0	90.0	0	∞						1
0.5	29.9		1000		87.6	1.9	88.8	0.3	25500	25500					2,3
2.5	29.4		1000		78.2										
4.9	28.8	1060	1000	84.7	67.8	14.3	78.5	2.1	265	296	0	286	10	0	4
4.9		1300	1000	84.7	67.8	14.3	78.5	2.1	265	592	0	555	51	14	5
5.1	28.7	845	20	84.5	67.0	14.6	78.0	2.7	245	279	0	142	139	2	6
10	27.5	550	20	81.6	50.2	16.7	67.4	3.6	64	105	0	42	86	3	
25	23.8	210	20	72.7	25.7	7.5	43.8	3.8	10.2	33	0	4.9	27.6	.5	
50	17.5	100	20	56.9	13.5	2.3	25.7	1.9	2.5	11	0	1.7	9.1	.2	
100	5	100	20	8.8	6.8	.6	13.5	.6	.8	3.2	0	1.7	1.4	.1	

(1) *At zero distance from a point the predicted radiative heat flux gain is ∞*
(2) *Very high heat fluxes predicted owing to closeness to point source*
(3) *Calculated plate temperature > fire gasses, hence convective cooling*
(4) *Reradiation (q_{rad}) and convection (q_{ic} or q_{conv}) act on both sides of the plate*
(5) *Calculated with radiation on both sides of the plate*
(6) *Max. value based on SEP model. i.e. Multi-point source model invalid at edge of flame.*

Plot temperature of plate against distance from centreline.



Observations based on worked example:

(1) The point source model overestimates the steady state plate temperature when the plate is within the fire.

(2) When just outside the flame, convective cooling and cooling by re-radiation have similar heat loss effects. As the distance from the flame increases so convective cooling becomes more significant relative to re-radiation (due to cooling of plate and re-radiation being based on a T^4 relationship).

(3) Outside the flame steady-state temperatures rapidly drop to a level at which structural integrity is probable. This assumes that the plate is not in the hot plume.

(4) Inside the flame, there is a significant difference in plate temperature dependent on whether fire radiation is assumed from one side or two, i.e. calculated temperatures and heat fluxes are largely a function of the assumptions made.

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CALCULATION SHEET

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Job Title WORKED EXAMPLE 2			
Subject Heat Flux Variation with Location Relative to Fire			
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	Checked by CAS	Date Jan 1993	

At locations outside the flame, cooling by convection was dominant on account of the low temperature of the ambient gasses. Calculate the temperature at 10 metres and 25 metres assuming the plate is in the hot plume with temperatures of 900°C and 600°C respectively.

__10 metres__

$\Sigma\ q_{ir}$ *as previously* $= 105\ kW/m^2$

try $\theta_{PL} = 805°C$

$\therefore \quad q_{ic} = 95 \times 81.6 \times 2 = 15.5\ kW/m^2$

$q_{rad} = 0.8 \times 5.67 \times 10^{-8} \times (805 + 273)^4 \times 2$

$= 122.5\ kW/m^2$

heat balance $\Rightarrow \quad q_{ir} + q_{ic} = q_{rad}$ *(conduction = 0)*

$105 + 15.5 = 120.5$

$\therefore$ *out of balance* $= 2\ kW/m^2$

__25 metres__

$\Sigma\ q_{ir}$ *as previously* $= 33\ kW/m^2$

try $\theta_{PL} = 550°C \quad \therefore\ q_{ic} = 72.7 \times 2\ (600 - 550) = 7.3\ kW/m^2$

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CALCULATION SHEET

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Job Title WORKED EXAMPLE 3			
Subject Conduction Along Member			
Client FABIG	Made by HGB	Date Jan 1993	
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EXAMPLE 3: ***CONDUCTION ALONG MEMBER***

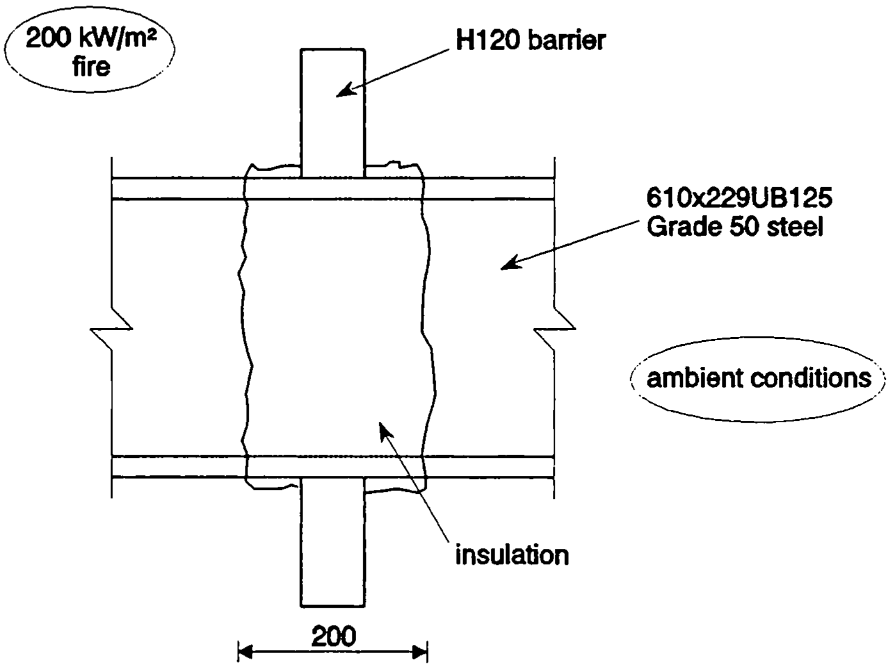
A 610×229UB125 passes through an H120 fire barrier, as illustrated below. The beam is unprotected except through the barrier. On one side it is exposed to a 200 kW/m² fire.

Estimate:

(1) The heat flux flowing along the member and hence through the wall

(2) The maximum temperature of the member on the non-fire side assuming convective cooling

(3) The approximate time taken to reach this temperature assuming a 1.5 metre length on the non-fire side.



(1) Heat flux along member

An upperbound estimate of the heat flux flowing along the member can be obtained by assuming that the temperature of the beam on the fire side of the 200 mm insulated zone is the fire temperature, with ambient conditions (20°C) on the non-fire side.

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Radiation $= \epsilon \sigma T_f^4$ — *IGN pp 4.11*

ϵ = *emissivity of flame*

σ = *stefan-Boltzman constant*

$= 5.67 \times 10^{-8}\ W/m^2K^4$

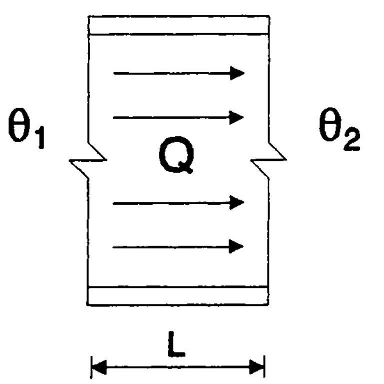
T_f = *fire temperature (°K)*

Assume optically thick flames giving $\epsilon = 1.0$

$\therefore 200 \times 10^3 = 5.67 \times 10^{-8}\ T_f^4$

$T_f = 1370\ °K$

$= 1097\ °C$



$$Q = \frac{KA}{L}(\theta_1 - \theta_2)$$

$L = 200\ mm$

$A = 160 \times 10^2\ mm^2$ — *Section Tables*

$K = 45\ W/m°C$ — *IGN Table 4.6*

$\therefore Q = 3877$ *Watts*

This is an upper bound estimate of the heat flux flowing along the member. Given a surface area for the beam of $\approx 2.0\ m^2/m$, *it represents just 1% of the received heat flux for a 1 metre length of member. Conduction along the member is therefore insignificant relative to the accuracy with which the fire loading can be defined.*

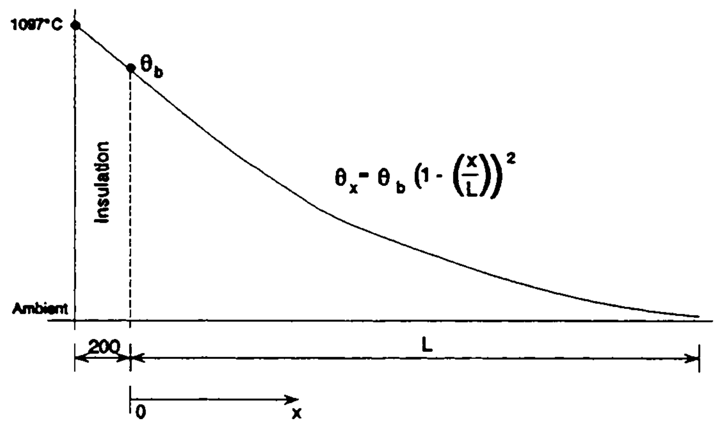
Note: Radiative cooling of the member on the non-fire side has been assumed to be negligible. This is conservative.

(2) Maximum beam temperature, non-fire side

Assume that steady-state temperature conditions have been reached.

Temperature on fire side is as for (1) = 1097°C

Assume conservatively that the temperature drops in proportion to the square of its distance from the insulation, and that the beam length is L (If no convection cooling along length, then T drops linearly with L. With convection T may be expected to drop more than linearly. The equation will tend to set θ_b higher than if a linear relationship were used.)



Let θ_x be the temperature rise of the beam above ambient.

$$\therefore \quad Q_{conv} = h\, A_s\, \theta_x$$

IGN p 4.11 (modified)

where Q_{conv} = convected heat (per unit length)
h = convective heat transfer coefficient
A_s = surface area of section (per unit length)

Now let dQ = the heat lost from a small length dx

$$\therefore \quad dQ = h\, A_s\, \theta_x\, dx$$

$$= h\, A_s\, \theta_b \left\{1 - \left[\frac{x}{L}\right]\right\}^2 dx$$

Total heat loss $= Q = \int_0^L dQ$

$$= h A_s \theta_b \int_0^L \left[1 - \left[\frac{x}{L}\right]\right]^2 dx$$

$$= h A_s \theta_b L$$

But from part (1), this must be equal to the heat conducted along the insulated part.

i.e. $\frac{1}{3} h A_s \theta_b L = \frac{KA_x}{L_I} (1007 - \theta_b)$

All terms are known except h, L and θ_b*.*

Determine h as follows:

$$h = 1.32 \left[\frac{\theta_s - \theta_a}{D}\right]^{1/4}$$

BS 5950: 1992

θ_s = *100°C (Conservative assumption)*

θ_a = *20°C*

D = *characteristic dimension*

= *0.6 m*

$= 4.5\ W/m^2K$

Now determine value of θ_b *for different values of L*

$3\ \theta_b L + 3.6\ \theta_b = 3950$

L	0	0.2	0.5	1.5	4.0
θ_b	1097	940	775	488	253

Note that these are steady state temperatures. It may take a considerable length of time for these temperatures to be achieved. The next section estimates the length of time for a 1.5m long beam.

(3) *Approximate time to reach temperature*

The average temperature of the section is ⅓rd θ_b

Assuming no heat loss by convection, the section must therefore absorb E Joules of energy where

$$E = C_s M \Delta \theta$$

C_s = *specific heat*

= 520 J/kg°C — IGN Table 4.6

M = *mass of section length*

= $125 \times 1.5 = 187.5$ kg

$\Delta\theta$ = *temperature rise* $= \frac{488}{3} \approx 163°C$

$\therefore E = 15.9 \times 10^6$ Joules

Assuming the heat transfer rate from (1), it would take $15.9 \times 10^{-6}/3877 = 4101$ seconds (68 minutes) to reach temperature. However, as temperature rises so the heat transfer reduces and convective losses increase.

In order to obtain the variation in temperature against time, it is proposed to numerically work through the problem. The assumption to assist this is that θ_b is always 3 times the average member temperature. The equations and coefficients of sections (1) and (2) are used.

The table used to calculate the rate of temperature rise is given below. Note that the end of the member in this analysis is always assumed to be at ambient temperature. In practice it would rise. In such circumstances convective cooling would *__increase__* *and the amount of energy required to heat the member would also increase. Both would increase the time taken to reach a given temperature.*

By taking the fire temperature as 1097 − 20 = 1077°C, ambient can be taken as = 0°C

Time (s)	Start θ_b (°C)	Heat transfer by conduction		Heat loss by convection		Net Heat Gain (KJ)	Total Heat (KJ)	Average Temp. (°C)
		rate (W)	total (kJ)	rate (W)	total (KJ)			
0-500	0	3877	1939	0	0	1939	1939	20
500-1000	60	3661	1831	270	135	1696	3635	37
1000-2000	112	3474	3474	504	504	2970	6605	68
2000-3000	203	3146	3146	914	914	2232	8837	91
3000-4000	272	2898	2898	1224	1224	1674	10511	108
4000-5000	323	2714	2714	1454	1454	1260	11771	121
5000-6000	362	2574	2574	1629	1629	945	12716	130
6000-7000	391	2470	2470	1760	1760	710	13426	138
7000-8000	413	2390	2390	1859	1859	531	13957	143
8000-9000	429	2333	2333	1931	1931	402	14359	147
9000-10000	442	2286	2286	1989	1989	297	14656	150
10000-12000	451	2254	4507	2030	4059	448	15104	155
12000-15000	465	2203	6609	2093	6279	330	15434	158

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OBSERVATIONS FROM EXAMPLE

(1) *Just 1% of the received heat flux from the fire is transmitted by conduction along the member, i.e the cooling effect of conduction is negligible.*

(2) *Due to the low rate of conduction, the maximum temperature of the beam on the non-fire side is considerably lower than that on the fire side of the barrier. Assuming the beam has reasonable length or connects into a suitable heat sink (larger member), the temperature on the non-fire side is unlikely to rise above 400°C, i.e., the member remains structurally sound.*

(3) *The example assumed an arbitrary temperature profile along the length of beam on the non-fire side. An x^2 relationship was chosen in order to maximise the temperature at the beginning of the beam and minimise heat losses by convection. The end of the beam was assumed to be at ambient temperature. These are considered to be conservative assumptions.*

(4) *It takes about 45 minutes for the peak temperature of the beam on the non-fire side to reach 50% of its ultimate maximum. It takes a further 55 minutes to reach 75% of ultimate maximum and a further 45 minutes to reach 90% of ultimate maximum, i.e. heating gets progressively slower.*

It is estimated that it takes 6 hours for the beam to approach ultimate temperature.

(5) *The example is based on a number of simplifying assumptions. However, these enable the problem to be quantified and it then becomes possible to quickly determine approximate heating rates.*

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CALCULATION SHEET

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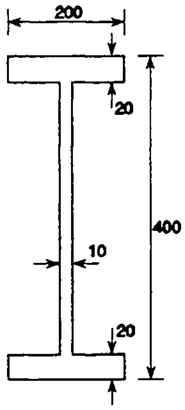
	Checked by: CAS	Date: Jan 1993	

EXAMPLE 4: ***MANUAL APPLICATION OF HEAT BALANCE EQUATIONS***



The heat flux from a fire rises from 0 → 200 kW/m² in 200 seconds. It then remains at this value for a long time. Ignoring convection, use the equations given in IGN, Section 4.4, to determine the temperature rise against time. Assume:

(1) *no fire protection;* $\epsilon_{steel} = 0.8$

(2) *10 mm of a fire protection material having the following properties. Apply as a box protection:*

$$K_i = 0.05 \text{ w/m°C}$$

$$\rho_i = 200 \text{ kg/m}^3$$

$$\epsilon_{in} = 0.7$$

(1) ***No insulation***

Specific heat of steel $= C_{ss} = 520$ *J/kg°C* — *IGN, Table 4.6*

Density of steel $= \rho_{ss} = 7850$ *kg/m³* — *IGN, Table 4.6*

For a thin section, assume no significant thermal gradient throughout thickness.

$$\therefore C_s = C_{ss},\ \rho_s = \rho_{ss}$$

All heat incident on the section will either be re-radiated, reflected or absorbed into the member. Ignoring convection, the heat balance equation becomes:

$$\epsilon\, q_{ir} = q_{rad} + q_{cond}$$

It is given that $\epsilon = 0.8$ (i.e. 20% reflected)

$$q_{rad} = \epsilon\, \sigma\, T_s^4$$ IGN, p 4.11

where $\sigma = 5.67 \times 10^{-8}\ W/m^2K^4$

T_s = surface temperature
= steel temperature

$$q_{cond} = d_s\, C_s\, \rho_s \frac{dT_s}{dt}$$ IGN, p 4.11

where d_s = thickness of steel.

d_s can be defined in terms of the H_p/A ratio for approximately uniform thickness sections.

$$\therefore\ d_s = \frac{1}{H_p/A} = \frac{A}{H_p}$$

For the section shown $d_s = \dfrac{11600}{1580} = 7.34mm$

Putting known values into the heat balance equation, paying attention to make units consistent, gives:

$$0.8\, q_{ir} = 4.53 \times 10^{-8}\, T_s^4 + 29969 \frac{dT_s}{dt}$$

Note that T_s must be defined in °K.

q_{ir} is also known as a function of time:

$$0 < t < 200, \qquad q_{ir} = 1000\,t\ (W/m^2)$$

$$t \geq 200, \qquad q_{ir} = 200{,}000\ (W/m^2)$$

The above equation can be solved numerically on a time stepping basis by defining:

$$\frac{dT_s}{dt} = \frac{(T_{s,t} - T_{s,t-1})}{\Delta t}$$

$$\Delta_t = t_t - t_{(t-1)}$$

$$\therefore\ 0.8 f(t) = 4.536 \times 10^{-8}\, T_{s,t-1}{}^4 + \frac{29969\, T_{s,t}}{\Delta t} - \frac{29969\, T_{s,t-1}}{\Delta t}$$

By assuming an appropriate value of Δt, and stepping through time, the only unknown at each stage is $T_{s,t}$. This is calculated at each time step and becomes $T_{s,t-1}$ for the next time step. Note that the main assumption of the method is to assume q_{ir} and T_s are constant over the time interval Δt. By solving the equations on a computer, Δt can be made sufficiently small that the solution will be very accurate. For the purpose of this example a tabular solution will be used.

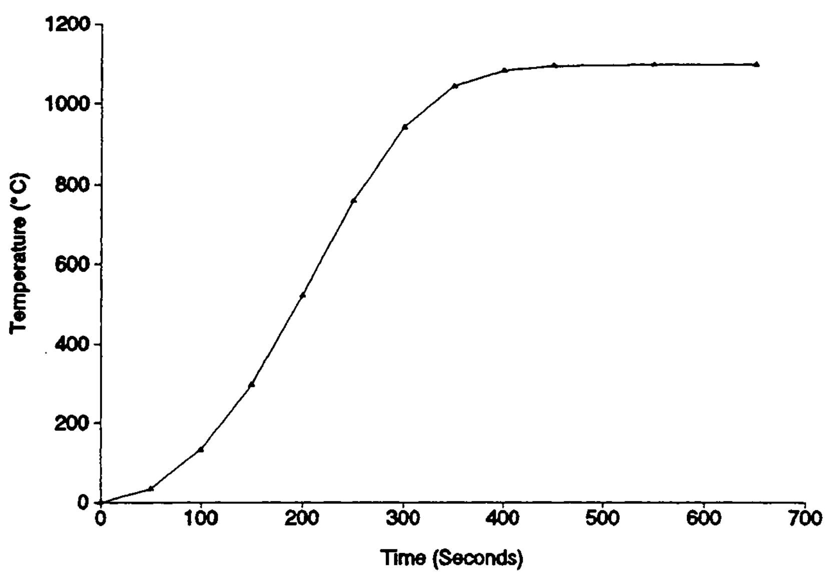
$$T_{s,t} = \left[0.8 f(t) - 4.536 \times 10^{-8}\, T^4_{s,t-1} + \frac{29969\, T_{s,t-1}}{\Delta t} \right] \frac{\Delta t}{29969}$$

Assume initial temperature of steel is ambient (=0°C).

$$T_{s,t} = \left[0.8 q_{ir} - 4.536 \times 10^{-8}\, T^4_{s,t-1} + \frac{29969\, T_{s,t-1}}{\Delta t} \right] \frac{\Delta t}{29969}$$

Δt (s)	Cumulative $t = \Sigma\Delta T$ (s)	q_{ir} at $\Sigma\Delta T - \frac{\Delta T}{2}$ (W/m^2)	$T_{s,t-1}$ (°K)	$T_{s,t}$ (°K)	$T_{s,t}$ (°C)
	0		273.0	273.0	0
50	50	25×10^3	273.0	305.9	32.9
50	100	75×10^3	305.9	405.4	132.4
50	150	125×10^3	405.4	570.2	297.2
50	200	175×10^3	570.2	795.8	522.8
50	250	200×10^3	795.8	1032.4	759.4
50	300	200×10^3	1032.4	1213.3	940.3
50	350	200×10^3	1213.3	1316.3	1043.3
50	400	200×10^3	1316.3	1356.0	1083.0
50	450	200×10^3	1356.0	1367.1	1094.1
100	550	200×10^3	1367.1	1369.7	1096.7
100	650	200×10^3	1369.7	1370.3	1097.3

i.e. *Temperature reaches maximum of ≈ 1100°C after approximately ten minutes*



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(2) ***With Insulation***

K_i	=	$0.05\ W/m°C$	ρ_{ss}	=	$7850\ kg/m^3$
ρ_i	=	$200\ kg/m^3$	C_{ss}	=	$520\ J/kg°C$
ϵ_i	=	0.7		=	$0.52\ kJ/kg°C$

d_i = *depth of insulation* = 10 mm (0.01 m)

H_p *for box profile* = 1.2 m

$A = 11600\ mm^2$

Use equation in IGN for "thick" fire protection — IGN, p 4.13

$$dT_{ss} = \frac{H_p\ K_i}{A\,C_{ss}\rho_{ss}d_i} \cdot \frac{1}{1 + \left[d_i \dfrac{\rho_i H_p}{\rho_{ss}A}\right]}(T_s - T_{ss})\,dt$$

$$= 1.23 \times 10^{-4}\ (T_s - T_{ss})\ dt \qquad (1)$$

where dT_{ss} = *change in steel temperature over time dt*

T_s = *surface temperature of insulation*

T_{ss} = *steel temperature.*

Now apply heat balance equation at surface:

$$\epsilon\, q_{ir} = q_{rad} + q_{cond}$$

$$\epsilon\, q_{ir} = 0.7 \times 1000\, t \qquad for \quad t < 200$$

$$= 0.7 \times 200 \times 10^3 \qquad for \quad t \geq 200$$

$$q_{rad} = \epsilon\, \sigma\, T_s^4$$

$$q_{cond} = K_i \frac{dT_s}{dx} = (T_{s,i} - T_{ss,i}) \frac{K_i}{d_i}$$

The above equations enable the problem to be solved using a time stepping procedure. However, care is required in order to result a stable solution. The main problem is the determination of T_s at each time step. The suggested procedure is to work out a new T_s before determining T_{ss}. This means that the new T_s is based on the old rate of conduction. However, provided dt is small, this will introduce only negligible error. A further problem is to determine an appropriate start value of T_s. If $T_{s,start}$ = 273°K, then q_{rad} is negligible and $q_{ir} \approx q_{cond}$. For this reason it is recommended that $T_{s,start}$ is based on the radiation at the first time step.

i.e.,

$$\therefore \quad T_{s,\,start} = \left[\frac{\epsilon_{in} q_{ir,i+1}}{\epsilon_{in}\sigma}\right]^{1/4} = T_{s,i}$$

proceed to determine q_{cond} based on $T_{s,i-1}$ and $T_{ss,i-1}$.

$$q_{cond} = (T_{s,i-1} - T_{ss,i-1})\frac{K_i}{di}$$

$$= 5(T_{s,i-1} - T_{ss,i-1})$$

since $q_{ir,i}$ and $q_{cond,i-1}$ are known, determine $T_{s,i}$ based on heat balance equation

$$T_{s,\,i} = \left[\frac{\epsilon_{in} q_{ir,i} - q_{cond,i-1}}{\epsilon_{in}\sigma}\right]^{1/4}$$

from equation (1) obtain change in steel temperature:

$$d\,T_{ss} = 1.23 \times 10^{-4}\,(T_{s,\,i} - T_{ss,\,i-1})\,dt$$

$$T_{ss,\,i} = d\,T_{ss} + T_{ss,\,i-1}$$

Both $T_{s,i}$ and $T_{ss,i}$ are known, and thus the procedure can be repeated.

The equations lend themselves to a computer program. A copy of such a program is included in example 5. For the purpose of this example a tabular solution will be given.

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dt	$\Sigma\, dt$	q_{ir}	q_{cond}	$T_{s,\,i}$	$d\,T_{ss}$	$T_{ss,\,i}$	$T_{ss,i}$ (°C)
-	0	-	-	969	-	273.0	0.0
50	50	50×10^3	3480	969	4.3	277.3	4.3
50	100	100×10^3	3459	1138	5.3	282.6	9.6
50	150	150×10^3	4276	1265	6.0	288.6	15.6
50	200	200×10^3	4881	1360	6.6	295.2	22.2
50	250	200×10^3	5323	1358	6.5	301.7	28.7
50	300	200×10^3	5283	1357	6.5	308.2	35.2
300	600	200×10^3	5245	1357	38.7	346.9	73.9
600	1200	200×10^3	5052	1358	74.6	421.5	148.5
1200	2400	200×10^3	4680	1359	138.2	559.7	286.7
1200	3600	200×10^3	3991	1361	117.9	677.7	404.7
1800	5400	200×10^3	3406	1362	151.2	828.9	555.9
1800	7200	200×10^3	2659	1493	118.0	946.9	673.9

From the table it will be seen that it takes 2 hours for the insulated section to reach a temperature of 675°C compared to under 4 minutes for the uninsulated section. This is for an $H_p/A \approx 100$ *and a comparatively thin layer of insulation. Fire loading at 200 kw/m*^{2} *all round can be regarded as intense (e.g. jet fire).*

EXAMPLE 5: COMPUTER SOLUTION OF HEAT BALANCE EQUATIONS AND Hp/A METHOD

The heat balance equations and Hp/A methods given in sections 4.4.1 and 4.4.2 of the Interim Guidance Notes have been combined in a computer program. A listing of the program is included. The program runs in QuickBasic/QBasic the latter of which is included as a standard utility with MSDOS 5.0.

Since the program can repeat a high number of calculations very rapidly, the method selected for solving the heat balance equations is slightly different to that given in other examples and implied in the IGN's. The system selected is to solve the heat balance equation at each time step in order to determine the surface temperature, T_s. The temperature of the underlying section is assumed to be the calculated temperature at the previous time step, $T_{ss,t-1}$. T_s is obtained by a rapidly converging trial and error procedure.

Another difference between the program and the IGN's is that q_{cond} is always determined using the equation for an insulator. By making the thickness of the insulator very low (e.g., 0.5 millimetres), and giving it the same conductivity as the base material, an uninsulated section can readily be modelled. An advantage of this approach is that the time step interval and solution procedure have been developed to be stable over a wide range of surface conditions. This enables the program to be used to check the benefits of unusual fire protection materials.

Convective heating and cooling is ignored by many programs. The program listed here includes convection. This may be switched off by setting h_c to zero.

There are two major problems with convection. The first is to determine what the appropriate boundary gas temperature is. For engulfed conditions the program assumes that the flame is optically thick with an emissivity of unity, and hence works out the effective fire temperature from the incident radiation using the Stefan-Boltzmann equation. However, if the fire is not optically thick or has a flame emissivity less than unity, then the boundary gas temperature could be higher.

The second problem is in determining appropriate convection coefficients. This complex parameter depends on at least the following; boundary gas velocity, boundary gas temperature, boundary gas density, size and shape of convective surfaces, temperature of convective surface. For hot surfaces of a variety of shapes, BS 5970 gives suggested values for h_c when cooling in air. These equations are appropriate for sections that are non-engulfed, however, there must be doubt about their validity in determining convection coefficients applicable to an engulfed object. The range of h_c that may typically occur is from as low as 5 (laminar air conditions) to in excess of 100 (turbulent, high velocity boundary gases). Selecting an appropriate value clearly has a significant influence on the extent of convection.

For simplicity the program ignores a number of phenomena often associated with fire protection materials:

- changes in properties (e.g., thermal capacity, conductivity) with temperature;
- moisture content, hence 100°C temperature rise plateau;
- energy absorbed in changing chemical state.

Examination of the program listing will show that these phenomena could be added in. It is doubtful, however, whether such information is available for the majority of fire protection materials.

The program permits a six point fire curve to be input. This could easily be increased or replaced with equations defining standard fire curves. However, experience shows that the rate of heating of a section is not that sensitive to the exact shape of the fire curve and that 6 points should be adequate for the majority of fire situations.

EXAMPLES BASED ON PROGRAM

The program has been used to produce 11 examples of the application of the heat balance equations. Each example has the same Hp/A (i.e., section properties) and the same fire curve. The main changes are in the insulation and the assumed location relative to the fire (i.e., engulfed or non-engulfed). Examples are also included with and without convection. The examples serve to illustrate both the use of the program and some of the characteristics associated with different fire protection methods. The examples are as follows:

(1) No insulation, No special coatings, No convection
(2) No insulation, No special coatings, With convection, Engulfed
(3) No insulation, No special coatings, With convection, Non-engulfed
(4) Insulation, No convection
(5) Insulation, With convection, Engulfed
(6) Insulation, With convection, Non-engulfed
(7) Reflective coating, No insulation, No convection
(8) Reflective coating, No insulation, With convection, Engulfed
(9) Reflective coating, No insulation, With convection, Non-engulfed
(10) Reflective coating + thin insulation, With convection, Engulfed
(11) Reflective coating + super insulation, With convection, Engulfed

Each example is discussed briefly in turn. For all cases it will be assumed that the member has a critical temperature of 650°C, i.e., we are interested in how long it takes to reach this temperature.

<u>(1) No insulation, No special coatings, No convection</u>

This simulates bare steel. Input values are reported in the results.

The input radiation is given as Qinc in the results. Note that Qinc must balance the four remaining heat flow components:

i.e., Qinc = Qrefl + Qconv + Qrerd + Qcond

The results show that the surface temperature at each time is equal to or slightly greater than the section temperature. This would be expected for an insulated section having the insulator modelled as a thin, highly conductive layer.

The temperature of the section rises to 650°C in about 7 minutes. Comparison with Qinc shows that the section exceeds this temperature before the fire has reached full intensity. Heating is therefore very rapid indeed (if the fire were to reach full intensity quicker, the section would heat quicker).

Comparison of the heat flow components shows that 15% of Qinc is being reflected, with the rest being shared between re-radiation (Qrerad) and heating of the section (Qcond). However, as the section gets hotter, so Qrerad gets relatively higher and Qcond relatively lower. At a time of 20 minutes steady state conditions exist with Qcond = 0. The section temperature is the same as the effective fire temperature.

After 60 minutes the fire lowers in intensity. This results in Qcond being negative, meaning that heat is being transferred out of the section back into the fire environment.

Note that there is no convection in this analysis.

(2) No insulation, No special coatings, With convection, Engulfed

This example is the same as (1) except that convection is included. It is necessary to provide information concerning the temperature of the gases adjacent to the surface. In this example the surface is specified as being engulfed. In this case the temperature of the adjacent gases is assumed to be equal to the effective temperature of the fire. A convection coefficient of 30 W/m^2K has been used.

The results show that the section heats slightly quicker than in example (1), reaching 650°C in just over 6 minutes compared to 7 minutes. This is to be expected since the surface is taking in extra heat by convection. Note that since the convection term appears on the right hand side of the heat balance equation defined in the results, a negative value represents heat in.

The heat flows into and out of the section show that Qconv is small relative to Qinc. Even if the convection coefficient were much higher (say 100), for most of the heating period the radiation would be the dominant heat source.

As the fire eases in intensity (from 60 minutes onwards), convection results in a slight cooling of the section. This effect, however, is negligible.

(3) No insulation, No special coatings, With convection, Non-engulfed

This example is as for (2) except the section is assumed to be non-engulfed. In such circumstances the program requires a temperature to be input to represent the temperature of the adjacent gases. A value of 150°C has been used.

The results show the section taking a little over 7 minutes to heat to 650°C, a little longer than in (1).

The section heat flows correctly show the section gaining heat by convection until the surface temperature exceeds 150°C. Thereafter there is a steady level of convective cooling. If the convection coefficient were higher, this would lower the maximum temperature quite considerably. As it is, the maximum temperature is 57°C lower than in (2). Since this is greater than 650°C this is of no significance.

Example (3) assumes the same level of radiation as (2). In practice, if the section is non-engulfed, there is likely to be a significant reduction in the magnitude of the radiation incident upon the surface. Combined with convective cooling, this is likely to lead to a considerable increase in the length of time taken to reach 650°C. Example (3a) is the same as example (3) except that the incident radiation is halved. This shows that the section takes just over 12 minutes to reach 650°C. Given the T^4 relationship between temperature and radiation, this is a marked increase.

(4) Insulation, No convection

This is a similar problem to (1) except that the section has a 10mm layer of insulation. This has a marked effect upon the length of time to reach 650°C, being approximately 64 minutes.

The most obvious difference between (1) and (4) is the large temperature difference between the surface of the insulation and the section. This results in most of the heat being reradiated. The amount of heat being conducted into the section through the insulation is no more than one tenth of that being conducted in (1). An interesting point to note, however, is that Qrerad in (4) peaks at a lower value than in (1). However, study of the two curves will show that the area under the Qrerad curve in (4) is greater than that in (1).

The insulation in this example is not that thick, yet it has increased endurance by a factor of 10. This shows the significant benefit of even a small amount of insulation.

(5) Insulation, With convection, Engulfed

Adding convection into example (4) makes virtually no difference to the rate of heating. This is because the surface of the insulation rapidly rises towards the temperature of the fire. There is therefore almost no differential temperature between the adjacent gases and the surface, and therefore convection is very low.

(6) Insulation, With Convection, Non-engulfed

This is the same as (5) except that the temperature of adjacent gases is assumed to be 150°C. This results in convective cooling (Qconv positive) with a lowering of the surface temperature and hence an increase in the length of time to reach 650°C from 64 minutes to about 71 minutes.

Given that the heat flow due to convection is approximately three times that due to conduction, a far larger increase in endurance time may have been expected. However, study of the heat flows shows that the heat loss due to convection was almost exactly balanced by a corresponding change in the heat loss due to re-radiation. This is because re-radiation is a function of T_s^4. A small change in surface temperature therefore leads to a significant reduction in re-radiation. A large change in surface temperature would be required to cause a significant reduction in conduction.

(7) Reflective coating, No convection

There are fire protection products on the market which are based on the principle of reflecting radiation. Some of these coatings are very thin with little insulation. This example models such a product. It is assumed that 95% of the radiation is reflected.

Example (7) has no convection. This shows it taking approximately 51 minutes for the section to reach 650°C. Given that such a thin system could be applied as a conventional paint, this is a considerable achievement. It is also an indication that highly reflective metals such as aluminium may perform better in fires than straight comparison of other properties would indicate.

As would be expected, there is very little re-radiation from the surface. Instead, nearly all the heat absorbed by the surface is conducted into the surface to heat up the section.

(8) Reflective coating, With convection, Engulfed

A concern of highly reflective coatings is that they can show widely varying characteristics depending on the test conditions. Thus, in highly radiative test conditions they may perform exceptionally well, as shown in example (7). However, in convective test conditions their performance may be less impressive. This is illustrated in example (8) where the addition of convection with a comparatively low convection coefficient of 30 W/m^2K reduces the duration to reach 650°C from 51 minutes to 18 minutes. This is short compared to the insulated section but nearly three times longer than the uninsulated section under similar conditions (ref. example (2)).

The heat flows show that most of the heat heating the section derives from convection. Re-radiation is only nominal.

(9) Reflective coating, With convection, Non-engulfed

Outside the flame convection generally acts to cool. This cooling, coupled with the high level of reflection, results in a very slow rate of heating. Example (9) shows that the section fails to reach 650°C, peaking at circa 430°C after 75 minutes. This illustrates that convective cooling outside the flame can be significant if the amount of radiation being absorbed by the surface is low. Since received radiation tends to drop fairly rapidly on exiting the flame, this is significant.

(10) Reflective coating + thin insulation, With convection, Engulfed

Examples (7), (8) and (9) show highly reflective surfaces not performing as well as insulated surfaces, but considerably better than unprotected steel. Combining a thin layer of insulation with a highly reflective coating may be expected to give good performance. Example (10) considers 5mm of insulation for the worst thermal loading condition (with convection, engulfed). This gives an endurance time of 44 minutes. This is three times that of the reflective coating on its own, but 20 minutes shorter than 10mm of insulation. In fact, since the surface of the insulation rapidly heats towards the temperature of the fire, re-radiation makes the insulator as effective as a reflector (i.e., Qrefl + Qrerad for an insulator = Qrefl for a highly reflective material). Combining the reflective coating with an insulator does give some benefit, but it is far less significant than may be anticipated.

(11) Reflective coating + super insulation, With convection, Engulfed

There are some insulation materials around which have exceptional insulation properties, particularly at elevated temperature. This example shows that a passive fire protection system comprising 5mm of such an insulant with a reflective coating can give significant protection, in this case taking approx. 92 minutes to reach 650°C. This is 28 minutes longer than 10mm of normal insulation. It shows the potential of high tech fire protection systems.

CONCLUSION FROM EXAMPLES

A number of conclusions can be drawn from these examples:

- for highly radiative fires, convection effects are negligible. However, as the radiation reduces, so convective effects become more significant and it may be necessary to include this in a model.

- outside a flame, convective cooling may significantly increase the length of time taken to reach a given temperature.

- highly reflective coatings have a significant benefit, both inside and outside a flame. However, for engulfed conditions convective heating is likely to become dominant resulting in less benefit than may initially be anticipated. Where adjacent gases are cool, reflective coatings will very significantly increase the duration to member failure.

- the only protection against convective heating is to provide an insulator with a low conductivity.

- combining reflective coatings with insulators does not give the best of both worlds since the surface layer of an insulator will re-radiate most of the radiation received. The surface temperature in these conditions will only be slightly higher than if the insulator were covered by a highly reflective coating.

- a little fire protection can considerably increase the duration that a member survives in a fire, i.e., a little fire protection is much better than none.

EXAMPLE 1
No insulation, No special coatings, No convection

Surface emmissivity	=	0.85
Insulation thermal conductivity	=	45.000 W/mC
Thickness of insulation	=	0.001 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Convection is ignored in this analysis

TIME	SURFACE TEMP.	SECTION TEMP.	HEAT FLOWS INTO AND OUT OF SECTION				
			Qinc =	Qrefl +	Qconv +	Qrerad +	Qcond
(mins)	(degC)	(degC)	(kW/m2)	(kW/m2)	(kW/m2)	(kW/m2)	(kW/m2)
0	15	15	0.4	0.1	0.0	0.3	0.0
1	29	29	25.0	3.7	0.0	0.4	20.8
2	74	73	50.0	7.5	0.0	0.7	41.8
3	150	148	75.0	11.2	0.0	1.5	62.2
4	254	252	100.0	15.0	0.0	3.7	81.3
5	380	378	116.7	17.5	0.0	8.8	90.4
6	517	515	133.3	20.0	0.0	18.8	94.6
7	655	653	150.0	22.5	0.0	35.7	91.8
8	784	782	166.7	25.0	0.0	60.1	81.6
9	894	892	183.3	27.5	0.0	89.3	66.5
10	981	979	200.0	30.0	0.0	119.0	51.0
15	1094	1094	200.0	30.0	0.0	168.4	1.6
20	1097	1097	200.0	30.0	0.0	170.0	0.0
25	1097	1097	200.0	30.0	0.0	170.0	0.0
30	1097	1097	200.0	30.0	0.0	170.0	0.0
40	1097	1097	200.0	30.0	0.0	170.0	0.0
50	1097	1097	200.0	30.0	0.0	170.0	0.0
60	1097	1097	200.0	30.0	0.0	170.0	0.0
75	1057	1057	175.0	26.2	0.0	150.8	-2.1
90	1008	1008	150.0	22.5	0.0	129.8	-2.3
105	953	953	125.0	18.7	0.0	108.9	-2.7
120	890	890	100.0	15.0	0.0	88.1	-3.1

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 2
No insulation, No special coatings, With convection, Engulfed

Surface emmissivity	=	0.85
Insulation thermal conductivity	=	45.000 W/mC
Thickness of insulation	=	0.001 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.1	0.0	0.3	0.0
1	44	43	25.0	3.7	-14.9	0.5	35.7
2	113	111	50.0	7.5	-17.5	1.1	58.9
3	213	211	75.0	11.2	-17.6	2.7	78.6
4	340	338	100.0	15.0	-16.2	6.8	94.4
5	481	479	116.7	17.5	-13.3	15.6	96.9
6	622	620	133.3	20.0	-10.3	30.9	92.7
7	751	750	150.0	22.5	-7.5	53.1	81.9
8	862	860	166.7	25.0	-5.2	79.9	66.9
9	950	948	183.3	27.5	-3.6	107.7	51.7
10	1017	1016	200.0	30.0	-2.4	133.3	39.1
15	1096	1096	200.0	30.0	-0.1	169.2	0.9
20	1097	1097	200.0	30.0	-0.0	170.0	0.0
25	1097	1097	200.0	30.0	-0.0	170.0	-0.0
30	1097	1097	200.0	30.0	-0.0	170.0	0.0
40	1097	1097	200.0	30.0	-0.0	170.0	0.0
50	1097	1097	200.0	30.0	-0.0	170.0	0.0
60	1097	1097	200.0	30.0	-0.0	170.0	-0.0
75	1057	1057	175.0	26.2	0.1	150.7	-2.1
90	1008	1008	150.0	22.5	0.2	129.7	-2.4
105	953	953	125.0	18.7	0.2	108.7	-2.7
120	889	889	100.0	15.0	0.3	87.8	-3.1

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 3
No insulation, No special coatings, With convection, Non-engulfed

Surface emmissivity = 0.85
Insulation thermal conductivity = 45.000 W/mC
Thickness of insulation = 0.001 metres
Section specific heat capacity = 520.0 J/kgC
Density of section material = 7850 kg/m3
Hp/A section factor = 100.0 m-1
Surface temperature at start = 15 degC
Steel temperature at start = 15 degC

Member is outside flame. Convection calculations are based on an input temperature of 150 degC

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	16	0.4	0.1	-4.0	0.3	4.0
1	35	34	25.0	3.7	-3.5	0.4	24.3
2	84	83	50.0	7.5	-2.0	0.8	43.7
3	161	159	75.0	11.2	0.3	1.7	61.7
4	263	261	100.0	15.0	3.4	4.0	77.7
5	381	379	116.7	17.5	6.9	8.8	83.4
6	505	503	133.3	20.0	10.7	17.7	85.0
7	628	627	150.0	22.5	14.4	31.8	81.3
8	743	741	166.7	25.0	17.8	51.3	72.6
9	841	840	183.3	27.5	20.7	74.3	60.7
10	922	921	200.0	30.0	23.2	98.3	48.5
15	1036	1036	200.0	30.0	26.6	141.5	1.9
20	1040	1040	200.0	30.0	26.7	143.2	0.1
25	1040	1040	200.0	30.0	26.7	143.3	0.0
30	1040	1040	200.0	30.0	26.7	143.3	0.0
40	1040	1040	200.0	30.0	26.7	143.3	0.0
50	1040	1040	200.0	30.0	26.7	143.3	0.0
60	1040	1040	200.0	30.0	26.7	143.3	0.0
75	997	998	175.0	26.2	25.4	125.6	-2.2
90	945	945	150.0	22.5	23.9	106.2	-2.5
105	886	886	125.0	18.7	22.1	87.0	-2.9
120	818	818	100.0	15.0	20.0	68.3	-3.3

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 3a
No insulation, No special coatings, With convection, Non-engulfed

Surface emmissivity = 0.85
Insulation thermal conductivity = 45.000 W/mC
Thickness of insulation = 0.001 metres
Section specific heat capacity = 520.0 J/kgC
Density of section material = 7850 kg/m3
Hp/A section factor = 100.0 m-1
Surface temperature at start = 15 degC
Steel temperature at start = 15 degC

Member is outside flame. Convection calculations are based on an input temperature of 150 degC

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	16	0.4	0.1	-4.0	0.3	4.0
1	28	27	12.5	1.9	-3.7	0.4	13.9
2	55	54	25.0	3.7	-2.9	0.6	23.6
3	96	95	37.5	5.6	-1.6	0.9	32.6
4	149	148	50.0	7.5	-0.0	1.5	41.0
5	212	211	58.3	8.7	1.9	2.7	45.0
6	281	280	66.7	10.0	3.9	4.5	48.2
7	353	352	75.0	11.2	6.1	7.4	50.2
8	428	427	83.3	12.5	8.3	11.6	50.9
9	502	501	91.7	13.7	10.6	17.4	49.9
10	574	573	100.0	15.0	12.7	24.8	47.5
15	766	766	100.0	15.0	18.5	56.2	10.3
20	800	800	100.0	15.0	19.5	64.0	1.5
25	805	805	100.0	15.0	19.7	65.1	0.2
30	806	806	100.0	15.0	19.7	65.3	0.0
40	806	806	100.0	15.0	19.7	65.3	0.0
50	806	806	100.0	15.0	19.7	65.3	-0.0
60	806	806	100.0	15.0	19.7	65.3	0.0
75	773	773	87.5	13.1	18.7	57.6	-1.9
90	729	729	75.0	11.2	17.4	48.5	-2.1
105	679	679	62.5	9.4	15.9	39.6	-2.4
120	623	623	50.0	7.5	14.2	31.1	-2.7

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 4
Insulation, No convection

Surface emmissivity	=	0.85
Insulation thermal conductivity	=	0.100 W/mC
Thickness of insulation	=	0.010 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.1	0.0	0.3	0.0
1	504	19	25.0	3.7	-1.1	17.5	4.8
2	664	28	50.0	7.5	-1.0	37.1	6.4
3	771	38	75.0	11.2	-0.9	57.3	7.3
4	854	49	100.0	15.0	-0.8	77.7	8.0
5	901	61	116.7	17.5	-0.7	91.5	8.4
6	943	74	133.3	20.0	-0.7	105.3	8.7
7	981	87	150.0	22.5	-0.6	119.2	8.9
8	1016	100	166.7	25.0	-0.6	133.1	9.2
9	1049	113	183.3	27.5	-0.6	147.1	9.4
10	1079	127	200.0	30.0	-0.6	161.0	9.5
15	1080	195	200.0	30.0	-0.5	161.7	8.9
20	1082	258	200.0	30.0	-0.5	162.2	8.2
25	1083	316	200.0	30.0	-0.4	162.8	7.7
30	1084	370	200.0	30.0	-0.4	163.3	7.1
40	1086	468	200.0	30.0	-0.4	164.2	6.2
50	1087	553	200.0	30.0	-0.3	165.0	5.3
60	1089	626	200.0	30.0	-0.3	165.6	4.6
75	1045	713	175.0	26.2	-0.2	145.6	3.3
90	997	774	150.0	22.5	-0.2	125.4	2.2
105	942	813	125.0	18.7	-0.1	105.1	1.3
120	878	832	100.0	15.0	-0.0	84.6	0.5

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 5
Insulation, With convection, Engulfed

Surface emmissivity	=	0.85
Insulation thermal conductivity	=	0.100 W/mC
Thickness of insulation	=	0.010 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.1	0.0	0.3	0.0
1	504	19	25.0	3.7	-1.1	17.5	4.8
2	664	28	50.0	7.5	-1.0	37.1	6.4
3	771	38	75.0	11.2	-0.9	57.3	7.3
4	854	49	100.0	15.0	-0.8	77.7	8.0
5	901	61	116.7	17.5	-0.7	91.5	8.4
6	943	74	133.3	20.0	-0.7	105.3	8.7
7	981	87	150.0	22.5	-0.6	119.2	8.9
8	1016	100	166.7	25.0	-0.6	133.1	9.2
9	1049	113	183.3	27.5	-0.6	147.1	9.4
10	1079	127	200.0	30.0	-0.6	161.0	9.5
15	1080	195	200.0	30.0	-0.5	161.7	8.9
20	1082	258	200.0	30.0	-0.5	162.2	8.2
25	1083	316	200.0	30.0	-0.4	162.8	7.7
30	1084	370	200.0	30.0	-0.4	163.3	7.1
40	1086	468	200.0	30.0	-0.4	164.2	6.2
50	1087	553	200.0	30.0	-0.3	165.0	5.3
60	1089	626	200.0	30.0	-0.3	165.6	4.6
75	1045	713	175.0	26.2	-0.2	145.6	3.3
90	997	774	150.0	22.5	-0.2	125.4	2.2
105	942	813	125.0	18.7	-0.1	105.1	1.3
120	878	832	100.0	15.0	-0.0	84.6	0.5

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 6
Insulation, With convection, Non-engulfed

Surface emmissivity = 0.85
Insulation thermal conductivity = 0.100 W/mC
Thickness of insulation = 0.010 metres
Section specific heat capacity = 520.0 J/kgC
Density of section material = 7850 kg/m3
Hp/A section factor = 100.0 m-1
Surface temperature at start = 15 degC
Steel temperature at start = 15 degC

Member is outside flame. Convection calculations are based on an input temperature of 150 degC

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION: Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	101	15	0.4	0.1	-1.5	0.9	0.9
1	400	18	25.0	3.7	7.5	9.9	3.8
2	571	25	50.0	7.5	12.6	24.4	5.5
3	688	34	75.0	11.2	16.1	41.1	6.5
4	778	44	100.0	15.0	18.8	58.8	7.3
5	829	55	116.7	17.5	20.4	71.1	7.7
6	874	67	133.3	20.0	21.7	83.5	8.1
7	916	79	150.0	22.5	23.0	96.2	8.4
8	953	91	166.7	25.0	24.1	109.0	8.6
9	988	104	183.3	27.5	25.1	121.9	8.8
10	1020	117	200.0	30.0	26.1	134.9	9.0
15	1022	181	200.0	30.0	26.2	135.4	8.4
20	1023	241	200.0	30.0	26.2	136.0	7.8
25	1024	297	200.0	30.0	26.2	136.5	7.3
30	1025	348	200.0	30.0	26.3	137.0	6.8
40	1027	441	200.0	30.0	26.3	137.8	5.9
50	1029	521	200.0	30.0	26.4	138.5	5.1
60	1031	591	200.0	30.0	26.4	139.2	4.4
75	985	673	175.0	26.2	25.0	120.6	3.1
90	933	730	150.0	22.5	23.5	102.0	2.0
105	874	764	125.0	18.7	21.7	83.4	1.1
120	805	779	100.0	15.0	19.7	65.1	0.3

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 7
Reflective coating, No convection

Surface emmissivity	=	0.05
Insulation thermal conductivity	=	0.500 W/mC
Thickness of insulation	=	0.001 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Convection is ignored in this analysis

TIME	SURFACE TEMP.	SECTION TEMP.	HEAT FLOWS INTO AND OUT OF SECTION				
			Qinc =	Qrefl +	Qconv +	Qrerad +	Qcond
(mins)	(degC)	(degC)	(kW/m2)	(kW/m2)	(kW/m2)	(kW/m2)	(kW/m2)
0	15	15	0.4	0.4	0.0	0.0	0.0
1	18	16	25.0	23.8	0.0	0.0	1.2
2	23	18	50.0	47.5	0.0	0.0	2.5
3	30	23	75.0	71.3	0.0	0.0	3.7
4	39	29	100.0	95.0	0.0	0.0	5.0
5	49	37	116.7	110.8	0.0	0.0	5.8
6	59	46	133.3	126.7	0.0	0.0	6.6
7	71	56	150.0	142.5	0.0	0.0	7.5
8	85	68	166.7	158.3	0.0	0.0	8.3
9	99	81	183.3	174.2	0.0	0.1	9.1
10	114	95	200.0	190.0	0.0	0.1	9.9
15	187	167	200.0	190.0	0.0	0.1	9.9
20	259	240	200.0	190.0	0.0	0.2	9.8
25	330	311	200.0	190.0	0.0	0.4	9.6
30	400	381	200.0	190.0	0.0	0.6	9.4
40	533	515	200.0	190.0	0.0	1.2	8.8
50	654	639	200.0	190.0	0.0	2.1	7.9
60	760	747	200.0	190.0	0.0	3.2	6.8
75	871	864	175.0	166.3	0.0	4.9	3.9
90	926	923	150.0	142.5	0.0	5.9	1.6
105	941	941	125.0	118.8	0.0	6.2	0.1
120	929	931	100.0	95.0	0.0	5.9	-0.9

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 8
Reflective coating, With convection, Engulfed

Surface emmissivity	=	0.05
Insulation thermal conductivity	=	0.500 W/mC
Thickness of insulation	=	0.001 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.4	0.0	0.0	0.0
1	61	30	25.0	23.8	-14.4	0.0	15.6
2	97	56	50.0	47.5	-18.0	0.1	20.4
3	135	88	75.0	71.3	-19.9	0.1	23.6
4	177	125	100.0	95.0	-21.1	0.1	26.0
5	217	163	116.7	110.8	-21.2	0.2	26.9
6	259	203	133.3	126.7	-21.2	0.2	27.6
7	301	244	150.0	142.5	-21.0	0.3	28.2
8	344	286	166.7	158.3	-20.8	0.4	28.7
9	387	329	183.3	174.2	-20.4	0.5	29.1
10	430	372	200.0	190.0	-20.0	0.7	29.3
15	609	564	200.0	190.0	-14.6	1.7	22.9
20	747	712	200.0	190.0	-10.5	3.1	17.5
25	849	823	200.0	190.0	-7.4	4.5	13.0
30	924	905	200.0	190.0	-5.2	5.8	9.4
40	1015	1005	200.0	190.0	-2.5	7.8	4.7
50	1059	1054	200.0	190.0	-1.2	8.9	2.2
60	1080	1078	200.0	190.0	-0.5	9.5	1.0
75	1071	1073	175.0	166.3	0.6	9.3	-1.1
90	1036	1040	150.0	142.5	1.0	8.3	-1.9
105	990	994	125.0	118.8	1.3	7.2	-2.3
120	934	940	100.0	95.0	1.6	6.0	-2.7

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 9
Reflective coating, With convection, Non-engulfed

Surface emmissivity = 0.05
Insulation thermal conductivity = 0.500 W/mC
Thickness of insulation = 0.001 metres
Section specific heat capacity = 520.0 J/kgC
Density of section material = 7850 kg/m3
Hp/A section factor = 100.0 m-1
Surface temperature at start = 15 degC
Steel temperature at start = 15 degC

Member is outside flame. Convection calculations are based on an input temperature of 150 degC

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION: Qinc (kW/m2)	= Qrefl (kW/m2)	+ Qconv (kW/m2)	+ Qrerad (kW/m2)	+ Qcond (kW/m2)
0	23	16	0.4	0.4	-3.8	0.0	3.8
1	31	21	25.0	23.8	-3.6	0.0	4.8
2	40	29	50.0	47.5	-3.3	0.0	5.8
3	51	38	75.0	71.3	-3.0	0.0	6.7
4	64	48	100.0	95.0	-2.6	0.0	7.6
5	76	60	116.7	110.8	-2.2	0.0	8.0
6	89	72	133.3	126.7	-1.8	0.0	8.5
7	102	85	150.0	142.5	-1.4	0.1	8.9
8	116	98	166.7	158.3	-1.0	0.1	9.3
9	131	112	183.3	174.2	-0.6	0.1	9.7
10	146	126	200.0	190.0	-0.1	0.1	10.0
15	209	193	200.0	190.0	1.8	0.2	8.1
20	259	246	200.0	190.0	3.3	0.2	6.5
25	300	289	200.0	190.0	4.5	0.3	5.2
30	332	324	200.0	190.0	5.5	0.4	4.2
40	378	373	200.0	190.0	6.8	0.5	2.6
50	407	404	200.0	190.0	7.7	0.6	1.7
60	426	424	200.0	190.0	8.3	0.7	1.0
75	429	430	175.0	166.3	8.4	0.7	-0.3
90	413	415	150.0	142.5	7.9	0.6	-1.0
105	386	388	125.0	118.8	7.1	0.5	-1.4
120	353	356	100.0	95.0	6.1	0.4	-1.5

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 10
Reflective coating + thin insulation, With convection, Engulfed

Surface emmissivity	=	0.05
Insulation thermal conductivity	=	0.100 W/mC
Thickness of insulation	=	0.010 metres
Section specific heat capacity	=	520.0 J/kgC
Density of section material	=	7850 kg/m3
Hp/A section factor	=	100.0 m-1
Surface temperature at start	=	15 degC
Steel temperature at start	=	15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.4	0.0	0.0	0.0
1	425	19	25.0	23.8	-3.5	0.7	4.1
2	557	26	50.0	47.5	-4.2	1.3	5.3
3	650	34	75.0	71.3	-4.5	2.1	6.2
4	725	44	100.0	95.0	-4.6	2.8	6.8
5	769	54	116.7	110.8	-4.7	3.3	7.2
6	809	65	133.3	126.7	-4.7	3.9	7.4
7	847	76	150.0	142.5	-4.7	4.5	7.7
8	881	87	166.7	158.3	-4.6	5.0	7.9
9	914	99	183.3	174.2	-4.6	5.6	8.2
10	945	111	200.0	190.0	-4.6	6.2	8.3
15	955	171	200.0	190.0	-4.3	6.4	7.8
20	964	226	200.0	190.0	-4.0	6.6	7.4
25	972	279	200.0	190.0	-3.8	6.8	6.9
30	980	329	200.0	190.0	-3.5	7.0	6.5
40	995	419	200.0	190.0	-3.1	7.3	5.8
50	1007	498	200.0	190.0	-2.7	7.6	5.1
60	1018	569	200.0	190.0	-2.4	7.9	4.5
75	991	655	175.0	166.3	-1.8	7.2	3.4
90	957	718	150.0	142.5	-1.4	6.5	2.4
105	915	761	125.0	118.8	-0.9	5.6	1.5
120	863	787	100.0	95.0	-0.5	4.7	0.8

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

EXAMPLE 11
Reflective coating + super insulation, With convection, Engulfed

Surface emmissivity = 0.05
Insulation thermal conductivity = 0.040 W/mC
Thickness of insulation = 0.005 metres
Section specific heat capacity = 520.0 J/kgC
Density of section material = 7850 kg/m3
Hp/A section factor = 100.0 m-1
Surface temperature at start = 15 degC
Steel temperature at start = 15 degC

Member is engulfed. For convection calculations make boundary gases the same temperature as the fire (optical thickness assumed).

Convection coefficient = 30.0 W/m2K

TIME (mins)	SURFACE TEMP. (degC)	SECTION TEMP. (degC)	HEAT FLOWS INTO AND OUT OF SECTION Qinc = (kW/m2)	Qrefl + (kW/m2)	Qconv + (kW/m2)	Qrerad + (kW/m2)	Qcond (kW/m2)
0	15	15	0.4	0.4	0.0	0.0	0.0
1	445	18	25.0	23.8	-2.9	0.8	3.4
2	581	24	50.0	47.5	-3.5	1.5	4.5
3	676	31	75.0	71.3	-3.7	2.3	5.2
4	752	39	100.0	95.0	-3.8	3.1	5.7
5	796	48	116.7	110.8	-3.9	3.7	6.0
6	836	57	133.3	126.7	-3.9	4.3	6.2
7	874	66	150.0	142.5	-3.9	4.9	6.5
8	908	75	166.7	158.3	-3.8	5.5	6.7
9	941	85	183.3	174.2	-3.8	6.1	6.8
10	971	96	200.0	190.0	-3.8	6.8	7.0
15	978	146	200.0	190.0	-3.6	6.9	6.7
20	984	193	200.0	190.0	-3.4	7.1	6.3
25	990	239	200.0	190.0	-3.2	7.2	6.0
30	996	282	200.0	190.0	-3.1	7.3	5.7
40	1006	362	200.0	190.0	-2.7	7.6	5.2
50	1015	434	200.0	190.0	-2.5	7.8	4.7
60	1024	499	200.0	190.0	-2.2	8.0	4.2
75	992	581	175.0	166.3	-1.8	7.3	3.3
90	955	645	150.0	142.5	-1.4	6.4	2.5
105	910	691	125.0	118.8	-1.1	5.6	1.8
120	857	722	100.0	95.0	-0.7	4.6	1.1

Qinc = radiation incident on the surface from the (fire) source
Qrefl = radiation reflected directly from the surface
Qconv = convective heat into (-ve) or out of (+ve) the surface
Qrerad = re-radiation from surface due to elevated surface temperature
Qcond = conductive heat out of (+ve) or into (-ve) the surface

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CALCULATION SHEET

Job No. *OFF 3197*	Sheet *1* of *2*		Rev.
Job Title *WORKED EXAMPLE 5*			
Subject *Basic Computer Program*			
Client *FABIG*	Made by *HGB*	Date *Feb 1993*	
	Checked by *CAS*	Date *Feb 1993*	

Derivation of Equations used in BASIC Program

$\epsilon\, q_{ir} + q_{ic} \quad = \quad q_{rad} + q_{conv} + q_{cond}$

noting that: $q_{ic} \equiv -q_{conv} \quad = \quad h_c\, (T_s - T_a)$

if $T_a > T_s$, *then heat into section*

if $T_a < T_s$, *then heat out of section*

h_c *can be a complex function of* T_a, T_s *and geometry*

define $q_{refl} \quad = \quad (1 - \epsilon)\, q_{ir}$

$\therefore$ *heat balance equation*

$q_{ir} \quad = \quad q_{refl} - q_{rad} + q_{conv} + q_{cond}$

where:

$q_{refl} \quad = \quad (1 - \epsilon_s)\, q_{ir}$

$q_{rad} \quad = \quad \epsilon_s\, \sigma\, T_s^4$

$q_{conv} \quad = \quad h_c\, (T_s - T_a)$

$$q_{cond} \quad = \quad k\, \frac{dT_s}{dx} \quad = \quad l\, \frac{(T_s - T_{ss})}{thickness}$$

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CALCULATION SHEET

Job No.	OFF 3197	Sheet 2 of 2	Rev.
Job Title	WORKED EXAMPLE 5		
Subject	Basic Computer Program		
Client	FABIG	Made by HGB	Date Feb 1993
		Checked by CAS	Date Feb 1993

$$\epsilon_{qir} + h_c\,(T_f - T_s) \quad = \quad \epsilon\,\sigma\,T_s^4 + \frac{k}{d}\,(T_s - T_{ss})$$

Grouping T_s terms gives

$$\epsilon\,\sigma\,T_s^4 + h_c\,T_s + \frac{k}{d}\,T_s \quad = \quad \epsilon_{qir} + h_c\,T_f + \frac{k}{d}\,T_{ss}$$

$$\therefore\ K_1 \quad = \quad T_s^4 + K_2\,T_s$$

where:

$$K_1 \quad = \quad \frac{(\epsilon_{qir} + h_c\,T_f + \frac{k}{d}\,T_{ss})}{\epsilon\ \sigma}$$

$$K_2 \quad = \quad \frac{1}{\epsilon\ \sigma}\left[h_c + \frac{K}{d}\right]$$

Note: for engulfed conditions

$$T_f \quad = \quad \left[\frac{q_{ir}}{\sigma}\right]^{1/4}$$

and for non-engulfed conditions

T_f = *Ambient temperature*

APPLICATION OF HEAT BALANCE EQUATIONS AND Hp/A METHOD BASIC COMPUTER PROGRAM LISTING

```
CLS
'input parameters
OPEN "results" FOR OUTPUT AS #1
PRINT #1, : PRINT #1, : PRINT #1,
PRINT #1, "     EXAMPLE 10"
PRINT #1, "     Reflective coating + thin insulation, With convection, Engulfed"
PRINT #1, : PRINT #1,
emm = .05                    'surface emmissivity
kpm = .1                     'thermal conductivity of protection, W/mC
                             'if no protection, set to conductivity of base material
thick = .01                  'thickness of protection, m.  If no insulation set
                             'to 0.0005m.  Effect on results is negligible
shc = 520!                   'specific heat capacity, J/kgC
dens = 7850                  'density of section, kg/m^3
hpa = 100                    'Hp upon A section factor, m^-1
sigma = 5.67 * .00000001#    'Stefan Boltzmann constant, W/m^2K^4
tas1 = 15                    'Surface temperature at start, degC
tas2 = 15                    'Steel temperature at start, degC
tas3 = 150                   'Ambient temperature for convection calcs, degC
                             'used only if loc =0
location = 1                 'set to 1 if engulfed, else set to 0
hc = 30!                     'convection coefficient, ambient to surface, W/m^2K
                             'set hc=0 if convection to be ignored
ts1 = tas1 + 273             'start temperature of surface, K
tss1 = tas2 + 273            'start temperature of section, K
tas3 = tas3 + 273            'boundary gas temp. for convection, K
DIM ts(1201)                 'surface temperatures each 6 seconds
DIM tss(1201)                'section temperatures each 6 seconds
DIM qinc1(1201)              'gross radiation received at section
DIM qrefl(1201)              'radiation reflected from surface
DIM qconv(1201)              'net convection, (-)ve is cooling, (+)ve is heating
DIM qrerad(1201)             'cooling due to re-radiation
DIM qcond(1201)              'heat into member as conduction
DIM heat(6, 2)               'heat file

'Input 6 point heat curve, first point being 0, 0
heat(1, 1) = 0!              'time, minutes
heat(1, 2) = 0!              'heat input, W/m^2
heat(2, 1) = 2               'time
heat(2, 2) = 50000!          'heat
heat(3, 1) = 4               'time
heat(3, 2) = 100000!         'heat
heat(4, 1) = 10              'time
heat(4, 2) = 200000!         'heat
heat(5, 1) = 60              'time
heat(5, 2) = 200000!         'heat
heat(6, 1) = 120             'time
heat(6, 2) = 100000!         'heat
```

```
PRINT #1, USING "     Surface emmissivity             = ###.##"; emm
PRINT #1, USING "     Insulation thermal conductivity = ##.### W/mC"; kpm
PRINT #1, USING "     Thickness of insulation         = ##.### metres"; thick
PRINT #1, USING "     Section specific heat capacity  = ####.# J/kgC"; shc
PRINT #1, USING "     Density of section material     = ###### kg/m3"; dens
PRINT #1, USING "     Hp/A section factor             = ####.# m-1"; hpa
PRINT #1, USING "     Surface temperature at start    = ###### degC"; tas1
PRINT #1, USING "     Steel temperature at start      = ###### degC"; tas2
PRINT #1,

'print details about convection
IF location = 1 AND hc > 0 THEN
PRINT #1, "     Member is engulfed.  For convection calculations make boundary gases"
PRINT #1, "     the same temperature as the fire (optical thickness assumed)."
PRINT #1,
PRINT #1, USING "     Convection coefficient          = ####.# W/m2K"; hc
END IF
IF location = 0 AND hc > 0 THEN
PRINT #1, "     Member is outside flame.  Convection calculations are based on an input"
PRINT #1, USING "     temperature of #### degC"; tas3 - 273
PRINT #1,
PRINT #1, USING "     Convection coefficient          = ####.# W/m2K"; hc
END IF
IF hc = 0 THEN PRINT #1, "     Convection is ignored in this analysis"

PRINT #1, : PRINT #1,

'convert times on heat curve from 60 second intervals to 6 seconds
FOR i = 1 TO 6
heat(i, 1) = heat(i, 1) * 10
NEXT

'set control constants to their initial values
KNT = 1 : qinc = 0! : tsn1 = 0! : TSSN = tss1 : krerad = emm * sigma : DTSS = 0! : DT = 6!

'program will solve equations at 6 second intervals.

FOR i = 1 TO 1201

      'routine to determine value of heat input for time i
      IF (i - 1) = heat(KNT, 1) THEN KNT = KNT + 1
      IF KNT > 6 THEN KNT = 6
      qinc = heat((KNT - 1), 2) + ((heat(KNT, 2) - heat((KNT - 1), 2)) * (i - 1! - heat((KNT - 1), 1)) / (heat(KNT, 1) - heat((KNT - 1), 1)))

      'set results matrices to zero
      ts(i) = 0! : tss(i) = 0! : qinc1(i) = 0! : qrefl(i) = 0! : qconv(i) = 0! : qrerad(i) = 0! : qcond(i) = 0!
```

```
        'calculate changes in temperature

                'set initial values for trial and error solution of equations
                IF i = 1 THEN
                qinc = sigma * (ts1) ^ 4!
                tsn1 = ts1
                ELSE
                tsn1 = ts(i - 1) + 273
                END IF

                'counter on screen so progress of run can be monitored
                CLS
                PRINT "NUMBER OF PROGRAM LOOPS REMAINING = ", (1201 - i)

                'constants for heat balance equations (engulfed & non-engulfed)
                IF location = 1 THEN
                K1 = ((emm * qinc) + (kpm * TSSN / thick) + (hc * (qinc / sigma) ^ .25)) / krerad
                ELSE
                K1 = ((emm * qinc) + (kpm * TSSN / thick) + (hc * tas3)) / krerad
                END IF
                K2 = ((kpm / thick) + hc) / krerad
                K3 = K1 * .000001

                'trial & error solution of heat balance equation.  Use previous
                'section temperature.  Solve for surface temperature, tsn1
startloop:
                P1 = tsn1 ^ 4 + K2 * tsn1 - K1
                IF P1 > K3 THEN tsn2 = tsn1 * .98
                IF P1 < (K3 * -1!) THEN tsn2 = tsn1 * 1.02
                IF P1 <= K3 AND P1 >= (K3 * -1) THEN GOTO endloop
                P2 = tsn2 ^ 4 + K2 * tsn2 - K1
                tsn1 = tsn1 + (P1 / (P1 - P2) * (tsn2 - tsn1))
                GOTO startloop
endloop:
               'determine changes in section temperature based on new surface
               'temperature.  Calculate heat flows for reporting.
                DTSS = DT * (tsn1 - TSSN) * kpm / thick * hpa / shc / dens
                TSSN = TSSN + DTSS
                ts(i) = tsn1 - 273
                tss(i) = TSSN - 273
                qinc1(i) = qinc
                qrefl(i) = (1 - emm) * qinc
                IF location = 1 THEN
                qconv(i) = hc * ((qinc / sigma) ^ .25 - tsn1) * -1!
                ELSE
                qconv(i) = hc * (tas3 - tsn1) * -1!
                END IF
                qrerad(i) = krerad * tsn1 ^ 4
                qcond(i) = kpm * (tsn1 - TSSN + DTSS) / thick
NEXT
```

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```
'print results to screen and file
CLS
PRINT #1, : PRINT #1,
PRINT #1, "     -----------------------------------------------------------------"
PRINT #1, "           | SURFACE  SECTION |     HEAT FLOWS INTO AND OUT OF SECTION"
PRINT #1, "      TIME |  TEMP.    TEMP.  |"
PRINT #1, "           |                  | Qinc  =  Qrefl +  Qconv +  Qrerad + Qcond"
PRINT #1, "     (mins)| (degC)   (degC)  |(kW/m2)  (kW/m2)  (kW/m2)  (kW/m2)  (kW/m2)"
PRINT #1, "     -----------------------------------------------------------------"
i = 0

FORMATA$ = "      ###     ####     ####     ###.#    ###.#    ###.#    ###.#    ###.#"
FORMATB$ = "      ###  |  ####     ####   | ###.#    ###.#    ###.#    ###.#    ###.#"

PRINT USING FORMATA$; i; ts(1); tss(1); qinc1(1) / 1000; qrefl(1) / 1000; qconv(1) / 1000; qrerad(1) / 1000; qcond(1) / 1000
PRINT #1, USING FORMATB$; i; ts(1); tss(1); qinc1(1) / 1000; qrefl(1) / 1000; qconv(1) / 1000; qrerad(1) / 1000; qcond(1) / 1000

FOR i = 10 TO 90 STEP 10
IF i = 50 THEN PRINT #1, "           |                  |"
PRINT USING FORMATA$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
PRINT #1, USING FORMATB$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
NEXT

PRINT #1, "           |                  |"
FOR i = 100 TO 250 STEP 50
PRINT USING FORMATA$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
PRINT #1, USING FORMATB$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
NEXT

PRINT #1, "           |                  |"
FOR i = 300 TO 500 STEP 100
PRINT USING FORMATA$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
PRINT #1, USING FORMATB$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
NEXT

FOR i = 600 TO 1200 STEP 150
IF i = 750 THEN PRINT #1, "           |                  |"
PRINT USING FORMATA$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
PRINT #1, USING FORMATB$; i / 10; ts(i + 1); tss(i); qinc1(i + 1) / 1000; qrefl(i + 1) / 1000; qconv(i + 1) / 1000; qrerad(i + 1) / 1000; qcond(i + 1) / 1000
NEXT

PRINT #1, "     -----------------------------------------------------------------"
PRINT #1,
PRINT #1, "     Qinc   = radiation incident on the surface from the (fire) source"
PRINT #1, "     Qrefl  = radiation reflected directly from the surface"
PRINT #1, "     Qconv  = convective heat into (-ve) or out of (+ve) the surface"
PRINT #1, "     Qrerad = re-radiation from surface due to elevated surface temperature"
PRINT #1, "     Qcond  = conductive heat out of (+ve) or into (-ve) the surface"
CLOSE #1
SHELL "COPY results PRN"
```

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CALCULATION SHEET

Job No. OFF 3197	Sheet 1 of 8	Rev.
Job Title WORKED EXAMPLE 6		
Subject Thermal Restraint Illustration		
Client FABIG	Made by HGB	Date Feb 1993
	Checked by CAS	Date Feb 1993

EXAMPLE 6: ***THERMAL RESTRAINT ILLUSTRATION***

The vast majority of structural members on offshore structures are stocky (50<Kl/r<90) and therefore well conditioned to enable simplified analysis techniques to be conservatively used. However, some members and structural systems may require more rigorous techniques. This example, based on one presented at the first FABIG technical meeting, shows how thermal restraint may lead to premature failure.

The example is shown in figure 1. The structure is assumed to have been originally constructed with cantilever walkway areas projecting outside the main structure. At some stage an equipment item has been added onto the walk way area. The design solution was to add a diagonal strut to take the additional load. In order to save weight, and since the strut was a "one off" design, the strut was designed with a high level of utilisation.

As part of the safety case it is subsequently decided that the walkway adjacent to the equipment item must remain passable. Also, failure of this part of the walkway would cause event escalation in an undesirable area. It is required to determine the temperature that the strut can rise to before collapse will occur. The AISC permissible stress method is to be used.

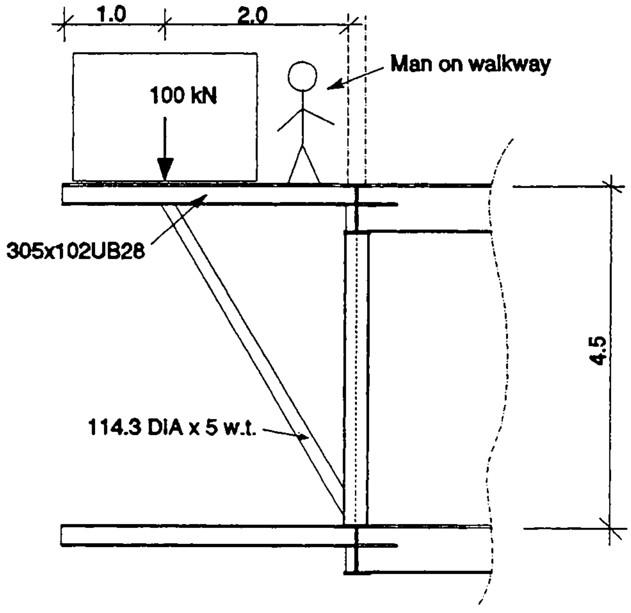

Figure 1 Diagram of problem

Check initial sizing of strut - method 1

This method assumes that all the equipment load is taken down the strut. It can be regarded as conservative since in practice a proportion of the load will be taken by the cantilever. The problem is shown with actual lengths in Figure 2.

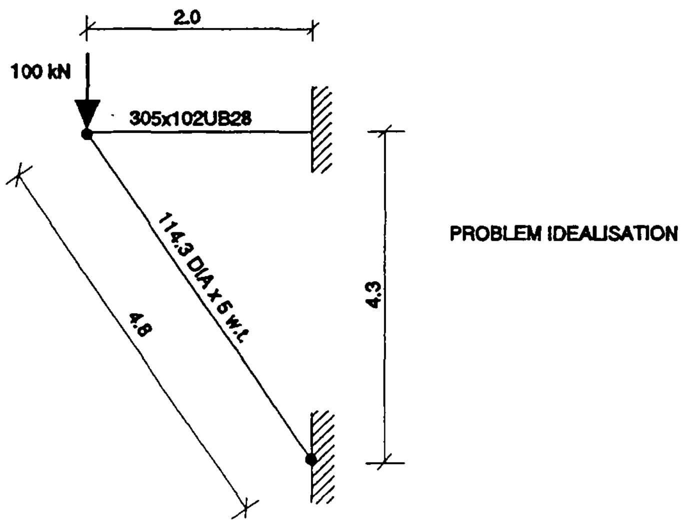

Figure 2 Idealisation of problem

Assume all 100 kN taken by strut. For simplicity, ignore self weight and bending.

Let axial force in strut $= P_s = (4.8/4.3) \times 100 = 111.6$ *kN*

Cross sectional area of strut $= A_s = 1720$ mm^2

$$\therefore \text{ acting axial stress } = f_a = P_s / A_s = 64.9 \text{ N/mm}^2$$

Determine allowable axial stress = F_a : first determine Kl/r ratio

$K = 1.0$

l = strut length = L_s = 4800 mm

r = radius of gyration of 114.3ϕ5 pipe = 38.7 mm

$\therefore Kl/r = 124$

Note that a Kl/r ratio of 124 is too slender for the majority of members used offshore. However, this value will still be used in this example for iluutration.

Using AISC: AISC 1.5.1.3.1

$$C_c = \sqrt{\frac{2\pi^2 E}{F_y}}$$

where $F_y = 345\ N/mm^2$
$E = 200{,}000\ N/mm^2$

$$C_c = 106.9$$

Since $Kl/r > C_c$ AISC 1.5.1.3.2

$$F_a = \frac{12\pi^2 E}{23\,(K(l/r)^2} = 66.9\ N/mm^2$$

Since $l/r > 120$ AISC 1.5.1.3.3

$$F_{as} = \frac{F_a}{1.6 - \dfrac{l}{200r}} = 68.27\ N/mm^2$$

Unity check = f_a / F_{as} = 0.95 < 1.0 and $\therefore$ member is OK

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CALCULATION SHEET

Job No. OFF 3197		Sheet 4 of 8	Rev.
Job Title WORKED EXAMPLE 6			
Subject Thermal Restraint Illustration			
Client FABIG	Made by HGB	Date Feb 1993	
	Checked by CAS	Date Feb 1993	

Check initial sizing of strut - method 2

Shortening of the diagonal strut as a result of load will result in some of the load being carried through the cantilever. By equating the displacement of the strut with the displacement of the cantilever, it is possible to determine the load split between the two members.

Assume 100 kN shared by strut and beam. Ignore self weight and bending.

Let P_B = amount of shear load taken by beam, length L_B

Now equate shortening of strut with bending displacement of beam:

$$\frac{4.3}{4.8}\,\frac{P_S\,L_S}{A_S\,E} = \frac{P_B\,L_B^3}{3\,E\,I_B} \qquad \text{But,} \quad \frac{4.3}{4.8}\,P_S + P_B = 100 \text{ kN}$$

There are two equations for the unknowns P_S and P_B.

Solving the equations (I_B = 5439 cm^4, L_B = 2000 mm) gives:

P_S = 105.6 kN (unity check approx. 0.92)

P_B = 5.4 kN

As expected, this unity check is only slightly lower than that which ignored any contribution from the cantilever.

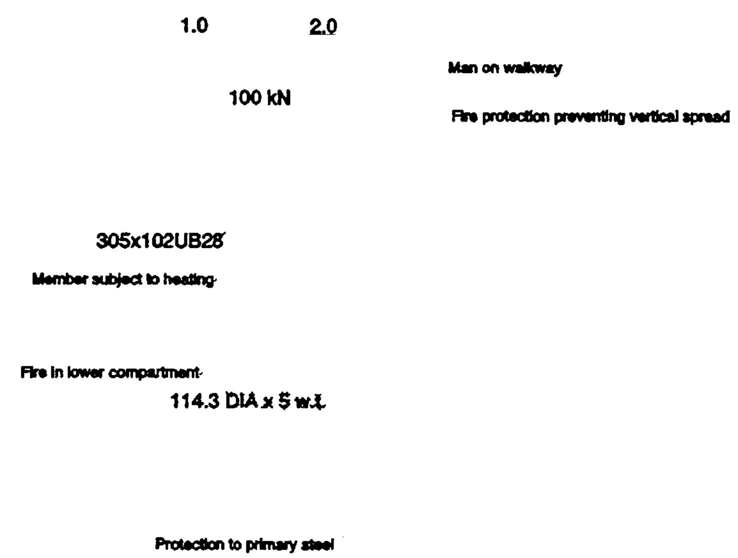


Figure 3 Fire loading condition

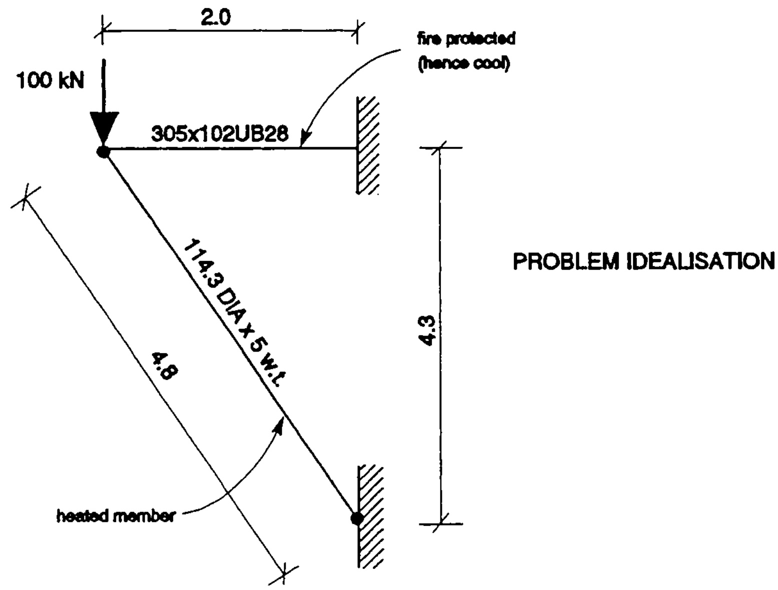


Figure 4 Idealisation of fire problem

Determine effect of heating strut

Figure 3 shows how the strut may be heated to a different temperature from the cantilever. In the figure the cantilever is shown as being fire protected in order to prevent vertical spread of the fire and to protect the walkway. The column connecting the upper level to the lower level is also shown protected. Both the cantilever and column will therefore heat at a much slower rate than the diagonal strut which can be assumed to be at an elevated temperature relative to both these members. Figure 4 shows the idealised situation.

The procedure for determining the effect of heating the strut is as follows:

- *Assume strut initially loaded at 105.6 kN*
- *Length increase due thermal expansion* $= \Delta T.\alpha.L_S$
  $(\alpha = 14 \times 10^{-6})\ K^{-1}$
- *An increase in length creates an extra load, W, in the strut due to the restraint offered by the beam*

- *W effectively causes strut to shorten by $W.L_S/A_S.E_S$*

- *E_B = 200,000 since fire protected. For E_S use elevated temperature values of E from IGN.* IGN Table 4.7

- *the change in strut length can be equated to the beam deflection:*

$$\frac{4.3}{4.8}\left[\Delta T\ \alpha\ L_S - \frac{W\ L_S}{A_S\ E_S}\right] = \frac{W\ L_B^3}{3\ E_B\ I_B}$$

- *rearranging and substitute for L_S, A_S E_B and L_B gives*

$$W = \frac{0.0602\ \Delta T\ E_S}{2.45x10^{-4}E_S + 2.5}$$

and $P_S = 105.6 + (W\ \Delta T)$ where ΔT = temperature rise

- *however note that W cannot exceed 75 kN as at this value the beam forms a plastic hinge and can react no more load:*

$(M_p = S_x \times f_y$ *where* $f_y = 345\ N/mm^2) = 140.7\ kNm$

$$W = \frac{140}{2} = 70 + 5 = 75\ kN$$

The allowable axial load in the strut can be determined as follows:

- *Base on AISC, section 1.5.1.3.2*

- *Since an "extreme" loadcase, remove 12/23 safety factor from the euler equations contained within the code check*

- *Use elevated temperature values for E as given in table 4.7 of the Interim Guidance Notes*

Table 1 shows the variation in P_S and Euler buckling load with ΔT. Note that two values of P_S are given. One takes into account the yielding of the cantilever (in the upwards direction) whilst the other ignores yielding.

ΔT	0	100	200	250	300	350	400	500	600
$P_{S,with\ yield}$	106	129	152	164	175	181	181	181	181
$P_{S,no\ yield}$	106	129	152	164	175	186	197	219	232
Euler Load	221	221	199	188	177	166	155	132	68

From Table 1 it can be seen that the member buckles when $\Delta T \approx 300°C$. For simplified analysis this would represent the failure temperature of the system. However, in practice there will be post-buckling capacity in the system. The following calculations attempt to determine the temperature at which final collapse will occur.

- *Without the strut the beam can support a load of 70kN before full hinge development. Therefore only 30kN needs to be taken by the strut (P_S = 33.5kN, since inclines to vertical)*

- *Assume lateral displacement of strut is δ.*

  $\therefore M_S = P_S.\delta$ *where M_S is the strut moment due to δ*

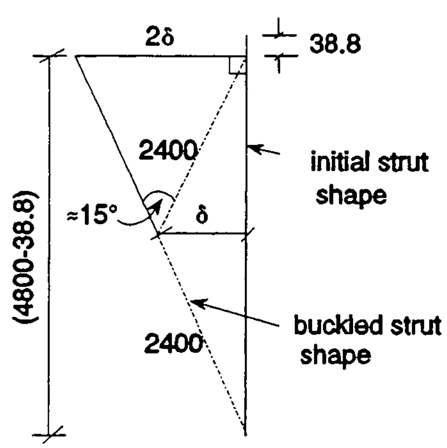
- *Determine effective change in length of strut post-buckling:*

  = *4.8/4.3(beam defln.) + thermal expansion - axial shortening*

$$= \frac{4.8}{4.3}\left[\frac{WL^3}{3EI}\right] + \Delta T \alpha L_s - \frac{P_S}{A_S E_S} L_S$$

$\Rightarrow 19.2 + 20.2 - 0.6 = 38.8mm$

- *using pythagoras's therom:*



$\delta = 0.5[(4800)^2 - (4800 - 38.8)^2]^{½} = 305$ mm

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- $M_S = 10.2$ *kNm*
- $M_C = S_x \times f_y = 12.6$ *kNm*

The plastic moment capacity of the pipe strut at 300°C is approximately 13 kNm. The capacity is therefore greater than the applied moment. This assumes, however, that the pipe can sustain a moment of 13 kNm at a rotation of about 15°. In practice the pipe section may buckle locally, causing a rapid loss of moment capacity. Therefore assume that the temperature limit is circa 300°C.

From Table 1 it is possible to determine the euler collapse load of the strut were there to be no restraint to thermal strains. This is obtained by determining the temperature at which the euler load is equal to the strut load with no thermal strains ($\Delta T = 0$). The corresponding temperature is approximately 550°C. i.e., the effect of thermal restraint is to cause the system to fail at about half the temperature it would otherwise have sustained.

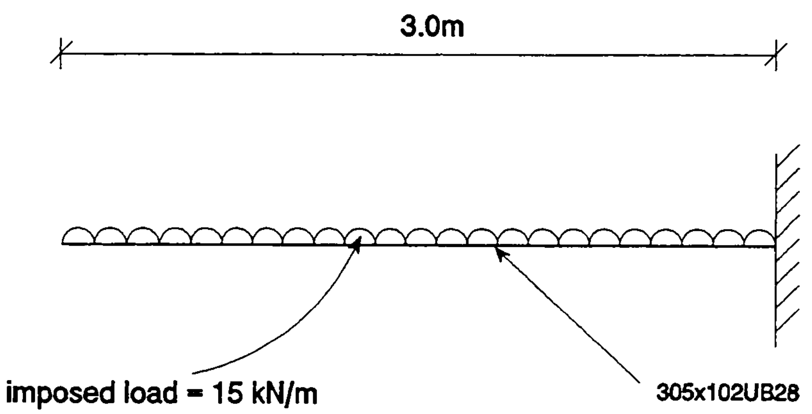
EXAMPLE 7: DIFFERENT METHODS OF THERMAL RESPONSE ANALYSIS

This set of examples is based on those presented at the first FABIG technical meeting. They show the application of the following methods in determining the critical temperature of two simple structural systems:

- *temperature limit method*
- *AISC permissible stress method*
- *BS 5950: Part 8 method*
- *manual non-linear analysis*

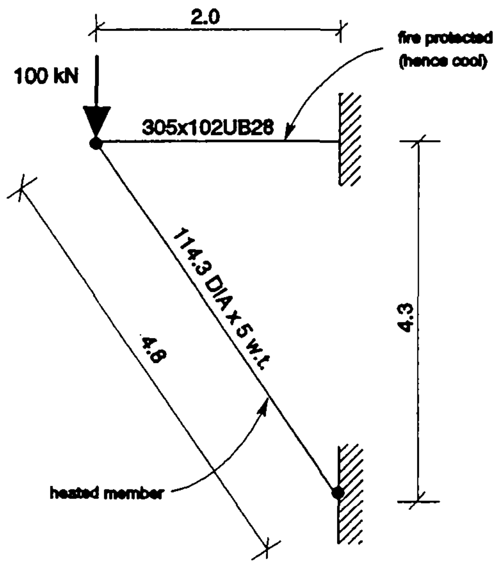
The methods above are listed in order of increasing sophistication. Note that in practice the methods may be used in combination with one another, or varied slightly, in order to form other methods.

The two structural systems that the methods are applied to are shown in figure 1 and figure 2.



- ignore dead weight
- assume flanges adequately restrained against lateral torsional buckling

Figure 1 Flexure Problem



Example 2

Figure 2 Compression Problem

TEMPERATURE LIMIT METHOD

All members identified as necessary for structural integrity (or to prevent undesired escalation) are protected such that their temperature does not exceed a specified temperature for the design duration. The magnitude of the specified temperature may lie between 400°C and 500°C. However, the 400°C figure is historically the more common limit.

There are two underlying assumptions in this method:

- *Internal forces due to differential thermal expansion equilibriate within the structure and are not shed prematurely as a result of buckling.*
- *The reduction in steel strength by heating to 400°C is approximately equal to removing the code safety factors.*

Using this method the limiting temperature for both the flexure and compression problems is 400°C.

AISC PERMISSIBLE STRESS METHOD

The method to be applied is as follows:

- *determine forces in member and resulting stresses at room temperature;*
- *from stresses, use curves in IGN figure 4.3 to determine the approximate temperature at which the member fails;*
- *use AISC code, modified to remove safety factors, to determine the utilisation at the first estimated temperature;*
- *adjust temperature and repeat code check until member utilisation is 100%. This is the limiting temperature.*

Flexure Problem

Determine applied moment:

$$M = \frac{WL^2}{2} = \frac{15\ .\ 3^2}{2} = 67.5\ kNm$$

The utilisation of the member (unity check) shall first be determined at room temperature:

$Z_x = 352 \times 10^3\ mm^3$ $\quad\quad \therefore f_b = \frac{M}{Z_x} = 191.8\ N/mm^2$

$F_y = 345\ N/mm^2$ $\quad\quad \therefore F_b = 0.66\ F_y = 227.7\ N/mm^2$ *AISC 1.5.1.4.1*

$$\text{unity check} = \frac{191.8}{227.7} = 0.84$$

In the above it is assumed that the compression flange is adequately restrained (e.g., by deck plating system) to prevent lateral torsional buckling.

In the extreme elevated temperature condition two changes will be made to the permissible stress, F_b:

- *the 0.66 "safety" factor can be set to unity*

- *the elevated temperature value of F_y will be used. This will be taken from IGN figure 4.3. Since the beam is in bending, and the section classifies as being able to sustain plastic moments, a reference strain of 1.5% shall be used.* (AISC Section 1.9, BS5950:Pt8)

For this particular case, the problem reduces to determining the temperature at which the yield stress of the material reduces to 191.8 N/mm². From IGN figure 4.3 this corresponds to a temperature of approximately 580°C. (IGN Figure 4.3)

In the above example, since only bending stresses were considered, it was not necessary to adopt a "trial & error" approach to finding the failure temperature. However, were there a more complex combination of loads, then since AISC applies different safety factors to the allowable stresses for different types of load components, it would be necessary to select a temperature and determine the unity check at that temperature. It may take several attempts to get a unity check close to 1.

Compression Problem

In this example the strut is assumed to be in pure compression, i.e., pinned at each end such that no moment is transferred into the member. From the calculations in the thermal restraint example it is possible to obtain the axial load in the member at room temperature and hence the axial stress.

$$P = 106\ kN \qquad A_s = 1720\ mm^2$$

$$\therefore \quad f_a = \frac{P}{A_s} = \frac{106 \times 10^3}{1720} = 61.6\ N/mm^2$$

The allowable stress is calculated in accordance with AISC, section 1.5.1.3:

$$l = 4.8\ m \qquad K = 1.0 \qquad r = 38.7\ mm$$

$$\therefore \quad \frac{K\,l}{r} = \frac{4800}{38.7} = 124$$

$$C_c = \sqrt{\frac{2\pi^2 E}{F_y}} = 106.9$$ AISC 1.5.1.3.1

$$\therefore \quad \frac{K\,l}{r} > C_c \quad and \quad F_a = \frac{12\pi^2 E}{23(Kl/r)^2} = 66.9\ N/mm^2$$ AISC 1.5.1.3.2

$$but \quad \frac{l}{r}\ also > 120 \qquad \therefore F_{as} = \frac{F_a}{1.6 - \dfrac{l}{200r}} = 68.3\ N/mm^2$$ AISC 1.5.1.3.3

The above equations result a room temperature utilisation of 0.90. At elevated temperature increases in stress due to thermal expansion are ignored and hence the acting stress , f_a, will remain the same. However, the allowable stress will change, partly as a result of removing the 12/23 safety factor and partly because of the change in material properties. Note also that the value of C_c will change as a result of the changes in E and F_y. The reduction in E will be taken from IGN table 4.7.

Now estimate the limiting temperature based on an allowable stress of:

$$F_a \approx \frac{f_a}{utilisation} \times \frac{1}{safetyfactor} \approx \frac{61.6}{0.90} \times \frac{23}{12} = 131.2\ N/mm^2$$

which represents full utilisation without a safety factor (assume everything changes linearly on F_y.

Based on a 0.5% yield criteria, the yield stress of the section has dropped to 130.9 N/mm^2 at a temperature of approximately 600°C. Determine unity check at this temperature: BS5950:Pt8

$$E_{600} = 0.31 \times 200{,}000 = 62{,}000, \qquad F_y = 131.2\ N/mm^2$$ IGN,Table4.7

$$\therefore C_c = 96.7 < \frac{K\,l}{r} \qquad and\ F_a = \frac{\pi^2 E}{(Kl/r)^2} = 39.8\ N/mm^2$$ AISC 1.5.1.3.1

$$unity\ check = \frac{61.7}{39.8} = 1.55$$

The unity check is greater than 1.0 since the allowable axial stress varies with E and not with F_y. Retry with temperature = 540°C.

$E_{540} = 0.48 \times 200{,}000 = 96{,}000, \qquad F_{y,540} = 180\ N/mm^2$ — IGN, Table4.7

$\therefore\ C_c = 102.6 < \frac{K\,l}{r} \quad and \quad F_a = 61.6\ N/mm^2$ — AISC 1.5.1.3.1, 1.5.1.3.2

$unity\ check = \frac{61.7}{61.6} \approx 1.0 \qquad \therefore\ limit\ temperature = 540°C$

The above method assumes that the AISC method is valid at elevated temperature. This may be regarded as acceptable where safety factors can be clearly identified and removed. Also where the code is based on fundamental equations. This is the case for compression, where removal of the safety factors reduces the allowable stress to the Euler stress. Inspection of the AISC code will reveal that this is generally the case for all the code equations. Note, however, that guidance on reference strains for elevated temperature properties has been taken from BS5950:Pt8.

BS 5950: PART 8

The method used is based on the code. The code was written specifically to determine the fire resistance of members. It will be applied to the example problems as follows:

- *determine appropriate load factors for the fire limit state and hence obtain design moments and forces acting in the member at room temperature;*
- *using BS 5950: Part 1 (main part of code), determine the room temperature resistance of the member;*
- *divide the acting moments and forces by the member resistance to obtain the load ratio;*
- *from table 5 in BS 5950: Part 8 obtain the limiting temperature.*

The method will be applied to the two example problems. Table 5 from BS 5950: Part 8 is given below:

TABLE 5, BS5950: PART 8

Case No.		Load Ratio (R) 0.7	0.6	0.5	0.4	0.3	0.2
	Members in compression						
(1)	Slenderness Ratio ≤ 70	510	540	580	615	655	710
(2)	Slenderness Ratio ≤ 180	460	510	545	590	635	635
	Members in bending						
(3)	Unprotected members, or protected members complying with Clause 2.3(a) or (b)	520	555	585	620	660	715
(4)	Other protected members	460	510	545	590	635	690
	Members in tension						
(5)	All cases	460	510	545	590	635	690

Flexure Problem

Since load assumed to be imposed, use $\gamma_f = 1.0$ — BS5950:Pt8 Table 2

$\therefore\ M_f$ = moment under fire conditions = = 67.5 kNm

M_c = moment capacity = $P_y\, S_x = 345 \times 408{,}000 = 140.8$ kNm — BS5950:Pt1 4.2.5

Load Ratio = R = = 0.48

For an unprotected beam in bending obtain a limiting temperature of approximately 592°C for a load ratio of 0.48. Note that linear interpolation is used between values in BS 5950: Part 8, table 5.

Compression Problem

Since load assumed to be imposed, use $\gamma_f = 1.0$ — BS5950:Pt8 Table 2

For a compression member with 70 < λ <180, table 5 of BS 5950: Part 8 gives a limiting temperature of 535°C for a load ratio of 0.53.

Note than in BS 5950: Part 8, members subjected to combined bending and compression should be treated as if a compression member. In such cases λ will correspond to the slenderness of the unrestrained length of the compression flange. In general this will be less than 70. In BS 5950: Part 8 table 5 the corresponding limiting temperatures are only a few percent less than if the member were treated as a bending element.

P_f = *axial load under fire conditions* = $\gamma_f P = 106$ kN

$$\lambda = \frac{L_E}{r_y} = 124 \qquad \therefore\ p_c = 117\ \text{N/mm}^2$$ *BS5950:Pt1 Table 27(a)*

$$P_c = A_g\, p_c = 1720 \times 117 = 200.9\ \text{kN}$$ *BS5950:Pt1 4.7.4*

$$R = \text{load ratio} = \frac{F_f}{P_c} = 0.53$$

SIMPLE NON-LINEAR ANALYSIS

In a statically indeterminate structure (i.e., with redundancy) the failure of a member in bending or buckling does not necessarily result in collapse of the structure. In a highly indeterminate structure the difference in temperature between first member failure and structure collapse may be considerable. In determining the endurance of a structure it may be desirable to account for this reserve strength.

For large structures the best approach is to use one of the non-linear finite element packages that are available. However, for simple problems where only a few members fail, it is possible to estimate reserve capacity by hand techniques.

The flexure problem is statically determinate. Therefore at formation of first hinge the system will collapse (i.e., member failure = structure failure).

The compression problem is not statically determinate. After failure of the diagonal strut (by buckling), some of the load can be redistributed into the beam section. The total load is therefore carried by a combination of the buckled strut and the cantilever beam. Also note that because the system is statically indeterminate, thermal expansion will create extra loads in the members.

The compression problem is the same as the example used to illustrate thermal restraint. In that example it was shown that:

- *the compression member buckles at a temperature of 300°C if thermal expansion is taken into account;*
- *the beam element, acting as a cantilever, cannot support all the load;*
- *the load carrying capacity of the buckled compression element and the cantilever is sufficient to support the load at 300°C assuming that the compression element can support the plastic moment. In practice the tubular compression element is likely to buckle locally and its load carrying capacity will reduce;*
- *the rotation imposed on the compression member was 15°. It was assessed that at this rotation the compression member would be unable to support significant moment, therefore the system would collapse.*

If the thermal expansion of the compression element is ignored, then the compression element will buckle at a higher temperature. This will correspond to the temperature calculated by the AISC or BS 5950 method.

SUMMARY OF RESULTS

The following table summarises the results from this worked example:

Analysis Method	*Example 1*	*Example 2*
400°C Temperature Limit	*400°C*	*400°C*
AISC Permissible Stress	*580°C*	*540°C*
BS 5950: Part 8	*595°C*	*535°C*
Simple non-linear analysis (including expansion)	*580/595°C*	*300°C*

The table illustrates a number of points:

- *the temperature limit method gives lower limiting temperatures than both the AISC and BS 5950 methods;*
- *the AISC and BS 5950 methods give similar limiting temperatures for both flexure and compression;*
- *including thermal expansion reduces the limiting temperature for the compression problem, even when simple non-linear techniques are adopted in order to include any post-buckling reserve strength.*

The limitation of the AISC and BS 5950 methods is that they assume that the stresses in the members remain unchanged at temperature. However, note that the 300°C obtained for the non-linear example was obtained using the AISC permissible code check, but by using the member load including the effects of thermal restraint. i.e., AISC and BS 5950 can be used to determine the limiting temperature of restrained slender members providing additional force components due to thermal restraint are included.