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Review of the Response of Pressurised Process Vessels and Equipment to Fire Attack

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ABSTRACT

Pressurised systems such as process vessels and associated equipment experience time and temperature dependent effects when subjected to a fire. When a pressurised system is affected by fire, its temperature rises and this reduces the load carrying capacity of the system's material. This combined with the pressure loading may lead to a failure of the system, additional leaks developing and escalation of the event.

In 1991, the offshore industry, with the participation of HSE, completed a Joint Industry Project on Blast and Fire Engineering for Topside Structures. The project included a review of the information available to date on the behaviour of pressure vessels affected by a fire. The current review aims to summarise the new knowledge gained since 1991 and identify gaps in the currently available information, particularly with respect to establishing sound modelling procedures and guidelines for industry.

This report and the work it describes were funded by the Health and Safety Executive and Shell International Exploration and Production B.V.

SUMMARY

If a pressurised vessel is attacked by fire, its temperature rises and this reduces the strength of the vessel. This, combined with the pressure within the vessel, may lead to failure of the vessel with catastrophic consequences. In the event of fire on offshore installations, the primary protection of pressurised process vessels and pipework is emergency depressurisation (EDP) via the blow-down system. These systems are currently designed to industry guidance, API RP 521 (1-1), but it is widely recognised that in some respects this is inadequate and inappropriate for offshore installations, for which it was not originally intended (see Section 5.2).

In 1991, the offshore industry, with the participation of HSE, completed Phase 1 of a Joint Industry Project on Blast and Fire Engineering for Topside Structures (see Appendix A). This comprehensive project included the thermal response of vessels and pipework exposed to fire and other closely related topics. Current industry guidance is encapsulated in the 'Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire' (Appendix A, SCI-P-112) based on the Phase 1 study. Together, these documents form the starting point from which the review described in this report has been carried out.

This review has been carried out jointly by HSE's Health & Safety Laboratory, CREA Consultants Limited and Shell Global Solutions for Roland Martland of HSE's Offshore Division. It aims to:

- address the response of pressurised process vessels and equipment to fire attack;
- review the current knowledge and available analysis techniques relating to this; and
- identify any gaps in knowledge that may need to be filled before new and comprehensive guidance can be given.

Other issues related to the design and operation of relief and blow-down systems are currently being addressed by the RaBs (**Relief and Blowdown Systems**) joint industry project. This project will result in new guidance to be published by the Institute of Petroleum (1-3). A new **ISO standard**, ISO 13702:1999 (1-4), has set out the requirements and guidelines for the **control and mitigation of fires and explosions on offshore production installations**. This review is intended to be consistent with these guidelines.

Section 2 reviews the **types of fires** that may occur on offshore installations and threaten pressurised process vessels. It was found that, on the whole, the **information** given in the 'Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire, SCI-P-112, 1992'**, remains valid for all the fire scenarios which concern jet fires and pool fires in the open**. However, **new information** is now available in the case of two-phase jet fires, the most notable gains being in understanding **the behaviour of jet fires and pool fires in modules (confined fires).** Many of the 'major difficulties and uncertainties' in the Interim Guidance Notes are no longer valid. Vessels may be **protected by water deluge** or **passive fire protection** (PFP). In the past, this has been provided for protection against pool fires rather than jet fires. Although no credit is normally taken for water deluge protection against jet fires, there are scenarios where it can be effective. A standardised jet fire resistance test of passive fire protection materials is now available, allowing reliable product comparison.

Failure of a pressurised vessel subjected to fire attack is related to its strength at elevated temperature. **Section 3** addresses the **variation of mechanical and thermal properties with temperature for steels used for pressurised systems**. It was found that there are insufficient data available to fully describe the temperature-dependant property of steels used in the

manufacture of pressure vessels used offshore. Such information is essential for the development of validated criteria used to define failure of vessels subjected to fire loads.
Section 4 elaborates on the methods for **predicting the thermo-mechanical response of pressurised systems** attacked by a fire. Failure of a vessel normally occurs when the combined stress in the vessel exceeds the vessel strength. However, this may not be the **mode of failure** if the vessel is also **stressed by connections and constraints** or there is **severe non-uniform heating**. It was found that **flanged connections** to vessels were **particularly vulnerable to non-uniform heating from a jet fire** and severe leakage may be as important as vessel rupture. Little information was found on the response of pressure relief valves engulfed in fire.

Section 5 deals with the **design of pressure relieving and depressurising systems**. The methods for sizing pressure relief systems and depressurising systems, both with and without external fires, are discussed. The BLOWDOWN computer code for modelling depressurising systems (without fire impingement) is identified as the only fully validated model available. Developments in **two-phase flow** are described and the aims of **a new Relief and Blow-down JIP** considered. **Current guidance** used to design emergency depressurisation systems to protect pressure vessels is **inadequate** for the severe fires that may occur on offshore installations.

Section 6 examines the experimental data and predictive methods available for assessing the **thermal response of pressure systems** when subjected to external fire. Most of the **tools** available to study the behaviour of a vessel and its contents in a fire have been **developed for LPG storage tanks** incorporating PRVs and have only been tested against a few experiments of small LPG tanks in fires. Vessels operating at modest pressures typically have thinner walls, making them more vulnerable. The walls of very high-pressure vessels provide such a large thermal mass that even severe fires may not result in failure of the vessel. **No validated model** exists for the **emergency depressurisation of a vessel with fire loading.**

Section 7 addresses **performance standards** for the **resistance of pressurised systems to fire attack.** These depend on having **robust engineering acceptance criteria** and a robust method to determine if these are met. **At present, these do not exist. Quantitative Risk Assessment (QRA)** is used to confirm that high-level performance standards are achieved, and the QRA rule sets also depend on having robust engineering acceptance criteria. **Guidance is needed** on the rule sets for use in QRA, the determination of **Design Accidental Loads** (DALs) and appropriate engineering acceptance criteria.

Section 8 describes a **case study** to demonstrate the capabilities and limitations of current techniques applied to assessing the response of a depressurising vessel exposed to a severe fire. The application of BLOWFIRE and the ANSYS finite element analysis programs to a model vessel are discussed. The case study illustrates the difficulties in using the current available tools and the need for improvement.

Current risk assessments have to either assume (possibly incorrectly) that the current industry guidance provides sufficient protection or assume (possibly unnecessarily) some form of worst case scenario where vessel rupture and fire escalation occurs. **Improved guidance** is **required** on:

- Fit-for-purpose and cost effective **protection of process vessels** and equipment **against fire loads**; and
- **Optimisation** of different **fire protection and mitigation options** including; depressurisation, active and passive fire protection and design and material specification of process vessels and equipment.

Further work is required to provide:

- A **methodology** or "route-map" outlining the steps necessary to **determine the thermal response** of a range of process vessels and equipment subject to a **range of fire loads**;
- **Guidance** on appropriate **failure criteria** for different equipment as a function of the design and material specification, the duty (temperature and pressure) and equipment contents;
- When possible, **generic depressurisation performance standards** to ensure vessel and equipment integrity (as a function of the equipment design and material specification, contents and duty). Alternatively, **guidance** on how to perform the necessary **analysis for specific situations** and also on the tools necessary to perform the analysis; and
- A **data set** designed for the development and validation of **response models** for **equipment containing multi-component fluids** typical of both the upstream and downstream oil industry.

CONTENTS

CONTENTS (continued)

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1. INTRODUCTION

1.1 BACKGROUND

If a pressurised vessel is attacked by fire, its temperature rises and this reduces the strength of the vessel. This, combined with the pressure within the vessel, may lead to failure of the vessel with catastrophic consequences.

In the event of a fire on offshore installations, the primary protection of pressurised process vessels and pipework is emergency depressurisation (EDP) via the blow-down system. These systems are currently designed to industry guidance, API RP 521 (1-1), but it is widely recognised that in some respects this is inadequate and inappropriate for offshore installations, for which it was not originally intended (see Section 5.2). For example, only heat transfer from ca. 100 kW m^{-2} fires to the liquid wetted wall is considered. A major concern is that heat loads from fire attack, implicit in the current guidance, are much lower than can be expected (up to 350 kW m^{-2}) in severe fires that may occur on offshore installations and thus EDP may not guarantee vessel protection.

The problem is illustrated in Figures 1.1 and 1.2 below, which show idealised time histories for a pressure vessel under fire attack.

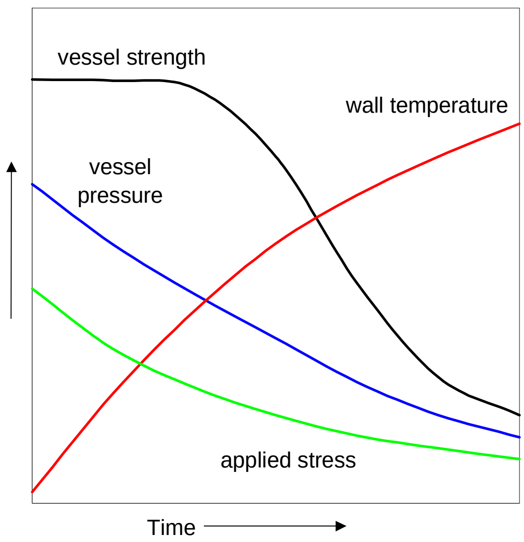

Figure 1.1 Vessel survives

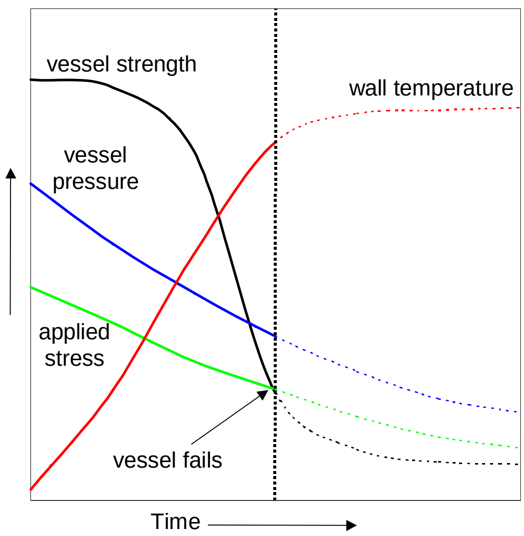

Figure 1.2 Vessel fails

In Figure 1.1, the vessel wall temperature increases slowly with time. The vessel strength initially remains constant but starts to significantly loose its strength once a critical temperature is reached (300-500 °C, depending on the type of steel). The vessel is being depressurised and the applied stress reduces proportionally with the decreasing internal pressure. The vessel strength always remains greater than the applied stress and thus the vessel is protected by the EDP.

In Figure 1.2, the same vessel is exposed to a more severe fire. The higher heat flux causes the vessel temperature to rise more rapidly and the vessel strength to decrease more rapidly. The vessel is being depressurised at the same rate as before, however, the strength is dropping too rapidly and the vessel will fail (rupture) once its strength is less than the applied stress.

It should be emphasised that the above example is greatly simplified. In reality, the vessel wall stress will not simply be the result of the internal pressure and non-uniform heating by the fire will create additional thermal stresses. If the vessel contains both liquid and vapour, the wet part of the wall will rise in temperature more slowly than the dry part in contact with the vapour. Failure of the vessel may be exacerbated by other mechanisms, such as loss of ductility during heating and differential thermal expansion. In practice, EDP will not be coincident with the onset of the fire due to delays in detection, emergency shutdown and activation of the EDP system. The fire source itself may not remain constant, but could reduce in intensity as a result of the EDP, if being fed by the same pressurised system.

In 1991, the offshore industry with the participation of the Offshore Safety Division of the HSE, completed Phase 1 of a Joint Industry Project on Blast and Fire Engineering for Topside Structures (see Appendix A). This comprehensive project included the thermal response of vessels and pipework exposed to fire and other closely related topics:

- thermal response of vessels and pipework exposed to fire (Appendix A, OTI 92 610);
- oil and gas fires - characteristics and impact (Appendix A, OTI 92 596);
- behaviour of oil and gas fires in the presence of confinement and obstacles (Appendix A, OTI 92 597);
- availability and properties of passive and active fire protection systems (Appendix A, OTI 92 607);
- passive fire protection requirements and test methods (Appendix A, OTI 92 606);
- experimental data relating to the performance of steel components at elevated temperatures (Appendix A, OTI 92 604);
- methodologies and available tools for the design/analysis of steel components at elevated temperatures (Appendix A, OTI 92 605);
- existing fire design criteria for secondary, support and system steelwork (Appendix A, OTI 92 608); and
- fire performance of explosion-damaged structural and containment steelwork (Appendix A, OTI 92 609).

Current industry guidance is encapsulated in the ‘Interim Guidance Notes for the Design and Protection of Topsides Structures Against Explosion and Fire’ (Appendix A, SCI-P-112) based on the Phase 1 study. Together, these documents form the starting point from which the review described in this report has been carried out.

More recently, 1995, a paper by Gayton and Murphy (1-2) re-stated concern that current industry guidance may not guarantee protection of vessels against rupture during fire exposure. Their paper proposes methods for calculating the heat input from different types of fires, the vessel wall temperature and internal pressure.

1.2 AIMS AND OBJECTIVES

The objectives of this study are to:

- address the response of pressurised process vessels and pipework to fire attack;
- review the current knowledge and available analysis techniques; and
- identify any gaps that may need to be filled before new and comprehensive guidance can be given.

Other issues related to the design and operation of relief and blow-down systems are currently being addressed by the RaBs (Relief and Blow-down Systems) joint industry project. This project will result in new guidance to be published by the Institute of Petroleum (1-3).

A new ISO standard, ISO 13702:1999 (1-4), has set out the requirements and guidelines for the control and mitigation of fires and explosions on offshore production installations. The current work is consistent with these guidelines, as are the terms, definitions and abbreviations used.

1.3 THIS REPORT

A brief description of the contents of this report is given as follows:

Section 2 reviews the **types of fires** that may occur on offshore installations and threaten pressurised process vessels. The characteristics of hydrocarbon pool and jet fires were thoroughly appraised in Phase 1 of the Blast and Fire Engineering JIP and the main findings of that study are summarised in Section 2.2. A number of major gaps in the knowledge on fires were identified and some have subsequently been addressed in Phase 2 and other JIPs, some of which are ongoing and confidential to the sponsors. This work has been reviewed and where possible it has been summarised. Defining the boundary conditions, at the fireside boundary of a vessel exposed to fire, is one of the key elements in assessing the response. If the vessel is **protected** by **water deluge** or **passive fire protection** this must be included. Recently, considerable gains have been made in understanding the value and limitations of these fire mitigation measures, this work has also been reviewed and is summarised in Sections 2.4 and 2.5.

Failure of a pressurised vessel subjected to fire attack is related to its wall strength at elevated temperature. **Section 3** addresses the **variation of mechanical and thermal properties with temperature for steels used for pressurised systems**. The steels that are commonly used for the components of pressurised systems on offshore installations are identified. An offshore separator system is used as an example. The section summarises the mechanical and thermal properties of the steels and identifies those that are missing in respect to the required knowledge of the behaviour of pressurised systems attacked by a fire.

Section 4 elaborates on the methods for **predicting the thermo-mechanical response of pressurised systems** attacked by a fire. The superposition of pressure and thermal stress is considered and the lack of adequate guidance highlighted. Literature on the **failure of pressurised systems** is reviewed and the main **stress distributions** and **failure mechanisms** identified. The effect of fire on the operation of relief devices is also discussed.

Section 5 deals with the **design of pressure relieving and depressurising systems**. The methods, both with and without external fires, for sizing pressure relief systems and depressurising systems are discussed. The BLOWDOWN computer code for modelling depressurising systems (without fire impingement) is identified as the only fully validated model available. Developments in **two-phase flow** are described and the aims of a new Relief and Blow-down JIP considered.

Section 6 examines the experimental data and predictive methods available for assessing the **thermal response of pressure systems** when subjected to external fire. Experimental work to examine the response of pressure vessels has been undertaken by several research teams and the work is summarised. In addition, several predictive models, which are designed to model the response of a pressure vessel exposed to fire, are discussed and their predictions compared with experimental data. The lack of a validated model for depressurisation under a fire load is identified.

Section 7 addresses **performance standards** for the **resistance of pressurised systems to fire attack.** These depend on having **robust engineering acceptance criteria** and a robust method to determine if these are met. **Quantitative Risk Assessment (QRA)** is used to confirm that high-level performance standards are achieved, and the QRA rule sets also depend on having robust engineering acceptance criteria. The section describes the relationship between performance standards, QRA, engineering acceptance criteria and **Design Accidental Loads** (DALs) and gives an illustration of how they may be **linked** together.

Section 8 contains a **case study** to demonstrate the capabilities and limitations of current techniques applied to assessing the response of a depressurising vessel exposed to a severe fire. The application of BLOWFIRE and the ANSYS finite element analysis programs to a model vessel are discussed.

Section 9 gives the conclusions in relation to new and missing data, modelling and guidance and gives overall conclusions.

Section 10 gives the main recommendations in regard to new guidance and the steps necessary to produce it.

Appendix A provides information about the Blast and Fire Engineering for Topside Structures project.

1.4 REFERENCES

1-1 API RP 521 Guide for pressure-relieving and depressuring systems, American Petroleum Institute, Fourth Edition, March 1997.

1-2 Gayton, P.W. and Murphy, S.N. Depressurisation Systems Design. IChemE Workshop “The Safe Disposal of Unwanted Hydrocarbons”, Aberdeen 1995.

1-3 Hydrocarbon Pressure Relief and Blowdown Systems: A Guide for Safe and Optimum Design, The Institute of Petroleum, in preparation.

1-4 ISO 13702:1999, Petroleum and natural gas industries - Control and mitigation of fires and explosions on offshore production installations - Requirements and guidelines.

2. FIRE HAZARDS AND MITIGATION

2.1 BACKGROUND

Fire and explosion hazards are recognised as particularly significant on offshore installations and a new International Standard, ISO 13702:1999 (2-1), sets out the requirements and guidelines for their control and mitigation. A key requirement is for a fire and explosion strategy (FES) to manage these hazardous events, based on a fire and explosion evaluation for the particular installation. The fire evaluation must identify the types of fires that could occur and quantify the hazard posed, based on the characteristics of the fire, probably via some form of fire modelling.

Some fires may threaten pressurised process vessels and in these cases specific mitigation measures must be considered in the FES. Active fire protection (area deluge or dedicated vessel deluge) or passive fire protection (fire resistant coatings or jackets) may be used.

2.2 FIRE CHARACTERISTICS

2.2.1 Past knowledge

The hazards, characteristics and physical properties of hydrocarbon jet fires and pool fires have been appraised in the Phase 1 reports of the Joint Industry Project on ‘Blast and Fire Engineering of Topside Structures’ (Appendix A, OTI 92 596/597/598). The report packages, FL1 and FL2, provide details of fire types and features relevant to the offshore environment and include pool and running liquid fires, jet fires, cloud fires, fireballs and confined fires. Experimental findings and theoretical approaches are appraised and, in particular, the degree to which the deterministic models of the time could quantify the hazard consequences are discussed. This early work represents the state of knowledge up to the beginning of 1991. However, in some areas both knowledge and the predictive capabilities of models have advanced since then. The FL1 and FL2 Phase 1 reports (Appendix A, OTI 92 596/597) also identified a number of major gaps in the knowledge. These included the poor ability to predict the effects of scale on fire behaviour, the effect of fuel type on fire characteristics and, most notably, effect of confinement on hydrocarbon fires at large scale representative of offshore conditions.

The main findings of the Phase 1 work were encapsulated in the ‘Interim Guidance Notes for the Design and Protection of Topsides Structures Against Explosion and Fire’ (Appendix A, SCI-P-112) based on the Phase 1 study. It is possible to revise the guidance on thermal loading from fires based on the new knowledge. However, the changes are not major since considerable good judgement was used in drawing up the interim guidance where gaps existed. The relevant information from the ‘Interim Guidance Notes for the Design and Protection of Topsides Structures Against Explosion and Fire’ (Appendix A, SCI-P-112) is summarised in Tables 2.1 and 2.2.

Table 2.1. Jet fires

<table>
<tr><th rowspan="2">FIRE SCENARIO</th><th rowspan="2">FLAME EXTENT AND GEOMETRY</th><th colspan="2">HEAT FLUX TO TARGET</th><th rowspan="2">COMBUSTION PRODUCTS</th></tr>
<tr><th>ENGULFED</th><th>NON-ENGULFED</th></tr>
<tr><td>Jet fire in the open. Gaseous release.</td><td>Use multiple point source model for flame length or surface emitter models for general flame shape.</td><td>See [Appendix A, OTI 92 596] 7.4.5.
50-300 kW m^{-2}. May be higher in larger flames and in those loaded with higher molecular components.</td><td>Use multiple point source or surface emitter model. In the far field i.e. >5 flame lengths, point source models are acceptable if F factor is known.</td><td rowspan="2">Field models are good for predicting toxic gas and smoke movement but not concentrations.</td></tr>
<tr><td>2-phase release</td><td>Major uncertainties (see [Appendix A, OTI 92 596] 7.7.9).</td><td>50-250 kW m^{-2} for flashing propane up to 20 kg s^{-1}. Major modelling difficulties. No other data exist.</td><td>Major uncertainties (see [Appendix A, OTI 92 596] 7.7.9, 7.3.4).</td></tr>
<tr><td>Jet fire in module. Fuel controlled.</td><td>Caution - flame shape changes due to impingement on objects.
Treat as open fire.</td><td colspan="2">Treat as open fire.</td><td rowspan="3">Field models predict smoke and toxic gas trajectories adequately. Concentrations and temperatures less well predicted. Probably greater smoke concentrations than open fires.</td></tr>
<tr><td rowspan="2">Ventilation controlled.</td><td>Major difficulties and uncertainties (see [Appendix A, OTI 92 597] 9.7.4). The open flame lengths may be extended as air access is denied.</td><td>See [Appendix A, OTI 92 597] 9.7.6.
Up to 400 kW m^{-2} in re-circulating gaseous propane flame.</td><td></td></tr>
<tr><td>In the extreme, external flames result. Models not developed.</td><td colspan="2">Major uncertainties because of induced re-circulation and incomplete burning. Needs to be treated on a case specific basis. There are no validated predictive models.</td></tr>
<tr><td>Jet fire below lower deck</td><td colspan="4">Treat as open fire, but note major difficulties with 2-phase releases, and flame extent for large releases into objects.</td></tr>
<tr><td>Wellhead blow-out</td><td colspan="4">Treat as open fires in naturally vented or explosion damaged well-bays.
Treat as ventilation controlled fire in enclosed well-bays.
N.B. Major uncertainties with 2-phase blow-outs and liquid dropout.</td></tr>
</table>

Table 2.2. Pool fires

FIRE SCENARIO	SOURCE TERMS		FLAME EXTENT GEOMETRY	HEAT FLUX TO TARGET		COMBUSTION PRODUCTS
	POOL SPREAD	MASS BURNING RATE		ENGULFED	NON-ENGULFED	
Pool fire on the open deck.	Limited by walls, edge of deck or local depressions.	Use literature data where applicable. Otherwise use Equation 6.1 in Reference [Appendix A, OTI 92 596]	Flame length use Eqn. 6.2 [Appendix A, OTI 92 596]. Flame tilt, use Eqn. 6.5 [Appendix A, OTI 92 596] or Eqn. 6.6 [Appendix A, OTI 92 596]. Flame drag, use Eqn. 6.7 [Appendix A, OTI 92 596]	100-160 kW m^{-2} (see table 6.4 in Reference [Appendix A, OTI 92 596]).	Use surface emitter model. Point source models acceptable beyond approx. 5 pool diameters if F factor known.	Field models are good for smoke and toxic gas movements, but not concentration.
Pool fire in module. Fuel controlled.	Normally limited by walls.	Treat as open deck fire.		Treat as open deck fires		
Ventilation controlled.		Major uncertainties (see [Appendix A, OTI 92 597] 9.5).	Major difficulties and uncertainties. Models not validated. Open flame length may be extended as air access is denied (see [Appendix A, OTI 92 597] 9.7.6).	Major problems. Severity may be greater than the same fire in the open. Models are not yet validated (see [Appendix A, OTI 92 597] 9.7.2).		Zone or field models can predict trajectories adequately but not concentrations.
Pool fire on sea. Oil.	Complex but known (see [Appendix A, OTI 92 596] 6.5.1).	Treat as open deck fire.		Treat as open deck fires.		
Subsea gas release.	Some data and theory available (see [Appendix A, OTI 92 596] 6.5.2).	Assume all gas burns over effective pool area (see [Appendix A, OTI 92 596] 6.7.1).	Treat as open deck fire with caution. Check mass burning rate does not far exceed normal pool fire values.	250-300 kW m^{-2} for large pools (see [Appendix A, OTI 92 596] 6.10.3).	Treat as open deck fire with caution.	Treat as open deck fire.
Pool fire at bottom of concrete leg.	Treat as ventilation controlled fire in enclosed well-bays. N.B. Flame may go out through lack of air.					May have more smoke and toxic gases than open fires.

On the whole, the information given in the above tables remains valid for all the fire scenarios which concern jet fires and pool fires in the open, although it is worth noting that new information is now available in the case of two-phase jet fires. The information given for jet fires and pool fires in a module is somewhat outdated in light of recent advances in experimental and theoretical work. Many of the ‘major difficulties and uncertainties’ descriptions in the above tables are no longer valid.

2.2.2 New knowledge

The most notable gains in knowledge have been in the area of unconfined (open) two-phase jet fires and confined jet fires and pool fires (compartment fires). These areas have also been the subject of the completed Phase 2 JIP work on ‘Blast and Fire Engineering of Topside Structures’ (Appendix A, Phase 2) and another ongoing JIP (2-2). The experiments in Phase 2 were carried out at large scale to study the effects of scaling and to generate conditions representative of potential offshore scenarios.

Unconfined two-phase jet fires

The work on unconfined fires in the Phase 2 JIP focussed on horizontal free jet fires of stabilised light crude oil and mixtures of stabilised light crude oil with natural gas. For practical reasons, the latter were made up from separate releases of oil and natural gas that were allowed to mix together in the external jet. The effect of these fires impinging on a cylindrical pipe target was also investigated. The main findings of this work were as follows:

- The free flame releases, of crude oil only, were not able to sustain a stable flame and one of the mixed fuel releases was also unstable.
- All the flames were particularly luminous compared with purely gaseous jet flames and generated large quantities of thick black smoke, mainly towards the tail of the flame.
- All the flames were highly radiative, with maximum time averaged surface emissive powers (SEP’s) ranging between 200 kW m^{-2} to 400 kW m^{-2}.
- The incident total heat fluxes (radiative and convective) measured on the pipe target were significantly higher for the mixed fuel tests than for the crude oil only tests, by a factor two in many cases. Typical values were in the range 50 kW m^{-2} to 400 kW m^{-2}. A summary of the results is reproduced in the Table 2.3.

Subsequently, further work (2-2) has been carried out as a separate JIP to quantify the hazards posed by realistic releases of ‘live’ crude oil containing dissolved gas and water.

Table 2.3. Heat fluxes from crude oil trials

Test no.	Fuel	Impinge-ment distance (m)	Measured radiative and convective heat fluxes incident on pipe target (kW m^{-2})											
			Front (θ = 0)				Top(θ = 90)				Back (θ = 180)			
			Convective		Radiative		Convective		Radiative		Convective		Radiative	
7	Oil	9	78	(67%)	39	(33%)	56	(37%)	96	(63%)	52	(25%)	158	(75%)
9	Gas/Oil (1:4)	9	174	(59%)	122	(41%)	108	(39%)	172	(61%)	120	(32%)	250	(68%)
10	Gas/Oil (2:3)	9	-	(-%)	102	(-)	196	(57%)	147	(43%)	130	(34%)	250	(66%)
8	Oil	15	21	(40%)	31	(60%)	3	(6%)	44	(94%)	0	(0%)	117	(100%)
11	Gas/Oil (1:4)	15	88	(57%)	67	(43%)	22	(14%)	135	(86%)	0	(0%)	200	(100%)
12	Gas/Oil (2:3)	15	141	(59%)	99	(41%)	48	(17%)	230	(83%)	20	(7%)	249	(93%)

Simulated 'live' crude was prepared in a pressure vessel from a cocktail of stabilised light crude oil, commercial propane and natural gas. Gas/oil ratios of 500 and 1500 scf bbl^{-1}, at pressures ranging from 30 to 100 barg were studied. In operation, the vessel vapour space pressure was maintained with natural gas as the mixture was discharged, via a mass flow meter, from a sharp edged orifice at the end of a discharge line. The orifice was sized to achieve a mass release rate of about 5 kg s^{-1} and the resultant horizontal jet fires were intense, about 20 m long, quite buoyant and very smoky. Measurements were made of flame size, shape and radiation, and heat transfer to a pipe target placed in the flame.

In a second phase of the project, water was injected into the release stream to examine the effect of high water-cut on flame stability. Small amounts of water had little effect on the characteristics of the fire, larger amounts produced a dramatic reduction in the smoke produced and increasing the water further took the flame to the point of extinguishment.

This work, which is nearing completion, confirms that jet fires from 'live' crude releases do not result in hazards more severe than had been predicted. Although the heat fluxes generated were similar to those found in the Phase 2 JIP study, with separate releases, the flashing 'live' crude jet fires were shorter and more buoyant. For high water-cut releases, smoke will be less of a hazard, although very high water-cut releases will not produce stable jet fires.

Confined fires

Both experimentally and theoretically, considerable advances have been made in understanding the characteristics and behaviour of jet fires and pool fires in the confined environments typical of offshore modules. Seminal work by Chamberlain (2-3 to 2-5) and the Phase 2 JIP (Appendix A), studied the effect of varying a range of parameters on the fire behaviour. These included the size of the openings (vents) in the walls of the compartment, the location of the vents, the fuel type (propane or condensate), the substrate for pool fires, the release height and pressure for jet fires. In the most recent large-scale tests, four vent sizes were investigated including the effect of splitting the vent. Gas temperatures within the compartment, wall temperatures, ceiling temperatures, target temperatures, heat fluxes to the walls, ceiling and target, gas composition of the smoke layer and fuel release rates were all measured. In some tests the effects of water deluge on the jet fires and pool fires were investigated. The main findings of this work were as follows:

- During the initial stages of fire development, confined jet fires and pool fires behave as they would in the open.
- After a short period, ranging from a few seconds to a few minutes, the development of the fire depends on the degree of ventilation control, specifically on the value of global stoichiometry. The global stoichiometry of the fire is the air/fuel ratio of the fire divided by the air/fuel ratio required for stoichiometric combustion. (The global stoichiometry is a measure of the amount of air actually feeding the fire through the side wall vents compared to the amount of air required for complete stoichiometric combustion of the fuel. Insufficient air corresponds to a ventilation controlled fire, excess air therefore corresponds to a fuel controlled fire).
- A well-defined horizontal interface between an upper hot gas/smoke layer and lower cool air layer forms. Depending on the relation and size of the vent flow, and the size and position of the fire source, the conditions for ventilation or fuel controlled burning are established.
- When the burning approaches ventilation controlled conditions, it is possible for the combustion at the interface between the two layers to become highly oscillatory and

unstable. This leads to vigorous combustion, high heat fluxes, and temperatures above 1350 °C due to soot oxidation.

- Before steady state conditions are achieved, incident heat fluxes and temperature rise rates can diminish if the fire enters a ventilation-controlled regime. Copious amounts of soot are produced from incomplete combustion, particularly when the temperature of the smoke layer is > 900 °C.
- Generally, carbon monoxide (CO) levels increase with increasing ventilation control for all fires, but the temperature and residence times are also important as these parameters determine the dominant combustion reaction kinetics.
- In ventilation controlled fires, soot ignition on exiting the vent can produce high levels of external radiation. Inside the compartment, heat fluxes and temperatures increase when the soot burns. The temperature and chemical composition of the smoke layer at the vent determine whether or not sustainable combustion is possible.
- The region of maximum combustion intensity shifts from the jet or pool towards the vent as ventilation control increases.
- The overall burning rate of condensate pool fires enters a self-limiting regime as ventilation controlled conditions are approached such that the flame stoichiometry remains approximately constant. The final burning rate is lower than expected when comparing with the burning rate of an open pool fire of the same size.
- The results show there is no difference in the pool fire-burning rate between a pool fire on water or a pool fire on a steel substrate.
- At steady state conditions, incident heat fluxes to the surrounding walls, ceiling and impinged objects are comparable in magnitude to those found for impinging jet fires or pool fires in the open, but can be higher under certain conditions. The hot layer at the top of a module may engulf the upper, un-wetted and hence most vulnerable parts of process vessels.

2.3 FIRE MODELLING

2.3.1 Unconfined fires

The general status up to the end of 1991 of fire modelling capabilities with respect to predicting the characteristics of open jet fires and open pool fires is fully discussed in the FL1 report (Appendix A, OTI 92 596). The types of models appropriate to consequence fire modelling are placed into three categories, namely semi-empirical models, field models (numerical or computational fluid dynamics (CFD) models) and integral (phenomenological) models. Their strengths, weaknesses and validation are considered in generic terms in the FL1 report. It was generally accepted at the time that the semi-empirical models provided the most accurate and reliable predictions of the physical hazards associated with fires, providing their application is limited to the validation range of the model. This conclusion essentially remains valid today but CFD models are developing rapidly and are clearly the way forward for fires interacting with complex structures. A recent CFD study, carried out for HSE/OSD (2-6), showed good agreement with medium and large scale jet-fire experiments and gave a good insight into those conditions important in developing a jet-fire resistance test for passive fire protection materials.

At present, commercially available semi-empirical models can provide accurate prediction of flame shape, flame size and external radiation flux to external objects but not heat fluxes to impinged objects. The latter must be treated empirically, based on the relevant experiments described in Section 2.2.

2.3.2 Confined fires

The general status, up to the end of 1991, of fire modelling capabilities available for the prediction of confined jet fire and confined pool fire characteristics is fully discussed in the FL2 report (Appendix A, OTI 92 597). The principal modelling approaches include simple partial models (empirical), zone models and field models (numerical models). Partly due to the complete dearth of experimental data (see Section 2.2), most modelling efforts on compartment fires concentrated on cellulosic fuelled fires which are mostly relevant to domestic and commercial buildings onshore. The behaviour of large hydrocarbon fires under offshore conditions had received little attention at the time. The 'Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire' (Appendix A, SCI-P-112) states explicitly that "The ability to predict the overall behaviour of large hydrocarbon fires in offshore structures is poor".

As a direct result of recent efforts and major initiatives in undertaking experimental tests at various scales (see Section 2.2), new models are being developed that range from simple steady state zone models (2-7) through to more sophisticated transient zone models and CFD compartment fire models (2-8 & 2-9). The general level of understanding of compartment fire behaviour is now sufficiently good to assess most compartment fire hazards with some confidence for modules having simple geometries. In particular jet-fire and pool-fire temperatures, smoke layer temperatures, heat fluxes to surfaces within the module, the extent of external flaming and internal impingement zones can be reasonably well predicted. Estimates for CO concentrations in the smoke layer are also available based on empirical relationships to temperature and flame stoichiometry. Future improvement in model development should focus on evaluating the combustion product emissions from module vents, particularly for CO and smoke concentration, which are both treated simplistically in most models at present.

2.4 ACTIVE FIRE PROTECTION

2.4.1 Background

The primary form of fire protection to hydrocarbon processing areas is the water spray. Fixed deluge systems may be provided to:

- control pool fires and thus reduce the likelihood of escalation;
- provide cooling of equipment not impinged by jet fires;
- provide a means to apply foam to extinguish hydrocarbon pool fires; and
- limit effects of fires to facilitate emergency response and evacuation escape and rescue (EER) activities.

The four broad types of deluge system include:

- area protection designed to provide non-specific coverage of pipework and equipment within process areas;
- equipment protection designed to provide dedicated coverage of critical equipment such as vessels and wellheads;
- structural protection designed to provide dedicated coverage of structural members; and
- water curtains to reduce thermal radiation and to control the movement of smoke in order to provide protection to personnel during escape and evacuation.

In 1991 (Appendix A, OTI 92 607), active fire protection systems were still required to be designed for compliance with SI 611, "Offshore Installations: Guidance on Fire Fighting

Equipment ". The associated guidance note (2-10) was published in 1980 and only states a general water application rate of 12.2 $dm^3\ min^{-1}\ m^{-2}$. However, a redraft of the guidance, which was begun by the Department of Energy in the early 1990's, had the following recommended rates:

- 10 $dm^3\ min^{-1}\ m^{-2}$ to protect against pool fires; and
- 20 $dm^3\ min^{-1}\ m^{-2}$ for high pressure leak fires (jet fires).

These appear to be the current rates used offshore as indicated in the Interim Guidance Notes (Appendix A, IGN). However, variations are given in the IGN where the risk is high. For example, for high pressure jet fires impinging on structural steelwork and vessels, a rate of 400 $dm^3\ min^{-1}\ m^{-2}$ was adopted to provide protection to the area impinged by the jet fire.

Sometimes specific deluge is employed instead of, or as well as, the general area protection. Individual items, such as vessels or heat exchangers, are here protected as described in NFPA 15 (2-11). The water spray design would surround the equipment with medium velocity nozzles spaced at 2 to 2.5 m intervals and 0.6 m from the surface. Complicated objects would be covered by directing the sprays to an imaginary box enclosing the particular object. Application rates of 10 to 15 $dm^3\ min^{-1}\ m^{-2}$ are used.

Some of the benefits that may arise from high-level, general, area deluge are as follows:

- High level sprays may eliminate high level fire plumes from pool fires, reducing ceiling temperatures to about 450 °C. This effect is dependent on the fuel, air supply, obstructions and turbulence of the up-draught. However, procedures to assess this do not exist at present.
- The even application of water to the surface of a vaporising pool of hydrocarbon will reduce the rate of evaporation. Again this is difficult to quantify.
- High water application rates to dead crude or non-vaporising oils can cool or even extinguish a pool fire. The major benefit is that the oil is flushed into the drainage so disposing of part of the release. The drainage system should be designed to cope with the full rate of discharge, the water from additional hoses and the oil inventory.

This review does not deal with the methods of delivering the required spray rates, or with foam or halon systems, but concentrates on new information relating to the water spray rates required in process areas to protect against the fires characterised earlier in this section.

2.4.2 Effectiveness of water deluge

Directed deluge on tanks impacted by natural gas jet fires

Shirvill and White (2-12) performed a series of experiments impacting a 3 kg s^{-1} sonic natural gas flame on a 8.5 m long by 2.2 m diameter vessel placed 9 m from the release point. The vessel was protected by 24 (4 rows of 6), medium velocity, deluge nozzles intended to give a "uniform coverage of the vessel at a rate of 10.2 $dm^3\ min^{-1}\ m^{-2}$" As the total water flow used was 1060 $dm^3\ min^{-1}$ and the vessel surface area 104 m^2, this was the delivery rate to the nozzles rather than the actual amount of water on the vessel. Even with the deluge running before ignition of the natural gas (in practice there may be a delay before deluge commences), a large area on the front of vessel impacted by the jet fire was seen to be dry and unprotected. Temperatures in this region rose rapidly, reaching over 500 °C in 3 minutes. Experiments were also performed aiming a fire fighting water monitor at the jet-fire impingement area. It was concluded that:

- existing, directed deluge systems, acting alone, cannot be relied upon to provide protection of process plant and structural members from the effects of impinging natural gas jet fires; and
- a fire-fighting water monitor could be effective if aimed directly at the impinged area.

Directed deluge on tanks impacted by flashing liquid propane jet fires

The offshore water spray design is based on using medium velocity sprayers with application rates of 10 to 15 $dm^3\ min^{-1}\ m^{-2}$. Onshore, the Fire Offices' Committee’s *Tentative rules for medium and high velocity water spray systems* (2-13) state that horizontal cylindrical storage vessels should be protected by means of open medium velocity sprayers, not less than 6 mm bore, operating at pressures between 1.4 and 3.5 barg and should have cone angles between 60° and 125° to give an application rate of 10 $dm^3\ min^{-1}\ m^2$. The Tentative Rules were designed to protect against pool fires and HSL were commissioned by HSE/DST to determine the deluge rate which would protect an LPG tank against a flashing liquid propane jet fire. Trials (2-14) were performed using a ca. 2 $kg\ s^{-1}$ propane jet that enveloped a 1.2 m diameter, 4.5 m long LPG tank. The main conclusions from this work were as follows:

- A considerable amount of the water directed at the tank may miss the tank. Hence, an application rate calculated as the amount of water delivered to the nozzles divided by the tank surface area is a poor measure of how well a tank is protected by a deluge system.
- A deluge system, designed according to the Tentative Rules with 0.65 m stand-off and 95° cone angle, will protect against a 2 $kg\ s^{-1}$ flashing liquid propane jet fire if run at a water deluge pressure of 2.0 barg i.e. slightly above the 1.4 barg minimum allowed.
- Assessed in terms in terms of the amount of water delivered to the nozzles, ca. 30 $dm^3\ min^{-1}\ m^{-2}$ is required to protect a 2 tonne LPG tank.
- Whilst the Tentative Rules provide a basis for design, the design parameters (tank diameter, stand-off distance and spray angle) are usually optimised for water economy rather than fire protection. It is assumed that any of the allowed combinations will give the required fire protection. However, this may not be the case.
- The water spray significantly affected the jet flames resulting in cooler flames with less soot formation.

In addition to this, Shell and British Gas (2-15) were commissioned to determine the effectiveness a commercial deluge system in protecting a 2.2 m diameter, 8.5 m long LPG tank shell against flashing liquid propane jet fires of various sizes and stand-off distances. The deluge system was designed to deliver 10.2 $dm^3\ min^{-1}\ m^{-2}$ in accordance with NFPA 15 but actually delivered 17.6 $dm^3\ min^{-1}\ m^{-2}$, more in accordance with a design according to the Tentative Rules. The main findings from this work were as follows:

- A typical water deluge found on an LPG storage tank cannot be relied upon to maintain a water film over the whole tank surface in an impinging jet-fire scenario.
- Areas, where water film breakdown results in a dry patch, will increase in temperature to above 120 °C but the rate of temperature rise will be significantly less, by a factor between 1.5 and 5.8, when compared with the unprotected case.
- Close-to releases can result in liquid propane impinging on the target vessel with local low temperatures and ‘icing’ on the vessel wall, and high temperature gradients to surrounding hot areas.

Area deluge on confined jet and pool fires

As part of phase 2 of the Blast and Fire Engineering project (Appendix A, phase 2 & 2-16), the effect of water deluge on confined pool and jet fires was investigated. The main findings of this work were as follows:

- The effect of water deluge on ventilation controlled jet fires is to extinguish the fire.
- Fuel controlled jet fires may not be extinguished if insufficient time elapses before water deluge activation because the compartment must be 'hot' prior to deluge in order to extinguish the fire. Nevertheless the fire, if present, burns at a much reduced rate and is effectively controlled.
- It is possible for the fire to re-ignite after the water deluge is terminated due to the presence of hot gases or surfaces coming into contact with fuel.
- For the test conditions studied, there are no significant differences between the effects of water deluge on vertical and horizontal jet releases.
- Extinguished jet fires represent a potential explosion hazard if the fuel continues to be released.
- Generally, confined pool fires are not extinguished by water deluge, but the fire is controlled and burns at a much reduced rate.

Subsequently, further work at larger scale is being performed as a separate JIP. This work aims to quantify the benefits water deluge systems can provide, to gain an understanding of the physical processes involved and of the important factors which influence their effectiveness. The work is being undertaken in a rig consisting of a 20 m x 20 m flat deck to which has been added a water deluge system with a network of supply pipes enabling up to 156 spray nozzles to be located at a minimum spacing of 1.4 m. Water is supplied to the nozzles via a ring-main system at flow rates of up to 10,000 $dm^3\ min^{-1}$.

Phase 1 of the project was designed to investigate the main parameters governing the effectiveness of water deluge in controlling jet and pool fires and in reducing the surrounding external thermal radiation field. The main factors studied were the effect of varying the water deluge coverage rate, the effect of the size of the pool fires, the effect of the ambient weather conditions and an assessment of differences between the mitigating effects of sea and fresh water. The work showed that substantial benefits can be gained by using water area deluge, particularly in reducing the thermal radiation field around the fire. In the case of the pool fires, the deluge was shown to also reduce the size of the fire and, in certain circumstances, the interaction of the water with the pool lead to extinguishment. A further significant benefit was in the reduction in the smoke levels within and beyond the rig.

The second phase of work has continued to study a number of issues relating to the use of water deluge. One important area of the second phase of the work has been the study of the stability of natural gas jet fires in the presence of deluge. In the first phase of the work, during one experiment a natural gas jet fire was extinguished. This was an important finding for offshore operators as any potential benefits gained by the use of area deluge, in preventing heat transfer to surrounding plant, could be lost as an un-ignited gas release could present an explosion hazard as a result of gas build-up. Other areas studied in Phase II include the effectiveness of water deluge on condensate pool fires, crude oil jet fires and the effectiveness of the spray generated by different nozzles types.

For the majority of the tests, vessel or pipe targets manufactured from steel have been placed in the test rig and engulfed by flame. The thermal response of the targets has been monitored during tests and substantial information on the rates of temperature rise before and after the activation of the deluge has been gathered. This has provided an assessment of the ability of both area and dedicated deluge to provide protection of objects engulfed by pool or jet fire. This work is nearing completion.

2.4.3 ISO standard on control and mitigation of fires on offshore platforms

A new ISO standard (13702) on the control and mitigation of fires on offshore platforms (2-1) has been issued. This represents the latest available guidance on active fire protection. Annex B, "Guidelines on the control and mitigation of fires and explosions", includes a section on the use of active fire protection (AFP), and Annex C, "Typical design requirements for large integrated installations", has guidance on the selection of AFP systems for typical areas. In addition, it also provides examples of application rates of water based AFP systems. A rate of 10 $dm^3\ min^{-1}\ m^{-2}$ is recommended for the initial design in process areas **but** final selection of types and quantities / rates should be based on fire analyses and evaluations of fire-fighting systems.

2.5 PASSIVE FIRE PROTECTION

2.5.1 Background

Passive fire protection (PFP) is defined, in the recently issued ISO standard (2-1), as "a coating, cladding or free-standing system which, in the event of a fire, will provide thermal protection to restrict the rate at which heat is transmitted to the object or area being protected". These materials are used to:

- prevent escalation of the fire due to progressive releases of inventory, by separating the different fire risk areas;
- protect essential safety items and critical components such as separators, risers and topside emergency shutdown valves;
- minimise damage by protecting the critical structural members, particularly those which support the temporary refuge, escape routes and critical equipment; and
- protect personnel until safe evacuation can take place.

The required fire resistance may be achieved by the use of PFP in conjunction with active fire protection systems such as water deluge, in which case a minimal residual protection must be achieved should the active systems fail to operate. PFP is used particularly where active systems are impracticable, have insufficient reliability or where protection is needed within the probable response time of an active system.

Phase 1 of the Blast and Fire Engineering for Topside Structures project produced two reports relating to passive fire protection viz.

- *Passive fire protection: Performance requirements and test methods (Appendix A, OTI 92 606)* which appraises the performance requirements for offshore PFP systems and assesses the adequacy of the then current tests for ensuring that performance; and
- *Availability and properties of passive and active fire protection systems (Appendix A, OTI 92 607)* which reviews a selection of the various types of passive fire protection products which are used on offshore structures. Appendix C of OTI 92 607 contains a listing of manufacturers, products and product properties.

The Interim Guidance Notes (Appendix A, IGN) give an indication of how the information given in these reports should be applied.

After Phase 1, the areas of uncertainty were considered to be:

- furnace-based fire tests do not relate to conditions in “real” fires and there was a requirement for fire tests with a manageable, reproducible, well-characterised flame which is used in conditions which can be related to those in a “real” fire;
- smoke and toxic gas emissions need to be considered in the context of those from the primary fire;
- requirements for robustness (e.g. tolerance to mechanical damage, explosion resistance) and ability to predict long-term durability were lacking; and
- quality and maintenance were not given sufficient attention.

2.5.2 Types of PFP

There are many types of PFP materials on the market, which can be broadly categorised into groups as follows:

- Spray-applied and coating materials (comprising of primer, coating, top sealer coat and / or a decorative coat).
- Blanket / flexible jacket / wrap around systems.
- Prefabricated sections.
- Enclosures and casings.
- Composites.
- Seals and sealants.
- Fire walls.
- Systems (e.g. cable transits, inspection hatches, pipe penetration systems).

These are considered in greater detail in reference (2-17). As weight is at a premium offshore, spray applied epoxy intumescent and subliming coatings are most frequently used now, although cementitious materials were extensively used in the past.

2.5.3 Functional requirements

In ISO 13702 (2-1), the following functional requirements are given:

- PFP shall be provided in accordance with the Fire and Explosion Strategy (FES);
- PFP of essential systems and equipment, or enclosures containing such systems and equipment, shall be provided where failure in a fire is intolerable;
- where PFP is required to provide protection following an explosion, it shall be designed and installed such that deformation of the substrate caused by an explosion will not affect its performance;
- selection of the PFP systems shall take into account the duration of protection required, the type and size of fire which may be experienced, the limiting temperature for the structure/equipment to be protected, the environment, application and maintenance, and smoke generation in fire situations.

PFP materials should be approved for their intended use. Various "approved lists" (see 2-18 & 2-19) exist which contain general data such as name and location of manufacturer, brief description of product, areas of application and type of certification. Where general approvals from a recognised third party or governmental body are not available, PFP fire performance should be documented by test reports from a recognised fire test laboratory.

2.5.4 Fire resistance tests

Up to the early 1990's, most fire resistance tests (e.g. ISO 834, 2-20) were based on furnace tests in which a sample is exposed to a pre-determined heat-up regime whilst monitoring the thermal response on the reverse side of the sample. Originally, the heat-up regime used simulated cellulosic fires (e.g. from timber, paper or cotton) but then a hydrocarbon fire curve was developed to relate to hydrocarbon pool fires. The hydrocarbon curve has a steeper rate of temperature rise and a higher maximum temperature compared to the cellulosic fire curve.

Various hydrocarbon pool fire, or simulated pool fire, tests (2-21 to 2-23), which have been performed on LPG tanks, indicate that passive fire protection can adequately protect a pressure vessel against a hydrocarbon pool fire. However, the highest heat flux used was ca. 75 kW m^{-2} and thicker coatings than those tested may be required for vessels subjected to the high end of the pool-fire heat flux range identified in Table 2.2.

It was generally recognised that the conditions in the standard hydrocarbon furnace test do not represent characteristics such as heat flux and erosion found in full-scale high velocity jet fires. In the absence of a recognised test for jet fire resistance, *ad hoc* testing has been used, the most reliable being essentially full-scale demonstrations (2-24). A key recent improvement has been development of the Jet Fire Resistance Test of Passive Fire Protection Materials (JFRT, 2-25). This test involves use of a sonic, vapour-only 0.3 kg s^{-1} propane jet fire. The jet fire develops a high velocity combined with a high heat flux through a recirculating flame caused by firing the jet into a 1.5 m square, open fronted test box with 0.5 m sides. For structural steelwork, a steel box with a web is used as the test specimen with the vertical web intended to simulate edge features such as those on I-beams. The back face of the box may be flat (i.e. without a web) to represent vessels or tubular sections with large diameters or may be replaced by a panel with a representative join for panel applications. Tubular specimens are placed in front of a box to represent tubular sections. The test was shown (2-26) to reproduce key conditions typical of large scale fires resulting from high pressure releases of natural gas and is now widely used to assess PFP coatings and systems.

As part of the Commission of the European Community (CEC) funded joint project on hazard consequences of **J**et fire **I**nteractions with **VE**ssels containing pressurised liquids (JIVE 2-27 & 2-28), the Battelle Institute performed jet-flame impingement trials on unprotected and protected flange connections (2-29) and found that:

- typical LPG flange connections, and some new ones tested, do not resist jet fire attack;
- the time to loss of tightness depends on the intensity and position of the jet fire and can be as short as one minute;
- standard API 92 and BSI 87 tests provide no real information about loss of tightness in a realistic jet-fire scenario; and
- new protective measures are required for jet fires.

2.5.5 Prediction of PFP performance

In principle, the ability of a substrate to absorb heat is generally determined by its section factor or H_p/A ratio; i.e. the heated perimeter (H_p, m) divided by the cross-sectional area (A, m^2). A substrate with a large mass and small surface area will take a longer time to reach critical temperature than one with a small mass and large surface area. Hence, in furnace tests, it is usual to vary the duration, section factor and thickness in order to provide an estimate of the thickness required in a range of situations. However, in a jet-fire resistance test, the heating is non-uniform and the measure of performance is the maximum temperature of the substrate. In the JFRT, the substrate thickness used in the test should be as close as possible to the real application.

A number of approaches have been used to predict the thickness of PFP material required. These include empirical and analytical techniques. Due to the complexity of the different situations, the fact that furnace testing can currently only be controlled by a time/temperature relationship (as opposed to a heat flux) and the limitations on the number of tests a manufacturer can reasonably be expected to perform, increasing use is being made of computer modelling. These consist of finite element analysis programmes for solving steady state or transient two dimensional, non-linear heat transfer equations. There are a number of such programmes available commercially. The use of CFD codes to predict heat fluxes to vessels or test specimens is becoming increasingly important. A recent example of using this technique to model the heat flux from the Jet-Fire Resistance Test is given in reference (2-6).

2.5.6 Design and performance requirements

The new ISO (2-1) includes an Appendix C which gives typical fire integrity requirements. For example, for load bearing structures in process areas, resistance to a one hour jet fire at a critical temperature of 400 °C is required. The JFRT is mentioned as a suitable test. The reference temperature of 400 °C was used as a typical value for structural steel (see Section 3). For aluminium, the corresponding temperature is 200 °C and, for other materials, the critical temperature is the temperature at which the yield stress is reduced to the minimum allowable strength under operating load conditions.

There are certain operational requirements that are necessary for all types of PFP systems. At the moment, a general standard or code of practice, which addresses these needs, is not available.

2.6 CONCLUSIONS

The following conclusions have been drawn from the discussion of fire hazards and their mitigation on offshore installations given in Sections 2.1 to 2.5:

New and missing data

- On the whole, the information given in the ‘Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire, SCI-P-112, 1992’, remains valid for all the fire scenarios which concern jet fires and pool fires in the open. However, new information is now available in the case of two-phase jet fires. The most notable gains have been in understanding the behaviour of jet fires and pool fires in modules (confined fires) and many of the ‘major difficulties and uncertainties’ in the Interim Guidance Notes are no longer valid.
- Directed water deluge, at the currently specified rate (for pool fires) of 10 $dm^3\ min^{-1}\ m^2$, cannot be relied upon to maintain a water film over the whole surface of a pressure vessel in an impinging jet-fire scenario. However, the rate of heat transfer is reduced by at least a factor of two. Higher flow rates of water (ca. 2 – 3 times) through medium velocity sprayers, water monitors or high velocity sprayers can protect against some jet fires but, as yet, insufficient evidence is available for this to be relied upon.
- A Jet-Fire Resistance Test of Passive Fire Protection Materials has been developed. Except in cases where the geometry of a representative specimens severely effects the properties of the jet fire used, the test reproduces the conditions in a large (3 $kg\ s^{-1}$) natural gas jet fire.

This test, when used in conjunction with hydrocarbon furnace tests, offers a reliable method of comparing the performance of PFP materials in a jet fire. The evidence suggests further work is required to assess the resistance of flange connections to jet-fire attack. The data suggest that an adequate passive fire protection system can reduce the heat transfer to a pressure vessel by a factor of 10.

Models

- For unconfined fires, commercially available, semi-empirical, models can provide accurate prediction of flame shape, flame size and radiation flux to external objects but not heat fluxes to impinged objects. CFD fire models may be most suitable for fires interacting with complex structures. The gains in the understanding of the behaviour of confined hydrocarbon fires have yet to be translated into commercially available and validated models.
- No directed water deluge models currently exist that adequately link the amount of water delivered to the nozzles with the actual coverage of the protected equipment. These are required because the level of fire protection is more related to the water film thickness and water flow rate over the surface of the vessel than to the amount of water delivered to the nozzles.
- Empirical models that relate heat transfer to char thickness exist for intumescent PFP materials. There is increasing use of sophisticated models for PFP materials but these tend to be applied to specific products.

Guidance

- On the whole, the information given in the ‘Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire, SCI-P-112, 1992’, remains valid for all the fire scenarios which concern jet fires and pool fires in the open. However, an updating of the Interim Guidance Notes is under consideration. This should include the new information identified in this Section.
- More up-to-date guidance is required in relation to water deluge protection because current guidance is only for protection against pool fires. Although the guidance indicates that water deluge at the currently specified rates may not be suitable for protection against jet fires, there are scenarios in which it can be effective.
- The Jet-Fire Resistance Test is now widely used and the procedure is to be produced as a British standard. At present, the procedure only covers coating materials. However, it can be used to assess PFP systems such as cable transit systems. To allow for this, modifications are required to the procedure/British Standard.

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3. THERMO-MECHANICAL PROPERTIES OF STEELS

3.1 BACKGROUND

The thermo-mechanical properties of steels have been appraised in the Phase 1 reports (Appendix A, OTI 92 604/605/609) of the Blast and Fire Engineering project. Report package FR1 considers experimental data on the performance of steels and FR2 on methodologies and tools for the design/analysis of components at elevated temperatures. Work package FR6 considers fire performance of explosion damaged steelwork.

There are a number of national and international standards giving properties of steels used for pressurised systems. Each contains different grades of steel with different properties but there does not seem to be a consistent terminology. Sometimes the identifying numbers relate to yield and ultimate tensile strength, LT usually means tested for low temperature, and followed by a number which can indicate the test temperature. Often the materials are bought to the ASTM Standard but with project specific additional requirements.

The information below has been compiled from standards and vendor data. This section concentrates on the properties of steels most commonly used offshore for pressurised systems.

Two related projects are currently in progress viz.

- An ECSE project in the “Development of the Use of Stainless Steel in Construction”; and
- An HSE sponsored project on “Elevated Temperature and High Strain Rates Properties of Steels.

However, at the time of preparation of this report, no results from these projects had been published.

3.2 STEEL COMPONENTS

For this review, an offshore separator has been taken as a typical offshore pressurised system. A separator is comprised of the following main steel components:

- pressure vessel with nozzles and flanges (including bolts);
- pressure vessel supports;
- pipework with flanges (including bolts);
- pipework supports;
- valves; and
- pressure relief valves including flanges with bolts.

The above list covers the majority of the larger components of pressure vessel and piping systems. However, these systems always involve numerous other piping systems and structural details. For example, techniques such as the use of internally clad plate and the need to address the processing of sour product entail use of other standards and specifications.

3.3 MOST COMMONLY USED STEELS FOR PRESSURISED SYSTEMS

A sample of offshore design companies have indicated that the most commonly used steels are as follows:

- BS 1501-225-490B LT50;
- BS 1501-224-490LT;
- Carbon steel 516 Grade 70;
- 22 Cr duplex UNS S31803; and
- Stainless steel 316.

In addition to the type 316L stainless steels and duplex stainless steels, the use of higher alloys, e.g. alloy 28, 6 molybdenum superaustenitics, Incoloy 825 and Inconel 625 are becoming increasingly common. Reference (3-1) indicates that 22 Cr duplex UNS S31803 steel is also used under various names:

- 1.4462 EN international steel no;
- S 31803 ASTM international steel no;
- 2205 Avesta Sheffield steel name;
- 318S13 BS national steel designations, superseded by EN;
- 1.4462 DIN national steel designations, superseded by EN; and
- 2377 SS national steel designations, superseded by EN.

Based on the existing standards, these steels have the chemical compositions and ambient temperature properties shown in Table 3.1.

3.4 VARIATION OF PROPERTIES WITH TEMPERATURE

The Blast and Fire Engineering project, OTI 92 604 (Appendix A) examined the temperature dependent material properties for the various types of steels that were commonly found in offshore installations. Two broad groupings were identified, namely structural steels and ‘boiler’ steels used for pressure vessels and some piping. The type of treatment applied during the steel production process governs the strength and behaviour of that steel at elevated temperatures. Such treatments include normalising, quenching, tempering and stress relieving. If strength has been increased by heat treatment then it may be lost in the uncontrolled heating by a fire. Steels with enhanced properties at elevated temperatures (‘fire resistant’ steels) are presently available. These steels are more expensive than common structural steel and yet cheaper than the austenitic grades of stainless steel. For temperatures in the range 300 °C to 500 °C, ‘fire resistant’ steels maintain better strength characteristics than low carbon structural steels. Stainless steels have generally better fire resistance qualities than carbon steels in the temperature range 550 °C to 900 °C.

OTI 92 604 also elaborates on oxidation and oxygen starvation of steels exposed to elevated temperatures. Above about 400 °C, the rate of oxide formation on an exposed steel surface increases. This may be significant during a prolonged high temperature fire. However, relative to the loss of strength of the base steel, the loss of material thickness is negligible. In certain environments, particularly where oxygen is absent, there are a number of corrosion mechanisms that attack steel. Most of these mechanisms are accelerated at high temperatures, however, they affect only the steel surface.

Offshore pressure systems, which may be affected by fire, are normally fitted with emergency depressurisation systems. When activated, very rapid depressurisation occurs. This may lead to very low temperatures in some parts of the system. Steels used in these areas may have to withstand temperatures as low as ca. –50 °C as well as temperatures as high as 1300 °C. Some steels may become brittle at temperatures within this range and due account needs to be taken of this.

Table 3.1. Chemical compositions and mechanical properties at ambient temperature of steels for the pressure systems

Steel	Element		Nominal Plate Thickness (mm)		Tensile Strength (N mm^{-2})		Yield Strength (N mm^{-2})	Elongation (%)
	Description	%	Over	Up to and including	min.	max.	min.	min.
BS 1501-224-490LT	Carbon max. Silicon min. max. Manganese min. max. Phosphorus max. Sulphur max. Aluminium (metallic) min. Chromium max. Copper max. Molybdenum max. Nickel max.	0.22 0.10 0.40 0.90 1.80 0.030 0.030 0.015 0.25 0.30 0.10 0.75	3 16 40 63 100	16 40 63 100 150	490	510	325 315 305	21 21 20 19 19
BS 1501-225-490B LT50	Carbon max. Silicon min. max. Manganese min. max. Phosphorus max. Sulphur max. Aluminium (metallic) min. Niobium min. max. Chromium max. Copper max. Molybdenum max. Nickel max.	0.20 0.10 0.50 0.90 1.60 0.030 0.030 0.015 0.010 0.060 0.25 0.30 0.10 0.75	3 16 40 63 100	16 40 63 100 150	490	610	355 345 340	21 21 20 20 20
Carbon Steel 516 Grade 70	Carbon max. (according to thickness) Manganese (according to thickness and analysis type) Phosphorus max. Sulphur max.	0.27 to 0.30 0.85 to 1.30 0.035 0.035			485	620	260	17 to 21
22Cr duplex UNS S31803	Carbon Manganese Phosphorus Sulphur Silicon Chromium Nickel Molybdenum Nitrogen	0.030 2.00 0.030 0.020 1.00 21.0 to 23.0 4.50 to 6.50 2.50 to 3.50 0.08 to 0.20			630		460	25
Stainless Steel 316L (A.I.S.I.)	Carbon Manganese Phosphorus Sulphur Silicon Chromium Nickel Molybdenum	0.030 2.00 0.045 0.030 1.00 16.0 to 18.00 10.0 to 14.00 2.00 to 3.00			490		175	

Note: Carbon and manganese increase hardness (yield stress) and strength of the steel without affecting the high temperature properties.
Chromium and molybdenum enhance corrosion resistance, high temperature strength and creep resistance.
Nickel reduces the temperature at which ductile to brittle fracture occurs and so can be used at low temperatures without risk of brittle fracture.

The following mechanical properties at elevated temperatures should be considered for strength analysis of pressurised systems:

- modulus of elasticity;
- stress-strain curves;
- ductility; and
- creep.

The following thermal properties at varying temperatures should be considered for the thermal behaviour in fires:

- thermal expansion;
- thermal conductivity; and
- specific heat.

3.4.1 Pressure vessels steels

Mechanical properties

OTI 92 604 refers to BS 1501 (3-2) where Part 1 contains specifications for steels that are similar to the normal structural steels, but have guaranteed properties at elevated temperatures. Part 2 of the same standard contains specifications for alloy steels containing appreciable amount of chromium and molybdenum. These have the effect of improving the elevated temperature properties (Figure 3.14 of OTI 92 604 gives the reduction of yield stress for temperatures from 20 °C to 550 °C).

The variation of 0.2% proof stress with temperature (100 °C to 450 °C for carbon and carbon-manganese steels and 100 °C to 500 °C for alloy steels) is given in reference (3-3) for rolled steel for boilers, pressure vessels and special applications. European Norm EN 10028-2 (3-4) provides 0.2 % proof stress data, as a function of product thickness and temperature (range 50 °C to 400 °C).

Thermal properties

The thermal properties of steels to BS 1501: Part 1 and Part 2 may be assumed to be the same as for the common structural steels (OTI 92 604).

3.4.2 Stainless steel

Mechanical properties

In pressurised systems stainless steel is mainly used for pressure vessels and pipework. OTI 92 604 makes reference to BS 1501: Part 3 and gives yield stress reduction factors for Grades 316S31 and 304S31 for the temperature range from 20 °C to 1000 °C and 900 °C, respectively. Figure 3.12 of OTI 92 604 shows yield stress reduction factors for Grades 321S51, 316S31 and 304S51 for the temperature range from 20 °C to 700 °C. The three stainless steels perform differently at elevated temperatures and the degree of variation is greater than that which has been found for low carbon steels.

Work published in 1990 showed that the heat-resisting strength of stainless steel could be adversely influenced by the presence of hydrogen. A similar deleterious effect was recorded for hydrogen in stainless steels when the strain-rate effect was examined. Hydrogen is more likely to pass into the microstructure of stainless steels in pressure vessels.

Reference (3-5) gives the variation of 0.2% proof strength and ultimate tensile strength for UNS 31803 (or ASTM A240), also commonly called duplex 2205, for the temperature range from 20 °C to 800 °C.

Reference (3-1) includes typical data for duplex 2205 of modulus of elasticity in the temperature range from 20 °C to 300 °C. The minimum yield stress for duplex 2205 at ambient temperature is 460 to 480 N mm^{-2} but, during tests, this steel exhibited a yield stress of up to 600 N mm^{-2}. However, reference (3-1) does not include data on yield stress at temperatures higher than 300 °C. Other steel specifications from the same source include mechanical properties only up to 200 °C. Some reports on the behaviour of duplex steels at elevated temperature suggest that duplex steels may undergo a crystalline change, which may lead to embrittlement, at 280 °C when exposed to this temperature for more than 20 minutes.

BS1501 Part 3 (3-2) provides minimum proof stress data (both $R_{p0.2}$ and $R_{p1.0}$) at elevated temperatures up to 350 °C (318 duplex steel) and 700 °C (316 stainless steel).

Thermal properties

The variation of coefficient of thermal expansion, thermal conductivity and heat capacity of stainless steels are given in Table 3.4 of OTI 92 604 (Appendix A).

Reference (3-1) includes typical data for duplex 2205 for coefficient of linear thermal expansion, thermal conductivity and specific heat for the temperature range from 20°C to 300°C. This is summarised in Table 3.2.

Table 3.2. Thermal properties of DUPLEX 2205 steel

Temperature (°C)	Modulus of elasticity (kN mm^{-2})	Coefficient of linear thermal expansion ($x10^{-6}$ $°C^{-1}$)	Thermal conductivity (W m^{-1} $°C^{-1}$)	Heat capacity (J kg^{-1} $°C^{-1}$)
20	200	-	15	450
100	194	13	16	500
200	186	13.5	17	530
300	180	14	18	560

Like all duplex steels, 2205 is more prone to intermetallic precipitations than austenitic steels. Sigma phase formation may occur in the temperature range 700 °C to 975 °C, and embrittlement between 350 °C and 525 °C.

3.4.3 Structural steels (for vessel and pipe supports)

Mechanical properties

In order to obtain the material properties at elevated temperature there are two common tests available. A standard tensile test can be carried out at a specified elevated temperature. This is known as an isothermal test. Alternatively, a test can be carried out to monitor extension in which the steel is loaded and then heated whilst the load is maintained at a constant level. This test is known as an anisothermal test. The anisothermal test has been developed specifically for obtaining properties for fire calculations and it is not routinely used. It was used, however, as the basis for deriving the elevated temperature properties which are quoted in the European and British onshore codes. For normal structural steels it has been found that data obtained from an isothermal test method best describe the behaviour of steels in a fire. In an anisothermal test, the initial elastic behaviour of steel is underestimated. This is because an initial stress greater than the ‘room temperature’ yield stress cannot be applied. The effect of work hardening cannot be measured in an anisothermal test method.

Creep is time-dependent straining of a material. When materials are loaded to a creep sensitive level of stresses over a prolonged period of time, creep will occur. Creep causes deflections to increase although the applied load is kept constant. Onshore fire codes (3-6 & 3-7) ignore creep for up to 4 hours. When applied stresses are close to the yield point of a material, failure due to creep may occur in less than one hour.

OTI 92 604 recommends design properties at elevated temperatures. The strength quoted is based upon tensile tests. However, similar reductions in strength may be assumed for compression and shear. In OTI 92 604, the elevated temperature mechanical properties are based on British and European data and are as specified in the draft of EC3: Part 10 (April 1990) (3-7). These data are based on anisothermal test data supplemented with some isothermal test data.

The stress-strain curves for steel at elevated temperatures may be represented in four parts. These are:

- a linear elastic part;
- an elliptical transition part;
- a plastic part; and
- a linear reduction part.

The curve is illustrated in Figure 3.1 and the mathematical definitions of each part of the curve are given Table 3.3. Although not included in the draft of EC3, Part 10, a modification to take strain hardening into account has been proposed.

The proposed EC3, Part 10 data (3-7) are extremely close to the British Steel data given in BS 5950 Part 8. In BS 5950 Part 8 the strength of steel at elevated temperatures is given in terms of a strength reduction factor to be applied to the normal design strength. This factor is given for strains of 0.5%, 1.5%, and 2% for a range of temperatures.

The EC3 and BS 5950 Part 8 data are based upon a heating rate of 10 °C per minute. British Steel have compared steel strengths measured at different heating rates and propose a modification to take account of other heating rates. The modification is applied to the temperature when calculating the effective yield and is given in Table 3.3 of OTI 92 604. This is a method for allowing for the small effects of creep in fire calculations.

It should be noted that the correction table applies to heating rates tested for cellulosic fire requirements. OTI 92 604 states that no comparable table is available for offshore hydrocarbon fires and that no data on heating rates faster than 20 °C min^{-1} are available.

OTI 92 605 gives a stress-strain relationship based on the source data of BS 5950: Part 8. The stress-strain is made independent of time by including an allowance for creep effects.

Reference (3-1) gives fractions of modulus of elasticity and yield stress in respect of temperature as extracted from OTI 92 604. A technical note to the Interim Guidance Notes (Appendix A) gives an example of yield stress, ultimate tensile strength and percent elongation in respect of testing temperature in the range from room temperature to 1000°C for Grade 43A steel (the steel for which there are most data). These are given in Table 3.4.

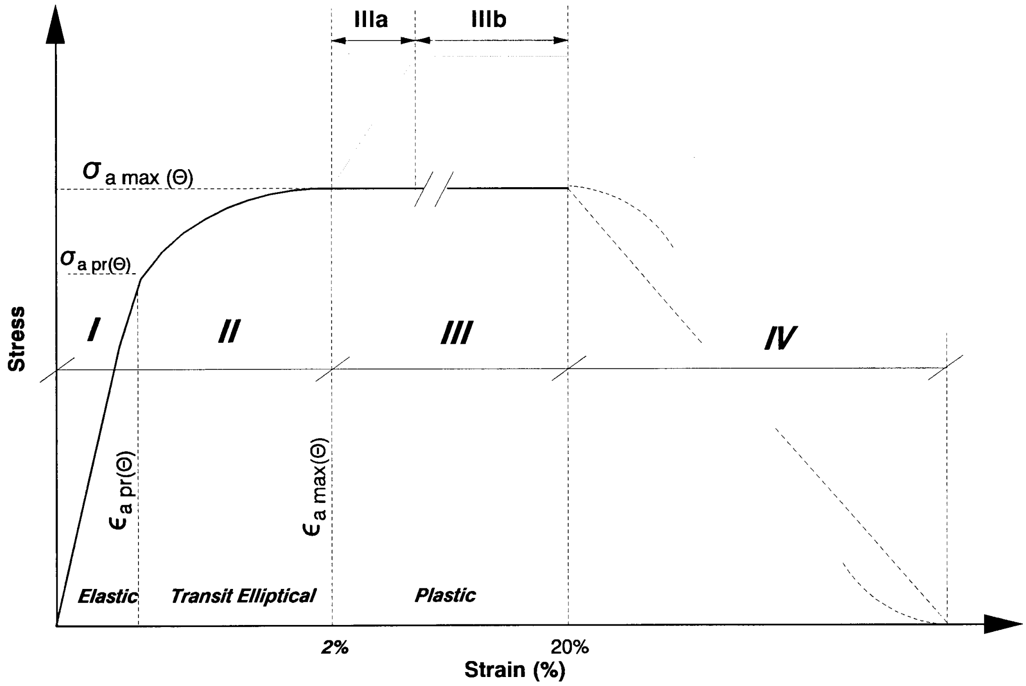

Figure 3.1. Idealisation of stress-strain curve at a particular temperature

Table 3.3. Formulae for stress-strain curves (the symbols are defined in Figure 3.1)

Range	$\sigma_{a(\theta)} =$	$\bar{E}_{a(\theta)} =$
I elastic	$\bar{E}_{a(\theta)} \cdot \varepsilon_{a(\theta)}$	$\bar{E}_{a(\theta)}$
II transit elliptical	$\frac{b}{a}\sqrt{a^2 - (\varepsilon_{a\,max(\theta)} - \varepsilon_{a(\theta)})^2} + \sigma_{a\,pr(\theta)} - c$ with $a^2 = \frac{\bar{E}_{a(\theta)}\,(\varepsilon_{a\,max(\theta)} - \varepsilon_{a\,pr(\theta)})^2 + c(\varepsilon_{a\,max(\theta)} - \varepsilon_{a\,pr(\theta)})}{\bar{E}_{a(\theta)}}$ $b^2 = \bar{E}_{a(\theta)}\,(\varepsilon_{a\,max(\theta)} - \varepsilon_{a\,pr(\theta)}) \cdot c + c^2$ $c = \frac{(\sigma_{a\,max(\theta)} - \sigma_{a\,pr(\theta)})^2}{2(\sigma_{a\,pr(\theta)} - \sigma_{a\,max(\theta)}) + \bar{E}_{a(\theta)}(\varepsilon_{a\,max(\theta)} - \varepsilon_{a\,pr(\theta)})}$	$\frac{b(\varepsilon_{a\,max(\theta)} - \varepsilon_{a(\theta)})}{a\sqrt{a^2 - (\varepsilon_{a(\theta)} - \varepsilon_{a\,max(\theta)})^2}}$
III plastic	$\sigma_{a\,max(\theta)}$	0

$\bar{E}_{a(\theta)}$ = Initial Young's modulus at temperature θ
$\sigma_{a(\theta)}$ = Stress at temperature θ
$\varepsilon_{a(\theta)}$ = Strain at temperature θ

Table 3.4. Example of mechanical properties at elevated temperature for grade 43A steel

Testing temperature (°C)	YS or 0.2% PS ($N\,mm^{-2}$)	1% PS ($N\,mm^{-2}$)	TS ($N\,mm^{-2}$)	Elongation (%)
20	247	232	445	36
125	229	232	530	-
210	245	232	530	14
330	223	232	478	30
400	196	232	394	31
500	168	192	246	35
520	141	174	220	36
600	103	117	124	48
600	102	119	128	52
700	50	52	53	76
710	48	53	55	66
800	36	39	54	51
900	29	24	39	68
1000	19	23	25	75
1000	17	25	25	68

BS 5950 gives strength reduction factors for yield stress at 0.5, 1.5 and 2.0 for steel complying with grades 43 to 50 of BS 4360 in the temperature range between 100°C and 1300°C. The values given in Table 3.5 may be applied to tension, compression or shear.

Table 3.5. Strength reduction factors for steel complying with grades 43 to 50 of BS 4360

Temperature °C	Strength reduction factors at a strain (in %) of:		
	0.5	**1.5**	**2.0**
100	0.97	1.00	1.00
150	0.959	1.000	1.000
200	0.946	1.000	1.000
250	0.884	1.000	1.000
300	0.854	1.000	1.000
350	0.826	0.968	1.000
400	0.798	0.956	0.971
450	0.721	0.898	0.934
500	0.622	0.756	0.776
550	0.492	0.612	0.627
600	0.378	0.460	0.474
650	0.269	0.326	0.337
700	0.186	0.223	0.232
750	0.127	0.152	0.158
800	0.071	0.108	0.115
850	0.045	0.073	0.079
900	0.030	0.059	0.062
950	0.024	0.046	0.052
NOTE 1 Intermediate values may be obtained by linear interpolation. NOTE 2. For temperatures higher than the values given, a linear reduction in strength to zero at 1300 °C may be assumed.			

Reference (3-8) gives the reduction factors for yield stress and modulus of elasticity for structural steel for the temperature range between 0 °C and 600 °C for the steel heating rates of 4 to 10 °C per minute. For faster heating rates, the reduction factor curves are quoted as conservative.

Reference (3-9) gives the temperature variation of yield stress and modulus of elasticity in accordance with ECCS. The following formulae are used:

Yield strength reduction factor:

$$\frac{\sigma_{y(T)}}{\sigma_{y(20)}} = 1 + \frac{T}{\left(767 \ln \left(T/1750\right)\right)} \qquad for \;\; 0 \le T \le 600\,°C$$

$$\frac{\sigma_{y(T)}}{\sigma_{y(20)}} = 108 \frac{\left(1 - \frac{T}{1000}\right)}{\left(T - 440\right)} \qquad for \;\; T > 600\,°C$$

Reduction factor for modulus of elasticity:

$$\frac{E_{(T)}}{E_{(20)}} = 1 + 1.59\,x10^{-4}\,T - 3.45x10^{-6}\,T^2 + 1.18x10^{-8}\,T^3 - 1.72\,x10^{-11}\,T^4 \quad for \;\; 0 \le T \le 600\,°C$$

$$\frac{E_{(T)}}{E_{(20)}} = 69\frac{\left(1-\frac{T}{1000}\right)}{T-440} \qquad for\ \ 600°C < T \le 1000°C$$

where:

$\sigma_{y(T)}$ is yield stress at temperature T °C
$\sigma_{y(20)}$ is yield stress at 20 °C
$E_{(T)}$ is modulus of elasticity at temperature T °C
$E_{(20)}$ is modulus of elasticity at 20 °C
T is temperature °C

Thermal properties

In accordance with OTI 92 604, the coefficient of linear expansion may be taken as 14 x 10^{-6} per degree centigrade in the range up to 750 °C. A graph illustrating the thermal elongation of steel is shown in Figure 3.6 of this reference.

3.4.4 Welds and bolts

Mechanical properties

Little information, relating to the elevated temperature properties of welds or bolts, is available. The yield-strength reduction factors for welds and bolts are given in BS 5950: Part 8.

OTI 92 609 (Appendix A) states that bolts do not behave very well in fires, and the higher the bolt specification, the poorer the fire enduring qualities. The loss of strength of Grade 4.6 bolts follows that of Grade 43 steel. For 8.8 bolts, the strength reduces after exposure to temperatures above 450 °C, being 80% at 600 °C and 60% at 800 °C.

Thermal properties

No reference, from 1991 or later, to thermal properties of welds and bolts has been found.

3.5 AVAILABLE DATA

The available data are marked in Table 3.6. The data related to the unmarked cells in Table 3.6 should be a subject to additional research and tests, and all data should be provided from –50 °C to 1300 °C.

It should be noted that not all of the material property data are available. For example, data that characterise ductility are missing. It is important that, for the survival of a pressurised system affected by a fire, the values of the properties related to functionality and survivability are known for the duration of the exposure. This includes the possible change of the crystalline structures of the steels that may be occur at specific temperatures during the heating-up process.

In addition, the variation of properties with sub-zero temperatures should be included for all the properties mentioned in this report. This is because the pressurised equipment may experience such temperatures during a rapid depressurising.

Table 3.6. Availability of thermo-mechanical properties for selected steels

Properties	**Steels**				
	BS 1501-225-490B LT50	**BS 1501-224-490LT**	**Carbon Steel 516 Grade 70**	**22 Cr duplex UNS S31803**	**Stainless steel 316**
Thermal properties					
Coefficient of thermal expansion	X(5)	X(5)	X(5)	X(6)	X(4)
Thermal conductivity	X(2)	X(2)	X(2)	X(5)	X(4)
Specific heat	X(1)	X(1)	X(1)	X(5)	X(4)
Mechanical properties					
Modulus of Elasticity	X(1)	X(1)	X(1)	X(5)	
Shear Modulus					
Poisson's Ratio					
Yield Stress	X(3)	X(3)	X(1)	X(4)	X
Ultimate Tensile Strength				X(5)	
Rupture Strain (Elongation)				X(7)	

NOTES

The properties are for temperature range from 20 °C to 1000 °C or greater unless specifically noted. All carbon steels have the same thermal properties.

X(1) The coefficient of thermal expansion, thermal conductivity and specific heat are the same for carbon steels.
X(2) For low carbon steel in general, not specific for this steel.
X(3) For Grade 223(490)(BS 1501) and only for temperature range from 150 ^{o}C to 450 ^{o}C.
X(4) Only for temperature range from 20 ^{o}C to 800 ^{o}C.
X(5) Only for temperature range from 20 ^{o}C to 300 ^{o}C.
X(6) Only for temperature range from 100 ^{o}C to 300 ^{o}C.
X(7) Only for 20 ^{o}C.

3.6 CONCLUSIONS

The following conclusions are made:

New and missing data

- The following steels have been identified as the most commonly used for pressurised systems such as an offshore separator: BS 1501-225-490B LT50, BS 1501-224-490LT, Carbon steel 516 Grade 70, 22 Cr duplex UNS S31803 and stainless steel 316.

- Although property data at ambient temperature are available, high and low temperature data are not available for some of the steels. Low temperature data are required if excessive cooling occurs during emergency depressurisation and high temperature data are required if the system is to be designed to withstand a significant fire loading.

Models

- The following thermal and mechanical properties of steel at elevated temperatures are required for the modelling of the thermo-mechanical response of pressurised systems: coefficient of thermal expansion, thermal conductivity, heat capacity, modulus of elasticity; shear modulus, Poisson's ratio, and stress-strain curves (or yield stress, ultimate tensile strength and rupture strain (elongation)).

Guidance

- Some performance standards require revision to incorporate the necessary property data at high and low temperatures. It is important for the survival of a pressurised system affected by a fire that all the properties related to functionality and survivability are such that they meet the criteria of performance standards throughout the entire survivability time.

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4. PREDICTION OF THERMO-MECHANICAL RESPONSE

4.1 BACKGROUND

Structural response is considered in three of the Phase 1 Blast and Fire Engineering project reports (Appendix A, OTI 92 608/609/610) viz.

- OTI 92 608 - Existing Fire Design Criteria for Secondary, Support and System Steelwork;
- OTI 92 609 - Fire Performance of Explosion Damaged Structural and Containment Steelwork; and
- OTI 92 610 - Thermal Response of Vessels and Pipework Exposed to Fire.

OTI 92 608 reviews the available information on the design of secondary steelwork when subjected to fire. Vessel supports and other structures which support such services are considered under secondary steelwork in this reference. Little information was identified which directly related to fire design criteria. Fire-resistant strategies should be considered for secondary steelwork since its failure could lead to the escalation of an incident. OTI 92 608 suggests that, almost without exception, secondary steelwork had not previously been designed to withstand fire loads.

The second report, OTI 92 609, elaborates on the combined effects of fire and explosion with emphasis on structures. It outlines a solution procedure that may be applicable to the analysis of pressurised systems but, to make it useable for emergency depressurisation response, the time effects of depressurising would need to be incorporated.

In the third report, OTI 92 610, failure mechanisms of pressurised systems are considered. The pressurised equipment will fail, i.e. rupture, when it is subjected to a stress in excess of the strength of the material from which it is fabricated. For a thin-walled vessel or pipeline under normal conditions, this is usually taken as the ultimate tensile strength. This simple statement is, however, difficult to put into practice with a vessel subject to a non-uniform temperature distribution. This is because thermal stresses are created in addition to the stresses caused by the internal pressure, and also results in material of variable strength over the surface of the vessel.

As a first approximation, one might expect the vessel to fail at the point at which the total superposed stress – pressure and thermal – exceeds the material strength. In practice the plastic deformation and hence stress relaxation that will have occurred before failure is reached will make this calculation difficult.

The effect of plastic deformation will be particularly noticeable if the vessel or pipe has locally high stresses, or equivalently, local regions of low strength. Thus, the effect of a fully engulfing pool fire will be very different from that of localised fire engulfment. Little quantitative information is available as to the effect of this, and as a consequence, stress is usually calculated on the basis of elastic behaviour.

4.2 SUPERIMPOSED PRESSURE AND THERMAL STRESS

Provided the stresses remain elastic, the determination of pressure stress is a well established procedure and is the basis of the pressure vessel design codes, such as BS 5500 (4-1). Some vessel designs may be too complex for these codes, in which case finite element techniques will be required, but this is an increase in computational complexity rather than a change in procedure.

The determination of thermal stresses for a vessel which remains elastic in situations in which there is a substantial degree of symmetry (e.g. a pressure vessel in which the inside temperature differs from the outside, but with no temperature variation over the surface), is well established and analytic solutions are often possible.

Such solutions, however, are rarely available when there is significant temperature variation across the surface. This latter case is expected to be the norm where the temperature gradient is caused by flame impingement or by a pool fire under a large vessel. Solution of these cases is possible by finite element techniques, often using the same computer program as is used for the pressure stress calculations.

The relative magnitude of thermal stresses and pressure stresses varies enormously. For thin-wall vessels, the thermal stress caused by a temperature difference through the wall is relatively small. However, temperature differences over the surface of the wall can lead to significant stresses.

In OTI 92 610, a simple finite element model of a horizontal tank, containing a volatile liquid subjected to a pool fire and held at constant pressure through the action of a pressure safety valve, gave a maximum thermal stress (which was in the axial direction) some 3 to 4 times the hoop stress due to pressure. The large thermal stresses arose because the lower part of the vessel was held at essentially constant temperature (through a high heat transfer coefficient to the boiling liquid), while the temperature of the upper part of the vessel increased rapidly. This gave rise to a large difference in thermal expansion between the lower and upper zones, and, hence, high thermal stress. If, on the other hand, the vessel had contained only gas, the axial thermal stress would have been quite small compared with the hoop stress arising from the internal vessel pressure.

The wide variation in the relative magnitude of thermal and pressure induced stresses makes it difficult to make generalisations because if the direction of maximum stress changes, so also would one expect the direction of initial fracture to change, hence leading to a different mode of failure. The examples above are the two extremes; there will be many intermediate cases which will be very difficult to quantify without a full finite element analysis.

The distribution of stresses in pressure vessels, used as part of offshore pressurised systems, is as follows:

Due to internal pressure:
- Hoop, radial and longitudinal stress;
- Increased stress concentrations at nozzles, supports and other attachments (stress risers).

Due to thermal gradients:
- In the radial direction through the vessel shell;
- In the circumferential direction at the liquid and vapour interface;
- In the longitudinal direction if the vessel is only partially heated;
- In areas where thermal gradients are caused by different thickness of the material (different thermal mass) as thicker components heat up more slowly than thinner ones.

Due to external loads:
- from pipework; and
- from the supporting structure.

Due to creep:
- at stress levels close to the yield stress and at high temperatures.

4.3 EXISTING GUIDANCE ON FAILURE OF PRESSURE VESSELS AND PROCESS PIPING

The only guidance currently available that directly relates to prediction of vessel and piping failure is given in reference (4-2). This reference considers determination of protection of pressure vessels and process piping in the following five steps:

1. Identification of fire types.
2. Effect of fire water.
3. Heat flux values for depressurising/rupture calculations.
4. Depressurising/rupture calculations.
5. Evaluation of failure mode.

The depressurising/rupture calculations are to be performed for each major pressure vessel and piping segment, establishing internal pressure fluctuation, wall material temperature and residual strength, as a function of time. This includes the determination of whether rupture will occur during depressurising and the identification of time to rupture.

In Step 5, for areas where the pressure vessels and piping segments withstand the fire without rupture (for the time required to depressurise all pressure vessels and piping systems to 50% of initial pressure or 4.5 barg whichever is lowest), the protection is considered acceptable.

If a rupture occurs before this state of depressurisation has been reached, an acceptance of the situation will have to be judged based on the risk analyses. Residual quantities and escalation potentials, both within the area and towards adjacent areas, are to be evaluated. When rupture cannot be accepted, i.e. the risk acceptance criteria are not met, the provision of additional protective systems and arrangements is to be implemented. This can be:

- Upgrading of active fire water systems so that credit from fire water can be taken.
- Application of passive fire protection that will reduce the heat loads to the exposed pressure vessels/piping.
- Modifications to pressure vessel/piping design (material, wall thickness, etc.).
- Modifications to the general arrangements that have an impact on the time to rupture.
- Change from manual to automatic depressurisation.

The reference does not elaborate on what methods may be applied for the rupture calculations or which rupture criteria may be used.

4.4 FAILURE IN PRESSURE SYSTEMS

4.4.1 Vessels and pipes

Reference (4-3) suggests that maintenance of the integrity of the pressure system and avoidance of loss of containment is the essence of the loss prevention problem. It is necessary, therefore, to consider the failures that occur in pressure systems. Of particular importance are catastrophic failures in service and failure in a fire. Mechanical failure is not included in this document. Failures of pressurised systems in a fire may be accelerated by deterioration of the system during normal operation.

Pressurised equipment exposed to fire will experience an increase in temperature. This may result in a time-varying stress and it is expected that, once this time-dependent maximum stress equals the material strength, the equipment fails. However, the material strength is itself decreasing in time as the material weakens with increasing temperature.

OTI 92 610 indicates that, for carbon steel vessels or pipes built according to the usual codes (with a burst pressure at normal temperature some 2.5 to 4.0 times the maximum working pressure and operating at the maximum working pressure), the reduction in strength is such that failure would be expected at around 500 to 550 °C if thermal stresses are negligible. If the vessel is operating below its maximum working pressure, the failure temperature is correspondingly higher, for example, if the pressure is 50% of the maximum working pressure, failure might be expected at 550 to 600 °C.

The time for failure to occur depends on the severity of the fire, the extent and type of fire protection, and the pressure response of the vessel or pipework (including the emergency depressurisation system). This can vary between a few minutes and a few hours. Consequently, the relevant strength criterion is more appropriately taken as the creep rupture strength, rather than the short term tensile strength since, as the time to rupture goes to zero, the creep rupture strength becomes equal to the short term tensile strength. The latter is just a particular case of the former.

The use of creep rupture stress does not imply the presence of significant creep strain at failure. For the time periods of interest, the creep strain is likely to be small, but the creep rupture stress may well be significantly lower than the short term tensile stress.

For a given vessel or pipe made of a given material at a fixed internal pressure (or, more accurately, stress) and temperature, the time to failure due to creep rupture can be determined by the Larson-Miller method (OTI 92 610). This method is based on the assumption that creep is a rate process governed by an Arrhenius-type equation and the experimental observation that time to rupture is inversely proportional to creep strain rate.

For a vessel or pipe exposed to fire, the temperature will not, however, be constant in time. While there is no properly validated way to account for this, a common and acceptably accurate way is to use the Robinson Method (or Life Fraction Rule) (OTI 92 610). This is based on the assumption that the time to failure resulting from the overall pressure-temperature history of the vessel can be related to experimental failure times under particular values of pressure and temperature for a vessel of similar construction.

In view of the complexity of creep rupture calculations, the short term tensile failure criterion is often used, in which failure is presumed once the stress level reaches the short term ultimate tensile strength. Further information on creep is given in reference (4-4).

The overestimation of time to failure is probably not too important if the rate of temperature rise of the vessel or pipe is high. In this case, the final stages of the failure process are likely to be extremely fast, since strength decreases very rapidly with temperature when the material is hot. However, for slower temperature rises, for example where failure takes an hour or more, the creep rupture stress criterion should always be used unless one is sure that this is not necessary.

Finite element analysis, based on observations of pressure vessels involved in a fire, indicates that the failure of pressurised systems appears to be a combination of a substantial yield at a level of the ultimate stress and a fast propagation of a structural failure. An example of the application of finite element analysis is given in the case study in Section 8.

4.4.2 Flange connections

Reference (4-5) describes experiments to determine the thermal response of flange connections, the time to loss of tightness and failure modes during jet-fire attack. The tests established that the tightness of a flange connection may be lost and new leaks formed between 1 and 8 minutes after the start of the fire. An asymmetric temperature distribution develops in the flange connection even in an engulfing jet flame, the downstream side of the flange being hotter. The loss of tightness was attributed, in all cases, to the same cause viz: the decrease of the contact pressure because the temperature induced expansion of flange bolts was higher than that of the flanges. Moreover, because of the thermal gradient in the flange connections, the bolts elongate differently and the leaks occur in the areas with higher temperature. It was concluded from this work that:

- Standard tests according to API and British Standards provide no real information on the loss of tightness in real fire scenarios of jet-fire impingement.
- In the tests, the elongation of the bolts remained in the elastic range.
- The sealings showed little or no damage, and after cooling down at the end of the tests some tests samples even re-gained their tightness.
- In a real fire case the loss of tightness would lead to a damage of the sealings as the leaks would ignite and damage the sealings.

4.4.3 Vessel supports

Vessel supports, sometimes also termed as saddles, are structures that are fabricated of plates. They may fail by:

- local plate buckling that may result in the overloading of the remaining part of the saddle with an excessive deflection as a result, or
- global failure of the saddle where there is no capacity for load shedding, resulting in an excessive deflection.

Either case would result in a downward displacement of the vessel and large strains on flanges and valves, leading to escalation through further leaks and fires. Global failure of saddles may be assessed using the section factor approach (see below). However, it should be noted that saddles normally rest on primary or secondary steelwork.

The time endurance of a steel structure can be defined as the length of time the structure will maintain its strength and stability in a fire. Secondary steelwork may be selected purely on a basis of section shape and size. Such an approach contrasts with primary steel, which is often selected on the basis of a computed stress utilisation. In a fire the performance of secondary steel will tend to be dominated by section factor effects or, at least, from a comparative standpoint the section factor will be more important for secondary structures than for primary structures.

The section factor H_p/A (i.e. the controlling parameter that determines the rate of temperature rise in a steel member) is the ratio of the heated 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}. Typical values of section factors range from 60 m^{-1} to 240 m^{-1} for universal beams of size 914 x 419 mm to 305 x 102 mm, respectively. Sections with a low H_p/A factor heat at a slower rate that sections with a high section factor.

Time endurance of steel members also depends upon how much mechanical load the element is carrying. An unloaded beam will generally last longer than a heavily loaded column. OTI 92 608 indicates that is common practice to assume that beams fail at 550 °C and columns fail at 450 °C if they are made from ordinary structural grades of steel. This rule of thumb assumes a working stress level corresponding to a unity check (unity check = the member stress as a fraction of allowable stress) of 1.00 under *in situ* loads.

OTI 92 608 suggests that, when large deflection or plastic analysis is undertaken, different criteria are required and it is appropriate that such criteria have a 'limit state' component in their description. In fire analysis, it has been suggested that a beam central deflections of L/10 constitutes a *de facto* collapse. Smaller deflections, although appropriate for elastic design methodologies, would lead to inefficiency in the design of fire resistant structures. A central beam deflection of L/30 is used as one criterion for failure in BS 476 (4-6) fire testing; the value L/30 appears conservative.

System steelwork is used to form and hold up the process system. The fire design requirement for system steelwork is simply to ensure that failure does not promote the escalation of fire. With pressurised piping systems the contained fluid, particularly if a gas, is likely to have a low weight per unit length of pipe compared to the pipe self-weight. The pipe supports play a key role in the maintenance of integrity of the pipework system. Valves, joints and other pipework fittings can rupture or leak if subjected to large strains, which could develop if one or a number of pipe supports were to fail. Bolted fittings themselves will need to be examined as their performance in a fire could possibly be short-lived; if a bolt is under tension at ambient temperatures then it will go slack if heated. Sealing devices in valves may break down at elevated temperatures.

The fundamental importance of the fire resistance of the vessel support in breaking the chain of fire escalation has been realised for some time and thus has been a key consideration in the design of safe platforms for many years. Vessel supports take on many shapes and forms. A large vessel may have immediate supports connecting it to the structural framework. OTI 92 608 suggests that supporting deck beams should be given the same level of fire protection as those which are in immediate contact with the process vessel.

4.4.4 Effect of fire on relief devices

Parry (4-7) summarises the available knowledge on the effect of fire on the operation of relief devices. He notes that the effect of fire on relief devices has not been well documented, although there have been many opportunities to inspect relief devices after a fire. He argues that the following theoretical considerations should be noted.

Safety valves

Fire produces thermal expansion and distortion. The thermal expansion of a helical spring above its normal operating temperature will tend to reduce its tension and hence lower the set pressure, so the effect tends to be in a safe direction. On the other hand, unequal thermal expansion can distort the valve spindle leading to jamming of the valve and a possible reduction in the discharge capacity. Fortunately, the small size of a safety valve in comparison with the size of a serious fire will tend to equalise temperature throughout the valve and minimise distortion.

Bursting discs

Similarly, the effect of high temperature on a bursting disc is to lower the bursting pressure or destroy it altogether and hence the tendency is in a safe direction.

4.5 COMPARISON OF LPG TANK FAILURE DATA WITH SIMPLE PREDICTED METHODS

4.5.1 Hydrocarbon pool fires

Moodie (4-8) describes a kerosene pool fire trial on 40% filled 0.25 tonne propane tank. Due to a pressure relief valve jamming shut, the vessel ruptured at an internal pressure of 35 bar and maximum wall temperature of 600 °C. The vessel ruptured along the top and around the two end caps opening the vessel out. Metallurgical examination revealed no indication of failure due to cracks and was consistent with failure by hoop stress in the wall. From thick-walled cylinder theory (4-9), Moodie used the following expression:

$$P_b = \frac{2}{\sqrt{3}}\sigma_y A \ln K$$

$$A = 2 - \frac{\sigma_u}{\sigma_y} \qquad for \ 0 < T < 700$$

$$A = 1 \qquad for \ T > 700$$

Where:

K	is the ratio of the outside and to the inside diameter
P_b	is the burst pressure
T	is the metal temperature °C
σ_u	is the ultimate tensile strength at temperature T
σ_y	is the yield strength at temperature T

If the wall temperature in this test is taken as 600 °C, the burst pressure calculated by the above equation is 38 bar, which is within 8% of the observed value.

4.5.2 Flashing liquid propane jet fires

As indicated in Section 6, HSL performed flashing liquid propane jet-fire trials taking 2 tonne propane tanks to failure (4-10 and 4-11). The tanks were fitted with PRVs with a set pressure of 17.24 barg and initially contained 20%, 41%, 60% and 85% (by volume) propane. When heated by the jet fire:

- the temperature of the tank walls increased to between 700 and 870 °C at failure;
- the yield and tensile strength of the vessel walls decreased (see Figure 4.1);
- for all but the 20% full tank, after PRV opening (18.1 - 18.8 barg) the pressure continued to increase after an initial 3 bar drop until tank failure in the range 18.6 to 24.4 barg; and
- as the walls were heated and stressed, they progressively lost strength until a crack was formed. Since the tanks were severely weakened, the crack propagated rapidly leading to catastrophic failure of the tanks after no more than 5 minutes fire engulfment.

Subsequently to the trials, samples were cut from the bottoms of the tank, below the liquid level, where it was known that the steel temperature had not exceeded 300 °C. A set of 0.2% proof stress and ultimate tensile stress measurements, from ambient to 900 °C, were taken from the steel from the 60% full tank. Check measurements were made at 500 °C and 700 °C on the steel from the other three tanks. The results for a 60% full carbon steel tank are illustrated in Figure 4.1.

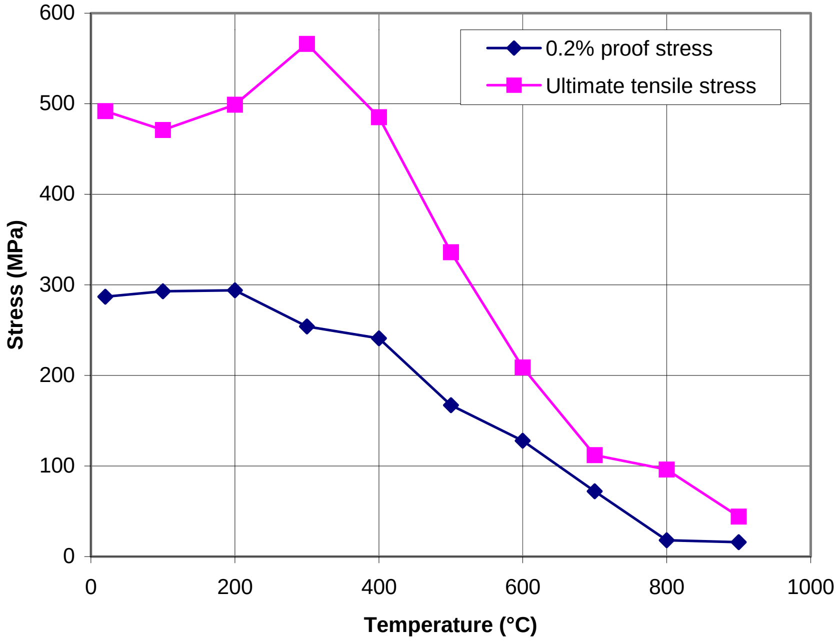

Figure 4.1. 0.2% Proof stress and UTS for 60% full tank steel

Melhem et al (4-12) suggests that in the cases where the tanks failed because of longitudinal forces, then the following simple relationship can be used to predict the burst pressure.

$$P_{burst} = 2 . t \sigma_y . / r$$

where t = wall thickness (m),
σ_y = yield strength at shell temperature (MPa); and
r = tank radius.

The calculated (Melhem and Moodie methods) bursting pressures, using the measured elevated temperature steel strengths, are compared with the measured bursting pressures in Table 4.1.

Table 4.1. Predicted and measured bursting pressures

Fill level	Max. wall temp. (^oC)	0.2 % yield stress at wall temper -ature (MPa)	Ultimate tensile stress at wall temper-ature (MPa)	Wall thick-ness (mm)	Outside diameter (m)	Predicted bursting pressure (bar)			Measured bursting pressure (bar)
						Melhem	Moodie		
							A=1	A > 1	
41%	704	45	118	7.5	1.2	11.3	6.5	10.6	21.3
60%	821	18	96	7.1	1.2	4.3	2.5	4.5	18.6

Both expressions considerably underestimate the actual bursting pressure. The measured steel strengths indicate that the 0.2% proof stress was much less than the ultimate tensile strength at temperatures of 700 °C and above and therefore it may not be valid to take A=1 at ≥ 700 °C.

The evidence suggests that vessels operating at modest pressures are most vulnerable due to their thinner walls. The walls of very high-pressure vessels provide such a large thermal mass that even severe fires should not cause the shell of the vessel to fail.

4.6 EXISTING PRESSURE VESSEL CODES AND STANDARDS

The ASME Boiler and Pressure Vessel Code (4-13) and BS 5500 (4-1) have been reviewed for methods for prediction or acceptance criteria for pressurised systems affected by accidental fire. Reference (4-11) has been found to be more comprehensive than reference (4-1). None of the references give guidance for prediction of the stress response of pressurised systems affected by fire. ASME VIII, Division 3 includes KD-112 and KD-113 which may be used as a basis for the development of such guidance for pressurised systems affected by accidental fire. These are considered as follows.

4.6.1 KD-112 Basis for design temperature

When the occurrence of different metal temperatures during operation can be definitely predicted for different axial zones of the vessel, the design of the different zones may be based on their predicted temperatures.

When the vessel is expected to operate at more than one temperature and under different pressure conditions, all significant sets of temperature and coincident pressure should be considered.

The metal temperature under steady operating conditions may vary significantly through the thickness. The temperature used in the design should be not less than the mean temperature through the thickness of the part being examined under the set of conditions considered. If necessary, the metal temperature should be determined by computations or by measurements from equipment in service under equivalent operating conditions. However, in no case should the temperature at any point in the metal, or the design temperature, exceed the maximum temperature in the yield strength tables in Section II, Part D for the material in question or exceed the temperature limitations specified elsewhere in this Division, except as provided in KD-113.

In vessels exposed to repeated fluctuations of temperature in normal operation, the design should be based on the highest fluid temperature, unless the designer can demonstrate by calculation or experiment that a lower temperature can be justified.

For determination of the fracture toughness to be used in the fracture mechanics evaluation, the minimum design metal temperature at the point of interest shall be used. The lower limit of the metal temperature during the hydrostatic test is given in KT-320.

It is the responsibility of the designer to specify the anticipated temperature of the overpressure relief device.

4.6.2 KD-113 Upset conditions

Sudden process upsets, which occur infrequently, can cause local increases or decreases in metal surface temperature. For the purpose of the static pressure design requirements, no credit should be taken for that portion of the wall thickness, which is predicted to exceed the maximum temperature permitted in the material's yield strength table. The minimum metal surface temperature, which occurs during sudden cooling, should be considered in the fracture toughness evaluations. A complete stress and fracture mechanics analysis is required for any credible upset condition.

The ASME documents also cover the design of flanges, bolts, openings, closures, heads, seals, attachments, supports, external heating, cooling jackets, layered vessels, wire wound vessels, interlocking strip wound vessels, welded vessels and pressure relief devices.

4.6.3 Other analytical and experimental work

Project work has been recently undertaken in Norway by Petrell a.s., where a possible failure of a separator was assessed by a combined analysis of effects of a jet flame, temperature, temperature-dependent thermo-mechanical properties and stress.

A hydrocarbon jet flame was simulated using Computational Fluid Dynamics. Heat transfer properties between the flame and the vessel shell outer surface and between the vessel shell inner surface and the hydrocarbon liquid and vapours/gas were determined semi-empirically using dimensionless parameters such as Reynolds and Nusselt numbers.

A 3-dimensional transient temperature field was calculated in the vessel shell using the temperature-varying thermal conductivity and specific heat for steel S31803 as shown in Section 3 (the vessel material was 2205, Grade S31803).

The hydrocarbon liquid and vapours/gas inside the separator was heated up by the fire through the vessel wall and the flashing of the liquid was calculated together with the corresponding increase of the internal pressure in the vessel. (The vessel that was assessed was designed to the operating pressure of 13 bar, where pressure safety valve (PSV) should open at 20 bar. The analysis was undertaken for a scenario where the depressurising (blow-down) system of the separator was fully activated at the onset of the fire. The PSV and the depressurising system were designed according to API 521.)

Simplistic formulae for a cylinder with end heads obtained from reference (4-14) were used to calculate the applied stress in the vessel. The stress was compared with the temperature varying yield stress for steel S31803 shown in Section 3. The applied stress exceeded the yield stress before the PSV opened; i.e. the vessel failed before a part of the safety system designed according to API was activated. Another stress check was carried out using the formulae in Lees (4-3), described in Section 4.5.1. Again, the vessel failed before the PSV opened.

A number of additional references that address analytical and experimental studies of combined thermal and stress effects on pressurised systems have been reviewed (4-15 to 4-18). The conclusions from the data found are incorporated in the general conclusions to this section and support the view that the response of pressurised systems in accidental fires has not been adequately addressed.

4.7 CONCLUSIONS

The following conclusions are made:

New and missing data

- The distribution of stresses in pressure vessels is due to internal pressure, thermal gradients, external loads and creep.
- The data suggest that the most probable failure mechanism is as follows:
    - At elevated temperatures, the combination of mechanical stress, thermal stress and stress concentrations due to stress risers and the associated strains lead to a local exceedence of the ultimate tensile strength and the rupture strain. A ductile rupture occurs and an initial crack forms.
    - Local stress around the crack re-distributes with a very high stress concentration at the crack tip.
    - The conditions at the crack tip are such that fracture criteria are exceeded and the vessel “unzips” in a trajectory that is approximately normal to the direction of principal stress.
    - In fires, flange connections lose their tightness because of a decrease of the contact pressure. This is caused by the temperature-induced expansion of the flange bolts being higher than that of the flanges. Moreover, because of uneven heat distributions, the bolts elongate differently and leaks occur in the areas with higher temperature.

- Little information exists on the performance of pressure relief devices under fire engulfment conditions. Although standard tests exist for isolation valves engulfed in fire, there are no analogous tests for pressure relief devices. Such tests may need to be developed to ensure that the devices will operate in a satisfactory manner under fire loading.
- The evidence suggests that vessels operating at modest pressures are most vulnerable due to their thinner walls. The walls of very high pressure vessels provide such a large thermal mass that even severe fires should not cause the shell of the vessel to fail.
- Flanged connections to vessels are known to be particularly vulnerable to non-uniform heating from a jet fire and severe leakage may be as important as vessel rupture. The evidence suggests further work is required to assess the resistance of flange connections to jet-fire attack.

Models

- Although successful attempts have been found to simulate vessel behaviour using finite element analysis, no work has been carried out on any of the five steels identified in Section 3 as being in most common use offshore.

Guidance

- Only one reference gives guidance directly related to prediction of fire induced stress effects in pressure systems. However, this does not give details on which methods may be applied for rupture calculations or which rupture criteria should be used.

4.8 REFERENCES

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5. DESIGN OF PRESSURE-RELIEVING AND DEPRESSURISING SYSTEMS

5.1 BACKGROUND

Pressures vessels used offshore are fitted with pressure relieving systems to prevent over-pressurisation during operation and with depressurising systems for emptying the vessel in an emergency e.g. a fire.

The background to this topic area was described in the Blast and Fire Engineering project Phase 1 report on the prediction of single and two phase release rates (Appendix A, OTI 92 587). OTI 92 587 reviews methods of predicting the rate of release of material from storage on offshore modules. In particular, the brief was to consider releases with an equivalent diameter up to 100 mm in pipework or vessels containing pressurised gases or liquids of up to 100 bar. The two main areas of uncertainty in the review were that:

- there was an absence of thermodynamic data on the types of multi-component hydrocarbon mixtures, particularly of non-volatile components, used offshore;
- models for single-phase flow were mainly satisfactory but did not take proper account of non-ideal gas behaviour; and
- models of two-phase flow required further development and validation.

Since this report was prepared, a considerable amount of work has been performed in all these areas.

5.2 API 521 – GUIDE FOR PRESSURE-RELIEVING AND DEPRESSURING SYSTEMS

The main guidance used offshore for pressure relieving and depressurising systems is API RP 521 (5-1), which is used in conjunction with API 520 (5-13). API 520 primarily deals with calculation of the required pressure relief area and API 521 with the causes of overpressurisation, determining relief rates and the design of disposal systems. In API RP 521, the following definitions are used:

- A **pressure-relieving system** is an arrangement of a pressure-relieving device, piping and a means of disposal intended for the safe relief, conveyance and disposal of fluids in a vapour, liquid or gaseous state. A relieving system may consist of only one pressure relief valve or rupture disk, either with or without discharge pipe, on a single vessel or line. A more complex system may involve many pressure-relieving devices manifolded into common headers to terminal disposal equipment.
- A **vapour depressurising system** is a protective arrangement of valves and piping intended to provide for rapid reduction of pressure in equipment by releasing vapours. The actuation of the system may be automatic or manual.

Individual relieving rates are determined by pressure and temperature, since they affect the volumetric and compositional behaviour of liquids and vapours. In a fire, additional vapour is generated from the liquid present and the rate at which vapour is generated changes with equilibrium conditions because of:

- the increased pressure in a confined space; and
- the heat content of streams that continue to flow into and out of the vessel.

Offshore, the liquid contained in the vessel will be a mixture of components with different boiling points. Initially, the vapour is rich in low boiling components but as heat is introduced into the fluids the higher boiling point components are successively introduced. Hence, it is necessary to know the change in vapour generation rates and composition with time so as to determine the peak relief rate.

For vapour depressurising, API 521 recommends "reducing the equipment pressure from initial conditions to a level equivalent to 50 per cent of the vessel's design pressure within approximately 15 minutes. This criterion is based on the vessel wall temperature versus stress to rupture and applies generally to vessels with wall thicknesses of approximately 25 mm or more. Vessels with thinner walls require a somewhat greater depressuring rate. The required depressuring rate depends on the metallurgy of the vessel, the thickness and initial temperature of the vessel wall, and the rate of heat input from the fire." "Where fire is controlling, it may be appropriate to limit the application of vapour depressuring to facilities that operate at 17.24 barg and above, where the size of the equipment and volume of the contents are significant. An alternative is to provide depressuring on all equipment that processes light hydrocarbons, and set the depressured rate to achieve 6.9 barg or 50 per cent of the vessel design pressure, whichever is lower, in 15 minutes. The reduced operating pressure is intended to permit somewhat more rapid control in situations in which the source of a fire is the leakage of flammable materials from the equipment to be depressured."

API 521 is primarily based on the requirements for depressurising vessels engulfed in open pool fires. This may not be appropriate for offshore process areas and congested onshore areas since:

- As good drainage and fire fighting equipment is assumed to be available, a heat flux of only 50% of the ca. 110 kW m^{-2} usually given for open pool fires is used to calculate the maximum vapour generation rate. This heat flux is very much lower than can occur in jet fires and confined pool fires (see Section 2.2.2);
- Heat transfer is only considered in relation to generation of vapour from the liquid contents of the vessel. No account is taken of the heat transfer to the wall of the vessel in contact with vapour which may reach temperatures (see Section 4.5.2) at which the vessel loses its structural integrity before the pressure has been sufficiently lowered.
- API 520 and 521 do not adequately deal with two phase flow although a revision of API 520 which may incorporate some of the DIERS work (see Section 5.3.1) is being considered.

5.3 PRESSURE RELIEVING SYSTEMS

Considerable work has been performed on sizing relief systems without and with fire impingement. The main references to sizing pressure relief systems without fire are given but this section concentrates on sizing pressure relief systems under fire loading.

5.3.1 Without fire

The Design Institute for Emergency Relief Systems (DIERS) was set up to improve the design of chemical reactor vents and also consider two-phase flow aspects of venting. The project produced a bench scale apparatus for simulating reaction behaviour, a design-tool computer program, simplified vent sizing equations and recommendations on two-phase methods for use within the vessel and pipework. There was some experimental validation. The main DIERS findings were published in the DIERS project manual (5-2). Since then, DIERS users groups have been set up in the United States and Europe. The US group have organised a number of conferences which highlight the DIERS technology and recent developments in vent system design (5-3 to 5-5). The HSE has produced a workbook on the relief sizing methods for chemical reactors (5-6). The workbook concentrates on hand or graphical calculation methods and gives numerous examples.

The Center for Chemical Process Safety (CCPS) has produced some guidelines on pressure relief and effluent handling (5-7). Calculation methods are given for sizing and rating of pressure relief devices and associated piping and for evaluating whether two-phase flow might occur. The basic equations are presented for fluid dynamics, including two-phase flow, and for sizing relief devices and piping. Methods for evaluating reaction thrust from relief system discharge are also covered. The book gives methods to calculate gas and two-phase flow mass fluxes and a CD-ROM is supplied with the book to facilitate such calculations. The programs are COMFLOW for gas-only flow and TPHEM for two-phase flow.

5.3.2 With fire

Fire relief methods assuming vapour-only relief

Stickles, Melhem and Eckardt (5-8) consider improvements to the design of fire emergency relief systems and have reviewed the industry codes such as the National Fire Protection Association (NFPA) 30 (5-9) and the American Petroleum Institute (API) Standard 2000 (5-10). It was concluded that these had limitations, particularly in regard to a tendency to use overly conservative designs or, in some cases, potentially unsafe designs. In addition, it was found that designing for a low-probability fire results in overly conservative designs that may prove too costly to implement. Fire characterisation (such as burning rate, emissive power, and flame temperature) tends to be based on onshore pool fires rather than offshore jet fires. It was considered that the NFPA / API heat input formulas need to be revised according to the findings of recent research.

Coker (5-11) argues that relief valves should be sized taking account of operational malfunction, such as equipment failure, fire or human error. Good judgement must be exercised in setting the relief requirements (set pressure, for example) in all design facets during hazard and operability studies. Five relief conditions were highlighted viz: gas or vapour, steam, liquid, air and fire. Other critical elements considered were noise suppression, preferred location of relief valves, and pressure drop requirements for stable operation involving the inlet pipe to the valve and tail pipe.

Parry (5-12) provides a valuable summary of fire relief, mostly based on the American Petroleum Institute (API) recommended practice 520 (5-13). The calculated relief rate in a fire is frequently the determining factor for the sizing of the relief device. However, there is some inconsistencies in the codes. The API methods assume only vapour is discharged through the relief device, however liquid may also sometimes be discharged. The basic formula given in API 520 for the heat absorbed by a vessel engulfed in fire is:

$$Q = 21000\ F\ A^{0.82}\ \text{(Imperial units) or } Q = 43.2\ F\ A^{0.82}\ \text{(SI units)}$$

Where Q = heat absorbed (BTU h^{-1} or kW)
A = effective wetted surface area of vessel (ft^2 or m^2)
F = environment factor

The effective wetted area of the vessel is defined as the surface area of the vessel in contact with liquid up to a height of 25 ft (7.62 m) above ground level or other surface that could sustain a fire. Some companies use larger values for the effective elevation. The philosophy of wetted area is that heat transferred to the liquid, will eventually boil and produce much more vapour, than heat transferred to the vapour phase, which produces only vapour expansion. If the vessel contains no liquid a different vent sizing method is used (see later). The environmental factor (F) is an attempt to correct the heat flow for the effect of insulation, water drenching and earth covering. The values used for F and limits of application give rise to most conflicts between codes.

The conversion of the heat flow into a vapour relief rate uses the latent heat of vaporisation of the liquid under relieving conditions.

$$W = Q/L$$

Where W = relief rate (lb h^{-1} or kg s^{-1})
L = latent heat of vaporisation (BTU lb^{-1} or kJ kg^{-1})

Values used in the equation must be at the pressure and temperature of relief rather than the normal working conditions.

Now, considering the other codes of practice, for pressure vessels with wetted areas in the range 10 to 2000 ft^2, the NFPA 30 (5-9) and OSHA (5-14) codes appear to give heat flows twice that of API 520. Between 2000 and 2800 ft^2, the NFPA/API520 heat flow ratio drops steadily from 2 to 1 and for wetted areas above 2800 ft^2 the NFPA / OSHA codes are identical to API 520. The NFPA / OSHA allow an environmental factor of 0.5 for good drainage beneath the vessel. The assumption is that a pool of liquid sustaining a fire would quickly run off to a sump out of range of the affected vessel. The effect of this environmental factor is to make the heat flow almost equal to that predicted by API 520 for effective wetted areas in the range 10 to 2000 ft^2. Parry suggests that API 520 should be the preferred code.

For gas filled vessels, the relief rate in a fire depends entirely on the thermal expansion of the gas. API 520 gives a method for calculating the effective discharge area of a safety valve as:

$$A = F\,A_e / P^{1/2}$$

Where A = required discharge area (in^2)
F = relief valve factor (see formula D-4 of API 520)
P = relieving pressure (psia)
A_e = exposed surface area of the vessel (ft^2)

The method has defects. Notably no allowance for insulation, no restriction on effective area and the relief rate is proportional to the exposed area, not to the exposed area to the power of 0.82 as usual. This calculation method can give relief rates for gas filled vessels higher than methods for liquid filled vessels discussed previously. However, the consequence of overheating the un-wetted wall of a pressure vessel can be disastrous.

Certain methods can be adopted to reduce the amount of heat absorbed during a fire. These measures can be accounted for using the environmental factor F.

For bare vessels with no specific fire protection, F is taken as 1.0. The NFPA/OSHA codes permit an F factor of 0.5 for good drainage. API does not permit this, but the API assumes good drainage and fire fighting equipment in the derivation of their formula.

All the codes permit factors for insulation, provided it is correctly specified and applied and impervious to the impact of fire hose streams. Although not explicit in the codes, the insulation should cover the whole vessel and not the lowest 30 ft. The NFPA/OSHA gives F = 0.3 for adequate insulation whereas API reduces F values as the thickness increases:

Insulation thickness (in)	1	2	3	4
F	0.3	0.15	0.1	0.075

The effect of fire protection on the calculation of fire relief should be treated conservatively. The API code is recommended with the modification of using the NFPA/OSHA F factors for insulation.

Although water sprinkler systems and water drenches are valuable for flammable inventories, the temptation to quantify their benefit should be treated with caution. The API code argues that water application systems are not foolproof and thus the environmental factor should not be quantified. This approach is preferred to other codes that permit an F factor of 0.3 for automatic water sprays.

Fire relief methods which consider two-phase flow

Al-Khudhairy (5-15) compares the methodologies for sizing emergency relief vents for storage vessels exposed to fire. Two approaches for assessing vent sizing requirements are used. The computer code SAFIRE (developed by DIERS (5-2)) and Leung's (5-2) simplified vent-sizing formulations are compared in terms of vent area and vessel behaviour predictions for a storage vessel, containing a non-reactive fluid, exposed to an external fire. The two approaches were found to yield very similar vent sizes for the cases of an ideal nozzle and pipe vent geometry of varying inclinations (vertical, horizontal and inclined) at overpressures in the ranges 0-40 % and 0-20 % above the set pressure. Furthermore, the vessel behaviour during relief predicted by Leung's approach is found to agree very well with that predicted by SAFIRE at overpressures in the range 0 - 15% for both types of vent geometries – nozzle and pipe.

For fire emergencies, Leung (5-16) suggests that, for non-reactive non-foamy systems, behaviour can be based upon vapour venting in the majority of cases. Criteria for liquid entrainment or droplet carryover is also considered for low design pressure storage tanks. For foamy non-reactive systems, the conservative approach would be to use the bubbly flow model in the uniform bulk heating case as a first approximation. The necessary material is covered in a paper by Leung (5-17). He considers that foamy systems require further investigation.

Fire relief sizing for non-reactive, non-foamy fluids has traditionally assumed vapour/gas venting. Two-phase swell tends to be small, typically only 5%, with bubbles adhering close to the wall. Thus mostly vapour enters the vent because the two-phase mixture lies below the vent. Liquid carryover can occur by incipient liquid entrainment (5-18). The entrainment velocity, which depends on the properties of the fluids, can be compared with the vent velocity, which depends on the geometric properties, to calculate the minimum free-board height below which two-phase venting can be expected. Entrainment is important for relief sizing for low design pressure storage vessels but can usually be ignored for relief sizing for pressure vessels.

Wilday (5-19) considers pressure relief system sizing for pressure-liquefied gases. The possibility of two-phase flow is considered. This would increase the required size of the relief system and perhaps require a different type of disposal system than vapour-only flow. Pressure relief systems for external fire can usually be sized for vapour-only flow. However, if depressurisation of the vessel occurs, two-phase flow through the relief system may result. Sizing methods are presented for pressure relief systems for pressure-liquefied gases, when two-phase flow is expected.

5.4 VAPOUR/GAS DEPRESSURISING SYSTEMS

5.4.1 Without fire

A number of computer models for emergency depressurisation exist but the model with most validation with offshore hydrocarbon inventories is BLOWDOWN and hence only this model is reviewed.

BLOWDOWN

A computer model called BLOWDOWN (5-20 to 5-22) has been developed to predict the response of rapid depressurisation of a vessel containing hydrocarbons. The program can predict pressure, fluid and vessel temperatures and multi-phase compositions within the vessel and the rate of discharge from the vessel though the depressurisation choke or orifice, all as a function of time.

The process of emergency depressurisation can be a hazardous operation as it can result in the generation of very low temperatures within the fluid in the vessel and of the vessel itself. At sufficiently low temperatures, the wall temperature of the vessel may fall below the ductile-brittle transition temperature, which can affect the integrity of the vessel. In addition, a second hazard is posed by the possible carry-over of liquid, either as condensed droplets or liquid entrained from within the vessel. Discharge of such liquid can create problems at a flare or vent system usually designed for gases only.

The BLOWDOWN code was written to meet the need to simulate what happens when a vessel is blown down and is able to predict:

- the pressure in the vessel;
- bulk fluid temperature and compositions;
- the amount of each fluid phase;
- temperature of the wall in contact with each phase in the vessel; and
- the flow rate, temperature, composition and phase distribution of the efflux from the vessel through the blow-down choke.

Model

The model addresses the physical processes that occur during emergency depressurisation, namely, fluid mechanics (flow of liquids and gases), heat transfer and thermodynamics (the pressure-temperature-composition correlation of the depressurisation). However, it is not possible to utilise a simple model of adiabatic flow (discharge) of perfect gas through an orifice because there are significant heat transfer processes occurring between the fluid, the vessel and the pipe walls. The result of this is the generation of significant temperature gradients both through the wall and around it.

The model considers a cylindrical vessel (orientated either vertically or horizontally) which is split into three zones containing hydrocarbons and water. Each zone corresponds to a distinct fluid phase:

- Zone 1 – gaseous hydrocarbon including evaporated water, below which is:
- Zone 2 – liquid hydrocarbon including dissolved water, below which is :
- Zone 3 – free (liquid) water including dissolved hydrocarbons.

All the fluid within the vessel is assumed to be at a uniform pressure and each fluid zone is assumed (and experimentally verified) to be well mixed and at a spatially uniform temperature and composition, though the temperature and composition can vary between zones. Mass, heat and momentum fluxes between zones, vessel walls and pipework is considered in detail; heat flux from the vessel wall to the surroundings depends on the precise nature of the surroundings. Heat, mass and momentum fluxes into and out of any pipework upstream and downstream of the choke are determined. Within the vessel, evaporation of the lighter liquid components and condensation of the heavier gas components leads not only to an inter-phase mass flux but also inter-phase heat flux.

The whole continuous depressurisation process is replaced by a series of discrete steps. These steps are of specified pressure decreases within the vessel and not of specified time duration, as is often utilised.

Validation

The BLOWDOWN model was validated by comparison with a large number of experimental measurements undertaken on large-scale vessels. The experiments were undertaken on three different size vessels (although the results from the smallest were not used because the vessel was considered too small and the checks indicated that the predictions are scale independent). The two vessels from which results were utilised were able to blow-down from top, side or bottom ports and temperature and pressure measurements were taken at several locations.

In the case of the largest vessel, mixtures of methane, ethane and propane together with some nitrogen were used in the vessel, creating initial pressures and temperatures of typically 120 bara and 22 °C, respectively. 18 tests were undertaken with this vessel. For the smaller vessel, nitrogen and carbon dioxide were used, representing a gaseous phase, such as methane, and a condensable phase, such as propane, receptively. Fifteen tests were performed using this vessel.

The results obtained from these and other tests show that the BLOWDOWN program predicts results which are in close agreement with experimental measurements. This agreement permits confidence to be placed in the predictions made by BLOWDOWN.

Summary

The BLOWDOWN model does not contain any “disposable” parameters that have to be adjusted for each case and as such, no adjustments have been made to ensure the good agreement between predictions and experimental results. This model is significantly more advanced than other models which are limited to two fluid zones (gas and liquid hydrocarbons) in the vessel and do not correctly handle the heat transfer or mass transfer through the choke. The BLOWDOWN model has therefore been shown to be a valid method of predicting the response of vessels during the blow-down process.

It is proposed that the model be further validated by comparison with experimental tests utilising three-phase systems (gas plus liquid hydrocarbons plus free (liquid) water) and against higher molecular weight hydrocarbons. In addition, the model has been modified (BLOWFIRE) to consider the situation where the vessel is exposed to external fire during the blow down process. This is addressed in Section 5.4.2.

Relief and Blowdown Systems JIP

In 1997, the Safe and Optimum Design of Hydrocarbon Pressure **R**elief **a**nd **B**low-down **S**ystems (RaBs) JIP was initiated in response to an identified need for guidance on sizing and design of relief systems in the offshore oil and gas industry and, specifically, the sizing of such systems for handling two-phase hydrocarbon fluids. The objective of the project is to produce an authoritative reference work highlighting good engineering practice in the design and operation of such systems, including detailed specific methodologies for handling two-phase fluids. The latter will be underpinned by an experimental programme that forms a substantial part of the project. The objective is not to re-write existing standards such as API RP 520/521, but to highlight problems and to provide a commentary on the application of design techniques. It is also seen as an expert document containing good practice based on past successes and failures. It is intended that the final document will be published by the Institute of Petroleum mid 2000.

The RaBs project will not specifically address fire loading of pressure vessels although it is recognised that this is another major inadequacy of current guidance. It is intended that the RaBs/IP document will refer to this report (Section 2) on the response of pressurised systems to fire attack, for the state-of-the-art information on fire loading.

5.4.2 With external fire

A computer program BLOWFIRE has been jointly developed by Shell Research at Thornton and Imperial College, London to predict the response of pressure vessels subjected to fire. The program is a combination of the BLOWDOWN code and proprietary Shell state-of-the-art knowledge of fire characteristics. Further information is given in Section 6.3.1 and an example of its application is given in the case study at Section 8.

5.5 CONCLUSIONS

The following conclusions are made:

New and missing data

- Data are now available for emergency depressurisation of multi-component hydrocarbon mixtures but not under fire loading conditions. Data are available relating to the response of vessels, containing liquid petroleum gas, to flashing liquid propane jet-fire impingement but only for pressure relief rather than emergency depressurisation. No data were found relating to emergency depressurisation under fire loading.

Models

- In order to perform pressure relief sizing in fire situations, it is necessary to determine whether two-phase relief can occur. Models for two-phase relief under fire loading exist but may not have been fully validated.
- Whilst a validated model exists for emergency depressurisation without fire loading, there are no fully validated models in which fire loading is taken into account.

Guidance

- Pressure relief systems are generally designed for gas-only flow. However, during emergency depressurisation, two-phase flow is more likely to occur and is not considered in the existing guidance. In particular, API 520 does not yet adequately address two-phase flow, although revision of this document is in progress. A pressure relief and blow-down systems JIP is in progress. This is aimed at investigation of two-phase relief of systems containing hydrocarbons, but not under fire conditions.
- Current industry guidance, e.g. API 521, does not consider severe fires, particularly impinging jet fires, that could lead to catastrophic failure of vessels before the inventory has been safely removed.
- The Center for Chemical Process Safety (CCPS) has produced guidance on pressure relief and effluent handling. This provides a method to calculate mass fluxes for gas and two-phase flow discharges.
- HSE has produced a workbook on the relief sizing methods for chemical reactors. This provides hand or graphical calculation methods for sizing relief systems.

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6. THERMAL RESPONSE OF PRESSURE SYSTEMS

6.1 BACKGROUND

The Blast and Fire Engineering project delivered one Phase 1 report (Appendix A, OTI 92 610) on the thermal response of vessels and pipework exposed to fire. This report examined the extent of knowledge of the physical processes that take place during fire attack and compared the predictive capabilities of the then available models with the requirements. The main areas of uncertainty identified were:

- Whilst the physical processes which take place when a vessel or pipework is exposed to fire are fairly well understood, many of the empirical relationships (such as heat transfer by forced convection or heat transfer in nucleate boiling) have been derived for simple fluids and may require modification for the complex hydrocarbon mixtures found offshore.
- Most of the models then available were LPG vessel fire response models. Whilst some of these models had been developed using pool fire data there were no validated models for jet-fire attack. None of the LPG vessel models catered for emergency depressurisation and only incorporated a simple pressure relief valve. Some of the models did not take the vessels to failure.

The aim of this section is to review the information currently available on experimental data and predictive methods for the response of vessels exposed to fire and, where possible, to assess the available models. Most codes were developed for fire engulfment of LPG (predominately propane, with a limited mixture of other hydrocarbons) vessels fitted with pressure relief valves. There are very few codes, such as BLOWFIRE, representative of the normal offshore situation with vessels fitted with emergency depressurisation systems. Hence, the review concentrates on improvements and validation of the LPG vessel models as these are virtually the only thermal response models available. Some information is given on BLOWFIRE but this has only been validated in pressure relieving mode rather than emergency depressurisation mode.

6.2 NEW EXPERIMENTAL DATA

A number of experimental studies have been made to determine the thermal response of pressure vessels with pressure relief, including taking vessels to failure resulting in Boiling Liquid Expanding Vapour Explosions (BLEVEs) or Boiling Liquid Compressed Bubble Explosions (BLCBEs).

Birk and Cunningham (6-1) discuss some experimental work on BLEVEs. A number of automotive propane vessels were exposed to a combination of pool and torch fires. Pressure-relief valve settings, vessel wall thickness and fire conditions were chosen as test variables. Vessels suffered thermal rupture in 22 of the 30 tests performed and in 11 of these a BLEVE occurred. Non-BLEVEs were associated with vessel crack formation followed by a prolonged jet release. Evidence is presented that links a BLEVE to vessel wall temperatures, wall thickness, liquid temperature and fill level.

Birk (6-2) gives an overview of scale effects with fire exposure of pressure liquefied gas (PLG) vessels. If the vessel ruptures, potential hazards include blast, projectiles, fire and toxic exposure. Tests have been done at various scales from glass tubes and spheres containing a few cm^3 to full scale rail vessel cars containing 130 000 dm^3 or more. These tests have been used to improve models for fireball diameters, duration, and blast and projectile effects. One of the most strongly debated scale issues relates to the flash evaporation of pressure-liquefied gases upon sudden depressurisation.

Venart (6-3) describes the anatomy of a BLEVE. The loss of containment from PLG vessels accidentally engulfed with fire, is complex, depends on many vessel and fire properties and has been seen to initiate BLEVEs. The latter has been interpreted as a single step event, which results in the adiabatic flash evaporation and dispersal of contents. Test data from experiments with vessels containing argon and water, argon alone, refrigerants R11 or R123 show that there is a possibility of a different, much more powerful incident, with higher fills. This phenomenon, a BLCBE is complex, involving many steps, and may lead to a potential detonation if the contents are flammable. Methods of protection against BLEVEs and BLCBEs are proposed.

Rirksomboon (6-4) describes small-scale BLEVE tests. 58 tests were conducted using modified 1 dm^3 and 2.3 kg test vessels engulfed in a pool fire to determine the failure characteristics of the vessels under BLEVE conditions. Five remnant patterns were observed for failed vessels: single hole, cross like teardown, two-piece teardown, three-piece teardown and shatters. Mathematical models have been developed and used for predicting the temperatures in the vessel and the surface temperatures along the circumference of the vessel during the period prior to a pressure relief valve opening. The models give good approximations of the measured values of the liquid and vapour temperatures.

Sumathipala (6-5) discusses the experimental study of the thermohydraulics of an externally heated PLG vessel and its behaviour during accidental fire engulfment is experimentally simulated. The variation of pressure and temperature distribution within the externally heated horizontal cylindrical container with a pressure relief valve (PRV) is presented. Small scale (40 dm^3) and moderate scale (10 000 dm^3) experimental behaviour is similar for a variety of temperatures. Experimental results show that the vapour space wall temperatures continue to increase despite vapour discharge through the PRV. Analysis of the PRV discharge at both scales of vessel indicates that the beginning of liquid entrainment is dependent upon both the interface height and interface quality.

K. Sumathipala *et al* (6-6) examined two-phase swelling and entrainment during pressure relief discharge. The influence of liquid space expansion, caused by thermal effects and boiling, on PRV behaviour is examined. A laboratory scale test facility was used to better understand the thermal hydraulics of externally heated vessels containing pressure-liquefied gases and the causes of BLEVE behaviour. Experimental programmes have been conducted and evaluated to determine better strategies for transport and storage containers.

Yu *et al* (6-7) have produced a preliminary physical and mathematical model to describe the behaviour of a BLCBE. The transient thermohydraulic behaviour of a vertical cylinder containing a pressure liquefied gas (propane) at 1 MPa initial pressure and 90% initial fill has been simulated with a crack suddenly opening up in the vapour space. Time dependent behaviour of the vapour and liquid regions have been numerically modelled. Analyses of the propagation of the depressurisation wave (caused by mass discharge through the crack) and the recompression wave (caused by the rise in the liquid vapour interface, due to void generation), permit the pressure, density and velocity fields in the vapour region and the void fraction and pressure distributions in the liquid region to be predicted.

HSL work on pool fire engulfment of LPG vessels is described in OTI 92 610. HSL subsequently undertook four failure mode trials involving unprotected, two-tonne, vessels containing various fills of propane. In each trial, a vessel was charged with propane to the required level. The vessel, located in a remote site, was engulfed with a flashing-liquid propane jet fire until it failed (6-8 to 6-11). The mass of the contents and temperature and pressure profiles were measured up to the point of vessel failure. Each vessel was fitted with a pressure relief valve (PRV) set to relieve at 17.24 barg. The PRVs were protected from the jet fire during the test by thermal insulation material. The four tests comprised of testing vessels with volumetric fill levels of 20%, 41%, 60% and 85% of industrial propane, containing about 10% butane.

The jet-fire scenario utilised in the tests was chosen to be representative of an ignited liquid discharge from a punctured vessel or damaged pipework located adjacent to the test vessel. The jet-fire conditions selected resulted in a flame which engulfed at least three quarters of the target vessels irrespective of the wind conditions. The jet fire chosen consisted of ignited, flashing, liquid propane, flowing at a rate of ca. 2 kg s^{-1} through a nozzle of diameter equivalent to 12.7 mm. The vessel was positioned at the still-air lift-off point of the flame. The flame temperatures were estimated to be in the range 900 to 1100 °C, based on the temperatures measured (6-12). The heat flux densities were determined to be in the range 180 to 200 kW m^{-2}. It was found that:

- The temperature of the tank walls increased to between 700 and 870 °C at failure;
- For all but the 20% full tank, after PRV opening (18.1 - 18.8 barg) the pressure continued to increase, after an initial 3 bar drop, until tank failure at 18.6 to 24.4 barg; and
- All the tanks failed catastrophically after no more than 5 minutes fire engulfment.

6.3 MODEL FEATURES

The following features should be considered when evaluating the available models of fire-engulfed pressure vessels:

- fire scenarios modelled;
- range of vessel geometries;
- modelling of protective coatings and other means of fire protection;
- heat transfer to the vessel;
- heat transfer to the vessel contents;
- energy balance between liquid and vapour phases;
- modelling the pressure relief system;
- energy balance during relief operation;
- degree of physical realism; and
- efficiency and correctness of the mathematical solution procedures used.

These fit with the general features of model evaluation protocols as presented by the European Commission's Model Evaluation Group (6-13).

The main heat transfer mechanisms normally employed are shown schematically in Figure 6.1. Some models (e.g. PLGS – see Table 6.1) allow for temperature stratification and heat transfer within the liquid phase.

6.4 VESSELS WITH A PRESSURE RELIEF VALVE

There are a number of computer models available for predicting the response of vessels fitted with PRVs when subjected to fire. All the models considered below predict the behaviour of LPG vessels. Webber (6-14) assesses these codes in terms of the mathematical and physical representations and Butler (6-15) compares the model predictions with measured data. The models considered and the key differences in physical representation are summarised in Table 6.1.

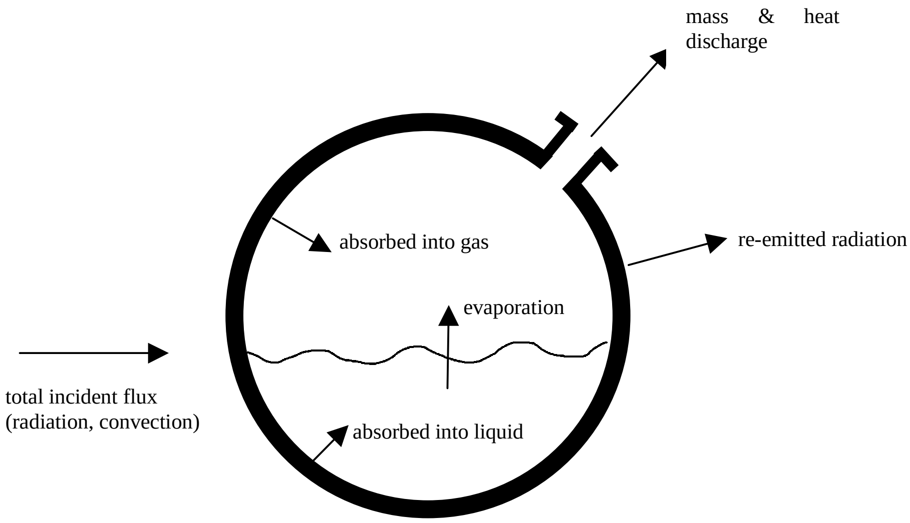

Figure 6.1. Heat transfer mechanisms

6.4.1 LPG vessel thermal response models

Webber (6-14) stresses the following points in regard to the models evaluated. The vessel pressure increases with time, in all of the models, to a roughly constant value. The failure pressure decreases slowly with time as the vessel temperature rises and the steel becomes weaker. When these two pressures meet those models which consider failure deem this to be the time of failure. Therefore, the time at which failure occurs will be sensitive to these pressures. Given that the vessel is not at a uniform temperature, the vessel strength (and thus failure pressure) will be determined by the temperature of the hottest localised stress riser. Detailed temperature variations are not considered by the models. In view of this, it is difficult to confirm how the time to failure can be predicted accurately.

Table 6.1. LPG vessel thermal response models

MODEL	SOURCE	DESCRIPTION
ENGULF	AEA Technology plc	The vessel is considered to have a vent, which may be either fully open or shut and expressions are given for the mass flux (considered gas only) through the vent, for choked and unchoked flow. The thermodynamics of the vessel are formulated on a per-time-step basis. Internal energy is lost from the system when the vessel vents (in proportion to the mass loss at a specific energy assumed to be that of an ideal gas at the vapour space temperature).
PLGS	University of New Brunswick	The model recognises that there will be different sub-zones within the liquid space, which it splits into four regions: • the bulk liquid at the bottom of the vessel in the centre; • a stratified liquid layer above the bulk liquid and below the gas space; and • two boiling regions down either side of the vessel. The vessel is considered to have a vent, which may be either fully open or shut. Swelling of liquid is included and may ultimately result in two-phase flow through the vent.
Tsolakis (not the BLOWDOWN model)	Imperial College of Science, Technology, and Medicine	The interface between the liquid and gas (described as a very thin layer) is considered to have its own temperature. There are thus three fluid zones in the vessel: the gas, the liquid, and the infinitesimally thin interface region. The vessel is considered to have a vent, which may be either fully open or shut. If the vent is above the liquid level, expressions are adopted for the mass flux (considered gas only) through the vent, for choked and unchoked flow. Vessel failure considerations are beyond the scope of this model.
HEATUP	Shell Research	The model recognises that there will be different sub-zones within the liquid space which is split as for PLGS. The vessel has a pressure relief valve, which may be either fully open or shut or, in one of the provided options, partly open. Expressions are given for the mass flux through the vent for liquid and vapour releases. Vapour releases are always assumed to be choked flow.

6.4.2 Comparison of predictions with trial data

A comparison (6-15) of the model predictions with the HSL data suggests that:

- the models vary between very simple (liquid and vapour phase with no temperature stratification) and more sophisticated (level swell, stratification) representation of the physical phenomena;
- few models accurately predict both the temperature and the pressure changes e.g. if the pressure is predicted accurately then it is not usual that the temperature is predicted accurately; and
- most of the models have been developed using the HSL vessel failure trial data i.e. they have been altered to a lesser or greater extent to fit the data rather than being validated with the trial data.

6.5 VESSELS WITH AN EMERGENCY DEPRESSURISING SYSTEM

6.5.1 General considerations

The emergency depressurisation of process vessels is complex and the behaviour of the process vessel during depressurisation varies depending on the vessel contents and the conditions of the vessel. During depressurisation at ambient temperature, the temperature of the vessel may drop dramatically as the contents are released, leading to the need to consider the minimum design temperature requirements of the vessel. At the same time, however, if the vessel is exposed to an engulfing fire, the behaviour of the vessel will be very different and the pressures and temperatures experienced will significantly differ from those normally considered. The design of depressurisation systems must therefore address both the depressurisation and also the characteristics of any impinging flame, which may be the cause of the emergency depressurisation. The design of such systems is considered in a paper by Gayton and Murphy (6-16), in which the need to have access to models which describe both the depressurisation process and fire characteristics is identified.

There are very few models available to predict the response of vessels during emergency depressurisation due to exposure to fire. However, one model which addresses such a situation is BLOWFIRE.

6.5.2 BLOWFIRE

The computer program BLOWFIRE (6-17) was jointly developed by Shell research at Thornton and Imperial College, London to predict the response of pressure vessels subjected to fire. The program is a combination of the BLOWDOWN code (see Section 5) and proprietary Shell state-of-the-art knowledge of fire characteristics. The scope of applicability covers cylindrical pressure vessels containing any mixture of hydrocarbons, subjected to arbitrary fire boundary conditions. The program allows for pressure increase or decrease in the vessel and can simulate a number of different discharge devices; PRV, EDV, nozzles, pipes, etc. Such versatility allows BLOWFIRE to be used for process vessels protected by emergency depressurisation, as well as LPG vessels protected by pressure relief. The modelling of heat and mass transfer mechanisms in BLOWFIRE is similar to that in BLOWDOWN. The main differences reside in the incorporation of the fire boundary condition in the BLOWFIRE code.

In the absence of experimental data on vessels undergoing emergency depressurisation in the event of fire engulfment, the BLOWFIRE model was used to predict the response of LPG vessels fitted with PRVs when subjected to a variety of fire conditions. The predictions obtained were compared with the experimental data obtained in the large scale HSL tests involving both jet-fire and pool-fire conditions (Section 6.2). In each case, the important parameters considered are:

- vessel and PRV pressure;
- temperature of bulk vapour, bulk liquid and walls;
- liquid level;
- discharge rate; and
- composition (for multi-component mixtures).

The results obtained were compared with predictions obtained using the HEATUP code (6-18). A significant difference between the two being that HEATUP models a hot layer, which may be important in the case where thermal stratification occurs, and BLOWFIRE does not. One important result of this difference is that BLOWFIRE overpredicts the vapour wall temperature and underpredicts the maximum wall temperature in the jet-fire experiments, compared to both the measured data and the HEATUP predictions. Comparisons of the various predictions highlight the conditions where thermal stratification is important.

From the comparison of BLOWFIRE against the LPG vessel heat-up experiments the following conclusions (6-17) were drawn:

- BLOWFIRE overpredicts the vapour temperature in the vessel and underpredicts the maximum wall temperature in the jet-fire experiments. This is a result of the enhanced thermal stratification within the vessel (3 or more layers) which is not modelled. The existence of an intermediate hot layer appears to be related to the intensity of the fire: as the heat transfer rate increases from a pool fire to a jet fire, thermal stratification becomes more likely. It is expected to be less important in emergency depressurisation of process vessels and after PRV opening in LPG storage unless the pressure starts rising again. Experimental work should clarify the significance of this phenomenon for commercial operations.
- For model improvement there is a need to check the external and internal heat transfer rates to the vapour phase. The contributions of natural convection and radiation to the heat transfer should be clarified.
- Despite recent progress in the characterisation of impinging fires there is considerable uncertainty concerning the heat fluxes and jet-flame temperatures appropriate to jet and pool fires impinging on vessels. Such information is essential for accurate simulation of vessel response. Any experimental program should address this issue with priority given to using the same geometry as the vessel response trials.

6.6 CONCLUSIONS

The following conclusions are drawn:

New and missing data

- The only jet-fire thermal response data obtained on a reasonable scale are from trials with ca. 2 kg s^{-1} flashing liquid propane jet fire impinging on 2 tonne LPG vessels fitted with a pressure relief valve. There appear to have been no fire engulfment trials performed on pressure vessels fitted with emergency depressurisation systems with either simple or multi-component hydrocarbon mixtures.

Models

- Most of the tools available to study the behaviour of a vessel and its contents in a fire have been developed for LPG storage tanks incorporating PRVs and these have only been tested against a few experiments of small LPG tanks in fires. This is a long way from vessels containing multi-component hydrocarbons at high pressure, being rapidly depressurised in an emergency.
- None of the models designed for predicting the response of LPG vessels exposed to engulfing fire are ideal. Most of the models either do not reliably predict the response or else the methodology applied is incorrect. Validation using a greater range of vessels and fire conditions is required.
- Confidence in the BLOWFIRE model for emergency depressurisation will only be established if it is successfully validated with medium scale experiments, under process conditions representative of offshore operations.

Guidance

- Due to a lack of validated models, no guidance is available on emergency depressurisation under fire loading conditions.

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7. PERFORMANCE STANDARDS FOR THE RESISTANCE OF PRESSURISED SYSTEMS TO FIRE ATTACK

7.1 BACKGROUND

Use of performance standards came to prominence following issue of the Prevention of Fire and Explosion and Emergency Response Regulations (PFEER, 7-1) and UKOOA Fire and Explosion Hazard Management Guidelines (7-2) in 1995. Performance standards are closely related to:

- Engineering Acceptance Criteria;
- Rule sets used in Quantitative Risks Assessments (QRA); and
- Design Accidental Loads (DALs).

The use and interrelationship of these are considered in this section.

7.2 PERFORMANCE STANDARDS

According to the PFEER regulations (7-1), "a performance standard is a statement, which can be expressed in qualitative or quantitative terms, of the performance required of a system, item of equipment, person or procedure, and which is used as the basis for managing the hazard, - e.g. planning, measuring, control or audit – through the lifecycle of the installation".

There are two levels of performance standards:

> **High level** performance standards are applied to the installation as a whole or to the major systems that comprise the installation. These are the goals for safety of the installation and relate to overall risk to persons on the installation. These standards are verified from the results of assessments of low level performance standards. High level performance standards are normally risk-based. For example, HSE use (7-3, 7-4) the following:
>
> - An *unacceptable risk* is one where the individual risk of a fatality is 10^{-3} per year or more for a worker, or 10^{-4} per year or more for a member of the public.
> - A *broadly acceptable* risk is one where the individual risk of a fatality is 10^{-6} per year or less.
> - A *tolerable* risk is deemed to exist in the range between 10^{-3} and 10^{-6} per year for a worker. All tolerable risks must be demonstrated to be as low as is reasonably practicable (ALARP).
>
> **Low level** performance standards are used to describe the required performance of lesser systems, which may contribute to the high level performance standards. Performance standards at this level relate to the principal safety critical systems used to detect, control and mitigate the major hazards. The selected systems should make a significant contribution to the overall acceptability of the hazard management arrangements. The performance standards should be directly relevant to the achievement of the system goal and the performance standards should be expressed in terms that are verifiable.

An important principle in setting performance standards is that the number and level of detail should be commensurate with the magnitude of the risk being managed.

Typically, the lower level performance standard for the resistance of pressurised systems to fire attack would be that isolation, depressurising and fire protection systems are functional, fit for purpose and available on demand.

Reference 7-5 contains requirements and guidelines for the control and mitigation of fires and explosions on offshore production installations, and uses an approach that is similar to the UK performance standards approach. This reference states that, in the process of fire and explosion evaluation and risk management, any risk reduction measures should be recorded so that they are available for those who operate the installation and for those involved in any subsequent change to the installation. For this record, the reference uses the term “strategy”. Two strategies are introduced, namely a Fire and Explosion Strategy and an Evacuation, Escape and Rescue Strategy. The strategies should describe the role and any functional requirements for each of the systems required to manage possible hazardous events on offshore installations. The functional parameters (integrity, reliability, availability, survivability and dependency), and the associated specifications, are equivalent to the performance standards approach in the UK safety legislation.

7.3 ENGINEERING ACCEPTANCE CRITERIA

Typically, the engineering acceptance criteria for a pressurised system to resist fire attack would be that stresses, deformations and/or temperatures remain below values that would compromise the integrity of the system. The criterion given in the API Recommended Practice 521 (see Section 5.2) aims to achieve this by depressurising the system at a recommended rate. However, this may be inappropriate and inadequate for offshore installations, for which it was not originally intended.

7.4 QUANTITATIVE RISK ASSESSMENT

Quantitative Risk Assessment (QRA) provides a structured approach to assessing the potential for incidents and expressing this potential numerically. In a QRA, risk is defined as the combination of the probability or frequency of occurrence of the outcome event and the potential consequences. The frequency of occurrence (i.e. outcome frequency) is normally calculated using Event Trees. The results from an event tree calculation may be an outcome frequency for loss of life. QRA uses “rule sets” to predict the effects of accidental loads on facilities and personnel. In general, rule sets are based on past experience, statistics and simplistic response models and require a great deal of judgement. As such, they sometimes do not accurately enough predict the behaviour of systems affected by accidental loads, which often leads to lack of conservatism or over-conservatism with inaccuracies in risk predictions as a result.

Typically, the rule set for a pressurised system in a fire would be no escalation of the initial fire event, dependant upon the performance standards and engineering acceptance criteria being met. With current knowledge, this rule set would either be, possibly incorrectly, that following industry guidance (engineering acceptance criteria) provides sufficient protection, or alternatively, and possibly unnecessarily, that the system will rupture and fire escalation will always occur.

The QRA provides the confirmation or otherwise that the high level performance standards are achieved. Every QRA is facility specific, i.e., it includes the consideration of expected standards of performance of systems that are critical in various accidental scenarios that may occur on the facility.

7.5 DESIGN ACCIDENTAL LOADS

Design Accidental Loads (DAL) are loads for those accidental events where the associated risks exceed the risk tolerability criteria. Therefore, the designed facility should successfully resist the DAL. This would require a lengthy iterative approach whereby a QRA is carried out first to identify those events and loads that cause the exceedance of risk tolerability criteria. Therefore, an approximate approach has been used which defines DAL as being associated with those events that have the order of magnitude of initiating frequency greater or equal to the tolerable outcome frequency. For example, when the tolerable outcome frequency is $5x10^{-4}$, the DAL are those loads with the initiating event frequency of 10^{-4} and higher.

The requirements for successful resistance of a facility to DAL are expressed in the form of performance standards. Typically, the performance standard would state that a pressure vessel should survive and remain functional during a postulated fire scenario. Again, in the terms of engineering acceptance criteria this means that applied stress in the vessel is not to exceed a defined allowable stress throughout the duration of a fire and thereafter.

7.6 LINK BETWEEN ENGINEERING ACCEPTANCE CRITERIA AND QRA

As implied by the above, the link between engineering acceptance criteria related to pressure systems and QRA may be made (7.6) using the following approach:

- Rule sets in a QRA are set to reflect the standards to which safety critical systems are to perform, e.g. no escalation of the initial fire event in an area.
- This rule set assumes that isolation and depressurising systems, and a dedicated deluge cooling system are functional, available on demand and survive the initial fire (performance standards).
- The systems are designed to meet the normal engineering acceptance criteria for stress, deformation, temperature etc.
- DALs are determined for the systems whose risk, calculated by QRA, exceeds the risk based performance standards.
- The pressure systems are redesigned to resist the DALs.

Before formal industry guidance could be given on such links, guidance is needed on the rule sets to use in QRA, the determination of DALs and appropriate engineering acceptance criteria.

7.7 CONCLUSIONS

The following conclusions are made:

New and missing data

- Performance standards related to the resistance of pressurised systems to fire attack depend on having robust engineering acceptance criteria and a robust method to determine if these are met. At present, these do not exist.

Modelling

- Quantitative Risk Assessment is used to confirm that high level performance standards are achieved, and the QRA rule sets also depend on having robust engineering acceptance criteria.

- Design Accidental Loads are determined for the risks calculated by the QRA that exceeded the risk based performance standards and the facility is designed to resist these DALs.

Guidance

- Guidance is needed on the rule sets for use in QRA, the determination of DALs and appropriate engineering acceptance criteria and on how these linked together.

7.8 REFERENCES

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8. ANALYSIS OF EXAMPLE PRESSURE VESSEL

An analysis of an example pressure vessel has been undertaken to demonstrate the thermal and mechanical response of pressurised equipment. The analysis has included a:

- depressurisation under fire loading analysis, and
- finite element analysis of the thermo-mechanical response.

The example vessel was chosen to illustrate several features found on pressure vessels offshore: horizontal vessel on saddle supports, wall thickness differences, nozzles and a man-way. The operating pressure and hence wall thickness were chosen such that the vessel was likely to be vulnerable to a severe fire to illustrate the important phenomena.

The vessel contents and the fire loading were chosen and defined in a way that satisfied the input requirements of BLOWFIRE. The composition of the vessel contents was chosen to be a complex mixture of hydrocarbons, not un-typical of a 2nd stage separator offshore. The fire loading scenario was chosen to be not too severe but for simplicity, uniformly distributed over the whole of the vessel's outer surface.

8.1 EXAMPLE VESSEL

The example pressure vessel is based on a design supplied by Aukra-Midsund Offshore AS (ref. Drg. No. 11-4A-AV-MS8-01021-5011) of a second stage separator. The design has been simplified (see Figure 8.1) to concentrate on the key points of interest. The vessel details are summarised in Table 8.1.

Table 8.1 Details of example vessel

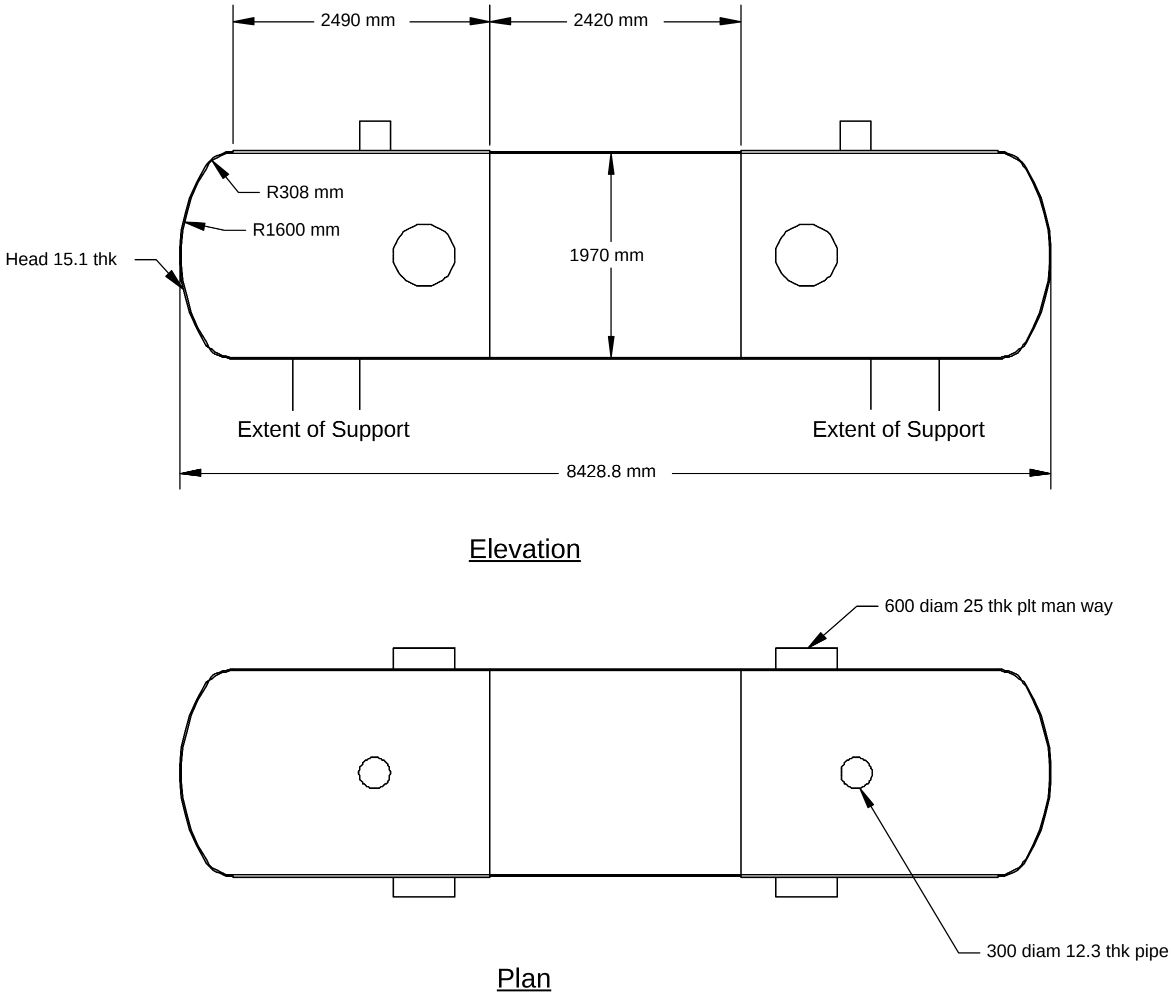
Vessel inner diameter	1970 mm
Wall thickness of the cylindrical part	16 mm, 20 mm, respectively as shown on the original drawing
Vessel head	as shown on the drawing (Figure 8.1)
Wall thickness of vessel head	15.1 mm
Vessel support (saddles)	as shown on the drawing, plate thickness = 12 mm
Operating pressure	21 barg
Design pressure	29 barg
Material	Carbon steel

8.2 DEPRESSURISATION UNDER FIRE LOADING ANALYSIS

The analysis of depressurisation under fire loading conditions was performed using the BLOWFIRE computer program (see Section 6).

8.2.1 Input data and assumptions

The vessel contents were considered to be a hydrocarbon condensate, filling the vessel to a height of 630 mm, corresponding to the medium liquid fill level specified in the original drawing of the vessel. A composition was selected (see Table 8.2) which gave the desired fill level and operating pressure. A number of runs were performed with BLOWFIRE to derive choke conditions that would allow depressurisation (under ambient conditions) according to the requirements of API 521 (see Section 5.2).



Note: The heads are 15.1 mm thick, the shell sections with connects are 20.0 mm thick and the centre section of the shell is 16.0 mm thick

Figure 8.1. Diagram of example vessel

Table 8.2. BLOWFIRE initial composition

HYDROCARBON	MOLE FRACTION
Carbon dioxide	0.01
Methane	0.25
Ethane	0.07
Propane	0.05
Butane	0.04
Pentane	0.02
Hexane	0.07
Octane	0.07
Nonane	0.15
Decane	0.27

In order to allow for the two different wall thicknesses and to compare ambient and fire loading conditions, four BLOWFIRE runs were performed. The main input parameters are summarised in Table 8.3.

Table 8.3. BLOWFIRE input parameters

PARAMETER	RUN 1	RUN 2	RUN 3	RUN 4
Overall internal length (m)	8.55	8.55	8.55	8.55
Internal diameter (m)	1.97	1.97	1.97	1.97
Shell wall thickness (mm)	16	16	20	20
Shell material	Carbon steel	Carbon steel	Carbon steel	Carbon steel
Vessels ends (dished) (mm)	15.1	15.1	15.1	15.1
Jet fire temperature (K)	ambient	1473	ambient	1473
Jet fire flow velocity (m s^{-1})	freely convecting	35.0	freely convecting	35.0
Incident radiation (kW m^{-2})	0	120	0	120
Absorbtance/emittance	0.65	0.65	0.65	0.65
Contents pressure (bara)	22	22	22	22
Initial wall temperature (K)	323	323	323	323
Initial contents temperature (K)	323	323	323	323

The horizontal discharge pipe between the separator and choke orifice was taken to have 300 mm i.d., 312 mm o.d, 10 m length and roughness of 0.046 mm, with an initial wall temperature of 283 K. The orifice choke was taken as 14 mm with a discharge coefficient of 0.8 and back pressure of 1 bara. A 30 second step size was used in each run.

8.2.2 Output data

The output data was given in the form of tabulations of:

- pressure;
- gas and liquid temperatures and molar compositions;
- inner and outer wall temperatures in contact with liquid and gas/vapour;

- inner and outer end cap temperatures in contact with liquid and gas/vapour;
- liquid height; and
- discharge conditions.

The plots of the variation of pressure, and inner and outer wall temperatures with time are give in Figures 8.2 to 8.4.

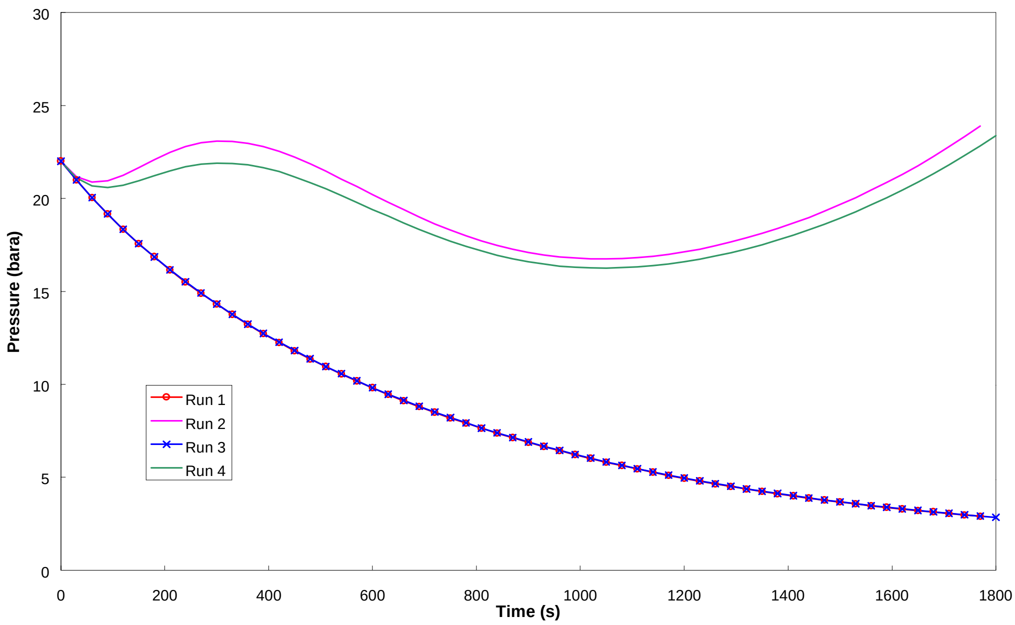


Figure 8.2. Pressures for runs 1 to 4

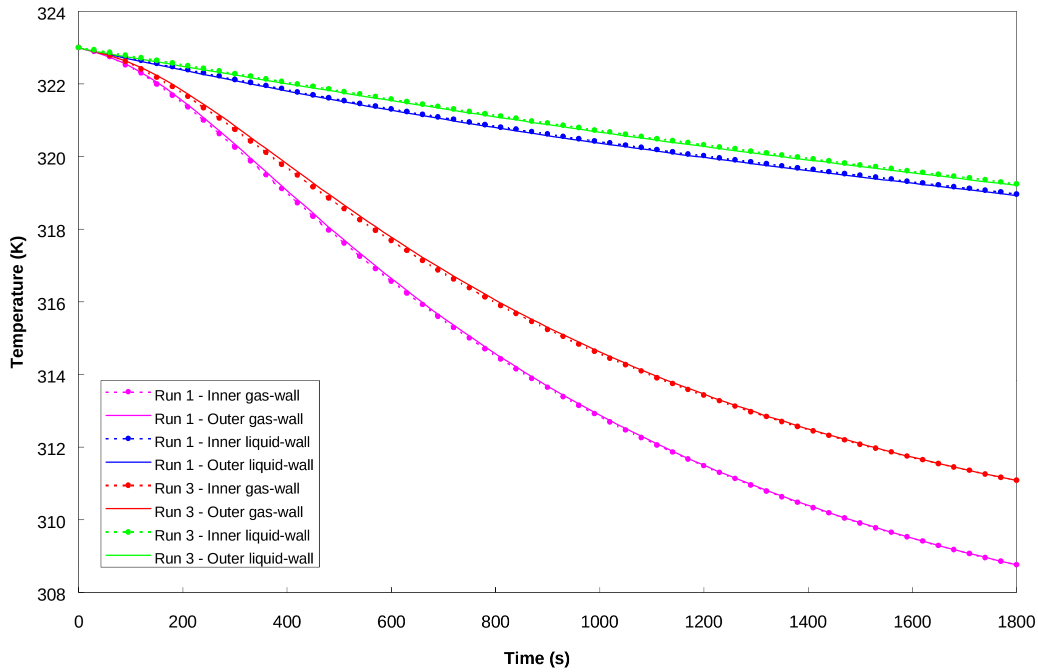


Figure 8.3. Inner and outer wall temperatures for runs 1 and 3

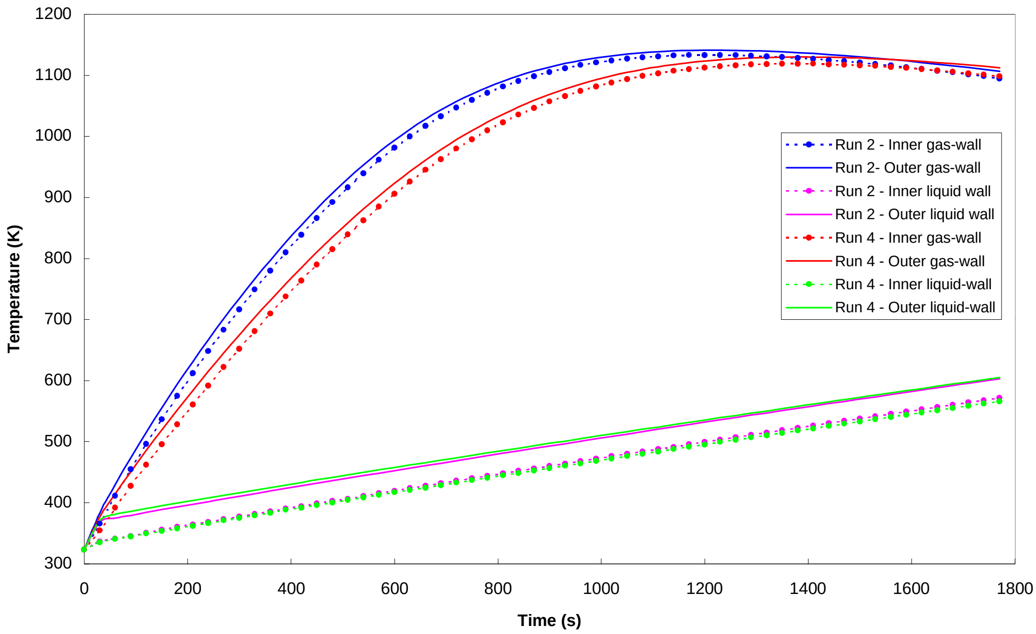


Figure 8.4. Inner and outer wall temperatures for runs 2 and 4

8.2.3 BLOWFIRE analysis

Runs 1 and 3 were performed at ambient temperature to demonstrate that, without fire input, the composition and discharge conditions selected gave a pressure decay that meets API requirements (see Section 5.2). 15 minutes after commencement of heating, the pressure was reduced from 22 bara to 6.9 bara.

Under fire loading conditions BLOWFIRE indicates that, after an initial pressure drop of about 1.5 bar in the first 90 seconds, the pressure increases to 23 bara after 5 minutes with the thinner walled vessel. The pressure then drops to 16.74 bar after 18 minutes before increasing again as the higher boiling point components are vaporised. Similar behaviour is observed with the thicker walled vessel although the pressures are slightly lower due to a lower heat transfer through the thicker walls. This type of behaviour has not been validated but could happen in practice.

8.3 ANALYSIS OF THERMAL AND MECHANICAL RESPONSE

The analysis of thermal and mechanical response has been carried as a coupled transient thermal and stress analysis using the finite element program ANSYS.

8.3.1 Conversion of BLOWFIRE output into form suitable for ANSYS input

BLOWFIRE provided the temperature-time relationships for the vessel wall in both the gas space and the liquid space. In the mechanical analysis, the worst case response occurs in the region with the largest thermal gradient. To calculate the thermal gradient, an iterative analysis was carried out to establish linear heat transfer properties. The heat flux defined for the flame is applied directly to the surface of the steel in conjunction with a radiation link to represent radiative heat loss. This radiation link is attached to a sink temperature defined as 273 K in this analysis. The flame temperature is linked to the steel by means of a convection link; the 1473 K temperature is applied to the remote node of the link. Two models are used, one to represent heat loss to the gas space and one for the liquid space. Both have identical external heat transfer properties, radiative and temperature loads and radiation link for external heat loss on the convection link for convective heat gain. The gas space model has heat loss to the gas by means of both convection and radiation; the liquid space model has convective heat loss only. The sink temperature for the two models is defined as the temperature varying gas or liquid temperature calculated by BLOWFIRE.

8.3.2 ANSYS input parameters

Using the above procedure, the following input parameters were derived:

- Flame convective heat transfer using an incident flame temperature of 1473K and a heat transfer coefficient of 25.5 W m^{-2} K^{-1}.
- Direct incident heat flux of 120 kW m^{-2}.
- Heat re-radiation (heat loss from surface) with emissivity of 0.67.
- Radiation to gas space, emissivity 0.49.
- Convection to gas space, heat transfer coefficient 23.1 W m^{-2} K^{-1}.
- Convection to liquid space, heat transfer coefficient 5099.8 W m^{-2} K^{-1}.
- Temperature varying thermal conductivity of carbon steel.
- Temperature varying specific heat capacity of carbon steel.

- Density of steel 7850 kg m^{-3}.
- Fire duration 30 minutes.

This data gave wall temperature-time curves within 2.5% of those given by BLOWFIRE. However, the pressure data used as input corresponded to that from BLOWFIRE Run 1 with no fire loading. Hence, the analysis is artificial but shows the case where the API depressurisation requirements are satisfied.

The structural model was based on the data given in Figure 8.1. It should be noted that no constraint or load was applied to the pipe connection at the top of the vessel.

8.3.3 ANSYS output parameters

The structural and thermal models were analysed using the Industry Standard FE program ANSYS version 5.5.3. The analysis utilised the Plastic Large Strain Shell (SHELL43) element. SHELL43 can model creep effects but these features have not been utilised.

The analysis model, one quarter of the whole vessel, consisted of 1715 elements (1624 shells and 91 spars), 1783 nodes and 9866 degrees of freedom. The outputs are given in the form of three-dimensional contour images of the:

- Von Mises equivalent stress;
- temperature distribution;
- yield stresses; and
- ultimate stresses.

8.3.4 ANSYS analysis

The thermal analysis results demonstrate a significant variation in the temperature rise of the vessel shell between the gas space and the liquid space. In the liquid space, the heat is rapidly transferred from the shell to the liquid leading to the liquid temperature controlling the shell temperature. In the gas space, the heat transfer from the steel to the gas is not as efficient. Therefore, the steel retains the heat leading to a rapid rise in the steel temperature. The steel temperature rises to 1200K in the gas space and 650K in the liquid space.

From the analysis, it has been demonstrated that the critical point of the shell, that is the point where the stress concentration is a maximum, moves with the loading history changes. The separator analysed has been subjected to a time-varying temperature and reducing internal pressure. In the initial stages of the loading history, the stress concentrations around the appendages, the nozzle, the man way and the support are as expected. As the temperature rises, despite the reducing pressure, the peak stress moves to be in the region in contact with the top surface of the liquid. This is where the highest temperature gradient exists, i.e. between the high temperature steel around the gas space and the lower temperature steel in the liquid space. Figures 8.5 and 8.6 demonstrate the extent of the temperature differential, i.e. up to approximately 400K, and stress differential. An additional effect noticed, as the temperature rises, is that the stress patterns clearly identify the change in plate thickness. As the temperature rises and the pressure falls the stress concentration is centred on the top of liquid level, the stress concentrations around the appendages begin to fall. Finally, the material reaches the ultimate stress with a peak stress at the change in thickness, again coincident with the top of liquid level. The program stopped at 14 minutes, probably because the elasticity tended to zero, indicating failure. Failure was also indicated at 6 minutes, but not at the 30 second steps either side, indicating that further analysis would be required.

This case study indicates that the critical region for a pressure vessel subjected to an engulfing fire is the top of the liquid level and is compounded by any abrupt change in plate thickness. It should be noted that a fire that does not completely engulf the vessel will produce a much more onerous result. Local heating of the shell will cause local material expansion. The expanding material will push against the colder unheated sections leading to premature buckling and an increased probability of failure.

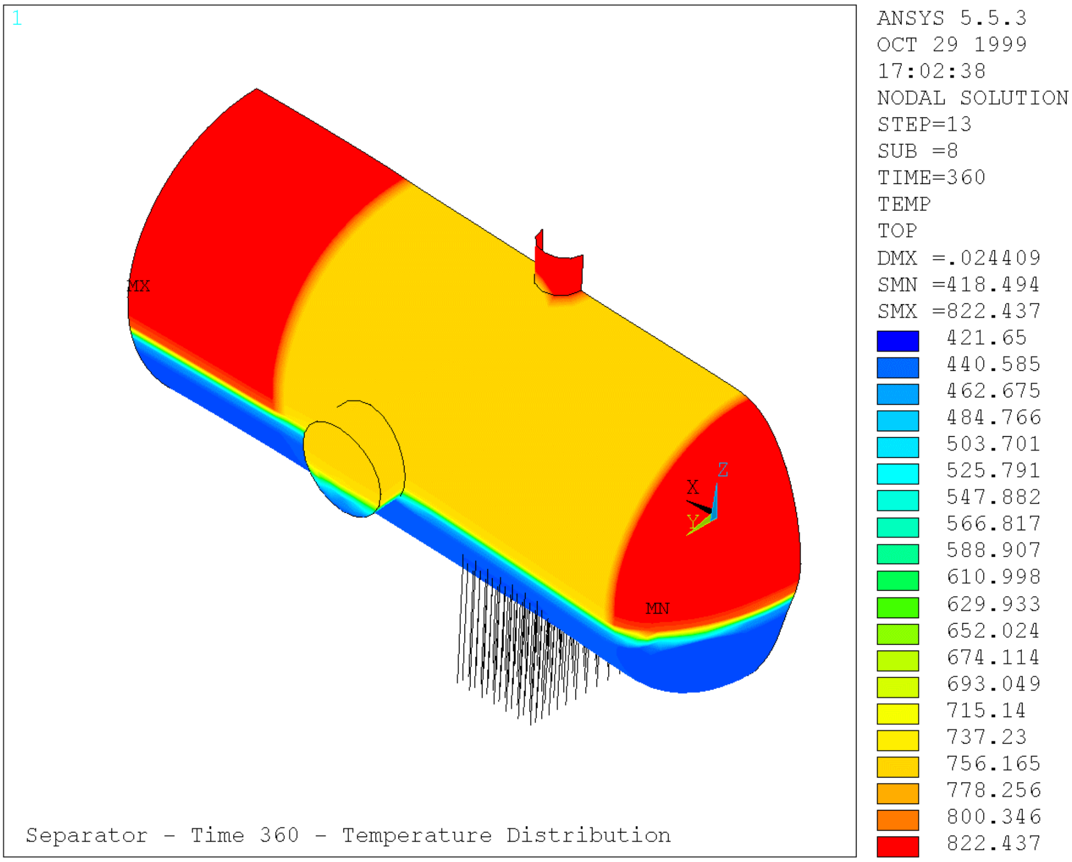


Figure 8.5. Temperature distribution at 360 seconds

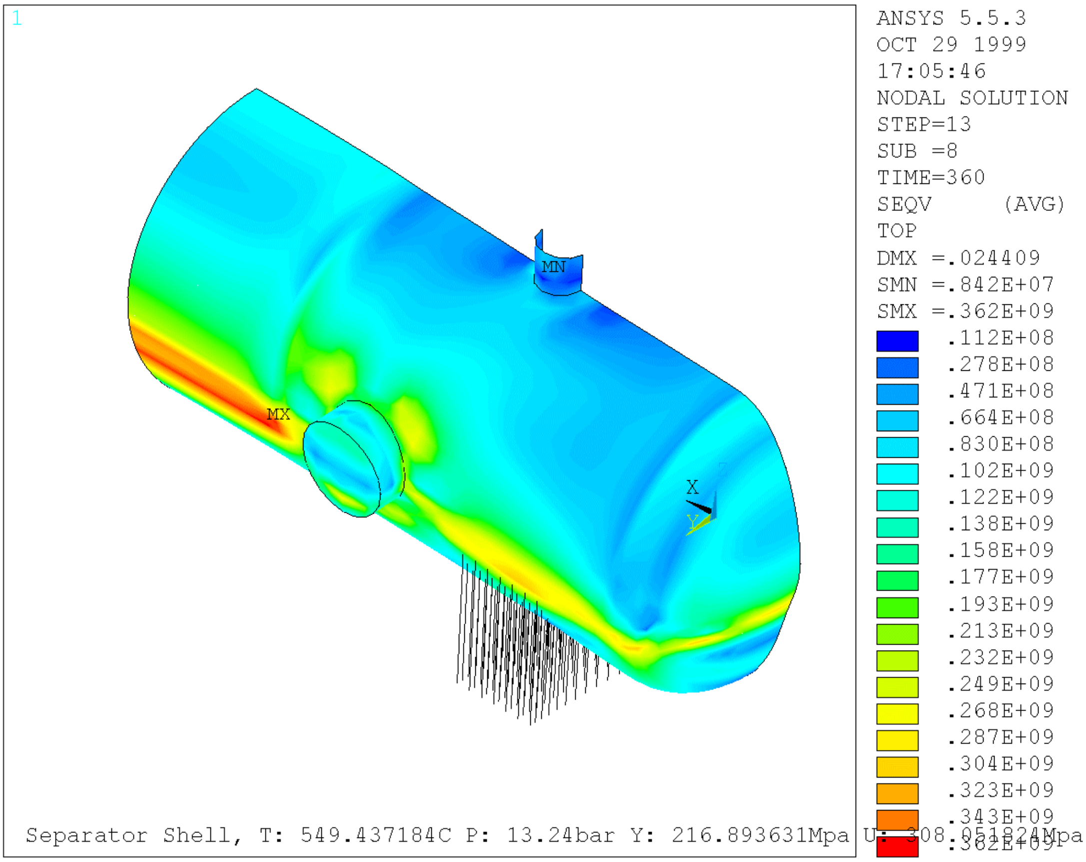


Figure 8.6. Von Mises equivalent stress at 360 seconds

8.4 CONCLUSIONS

The following conclusions are drawn:

New and missing data

- A system designed to depressurise under ambient conditions may not do so under fire conditions;
- The pressure in a fire-engulfed vessel may not drop much below the initial pressure;
- Failure to depressurise may lead to catastrophic failure of the vessel;
- More information is required on heat transfer to vessels and their contents and additional experimental validation is also required for any thermo-mechanical stress models.
- The accuracy of any analysis is dependant on the quality of the data available and currently there are insufficient to produce reliable models.

Models

- The finite element analysis indicates that:
  - failure is likely to take place at the liquid/vapour wall-interface. However, experiments with pool and jet fires on vessels containing LPG indicated that failure initiates at localised hot spots on the vapour wall;

- constraints/loads due to connected pipework may result in additional stresses and early failure at these locations; and
- even when depressurisation satisfying the API recommendations occurs, the vessel fails.

- The output from BLOWFIRE is not readily compatible with the input requirements of the ANSYS code and considerable processing of the data is required.
- From experimentation, it is known that the liquid/vapour interface is normally a two phase transition due to rapid boiling of liquid. This implies that some modification of the finite element analysis would be required to take this into account, because currently it assumes a sharp interface between the two phases. With a gradual transition between phases, the temperature gradient in the vessel wall would be less severe and other stress points e.g. at the hottest positions, may become more significant.

Guidance

- The case study suggests that the currently available models are insufficiently validated to allow production of suitable guidance on prediction of vessel behaviour in fires.

9. CONCLUSIONS

The following conclusions are given in terms of missing and new data, modelling and guidance in each topic area. Overall conclusions are given in the last subsection.

9.1 NEW AND MISSING DATA

Fire hazards and mitigation

- On the whole, the information given in the 'Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire, SCI-P-112, 1992', remains valid for all the fire scenarios which concern jet fires and pool fires in the open. However, new information is now available in the case of two-phase jet fires. The most notable gains have been in understanding the behaviour of jet fires and pool fires in modules (confined fires) and many of the 'major difficulties and uncertainties' in the Interim Guidance Notes are no longer valid.
- Directed water deluge, at the currently specified rate (for pool fires) of 10 $dm^3\ min^{-1}\ m^2$, cannot be relied upon to maintain a water film over the whole surface of a pressure vessel in an impinging jet-fire scenario. However, the rate of heat transfer is reduced by at least a factor of two. Higher flow rates of water (ca. 2 – 3 times) through medium velocity sprayers, water monitors or high velocity sprayers can protect against some jet fires but, as yet, insufficient evidence is available for this to be relied upon.
- A Jet-Fire Resistance Test of Passive Fire Protection Materials has been developed. Except in cases where the geometry of a representative specimen severely effects the properties of the jet fire used, the test reproduces the conditions in a large (3 $kg\ s^{-1}$) natural gas jet fire. This test, when used in conjunction with hydrocarbon furnace tests, offers a reliable method of comparing the performance of PFP materials in a jet fire. The evidence suggests further work is required to assess the resistance of flange connections to jet-fire attack. The data suggest that an adequate passive fire protection system can reduce the heat transfer to a pressure vessel by a factor of 10.

Thermo-mechanical properties of steel

- The following steels have been identified as the most commonly used for pressurised systems such as an offshore separator: BS 1501-225-490B LT50, BS 1501-224-490LT, Carbon steel 516 Grade 70, 22 Cr duplex UNS S31803 and stainless steel 316.

- Although property data at ambient temperature are available, high and low temperature data are not available for some of the steels. Low temperature data are required if excessive cooling occurs during emergency depressurisation and high temperature data are required if the system is to be designed to withstand a significant fire loading.

Thermo-mechanical response

- The distribution of stresses in pressure vessels is due to internal pressure, thermal gradients, external loads and creep.
- The data suggest that the most probable failure mechanism is as follows:
    - At elevated temperatures, the combination of mechanical stress, thermal stress and stress concentrations due to stress risers and the associated strains lead to a local exceedence of the ultimate tensile strength and the rupture strain. A ductile rupture occurs and an initial crack forms.

  - Local stress around the crack re-distributes with a very high stress concentration at the crack tip.
  - The conditions at the crack tip are such that fracture criteria are exceeded and the vessel “unzips” in a trajectory that is approximately normal to the direction of principal stress.
  - In fires, flange connections lose their tightness because of a decrease of the contact pressure. This is caused by the temperature-induced expansion of the flange bolts being higher than that of the flanges. Moreover, because of uneven heat distributions, the bolts elongate differently and leaks occur in the areas with higher temperature.
- Little information exists on the performance of pressure relief devices under fire engulfment conditions. Although standard tests exist for isolation valves engulfed in fire, there are no analogous tests for pressure relief devices. Such tests may need to be developed to ensure that the devices will operate in a satisfactory manner under fire loading.
- The evidence suggests that vessels operating at modest pressures are most vulnerable due to their thinner walls. The walls of very high pressure vessels provide such a large thermal mass that even severe fires should not cause the shell of the vessel to fail.
- Flanged connections to vessels are known to be particularly vulnerable to non-uniform heating from a jet fire and severe leakage may be as important as vessel rupture. The evidence suggests further work is required to assess the resistance of flange connections to jet-fire attack.

Pressure relief and depressurisation

- Data are now available for emergency depressurisation of multi-component hydrocarbon mixtures but not under fire loading conditions. Data are available relating to the response of vessels, containing liquid petroleum gas, to flashing liquid propane jet-fire impingement but only for pressure relief rather than emergency depressurisation. No data were found relating to emergency depressurisation under fire loading.

Thermal response of pressure systems

- The only jet-fire thermal response data obtained on a reasonable scale are from trials with ca. 2 kg s^{-1} flashing liquid propane jet fire impinging on 2 tonne LPG vessels fitted with a pressure relief valve. There appear to have been no fire engulfment trials performed on pressure vessels fitted with emergency depressurisation systems containing either simple or multi-component hydrocarbon mixtures.

Performance standards

- Performance standards related to the resistance of pressurised systems to fire attack depend on having robust engineering acceptance criteria and a robust method to determine if these are met. At present, these do not exist.

Case study

- A system designed to depressurise under ambient conditions may not do so under fire conditions.
- The pressure in a fire-engulfed vessel may not drop much below the initial pressure. Failure to depressurise may lead to catastrophic failure of the vessel.
- More information is required on heat transfer to vessels and their contents and additional experimental validation is also required for any thermo-mechanical stress models.

- The accuracy of any analysis is dependant on the quality of the data available and currently there are insufficient to produce reliable models.

9.2 MODELS

Fire hazards and mitigation

- For unconfined fires, commercially available, semi-empirical, models can provide accurate prediction of flame shape, flame size and radiation flux to external objects but not heat fluxes to impinged objects. CFD fire models may be most suitable for fires interacting with complex structures. The gains in the understanding of the behaviour of confined hydrocarbon fires have yet to be translated into commercially available and validated models.
- No directed water deluge models currently exist that adequately link the amount of water delivered to the nozzles with the actual coverage of the protected equipment. These are required because the level of fire protection is more related to the water film thickness and water flow rate over the surface of the vessel than to the amount of water delivered to the nozzles.
- Empirical models that relate heat transfer to char thickness exist for intumescent PFP materials. There is increasing use of sophisticated models for PFP materials but these tend to be applied to specific products.

Thermo-mechanical properties of steel

- The following thermal and mechanical properties of steel at elevated temperatures are required for the modelling of the thermo-mechanical response of pressurised systems: coefficient of thermal expansion, thermal conductivity, specific heat, modulus of elasticity; shear modulus, Poisson’s ratio, and stress-strain curves (or yield stress, ultimate tensile strength and rupture strain (elongation)).

Thermo-mechanical response

- Although successful attempts to simulate vessel behaviour using finite element analysis have been found, no work has been carried out on any of the five steels identified in Section 3 as being in most common use offshore.

Pressure relief and depressurisation

- In order to perform pressure relief sizing in fire situations, it is necessary to determine whether two-phase relief can occur. Models for two-phase relief under fire loading exist but may not have been fully validated.
- Whilst a validated model exists for emergency depressurisation without fire loading, there are no fully validated models in which fire loading is taken into account.

Thermal response of pressure systems

- Most of the tools available to study the behaviour of a vessel and its contents in a fire have been developed for LPG storage tanks incorporating PRVs and these have only been tested against a few experiments of small LPG tanks in fires. This is a long way from vessels containing multi-component hydrocarbons at high pressure being rapidly depressurised in an emergency.

- None of the models designed for predicting the response of LPG vessels exposed to engulfing fire is ideal. Most of the models either do not reliably predict the response or else the methodology applied is incorrect. Validation using a greater range of vessels and fire conditions is required.
- Confidence in the BLOWFIRE model for emergency depressurisation will only be established if it is successfully validated with medium scale experiments, under process conditions representative of offshore operations.

Performance standards

- Quantitative Risk Assessment is used to confirm that high level performance standards are achieved, and the QRA rule sets also depend on having robust engineering acceptance criteria.
- Design Accidental Loads are determined for the risks calculated by the QRA that exceeded the risk based performance standards and the facility is designed to resist these DALs.

Case study

- The simplified finite element analysis indicates that:
    - failure is likely to take place at the liquid/vapour wall-interface. However, experiments with pool and jet fires on vessels containing LPG indicated that failure initiates at localised hot spots on the vapour wall;
    - constraints/loads due to connected pipework may result in additional stresses and early failure at these locations; and
    - even when depressurisation satisfying the API recommendations occurs, the vessel fails.
- The output from BLOWFIRE is not readily compatible with the input requirements of the ANSYS code and considerable processing of the data is required.
- From experimentation, it is known that the liquid/vapour interface is normally a two phase transition due to rapid boiling of liquid. This implies that some modification of the finite element analysis would be required to take this into account, because currently it assumes a sharp interface between the two phases. If there is a gradual transition between phases, the temperature gradient in the vessel wall would be less severe and other stress points e.g. at the hottest positions, may become more significant.

9.3 GUIDANCE

Fire hazards and mitigation

- On the whole, the information given in the 'Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire, SCI-P-112, 1992', remains valid for all the fire scenarios which concern jet fires and pool fires in the open. However, an updating of the Interim Guidance Notes is under consideration. This should include the new information identified in Section 2.
- More up-to-date guidance is required in relation to water deluge protection because current guidance is only for protection against pool fires. Although the guidance indicates that water deluge at the currently specified rates may not be suitable for protection against jet fires, there are scenarios in which it can be effective.
- The Jet-Fire Resistance Test is now widely used and the procedure is to be produced as a British standard. At present, the procedure only covers coating materials. However, it can be used to assess PFP systems such as cable transit systems. To allow for this, modifications are required to the procedure/British Standard.

Thermo-mechanical properties of steel

- Some performance standards require revision to incorporate the necessary property data at high and low temperatures. It is important for the survival of a pressurised system affected by a fire that all the properties related to functionality and survivability meet the criteria of performance standards throughout the entire survivability time.

Thermo-mechanical response

- Only one reference gives guidance directly related to prediction of fire induced stress effects in pressure systems. However, this does not give details on which methods may be applied for rupture calculations or which rupture criteria should be used.

Pressure relief and depressurisation

- Pressure relief systems are generally designed for gas-only flow. However, during emergency depressurisation, two-phase flow is more likely to occur and is not considered in the existing guidance. In particular, API 520 does not yet adequately address two-phase flow, although revision of this document is in progress. A pressure relief and blow-down systems JIP is in progress. This is aimed at investigation of two-phase relief of systems containing hydrocarbons, but not under fire conditions.
- Current industry guidance, e.g. API 521, does not consider severe fires, particularly impinging jet fires, that could lead to catastrophic failure of vessels before the inventory has been safely removed.
- The Center for Chemical Process Safety (CCPS) has produced guidance on pressure relief and effluent handling. This provides a method to calculate mass fluxes for gas and two-phase flow discharges.
- HSE has produced a workbook on the relief sizing methods for chemical reactors. This provides hand or graphical calculation methods for sizing relief systems.

Thermal response of pressure systems

- Due to a lack of validated models, no guidance is available on emergency depressurisation under fire loading conditions.

Performance standards

- Guidance is needed on the rule sets for use in QRA, the determination of DALs and appropriate engineering acceptance criteria.

Case study

- The case study suggests that the currently available models are not sufficiently validated to allow production of suitable guidance for prediction of vessel behaviour in fires.

9.4 OVERALL CONCLUSIONS

The overall conclusions are as follows:

(i) On the whole, the information given in the 'Interim Guidance Notes for the Design and Protection of Topsides Structures Against Explosion and Fire, SCI-P-112, 1992', remains valid for all the fire scenarios which concern jet fires and pool fires in the open. However, new information is now available in the case of two-phase jet fires, the most notable gains being in understanding the behaviour of jet fires and pool fires in modules (confined fires). Many of the 'major difficulties and uncertainties' in the Interim Guidance Notes are no longer valid. Vessels may be protected by water deluge or passive fire protection (PFP). In the past, this has been provided for protection against pool fires rather than jet fires. Although no credit is normally taken for water deluge protection against jet fires, there are scenarios where it can be effective. A standardised jet-fire resistance test of passive fire protection materials is now available, allowing, reliable product comparison

(ii) Failure of a pressurised vessel subjected to fire attack is related to its strength at elevated temperature. It was found that there are insufficient data available to fully describe the temperature-dependant property of steels used in the manufacture of pressure vessels used offshore. Such information is essential for the development of validated criteria used to define failure of vessels subjected to fire loads.

(iii) Failure of a vessel normally occurs when the stress in the vessel wall exceeds the wall strength. However, this may not be the mode of failure if the vessel is also stressed by connections and constraints or there is severe non-uniform heating. It was found that flanged connections to vessels were particularly vulnerable to non-uniform heating from a jet fire and severe leakage may be as important as vessel rupture. Little information was found on the response of pressure relief valves engulfed in fire.

(iv) Most experiments on vessels under fire loading have been performed on Liquefied Petroleum Gas tanks. The vessels operating at modest pressures and typically have relatively thin walls, making them more vulnerable. The walls of very high pressure vessels e.g. 1st stage separators, provide such a large thermal mass that even severe fires may not result in failure of the vessel. Developments in two-phase flow have been made but are not fully included in guidance. Current guidance used to design emergency depressurisation systems to protect pressure vessels is inadequate for the severe fires that may occur on offshore installations.

(v) Most of the models available to study the behaviour of a vessel and its contents in a fire have been developed for LPG storage tanks incorporating pressure relief valves and have only been tested against relatively few experiments on small LPG tanks in fires. A validated model exists for the emergency depressurisation of a vessel without fire loading, but no validated model is available for vessels under engulfing fire conditions.

(vi) Performance standards related to the resistance of pressurised systems to fire attack depend on having robust engineering acceptance criteria and a robust method to determine if these are met. At present, these do not exist. Quantitative Risk Assessment is used to confirm that high level performance standards are achieved, and the QRA rule sets also depend on having robust engineering acceptance criteria. Guidance is needed on the rule sets for use in QRA, the determination of DALs and appropriate engineering acceptance criteria.

(vii) The case study, applying BLOWFIRE and the ANSYS finite element analysis programs to a model vessel, illustrated the difficulties in using the current available tools and the need for improvement.

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10. RECOMMENDATIONS

Current risk assessments have to either assume (possibly incorrectly) that the current industry guidance provides sufficient protection or assume (possibly unnecessarily) some form of worst case scenario where vessel rupture and fire escalation occurs. Improved guidance is required on:

- Fit-for-purpose and cost effective protection of process vessels and equipment against fire loads; and
- Optimisation of different fire protection and mitigation options including; depressurisation, active and passive fire protection and design and material specification of process vessels and equipment

Further work is required to provide:

- A methodology or "route-map" outlining the steps necessary to determine the thermal response of a range of process vessels and equipment subject to a range of fire loads;
- Guidance on appropriate failure criteria for different equipment as a function of the design and material specification, the operating duty (temperature and pressure) and equipment contents;
- When possible, generic depressurisation performance standards to ensure vessel and equipment integrity (as a function of the equipment design and material specification, contents and duty). Alternatively, guidance on how to perform the necessary analysis for specific situations and also on the tools necessary to perform the analysis; and
- A data set designed for the development and validation of response models for equipment containing multi-component fluids typical of both the upstream and downstream oil industry.

ACKNOWLEGEMENTS

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Appendix A

BLAST AND FIRE ENGINEERING FOR TOPSIDE STRUCTURES PROJECT

The Blast and Fire Engineering Project for Topside Structures was undertaken following the Piper Alpha disaster to address key issues relating to the characterisation and mitigation of offshore hydrocarbon explosions and fires. The project started in 1990 with the objective to collate, appraise and disseminate information on explosion and fire loads, the response of structures and production facilities to these loads and the options available to mitigate the loads and their effects.

A1. PHASE 1

The deliverables from Phase 1 of the project were:

- 26 reports that summarised the state-of-the-art on explosion and fire engineering in 1990/1991; and

- The Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire.

All the reports were prepared for The Steel Construction, Ascot in 1991 and were later published by the Health and Safety Executive as Offshore Technology Information (OTI) reports. The Interim Guidance Notes were published by The Steel Construction Institute. The titles and numbers of the reports generated are listed as follows under the work programme headings and with the work package reference.

General

Report number	Work package(s)	Title
OTI 92 585	G1(a)+G2	Generic foundation data to be used in the assessment of blast and fire Scenarios (including typical structural details for primary, secondary, and supporting structures/ components
OTI 92 586	G1(b)	Representative range of blast and fire scenarios
OTI 92 587	G1(c)	The prediction of single and two phase release rates
OTI 92 588	G3	Legislation, codes of practice and certification requirements
OTI 92 589	G4	Experimental facilities suitable for use in studies of fire and explosion hazards in offshore structures
OTI 92 590	G5	The use of alternative materials in the design and construction of blast and fire resistant structures

Blast loading

Report number	Work package(s)	Title
OTI 92 591	BL1	Gas/Vapour build-up on offshore structures
OTI 92 592	BL2	Confined vented explosions
OTI 92 593	BL3	Explosions in highly congested volumes
OTI 92 594	BL4	The prediction of the pressure loading on structures resulting from an explosion
OTI 92 595	BL5	Possible ways of mitigating explosions on offshore structures

Fire loading

Report number	Work package(s)	Title
OTI 92 596	FL1	Oil and gas fires – characteristics and impact
OTI 92 597	FL2	Behaviour of oil and gas fires in the presence of confinement and obstacles
OTI 92 598	FL3	Current fire research, experimental, theoretical and predictive modelling resources

Blast resistance

Report number	Work package(s)	Title
OTI 92 599	BR1	The effects of simplification of the explosion pressure-time history
OTI 92 600	BR2	Explicit analytical methods for determining structural response
OTI 92 601	BR3	Computerised analysis tools for assessing the response of structures subjected to blast loading
OTI 92 602	BR4	The effects of high strain rates on material properties
OTI 92 603	BR5	Analysis of projectiles

Fire resistance

Report number	Work package(s)	Title
OTI 92 604	FR1	Experimental data relating to the performance of steel components at elevated temperatures
OTI 92 605	FR2	Methodologies and available tools for the design/analysis of steel components at elevated temperatures
OTI 92 606	FR3	Passive fire protection performance requirements and test methods
OTI 92 607	FR4	Availability and properties of passive and active fire protection systems
OTI 92 608	FR5	Existing fire design criteria for secondary, support and system steelwork
OTI 92 609	FR6	Fire performance of explosion-damaged structural and containment steelwork
OTI 92 610	FR7	Thermal response of vessels and pipework exposed to fire

Interim guidance notes (IGN)

Report number	Title
SCI-P-112	The Interim Guidance Notes for the Design and Protection of Topside Structures Against Explosion and Fire

An updating of explosion and fire design guidance is under consideration. Further details are given in:

> Burgan B A, 1999, R349 Updating explosion and fire design guidance, FABIG Newsletter, Issue 24, , pp. 23-26, June 1999.

A2. PHASE 2

Phase 2 of the project had two main objectives viz:

- To provide specific information about the characteristics of hydrocarbon explosions and fires and means of mitigating these hazards; and
- To generate accurate information and data for use in evaluating and improving the accuracy of fire and explosion models.

The project included the following work packages:

- Unconfined jet fire test programme;
- Confined jet and pool fire test programme;
- Explosion test programme;
- Fire model evaluation exercise; and
- Explosion model evaluation exercise.

The reference to the final summary report is:

> Selby C A and Burgan B A, 1998, Blast and Fire Engineering for Topside Structures – Phase 2: Final Summary Report, SCI Publication no. SCI-P-253, The Steel Construction Institute, ISBN 1 85942 078 8.

Details of references to the individual reports comprising Phase 2 are given in this final summary report.

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