Economic Commission for Europe

Inland Transport Committee

Working Party on the Transport of Dangerous Goods

Joint Meeting of the RID Committee of Experts and the Working Party on the Transport of Dangerous Goods
Bern, 12-16 March 2018
Item 6 of the provisional agenda
Reports of informal working groups

23 February 2018

Report of the Informal Working Group on the reduction of the risk of a BLEVE during transport of dangerous goods

Transmitted by Spain on behalf of the Informal Working Group

Introduction

1. The Informal Working Group on the reduction of the risk of a BLEVE during transport of dangerous goods met on 20-22 February 2018 in Madrid on the basis of the mandate of the RID/ADR/ADN Joint Meeting, under the chairmanship of Mr. Claude Pfauvadel (France).

2. The Informal Working Group brought together experts representing contracting parties/member states, international organisations and industry as mentioned in the enclosed list of participants.

3. The following documents were taken as the basis for the continued work:

– ECE/TRANS/WP.15/AC.1/148, paragraphs 83-85
– http://www.unece.org/fileadmin/DAM/trans/doc/2017/dgwp15ac1/ECE-TRANS-WP15-AC1-2017-42e.pdf
– http://www.unece.org/fileadmin/DAM/trans/doc/2017/dgwp15ac1/ECE-TRANS-WP15-AC1-2017-GE-INF41a1r1e.pdf
– http://www.unece.org/fileadmin/DAM/trans/doc/2017/dgwp15ac1/ECE-TRANS-WP15-AC1-2017-GE-INF41e.pdf
– http://www.unece.org/fileadmin/DAM/trans/doc/2017/dgwp15ac1/ECE-TRANS-WP15-AC1-17-BE-inf15e.pdf

4. Additionally, the following document was also submitted to the participants:

– Cost-effective application of thermal production on LPG road transport tanks for risk reduction due to hot BLEVE incidents, A.M. Birk (Risk Analysis, Vol. 34, Nº 6, 2014)

5. After the approval of the agenda, the meeting began with a presentation by INERIS of the work done until the last Joint Meeting in September 2017, and the additional cases that have been calculated since then. Sweden also briefly presented the document “Cost-effective application of thermal production on LPG road transport tanks for risk reduction due to hot BLEVE incidents”.

6. After that, factors important to be taken into account for the future work were analysed, and have to be further studied:

Scope of work, affected substances

- Scope of works not only LPG; also other substances to be considered.
- Other gases with similar properties: ammonia, chlorine, DME.
- Interest in the study of LNG, but adaptations to the calculation model mentioned in the presentation are needed because LNG is refrigerated and the transport pressure is low.
- Some measures taken to avoid BLEVE, in particular the measures to protect the load from the fire, can be extended to pressure receptacles and could be similar to measures taken for the transport of goods of other classes (explosives?).

Fire (see paragraph 7 for possible fire scenarios)

- Pool fire of an infinite time is an unrealistic case.
- Necessary parameters to define fire: heat flux, ratio of convection/radiation, flame temperature and tank emission coefficient.
- IAEA packaging requirements: 800ºC for 30 minutes.
- 900ºC is considered by some as a too high temperature, difficult to maintain even in testing facilities.
- Ongoing works on ISO standard for definition of full fire engulfment (ISO 21843, determination of resistance to hydrocarbon pool fires for protection systems for pressure vessels).
- 6.7 asks portable tanks to be “able to resist a fire”, unrealistic requirement.

Pressure relieve valves

- Need to consider the functioning of safety valves in tanks lying on one side, including its operation in liquid phase.
- PRV should be designed to not be damaged by the fire itself. Effectiveness of PRV under the different scenarios in paragraph 7 should be verified.
- Need to assess the reliability and behaviour of safety valves in service. UK offered to provide data based on the verification of safety valves in intermediate and periodic inspections.

Coating

- Investigations on different materials that could be used as coating are needed. Examples could be polyurethane coating, glass wool under thin metal layer or intumescent paint, very different behaviour.
- Physical properties of the coating itself have to be checked, specially aging process. Need of data to be provided by manufacturers.
- Scratches in case of accident can be very different depending on the material of the coating.
- Experiences with glass wool coating. The Netherlands offered to provide data on its experiences on the subject.
- Test data for physical data for properties needed (input for calculations).

Calculation model used by INERIS

- It has been noted that to describe the fire the present calculation model only uses the heat flux and maximum temperature.
- Can be used to model scratches in coating. Properties of steel and thermal coating have been modelled separately.
- Improvements to the model and calculations to be done are listed in paragraph 9.

Cost-benefit analysis

- Valuable information, but very sensitive to input data.

– Input data, and some of the results, does not correspond to some data available from industry experience. Industry offered to provide this data to the group.
– Not enough knowledge about some input data used to be conclusive.
– Need to agree on a common decision making criteria.
– This kind of analysis is more applicable as comparative assessment of different measures.

7. The group agreed that the following scenarios should be analysed using the appropriate parameters to define the fire such as heat flux, ratio of convection/radiation, flame temperature and tank emission coefficient, taking into account the dynamic evolution of fire:

1. Scenario 1: Fire coming from the own transport unit. Possible origins of the fire are the tires, cabin and the fuel tank (for road tanks only).
2. Scenario 2: Fire fuelled by the own content of the tank (30.000 l, 50.000 l for rail). Possible pool fire.
3. Scenario 3: External fire coming from a different transport unit, for example second tank containing 30.000 l (50.000 l for rail) flammable liquid, possible pool fire.
4. Scenario 4: External fire, torch fire from one tank that affects a different tank. Specially to be considered for rail transport.

Different probabilities of occurrence should be assigned to each of these scenarios. The duration of the fire in the different scenarios will be limited by the combustive material present in the different scenarios.

8. The group agreed that the following measures could be interesting, both to mitigate and prevent the occurrence of a BLEVE:

a. Safety valves
b. Coating
c. Localized automated fire extinguishing systems for tyres, engines and fuel tanks
d. Local protection of load from tyres and axles for example by steel plates
e. Measuring temperature increase to detect starting fires
f. Measures on parking areas for dangerous goods trucks (Spain and France offered to provide data)

9. Taking into consideration the above mentioned discussions, it was agreed to try to study the inclusion into the INERIS FEM-analytical model of the convection/radiation ratio and the tank emission coefficient as parameters to model the fire and its effects. Afterwards this model could be used for analysis of the following cases with the help of it:

- Analysis of scenarios defined under paragraph 7
- Tank lying on one side; behaviour of PRV in liquid phase
- Analysis of non-closing PRV
- Estimation of the amount of water needed to cool the tank (0,5-3,5m^3/min?)
- Localized fire starting from cabin, tyres and fuel tank: will imply 3D modelling
- Modelling of damages caused by an accident in coating (small sized, scratches?)
- Modelling of LNG cryogenic vacuum insulated vessel, flammable liquids

France and INERIS will provide a time frame of possible calculations to be done, taking into account the associated agenda and costs, to the JM meeting in March.

10. Members of the working group, and also members from the JM are invited to provide, if possible, further information on the following topics:

- Information on localized automated fire detection and extinguishing systems for tyres, fuel tanks and engine on board
- Information on systems of shields and barriers protecting tyres, fuel tanks and engine
- Preventive measures in parking areas to segregate dangerous goods trucks
- Data and photos of scratches and damages on tanks in case of accident
- Experiences with different types of coatings

11. This information could be sent to the Spanish expert to mercancias.peligrosas@fomento.es to be further distributed into the working group.

12. Work done by the present working group should be brought to the attention of WP15. Additionally WP15 is invited to share information on relevant work done now in WP15 as for example fire protection measures and parking supervision for road vehicles.

Conclusions

13. Further work and additional data are still needed. The working group plans to meet again after calculations with the model have been done, which could be in October. Date and place of the next meeting will be defined according to the availability of data.

14. To define the new working plan and terms of reference of the group the JM is invited to consider all the mentioned work items under paragraphs 7-10 and to decide as appropriate.

15. The WG took note of the necessity to refer to the decision making guide to be produced by Risk Analysis WG hosted by ERA. As this guide has not yet been published this WG has not been able to consider it. Nevertheless, in this meeting no decision has been taken that would require a procedure such as established in that guide.

Annex

16. Annexed to this document is the presentation by INERIS that served as introduction and the document “Cost-effective application of thermal production on LPG road transport tanks for risk reduction due to hot BLEVE incidents” (Annex 1).

17. Additionally the following documents are added, that were distributed in the working group, but not analysed yet, for further use in successive meetings (Annex 2):

- List of historical BLEVES with different substances

- Articles from BAM on work done in 2015 on pressure relieves valves

- Images on incidents with LPG tanks

List of participants

Informal Working Group on the reduction of the risk of a BLEVE during transport of dangerous goods

Madrid (20-22 February 2018)

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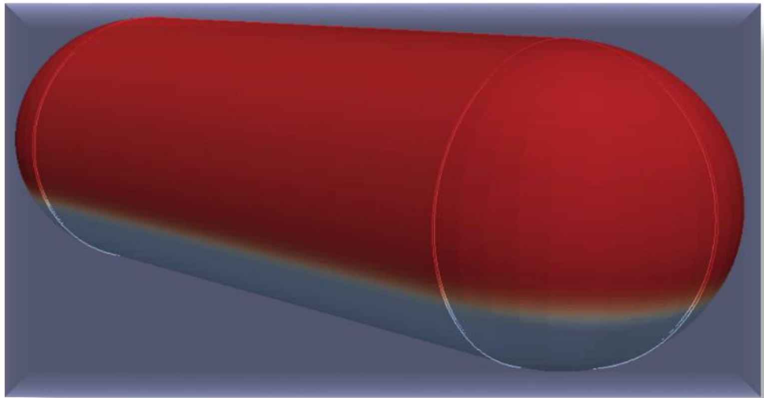
Annex I

Model for the thermal response of Liquefied Petroleum Gas Tanks subjected to accidental heat input



Etienne CATRY
INERIS
Structural Resistance Unit – Accidental Risk Division
BLEVE WG Madrid 02-2018
DRA-18-164468-01794A

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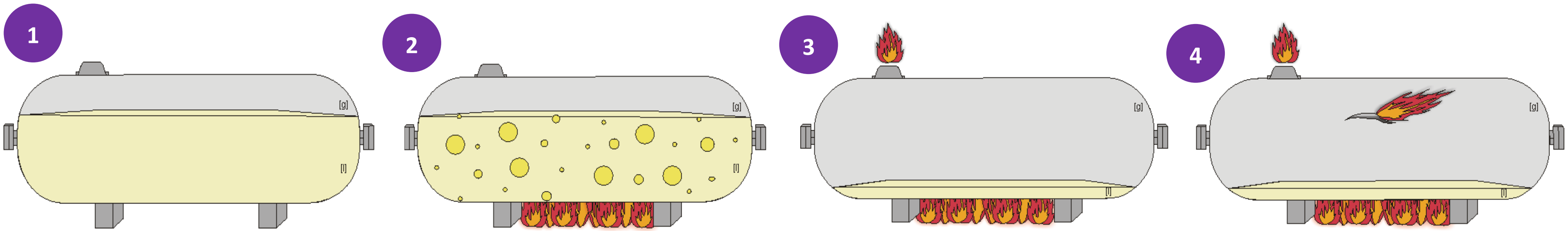
Summary

- Introduction – Context
- INERIS model presentation
- Tank subjected to full fire engulfment
  - Evaluation of a valve efficiency
  - Evaluation of thermal coating and increased steel thickness
  - Conclusion on the PRV/thermal coating efficiency
- Calculation on tanks subjected to fire on lower part
  - Calculation assumptions
  - Calculation results
  - Calculation analysis
- Conclusion after JM Sept 2017
- Calculation on tanks subjected to fire on lower part - Influence of flame temperature
  - Input data
  - Calculation results
  - Summary
- Conclusion after the complementary calculations

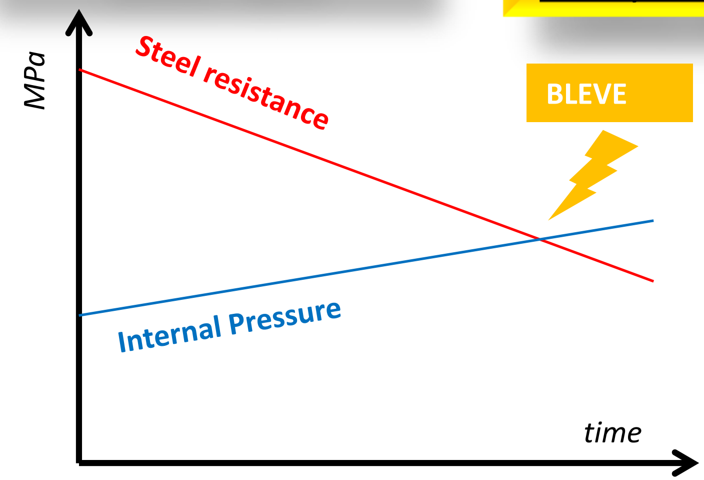
Reminder of previous work: reliability of the model demonstrated

- Previous work:
    - **development of a predictive tool** to study the behaviour of different configurations tanks
    - **demonstration of reliability of the model** by comparison with BAM experiments (2013)

BLEVE scenario



1. The tank is filled
2. Tank is located in fire
Boiled liquid
3. Pressure increases
Liquid level drops
<u>Safety valve opens</u>
4. Pressure continues to rise
Strength reduction of steel
<u>Tank wall cracks</u>



Behaviour diagramm of the tank

Methodological approach used for INERIS model

Input

- ❖ Steel wall characteritics (diameter, thickness,…)
- ❖ Thermal protection (safety valve, coating)
- ❖ Lading characteristics (Level filling, Products)
- ❖ External thermal load characteristics

MECHANICAL MODEL | **THERMAL MODEL**

Model Results

- ✓ Temperature distribution in the tank shell
- ✓ Temperature distribution in the lading
- ✓ Pression evolution
- ✓ Level filling evolution
- ✓ Stress cartography in the tank shell

Failure Criterion
:
Loss of containment

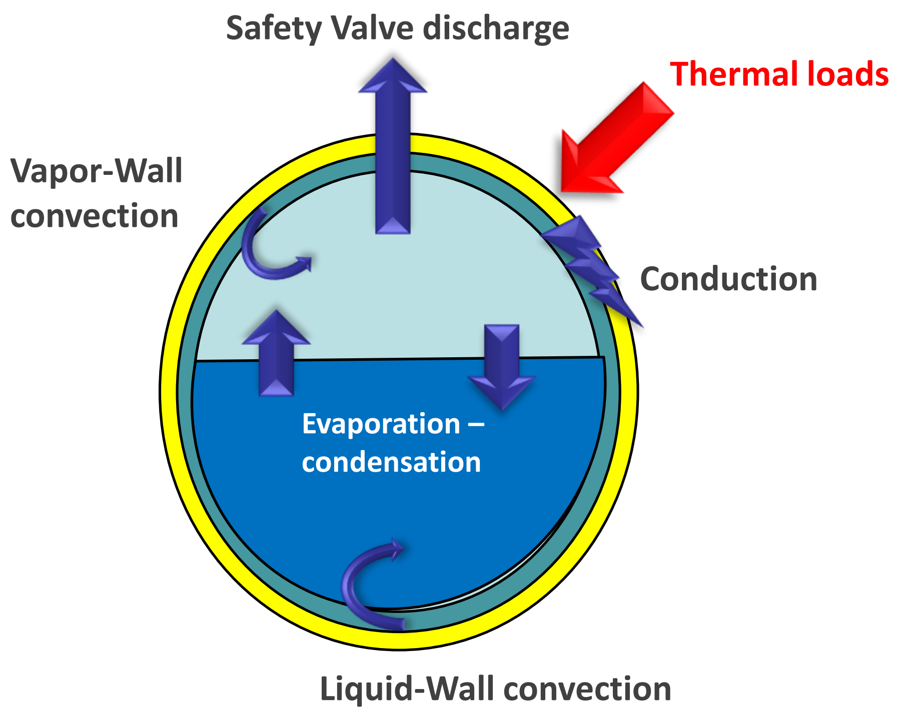
No collapse → ✓Effective protection

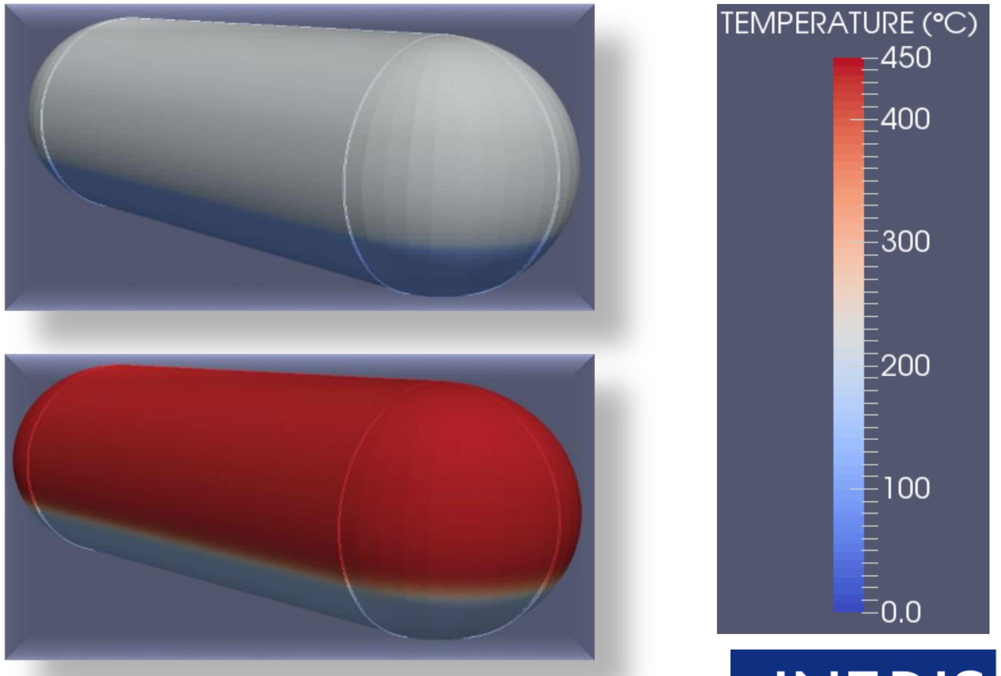
Collapse →

✓Effectiveness of the measure depending on time to failure (to be defined by authorities)

✓Non effective protection

- Models characteristics
  - Finite elements model for the tank shell (insulation + steel wall)
  - Analytical approach with a 2 phase model for the content. This model provides relevant results for tanks with a maximum capacity as used in transport (up to 100 - 150 m^3). This approach is widely used in industry (ex : Vessfire software developed by Petrell As)
  - **Objective**: To predict the temperature (for both phases) and pressure evolution of tanks (with or without coatings) when submitted to heat input.



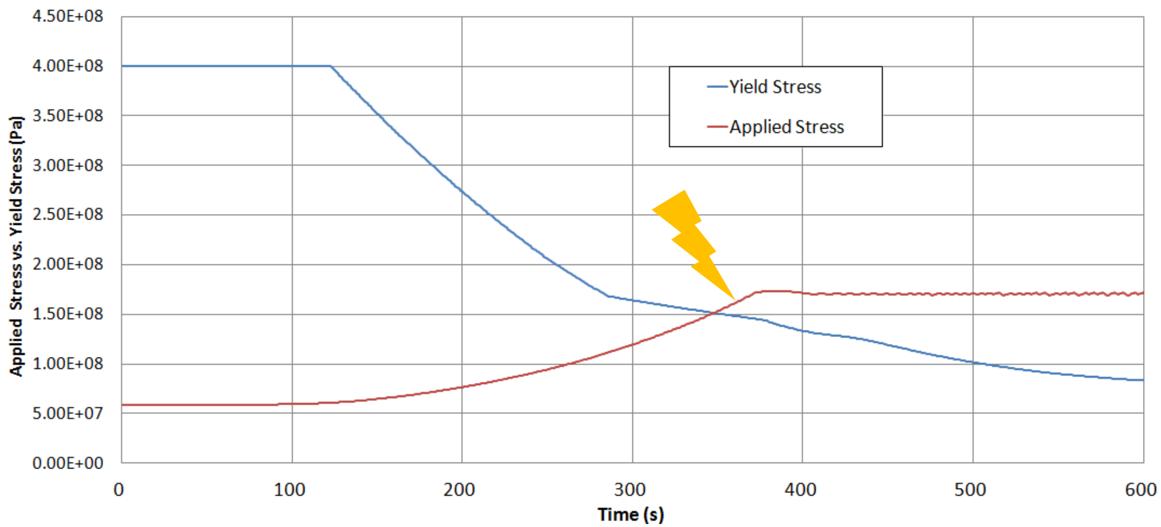


Thermal transfers in LPG tanks under thermal loads

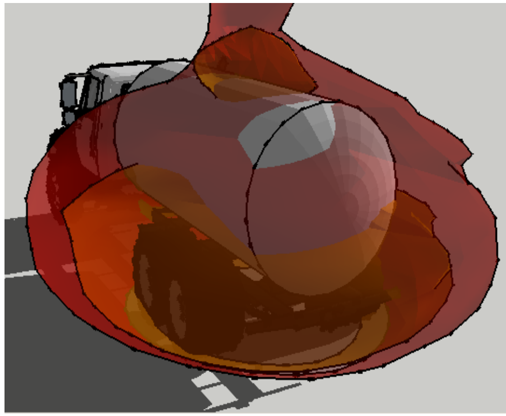
Evaluation of a valve efficiency - Calculation results

- Reminder: these results have been presented at RID ADR Joint meeting 03-2017
- Common PRV considered (diameter 2'' and $P_{opening}$ 16.5 bar) on LPG tank: volume 31m^3, filling rate 50%
- Thermal loading : full fire engulfment

=> **Safety valve is not efficient in that case**



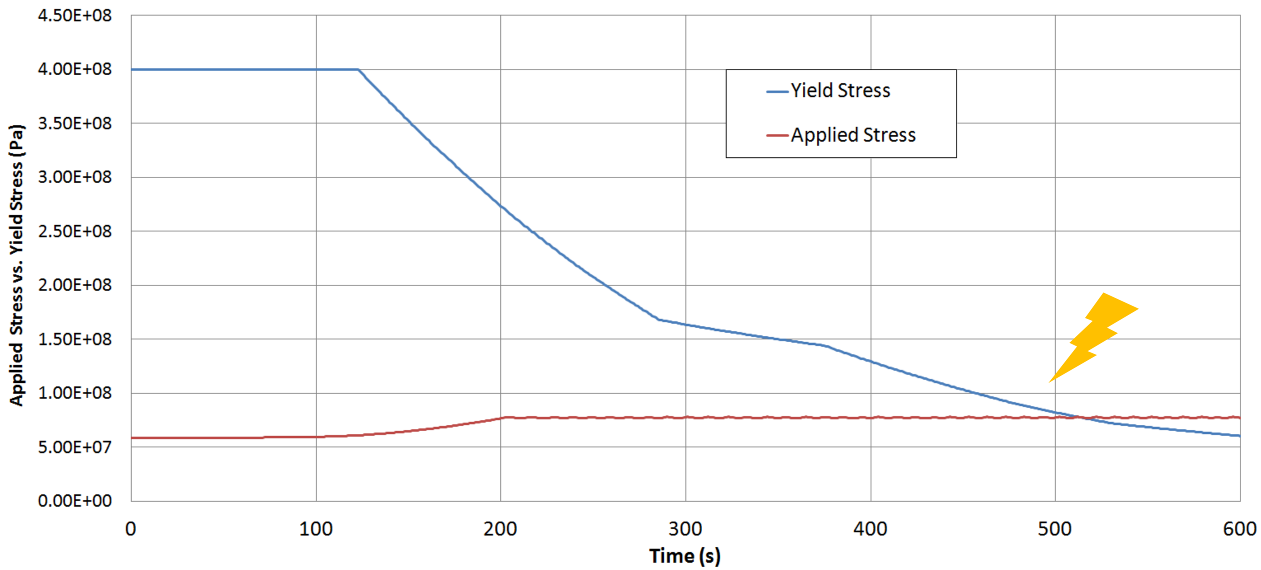
*Results considering **a common safety valve** : **risk of BLEVE***



Full fire engulfment

Evaluation of a valve efficiency - Calculation results

- Test of an ideal safety valve on the same tank with the same thermal loading
- This safety valve set to low pressure (8 bar) is not efficient:
  - A very low applied stress is observed as expected
  - Failure is due to a sharp fall of Yield stress of steel
  - This result can be generalized to all filling rate



Results considering ***an ideal safety valve (set to 8 bar)*** *:* ***risk of BLEVE due to sharp fall of Yield stress of steel***

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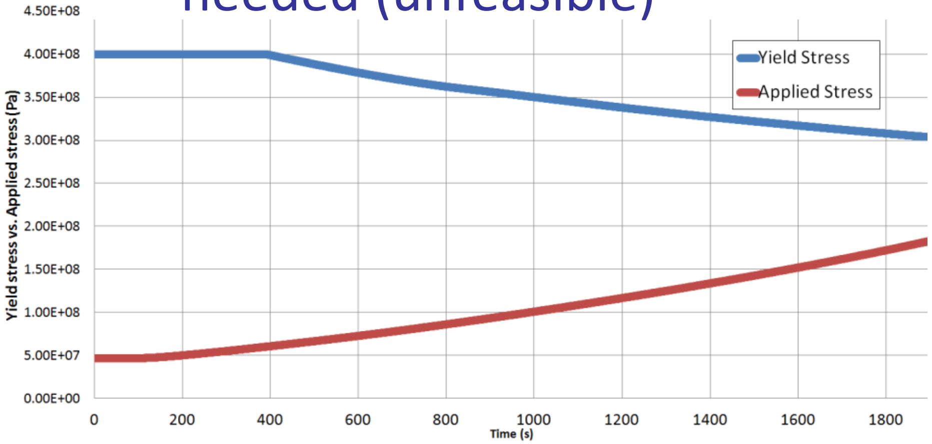
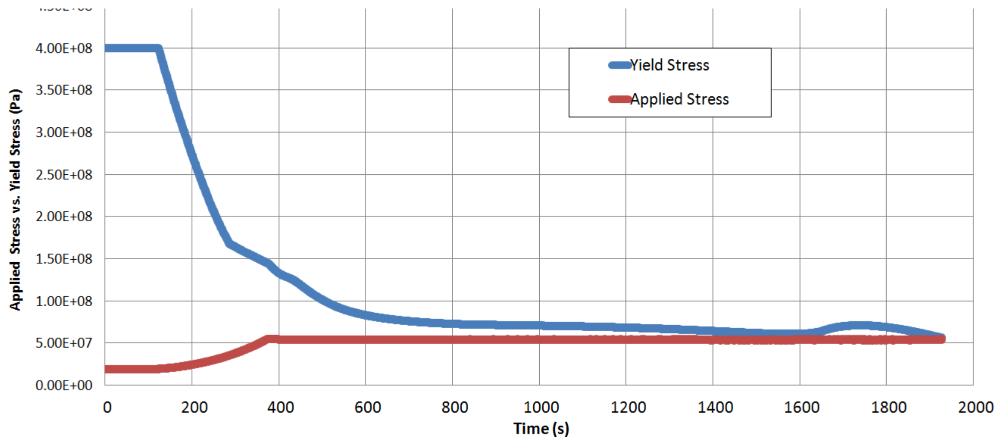
Evaluation of thermal coating and increased steel thickness - Calculation results

- 2 other protections are tested : thermal coating and increasing steel thickness of shell to 3 cm
- Thermal coating can avoid or delay BLEVE but several issues are raised concerning use on trucks:
  - no retrofit about ageing
  - behaviour with vibrations
  - behaviour with various climatic conditions
  - etc…
- Increasing steel thickness of shell is efficient to avoid BLEVE, but 3 cm thick shell are needed (unfeasible)



Results considering ***a thermal coating*** *:* ***no risk of BLEVE***



Results considering ***a 3 cm thick shell*** *:* ***no risk of BLEVE***

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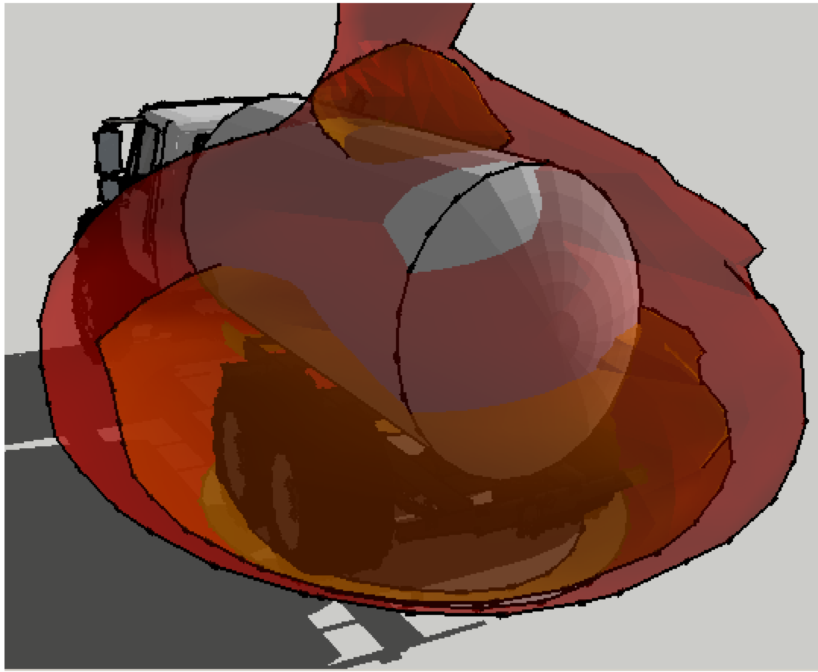
Conclusion on the PRV/thermal coating efficiency (RID ADR Joint meeting 03-2017)

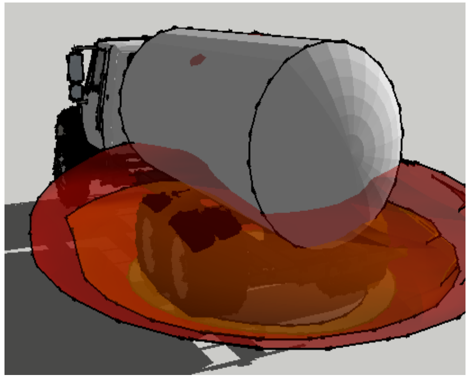
- Valves are not efficient for some scenarios (ex: full fire engulfment)
- Other protections (thermal coating or increasing of shell thickness) may delay/avoid BLEVE but may present issues (ageing, cost, etc…)



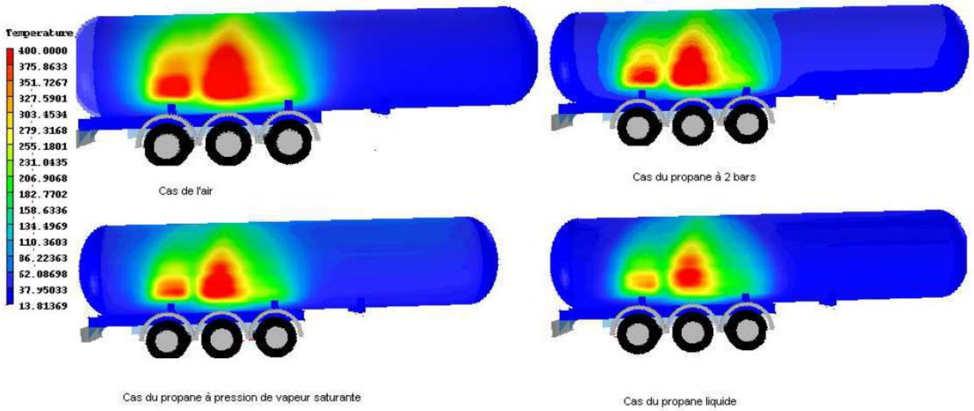
Full fire engulfment

Calculation assumptions

- Calculations led on tanks with safety valve only and subjected to a smaller size fire (localized on lower part of tank)
- New calculations are therefore led considering a smaller size fire scenario with the following conservative parameters:
  - Pool fire on lower part of tank
  - Fire reaches immediately intense burning on the entire length of the truck and has an infinite duration (a real fire can have a duration of 3 hours, and an intense burning of 30 minutes)



Large fire localized on lower half of tank
- Conservative hypothesis-

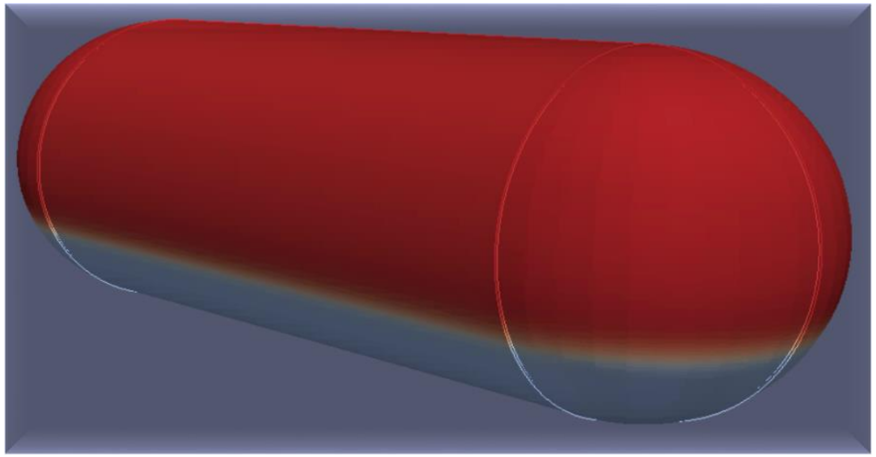


Calculation of real fire spreading [2]

[2] CFPB, Feu de pneus et de cabines sur des citernes GPL, Mai 2010

New calculation assumptions

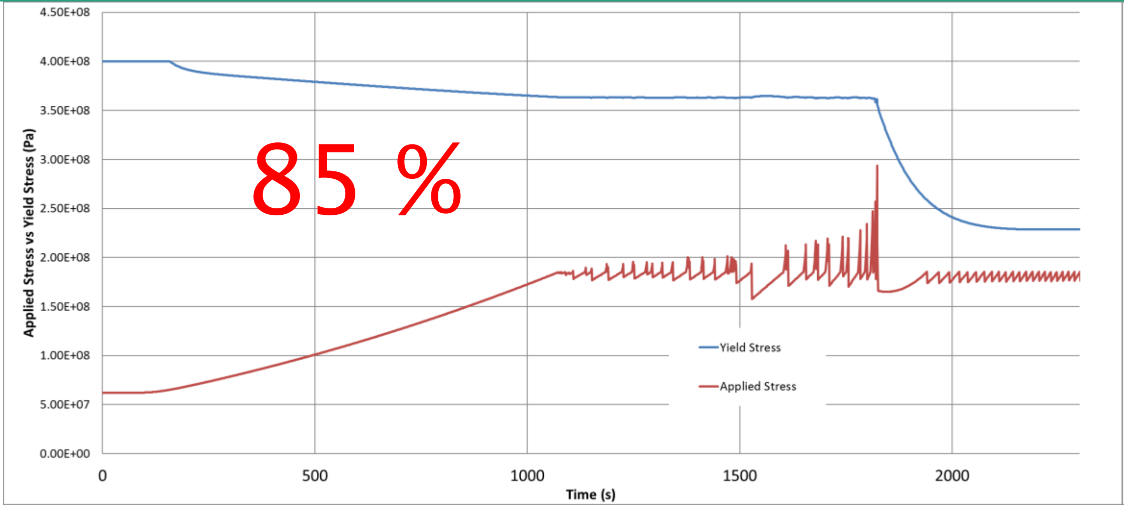
- Characteristics of the LPG tanks:
  - Volume: 31 m^3
  - Common PRV – pressure relief valve- (diameter: 2’’ & Popening: 16.5 bar)
- 3 scenarios are calculated for 3 filling rates (great influence on results): 85%, 50% and 30%



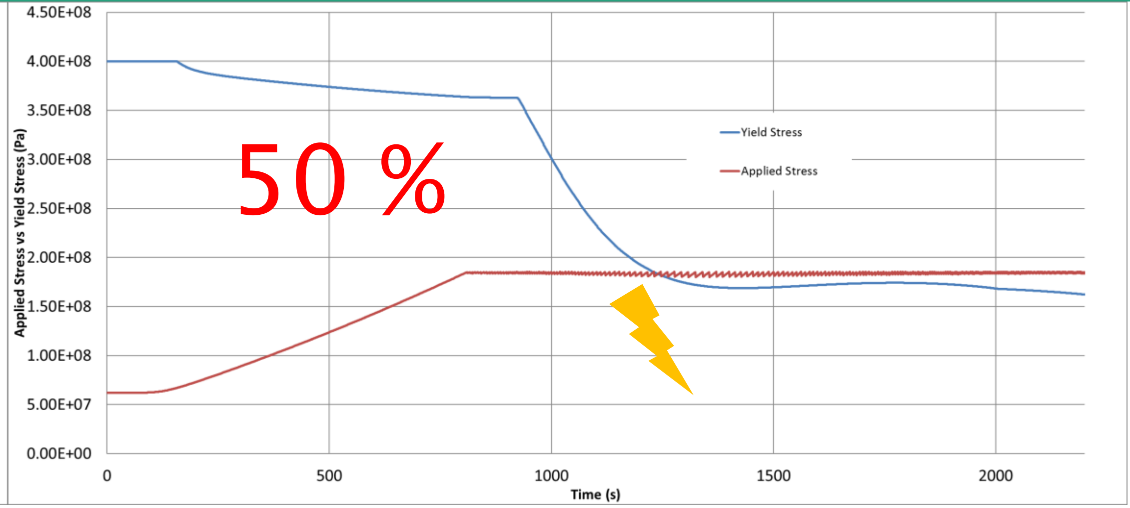
Example of heating of a tank
filling rate: 30 %

Calculation on tanks subjected to fire on lower part

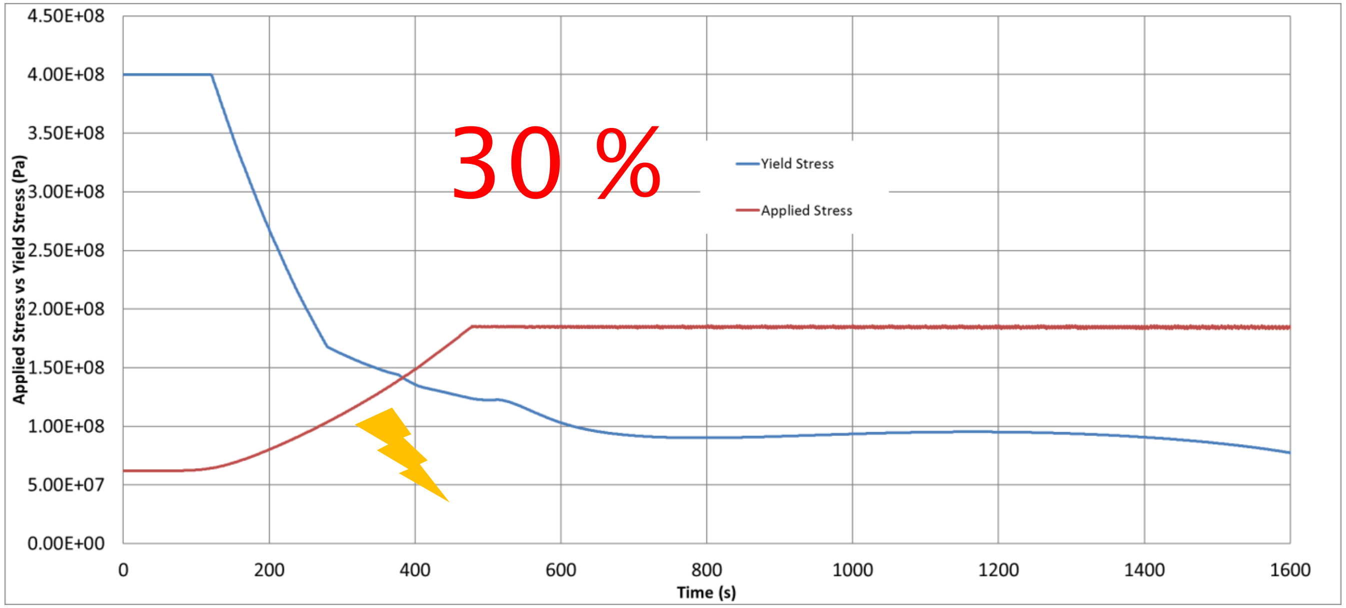
Calculation results



Filling rate: 85 % –safety valve efficient no BLEVE



Filling rate: 50 % – safety valve NOT efficient, BLEVE



Filling rate: 30 % – safety valve NOT efficient , BLEVE

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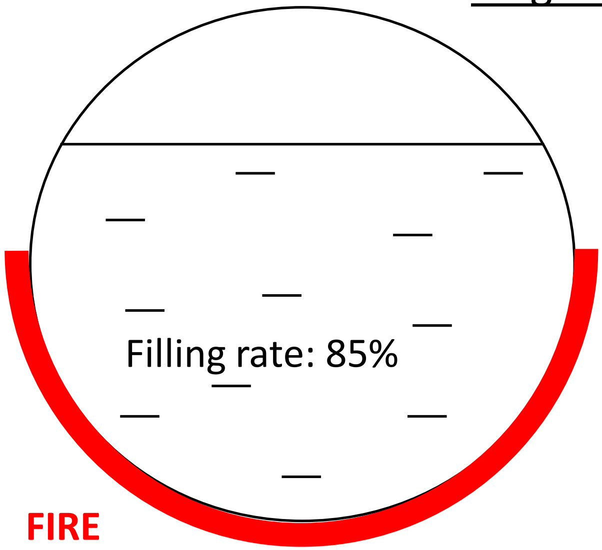
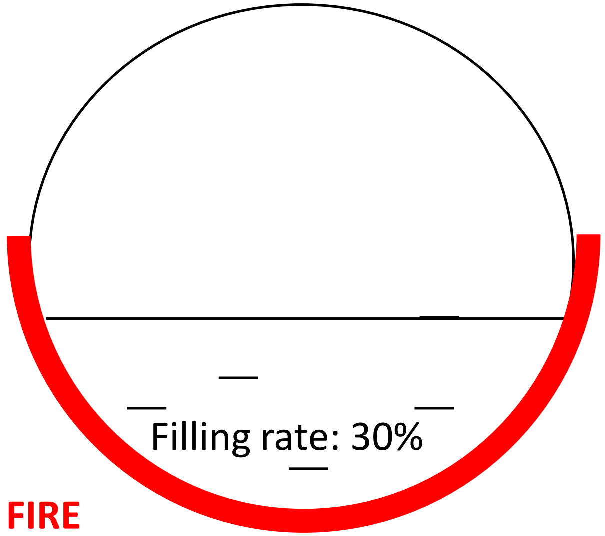
Calculation analysis

- Previous results show the great influence of filling rate on results
- The impact of filling rate is explained below:

Large Fire on liquid phase (Pool fire)



The steel shell in contact with the gas phase is **not** impacted by the fire



The steel shell in contact with the gas phase is impacted by the fire

→ In that case, most of blast scenarios can be excluded with a standard PRV

→ In that case, the blast risk is high (included case where the tank is equipped with an ideal PRV)

Conclusion after the JM sept 2017

- Full fire engulfment:
  - Safety valve only can not protect tank because of mainly the reduction in the steel resistance due to high temperature
  - Tank with an appropriate safety valve and a thermal coating could survive a full fire engulfment until it empties completely
- Fire on lower part only – tank equipped with a common pressure relief valve :
  - Pressure relief valve may avoid or delay BLEVE for tank with high filling rate
  - BLEVE may occur for low filling rate, due to heating of shell in direct contact with gas

- Way forward after the JM sept 2017:
  - Assess the maximal fire a tank equipped with the best safety valve would survive even under low (unfavorable) filling ratio conditions
  - In a first step lower fire temperatures were tested, with exposure of the totality of the lower half of the tank.
  - Calculations are performed on the same tank as previously :

- Characteristics of the LPG tanks:
  - Volume: 31 m^3 Common PRV – pressure relief valve- (diameter: 2'' & Popening: 16.5 bar)

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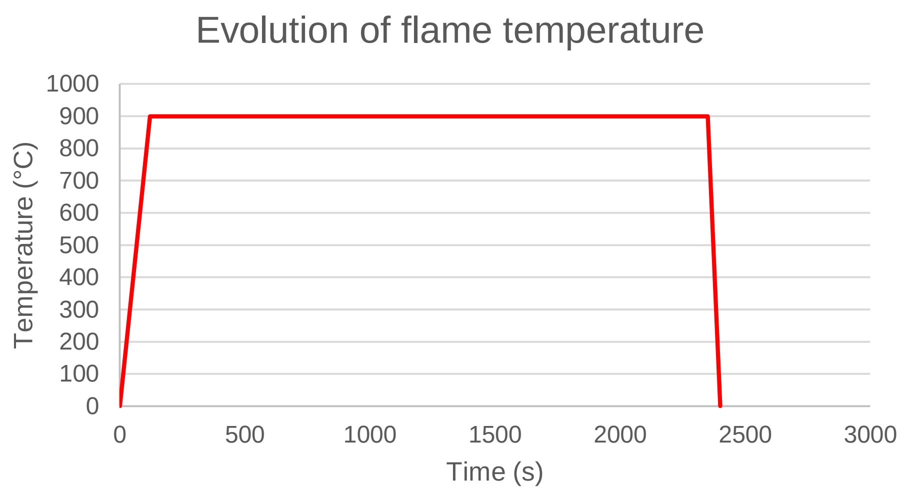
Calculation on tanks subjected to fire on lower part - Influence of flame temperature

Input data

- Tank are subjected to fire on lower part
- Radiative flux is characterized by:
  - Flame temperature
  - Duration

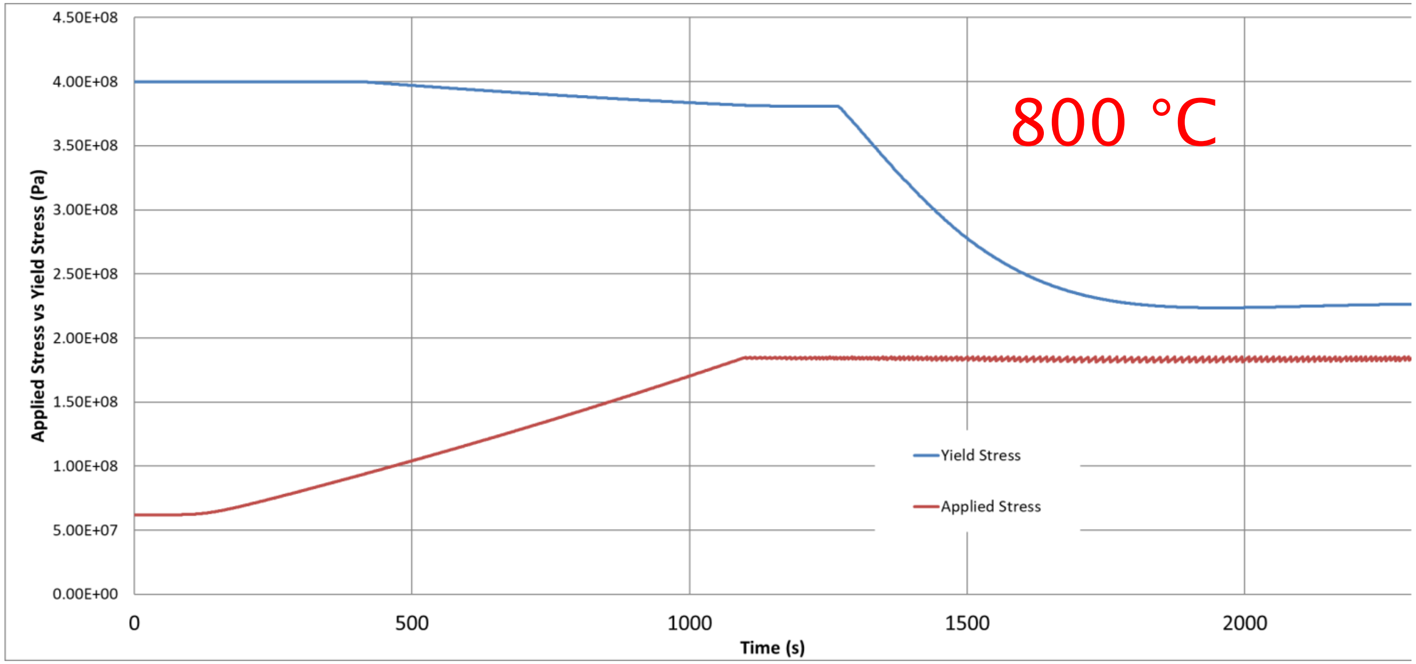


- Real temperature field is not uniform on the lower part. Following the position temperature may be lower than 900°C
  - Calculation are made considering uniform flame temperature between 500°C and 800°C

Influence of flame temperature

Calculation results

- 50% filling rate:
  - no BLEVE for a 800°C flame temperature



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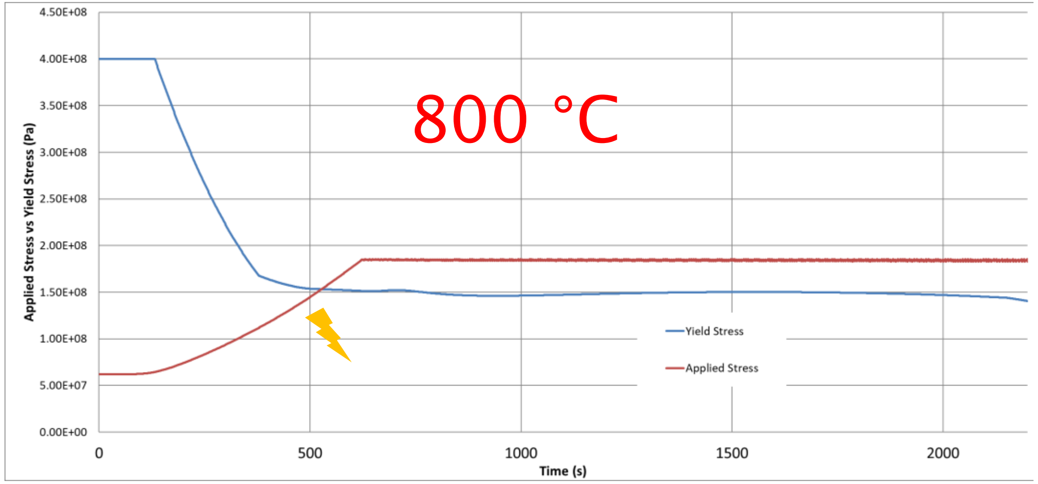
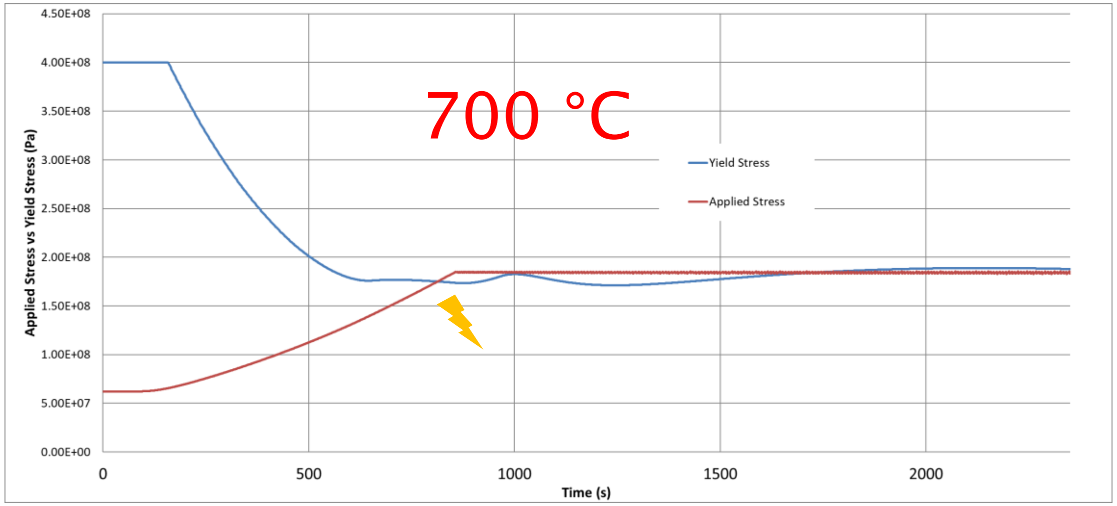
Influence of flame temperature

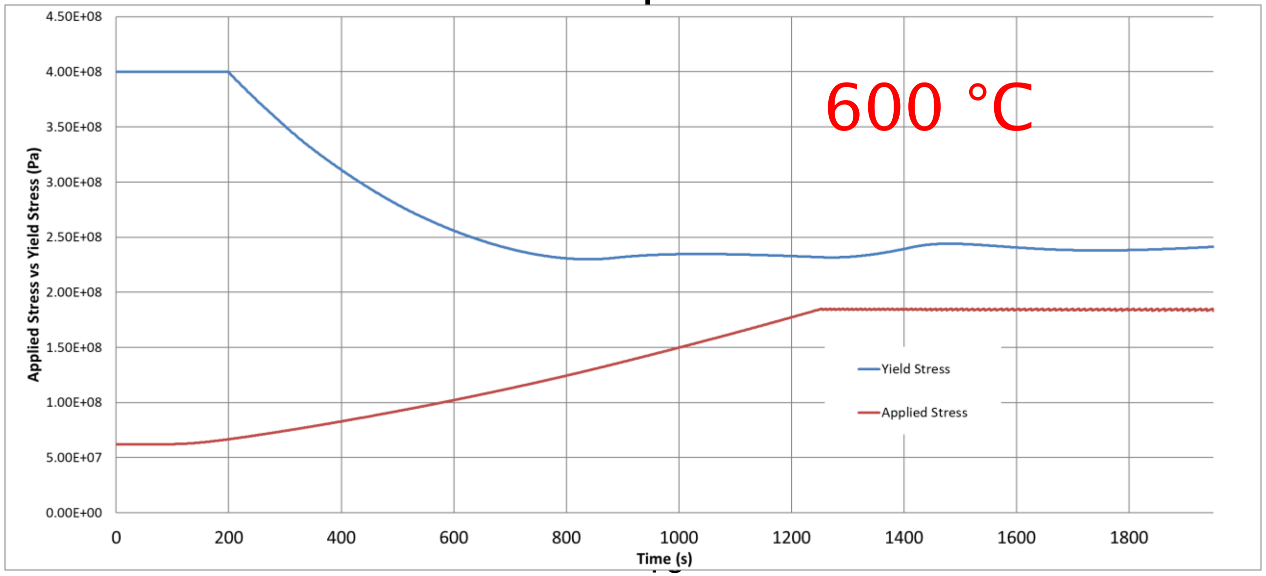
Calculation results

- 30% filling rate:
  - BLEVE for 700 and 800°C flame temperature





  - No BLEVE for a 600°C flame temperature



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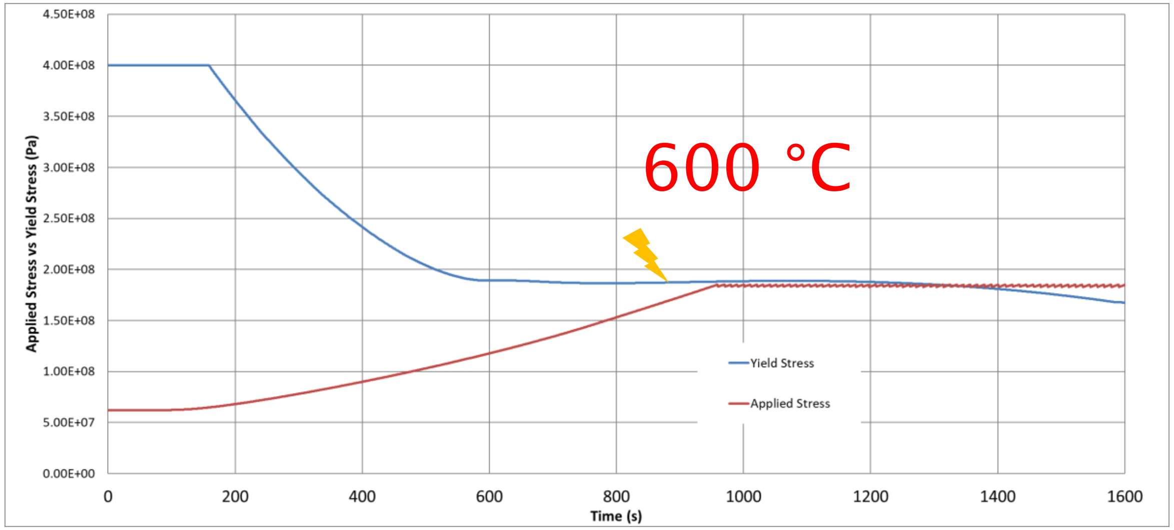
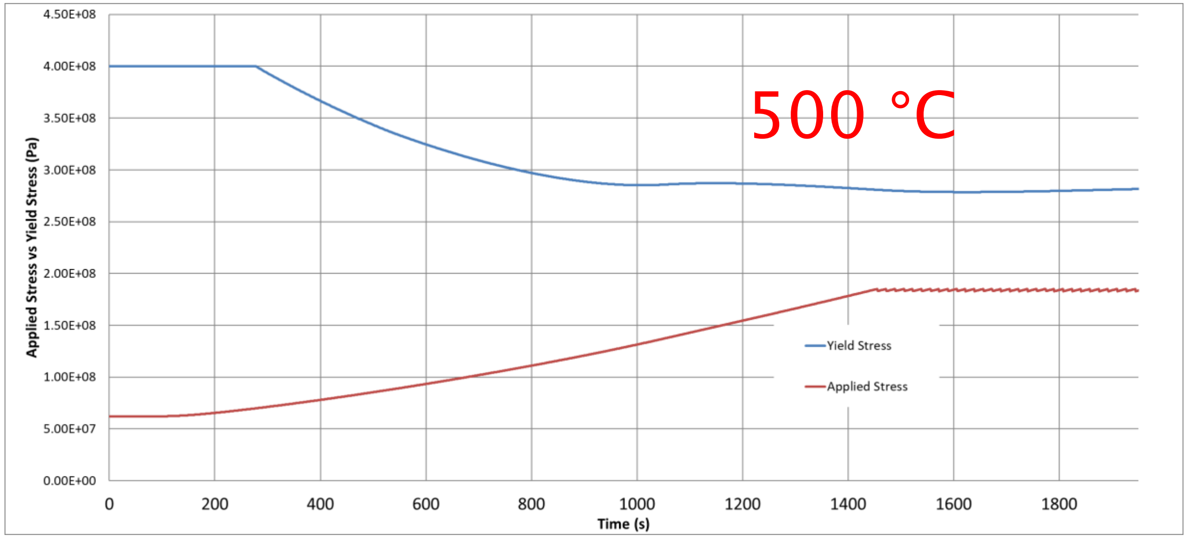
Calculation results

- 10% filling rate:
  - BLEVE for a 600°C flame temperature



  - No BLEVE for a 500°C flame temperature



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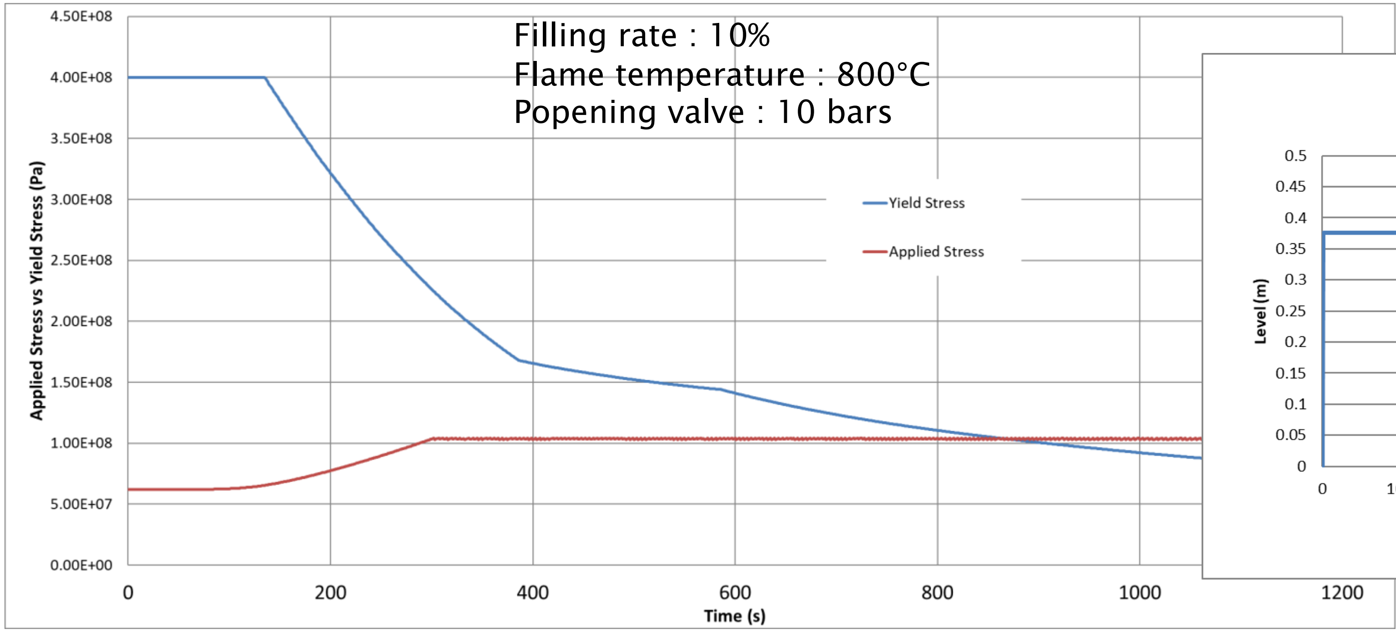
- Influence of flame temperature / filling rate

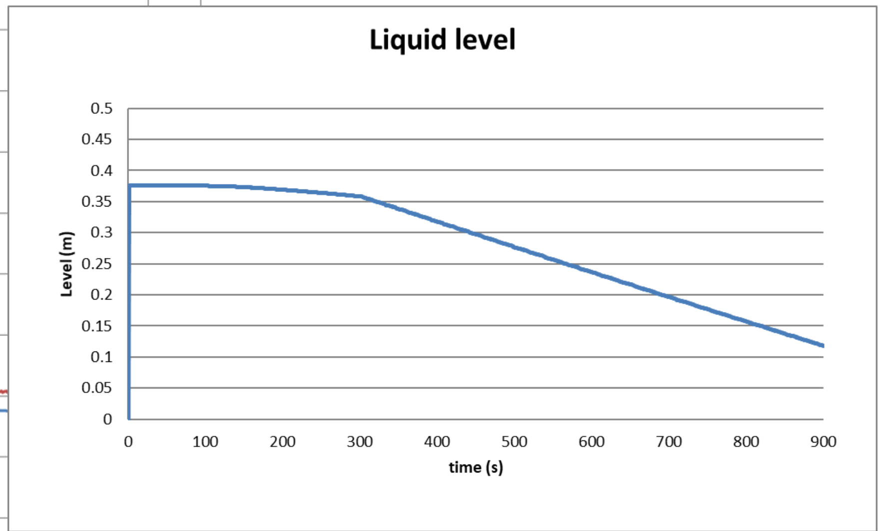
Filling rate \ Flame temperature	900 °C	800 °C	700 °C	600 °C	500 °C
10%	<400s	460s	680s	960s	>2400
30%	400s	530s	810s	>2400	>2400
50%	1200s	>2400s			
85%	>2400s				

Time before BLEVE for different flame temperature / filling rate

Conclusion after the complementary calculations

- **Conclusion:**
  - New calculation show that even when the lower half part is totally exposed to fire a lower temperature allows the tank to survive when equipped with a valve
  - Depending on the temperature this is possible even with low filling ratio
- **Fire on lower part only – tank equipped with a common pressure relief valve :**
  - When the filling ratio is very low the influence of the time needed for emptying the tank need to be assessed (depending in the type of valve the tank might get empty before critical pressure is reached)





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- **Way forward:**
  - Assess the maximal fire a tank equipped with the best safety valve would survive even under low (unfavorable) filling ratio conditions (considering possible improvement in safety valves not yet on the market)
  - Compare this fire with the fire most likely to happen. This « typical » fire can be estimated thanks to:
    - Refined modeling of fire using a computational fluid dynamics software (FDS – Fire Dynamics Simulator) to obtain better precision on distribution of heat fluxes for 3D tank modeling
    - Experiments
    - Statistical assessment to estimate the most likely fire hypothesis
  - Assess how protecting equipments (on wheels, fuel tanks, engine, cabin…) may allow to keep flame temperature under the safe value.
  - This will allow to assess the efficiency of safety valves in terms of risk reduction

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Cost-Effective Application of Thermal Protection on LPG Road Transport Tanks for Risk Reduction Due to Hot BLEVE Incidents

A. M. Birk*

A simplified risk and cost-benefit analysis is presented for the application of thermal protection (TP) on propane and LPG highway tanker trucks operating in North America. A risk analysis is performed to determine the benefits of risk reduction by TP, relative to the costs of applying and maintaining TP on a tanker truck. The results show that TP is cost effective if the tanker truck spends enough time (or travels enough distance) in areas of moderate or high population density. The analysis is very sensitive to a number of inputs, including: (i) value of life, (ii) hot boiling liquid expanding vapor explosion frequency, (iii) public exposure to severe hazards, and (iv) life cost of TP. With this simplified analysis, it is possible to generate tanker truck exposure times to the public that justify the application of TP based on cost and benefit considerations.

KEY WORDS: Cost-benefit analysis; Hot BLEVE; thermal protection

1. INTRODUCTION

From time to time, road tank trucks and rail tank cars carrying propane and LPG are exposed to accidental fire impingement that can lead to catastrophic failures resulting in boiling liquid expanding vapor explosions (BLEVEs).[1] These BLEVEs produce hazards including a fireball (or flash fire, or vapor cloud explosion), blast overpressure, and far-reaching projectiles. Many studies have been conducted,[2–4] and it has been clearly demonstrated that thermal protection (TP) can dramatically reduce the likelihood of a fire-induced BLEVE (sometimes called a Hot BLEVE[5]). However, the use of thermal TP is not widely implemented because of cost considerations (i.e., many tanks must be protected to mitigate one BLEVE).

In North America (NA), rail tank cars carrying LPG and propane are required to have both TP and pressure relief valves (PRVs). However, road tank trucks in NA carrying LPG and propane only require PRVs. Recent events in NA have triggered a reassessment of TP for highway tank trucks. In Europe, highway tankers and railway tank cars carrying LPG are not required to have TP, or a PRV. This is fundamentally different than the practice in NA. Europe is also considering TP for both rail and road tanks.

Because BLEVE incidents continue to occur around the world, some countries continue to consider the application of TP and PRV to rail and highway tankers for certain dangerous goods. However, in this modern age, the implementation of such technologies must be justified by a cost-benefit analysis (CBA). The decision to use these protection measures is no longer being based on technical requirements but rather cost-benefit ratios. Of course, cost-benefit studies are hindered by many uncertainties.

This article presents a simplified CBA that removes some of the complexities that appear to stall discussions of this important topic. This article is multidisciplinary as it includes mechanical and

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Table I. Fireball Hazard Summary

Tank Volume (m^3)	Propane Mass (kg)	Fireball D (m)	Fireball Duration (seconds)	Radius (m) to Heat Flux of 35 kW/m^2	Probability of Death at R for 35 kW/m^2
19	6,650	113	11.4	100	0.71
38	13,300	142	14.8	122	0.89
45	16,000	151	15.8	129	0.92
64	22,600	169	18.1	143	0.95

chemical engineering, risk analysis, and CBA. The objective of this article is to present an analysis that will lead to further discussion of the important inputs to this type of analysis. This may lead to a more focused effort to obtain accurate data on these key inputs.

2. THERMAL PROTECTION

TP of dangerous goods transport pressure vessels (tanks) usually involves a layer of thermal insulation to slow the heating effects of accidental fires.[2] PRVs are also used to control pressure buildup in tanks exposed to external heating. It has been shown that PRVs alone are not sufficient to protect a tank from fire if the fire is able to weaken the tank.[6]

If a tank is thermally protected but is not equipped with a PRV, then if the tank fails due to fire exposure, it will fail full of product and the resulting BLEVE will be very powerful. This means the main benefit of the TP by itself is the elimination or delay of the explosion. If the explosion does happen, it will be very strong. The main benefit is delay that gives time for evacuation and emergency response.

When a tank is equipped with both PRV and TP, then the TP acts to delay or prevent the explosion, and the PRV will act to reduce the pressure in the tank (i.e., reduce energy per unit mass), and to reduce the quantity (mass) of product in the vessel if there is an explosion. If there is a delayed explosion it will be less powerful because there will be less energy in the vessel at the time of failure. This was clearly shown by Townsend *et al.*,[6] where a full-scale rail tank car Hot BLEVE was delayed by 70 minutes (relative to a unprotected tank that failed in 24 minutes), and the product mass was reduced by over 75% by the combination of a PRV with TP. In this case, the TP was a thin (3 mm) layer of intumescent paint.[7]

3. COST-BENEFIT ANALYSIS

The application of TP to a fleet of tanks will be very expensive, and it may take years before this TP mitigates a single incident. For this reason, a CBA is necessary to justify such an expenditure.

A detailed CBA of TP for LPG highway trucks was recently conducted by Paltrinieri *et al.*[8] for tanker trucks operating in the Emilia–Romagna region of Italy near Bologna. They did a very detailed parametric study to show the sensitivity of the study to important input variables such as the value of human life, the life cost of TP, and the time value of money. They determined the financial benefits of the application of TP. Their study clearly showed that the final cost-benefit ratio depends very strongly on the input assumptions.

4. HAZARD ANALYSIS

The hazard in this study was assumed to come from a Hot BLEVE failure of a 45–60-m^3 propane road tank trailer. Such a BLEVE has the potential of producing a large fireball, blast overpressure, and projectiles. In this study, the main hazard was assumed to be the fireball that follows the BLEVE. The following have been assumed in the fireball analysis:

(i) full tank contents involved with fireball,
(ii) fireball surface emissive power 350 kW/m^2,
(iii) fireball diameter and duration as per Roberts *et al.*,[9]
(iv) fireball lift off equals fireball diameter,
(v) atmospheric attenuation as per TNO Purple Book,[10]
(vi) probability of death based on probit function as per TNO Purple Book,[10] and
(vii) no wind effects.

Table I gives a summary of the calculated BLEVE fireball hazard effects as a function of tank size as calculated using above assumptions.

As shown by Roberts *et al.*,[9] wind effects can modify the release and this can double the thermal radiation received at a remote target outside of the fireball. The fireball duration is a curve fit to the large-scale data of Roberts *et al.*[9]

From the TNO Purple Book,[10] it is suggested that clothing and building material will ignite when exposed to 35 kW/m^2 and therefore anyone exposed to this level of heat will be a casualty. Because buildings will burn at this heat rate, it can be assumed buildings will not provide protection.[10] Other researchers choose to apply an exposure factor to account for shielding of the people in the severe hazard zone. For example, Geffen[11] used a factor of 0.1 to reduce the number of fatalities in the severe hazard zone by 90%. Clearly, this dramatically affects the number of estimated fatalities and this has a huge effect on any cost-benefit study.

In this study, we have used a radius of 140 m to define the severe hazard region where the probability of death is high. The ground area of this zone is 0.062 km^2. In this study, we used an exposure factor of 0.3 to determine what fraction of the people in the severe hazard zone is killed. We believe this to be a reasonable compromise between 1.0 (Purple Book[10]) and 0.1 from Geffen.[11] These factors account for shielding by buildings, effects of unpopulated portions of the zone like roads, etc. As one would expect, these factors can change the result of a cost-benefit study quite dramatically.

5. FINANCIAL COST OF BLEVE

In this study, we considered three major costs due to a BLEVE incident:

(i) cost of fatalities,
(ii) cost of evacuation, and
(iii) cost of property damage.

No attempt has been made to account for other society costs such as business or society interruptions, which are significant. Including the reduction of these costs by the application of TP would add to the benefit side of the equation.

6. VALUE OF SAVING A LIFE (VSL)

A critical input to this CBA is the value of saving of a human life VSL. As described by Paltrinieri *et al.*,[8] the value of a human life is very difficult to define. Various studies have covered a range from less than \$1 million (M) to almost \$100 M (2012 Canadian \$). As discussed in the R2P2 document from the U.K. Health and Safety Executive,[12] the value of saving a human life used in CBA is not the same as what would be decided in a court of law if someone was accidentally killed.

One way to determine a VSL in a certain context is to determine what people would be willing to pay (WTP) extra for a commodity or a service to reduce the number of fatalities per year to one less than the current value.[13] In the present context, the WTP would be the amount people are willing to pay extra per liter of LPG purchased to save one life. Then, the total volume of LPG consumed by the public would determine the WTP-based value of saving one human life. We do not have this data point for LPG. In this study, we have used \$6.5 million (2012 Canadian \$) as the value of saving a human life. This is the middle of the range of \$3.5M–\$9.5M as reported in Chestnut and Di Civita[13] for a study involving human health issues.

7. OTHER COSTS

We could easily account for other costs, such as property damage within the severe damage zone by specifying a property value per unit area or per person. If we know the total number of people living in the severe hazard zone, we could approximate the number of homes by dividing by the average number of occupants. This value is around 2.4 people per house (TNO Purple Book[10]). From this, we could estimate the cost of property by multiplying by a value per home. In this case, we have used \$200K (2012 Canadian \$) per home. In other words, the property cost per person is \$200K/2.4 = \$83K or about 1.3% of a human life.

We could also account for evacuation/life disruption costs by considering the number of people evacuated, and the number of days they are evacuated. The compensation per day would depend on the details of the evacuation and on how long the people are kept away from their homes and life routines. People may not be able to move back into their homes for months. One could easily imagine average compensation costs per person of between a few hundreds and several thousands of dollars.

The number of people evacuated depends on the population density and how it varies with distance from the roadside. In NA, the evacuation radius for dangerous goods transportation accidents is usually 1.6 km. If we scale the number of people evacuated simply by the evacuation area versus the severe hazard area (i.e., $[1{,}600/140]^2 = 131$), we would expect about 131 evacuated people for every person in the severe hazard zone. If we assume a compensation per person of $2,000, this would give us an evacuation cost of $261K for each person in the severe hazard zone—or about 4% of the value of human life.

In other words, the economics of this problem are driven by the value of human life. However, even if no lives are lost, the cost of these incidents can be very large.

8. SAMPLE INCIDENT

In August 2008, a release of propane at a transfer facility in Toronto, Canada resulted in an explosion, fire, and a number of Hot BLEVE failures of LPG tank trailers. Two people died and over 12,000 people were evacuated. The main explosions happened in the middle of the night so people were in their houses sleeping, and this resulted in the small number of fatalities. There was extensive damage to many homes. With this many people evacuated in a 1.6-km radius, the population density would have been about 1,500 people/km^2 on average. Using the above costing approach, this incident had rough estimated costs of:

(i) calculated loss of life cost
- for exposure = 1.0, $598M (1,500 people/km^2 $\times$ 0.062 km^2 = 92 lives lost exposed to the fireball, Loss of life $ = $6.5M x 92 = $598M),
- for exposure = 0.3, $179M (loss of 28 lives),
- for exposure = 0.1, $60M (loss of 9 lives),
- actual lost life cost $13M (actual loss was two people),

(ii) evacuation $24M (12,000 people evacuated $\times$ $2,000 per person),
(iii) off-site property damage $7.7M (92/2.4 $\times$ $200K).

If we use the actual lives lost, the order of magnitude cost of this incident is $45M. There is currently a $300M lawsuit before the courts over this incident. This would suggest an average compensation of $25,000 per person evacuated, or $3.3M per person in the severe hazard zone.

9. HOT BLEVE FREQUENCY

The frequency of Hot BLEVEs of highway tankers is a key factor in the CBA. This frequency and the number of fatalities dominate the analysis. Frequency data for Hot BLEVEs are very limited in the public domain. In NA data, tanker truck incidents can involve releases, fires, and/or explosions. The term BLEVE is almost never used in the reporting of these incidents even when it clearly was a BLEVE. However, we know that BLEVEs do occur (Atlas Foundry in Tacoma, Washington 2007, Sunrise Propane in Toronto, Ontario in 2008) when they appear in the media. Dramatic video footage posted on the Internet from these incidents provides clear evidence that BLEVEs took place.

Tanks can fail with continuous releases or catastrophic releases. If an LPG vessel suffers a catastrophic release, then that is a BLEVE (see, for example, Abbasi and Abbasi[14]). It may be a Hot BLEVE (fire heating) or a Cold BLEVE (failure without fire). For example, a tanker truck that collides with a bridge and fails catastrophically is a Cold BLEVE. TP will not mitigate this type of BLEVE. However, if there is a collision that causes a leak, and a jetting fire starts, this type of event can escalate to a Hot BLEVE. TP can change the outcome of such an incident. In a recent European study,[8] it was assumed that 86% of BLEVEs are of the Hot type.

A Hot BLEVE is a BLEVE caused by fire heating of the vessel. The fire weakens the tank wall and also heats the LPG to produce a much more powerful BLEVE. We will only consider this type of BLEVE because this is the one where TP has the most benefit.

Hot BLEVE incidents can take place when the truck is traveling on the road, when the truck is parked, or when the truck is loading/unloading. Data suggest the highest BLEVE frequency takes place during loading/unloading.[15] For parked trucks, a Hot BLEVE may be the outcome of vandalism, or a leak, or due to domino effects at a site. On-road incidents may happen if there is a collision or a leak on road. Leaks may be caused by equipment problems, a collision, or an overfill during the loading process.

Other studies[8] have also only considered on-road accidents and they have assumed the incident frequency is proportional to the distance traveled. Of course, the per kilometer incident rate also depends on the road, speed, weather, driver fatigue, road condition, driving complexity, other traffic, etc.

A road incident that leads to a Hot BLEVE could have the following events:

(i) Frequency of road incident F_{inc};
(ii) Probability of release of product or fuel P_r;
(iii) Probability of fire P_f;
(iv) Probability fire impinges on tank P_{fi};
(v) Probability of severe fire heating of vessel (in vapour space) P_{fis}; and

Table II. Hot BLEVE Frequency Estimates for Road Incidents (Data from Canada, the United States, and the European Union)

Value	Data from Canada[16]	United States[11]	United States[18]	EU[3]
(a) Road incident frequency	1e−7 /km/truck	1.55e−6 with release	2.8e−7	3.31e−7
(b) Release probability	0.19	1	0.31	0.05
(c) Probability of fire impinging tank	1	0.016	1	0.86
(d) Probability of catastrophic release	0.102	0.30	0.051	0.105
$F_{HB} = (a) \times (b) \times (c) \times (d)$ Hot BLEVE frequency /km/truck	1.9e−9/km/truck	7.4e−9	4.4e−9	1.5e−9
Hot BLEVE freq for truck with 50,000 km/year full of product	9.5e−5/truck/year	3.7e−4	2.2e−4	7.5e−5
One in this number of trucks will suffer a Hot BLEVE in 30 years	351	90	152	444

(vi) Probability of thermal rupture and catastrophic release (BLEVE) P_B.

The Hot BLEVE frequency would be determined from the above using the following:

$$F_{HB} = F_{inc}\, P_r\, P_f\, P_{fi}\, P_{fis}\, P_B.$$

Unfortunately, the data for actual incidents are not provided in a very consistent way or at this level of detail. Table II shows the typical way incident data are presented. Based on limited data available for on-road truck incidents, the per km incident frequencies for dangerous goods vehicles are between F_{inc} = 1e−7[16] and 1.55e−6[11] incidents/km/truck (see Table II).

The final outcome of a Hot BLEVE has a frequency in the range of 1.5e−9 and 7.4e−9 Hot BLEVE/km/truck. If a truck travels 50,000 km/year full of product, this gives a Hot BLEVE frequency for that truck of between 7.5e−5 and 3.7e−4 Hot BLEVE/year. This only considers on-road incidents that lead to a Hot BLEVE. This is the type of incident that TP has a high probability of mitigating. Recall that this does not include loading/unloading incidents or incidents while parked, where TP can also provide a major benefit. We will focus on incidents along the road where the general public may be at risk. We have not considered the case of a tanker truck incident inside a road tunnel as described by Fabiano and Palazzi.[17] TP is likely to provide significant benefit there as well.

Let us continue with a round number of 1e−4 Hot BLEVE/year/truck for the Hot BLEVE frequency of a tank truck (F_{HB}). This suggests that any given truck could suffer a Hot BLEVE once in 10,000 years. Over a period of 30 years, this truck has a Hot BLEVE frequency of F_{HB} = 3e−3. Or in other words, in a fleet of 333 similar trucks we expect to see one on-road Hot BLEVE over a period of 30 years.

If we thermally protect these 333 trucks, we expect to mitigate one BLEVE incident in 30 years. The benefit value from this mitigation will depend on where this truck is when it suffers a BLEVE. If the truck suffers the Hot BLEVE in the middle of nowhere, then the financial benefit would be small. If the Hot BLEVE takes place in a densely populated area, then the benefit of TP could be very large.

10. TIME VALUE OF MONEY

The time value of money is usually accounted for in CBA. Different inflation rates are usually assumed for the costs and benefits. It is usually assumed that the benefits inflate faster than the costs.[12] Then, there is also a return on investment (ROI) used to discount the cash flows back to present-day dollars.

These different assumptions have a significant affect on the present value (PV) of the costs and benefits. However, they have little effect on the cost-benefit ratio CBR. For this reason, we will only present the costs and benefits in constant dollars.

11. PROTECTED VERSUS NONPROTECTED TANK TRUCKS

In this analysis, we are considering a 45-m^3 (12,000 U.S. gals.) tank truck like those used in NA to transport LPG and propane. These tanks are required to have a PRV. They are not required to have TP.

There are many benefits from TP and a PRV, including:

(i) eliminated fire-induced rupture (i.e., Hot BLEVE) in most cases,

(ii) delayed fire-induced rupture (i.e., if failure is delayed, then the hazards will be reduced because there will be less product in the tank at failure due to PRV action; delay also gives time for evacuation and this reduces the number of fatalities),
(iii) delayed activation of PRV under fire heating conditions (i.e., delayed hazards and domino effects),
(iv) reduced average PRV flow rate under fire heating conditions (i.e., reduced hazards).

12. TP BENEFIT

The TP considered in this CBA consists of a thin (3 mm) layer of intumescent paint.(7) This is similar to what was tested by Townsend *et al.*(6) on full-scale rail tank cars. In their full-scale fire tests, an unprotected rail tank car (125 m^3, filled to 94% capacity with propane) was exposed to an engulfing liquid hydrocarbon pool fire, and it suffered failure and a powerful BLEVE in 24 minutes. When this tank failed, it was still 40% full of liquid and it produced a very powerful BLEVE. The fill was reduced from 94% to 40% by the opening of the PRV. They then fire tested a thermally protected tank with a 3-mm layer of intumescent paint. The thickness they used (3 mm) was actually only 50% of the recommended thickness for that product. When the protected tank, filled to 85% capacity with propane, was exposed to an engulfing pool fire, the tank failed in 93 minutes. When this failure occurred, the tank was nearly empty of liquid (i.e., 3% liquid full). When this tank failed, the resulting BLEVE was much less powerful.

This was a clear demonstration (40 years ago) of how TP and a PRV can change the outcome of a Hot BLEVE incident. As it turned out, this was not the TP system adopted in NA for railway tanks carrying LPG. Most rail tank-car TP systems for LPG consist of a 13-mm ceramic blanket insulation covered by a 3-mm steel jacket.(19) This type of system is heavier, possibly more effective, and it was believed to be more robust, and able to survive the harsh rail environment.

13. RISK REDUCTION BY TP + PRV

Recent incidents in NA (Tacoma, Washington 2007, Toronto, Ontario 2008) have shown that jetting fires can cause LPG road tank failures in as little as six minutes. In this analysis, we have assumed two possible outcomes for the tank truck:

For the unprotected truck,

(i) The BLEVE takes place 10 minutes after the incident begins when the tank is still 80% full of liquid.
(ii) At failure, the liquid is saturated and at 60 °C.
(iii) Severe hazard zone (heat flux > 35 kW/m^2) is approximately 140 m radius.
(iv) Evacuation of the immediate area is incomplete due to short time span.

For the protected tank, the following assumptions have been made:

(i) BLEVE is delayed to 75 minutes.
(ii) When the BLEVE takes place the tank is nearly empty (3% liquid).
(iii) At failure the liquid is saturated at 50 °C.
(iv) Severe hazard zone reduced to 75 m radius.
(v) A total of 75 minutes provides time for much more effective evacuation resulting in no fatalities.

The above are credible assumptions based on expert opinion. They are supported by the experimental results of Townsend *et al.*(6) With the above assumptions, we would get 100% risk reduction for human life.

We know from testing(4,20) that TP delays 100% of Hot BLEVEs. In a high percentage of cases, the Hot BLEVE is prevented by the TP. However, we also know that TP systems can be damaged in transportation accidents so they are not 100% effective. For this reason, we have not assumed 100% effectiveness for the system. In this study, we have assumed 75% risk reduction. This assumed risk reduction applies to the on-road incident. Other incidents like loading and unloading incidents will also benefit from TP, and this would further add to the cost-benefit result.

With these assumptions, it is possible to generate risk reductions due to TP and individual risk (IR) and societal risk.

14. INDIVIDUAL RISK

In this case, we have simplified the individual risk as follows:

(i) Inside the severe hazard zone, the probability of death is 1.0.
(ii) The Hot BLEVE frequency is 1.4e−9/km.

In other words, the individual risk is very high for people inside the severe danger zone. However, because the truck is a moving hazard, the IR for an individual standing at the side of the road at a specific position could be very small since it only takes the truck a few seconds to pass by. If we take the 140-m radius for the hazard zone then the maximum distance traveled while exposing that person to the risk is $= 2 \times 140/1{,}000 = 0.28$ km/trip. If this truck does one such trip per week, then the final IR is 52 * 0.28 * 1.4e−9 = 2.e−8 per year, a very small and acceptable IR for that specific location on the side of the road. With this level of risk, we could accept 50 such trips per week and still have an IR of 1e−6 per year.

15. SOCIETAL RISK FROM SINGLE TRUCK

For the societal risk, we must add up all the kilometers traveled by the truck and apply these kilometers to the various population densities along the roadside. We must also consider the possible fatalities within the severe hazard zone.

The most severe incidents are those that involve moderate and high population densities. The highest potential for large numbers of fatalities is when the truck is on the road in a densely populated area. If a truck spends a significant fraction of its time (or km) operating in densely populated areas then the societal risk will be high.

To determine the societal risk, we need to know where the truck operates. We need to know the km traveled in specific population densities. This of course is route specific and requires detailed route analysis with population data. This was done recently by Paltrinieri *et al.*[8] for a region in northern Italy.

Here, we will consider a more generic approach. Here we will ask the question: How long can a truck operate in a populated area before TP is justified by a CBA? This will depend on the following:

(i) cost of TP per year,
(ii) financial benefit of saving lives,
(iii) Hot BLEVE frequency,
(iv) hazard exposure to the public, and
(v) risk reduction by TP.

16. COST OF TP

The life time cost of the TP includes the following:

(i) installation cost above normal paint cost,
(ii) maintenance and inspection cost above normal paint cost, and
(iii) added operating cost due to added weight of TP.

We have assumed the life of the vessel is 30 years. We have assumed the TP will require annual maintenance and will need a major refurbishment every 10 years. This is similar to the normal epoxy paint used on the vessel.

We have assumed the use of a thin (3 mm) intumescent coating. This is simply painted on in a single coat like the normal epoxy paint used on these tanks. This results in minimal extra installation cost and minimal weight increase (i.e., negligible impact on truck capacity). It also allows for simple inspection and maintenance.

In a conversation with an employee of a major manufacturer of intumescent paints, it was suggested that the total cost of installation of such paints is approximately \$24/mm/m^2 for a production run of many tanks. For a 3-mm thickness, this is \$72/m^2. A 45-m^3 tank trailer has a surface area of about 92 m^2, which gives an installation cost of around \$6,700. If we subtract the cost of the normal epoxy paint, the cost difference for installation of the intumescent paint is around \$5,000 per truck. This coating will need annual inspection and maintenance. It has been assumed the tank will be repainted very 10 years.

The extra cost over normal paint has been estimated to be \$30–\$60K per tank over the 30-year life of the tanker, or between \$1,000 and \$2,000 per year assuming constant dollars. It has not been possible to obtain cost estimates more accurate than this because we do not have operational experience with these thin intumescent paints on highway tank trucks. We need field trials to obtain such experience.

Earlier, we suggested that we would need to protect about 333 trucks to mitigate one Hot BLEVE incident in 30 years. This suggests an annual cost of between \$0.33 and \$0.67 million to protect the fleet of 333 trucks, or a cost of between \$10 and \$20 million to mitigate one BLEVE in 30 years.

This cost is a very controversial issue with the industry. It believes that the real cost of TP will be much higher. There is a need to do field trials to determine the real costs of operating a tanker with this TP system.

17. SIMPLIFIED CBA

The following assumptions and equations have been used in this simplified CBA.

As stated earlier, the basic Hot BLEVE frequency has been assumed to be:

$$F_{HB} = 1.32\text{e} - 9 \quad \text{HotBLEVE/truck/km.}$$

We have assumed the average trip length $tl = 300$ km per loaded trip and the total number of loaded trips per year of $nt = 300$, for a total km/year/truck of $S = 90{,}000$.

The Hot BLEVE frequency for a given population density zone z is simply the F_{HB} times the total km traveled in that zone, or:

$$F_{HBZ} = F_{HB} \times S_z \quad \text{Hot BLEVE/truck } in \times population\ zone\ z, \tag{1}$$

where

$S_z = nt \times tl_z$ and nt

= number of trips per year and

$\times\ tl_z$ is the trip length in the population zone z.

The traveled distance in a zone can be converted to a time spent in that zone if we know the average truck speed traveled in that zone. A convenient way to present this is the hours per week spent in a specific zone or HPW_z.

$$HPW_z = S_z / V / 52 \quad \text{hours/week in population zone } z.$$

In our case, we have assumed an average speed $V =$ 50 km/hour.

The number of fatalities was calculated assuming everyone exposed is killed in the severe hazard zone (R = 140 m, or ground area $A_{HZ} = 0.062$ km^2). We have assumed an exposure factor of $EXP = 0.3$. Fatalities Nz for a given population zone is:

$$N_z = PD_z \times A_{HZ} \times EXP \text{ fatalities in population zone } z,$$

where PD_z = population density (people/km^2) in zone z.

As stated earlier, the assumed value of saving a human life VSL is \$6.5M per life saved.

The expectation value for fatalities due to a Hot BLEVE for a specific population density zone z is:

$$EV_z = Nz \times F_{HBZ} \text{fatalities/year in population zone } z.$$

The change in expectation value due to the application of TP is:

$$dEV_z = RR \times EV_z$$

lives saved/year in population zone z,

where RR is risk reduction by TP = 0.75. This RR applies for a tank with both a PRV and TP.

Table III. Sample Case Inputs

Input Name	Value for Sample
LPG tank volume	45 m^3
Truck loaded trips per year	300
Loaded truck total km/year	90,000
Hot BLEVE frequency	1.32e−9 /km
Value of human life for CBA	\$6.5e6
Population exposure to hazards	0.3
Risk reduction by TP + PRV	0.75
Severe hazard zone radius	140 m
Cost of TP per year	\$2000.

From the above, the value of life saved per year for a give population zone is:

$$VL_z = dEV_z \times VSL \quad \$ \text{ per year, in population zone } z.$$

The estimated annual saving due to risk reduction is $\$RR$ and is equal to the sum of all VL_z. $\$RR$ is the financial benefit per year of applying TP to that truck:

$$\$RR = \sum_{z=1}^{n} VL_z = \sum_{z=1}^{n} RRPD_z EXPA_{HZ} VSLF_{HB} S_z.$$

The assumed life cost of TP (i.e., all costs including installation, operating, maintenance, etc.) was TPC = \$2,000/year (constant \$).

The cost-benefit ratio CBR = TPC/\$RR.

We have used a CBR = 1 to indicate if TP is justified. We have not applied any aversion factor. Where human life is concerned, some allow aversion factors of 10 or more (i.e., CBR = 10).[12]

18. SAMPLE CASE

Here, we present a simple example based on the inputs given above. We have repeated the assumed values in Table III.

Table IV shows the results for the sample case. In this sample, we have assumed a specific distribution of traveled km in each of the population zones. From this, we can calculate the Hot BLEVE frequency for each zone and from this the expected number of fatalities. For this specific example, we have calculated a financial benefit of \$2,185/year or slightly greater than the \$2,000 calculated for the TP cost per year. This value will of course vary dramatically if the truck spends less or more time in high population areas.

As can be seen from the table, the number of dollars from risk reduction (\$RR) is largest for the more

Table IV. Results of Sample Case

Pop dens peop/km^2 in zone	Frac of truck km in zone	Truck km/trip in zone	Truck km/trip in zone	F HB /yr in zone	People /incident in zone	Fat N /incident in zone	EV /yr in zone	dEV /yr in zone	\$ RR /yr in zone	km/yr in zone CBR=1	hr/wk in zone CBR=1	Ratio actual calculated
10	0.66	198	59,400	7.86E−05	0.6	0.2	1.45E−05	1.09E−05	$71	1,677,415	645.2	0.035
50	0.1	30	9,000	1.19E−05	3.1	0.9	1.10E−05	8.25E−06	$54	335,483	129.0	0.027
100	0.07	21	6,300	8.34E−06	6.2	1.8	1.54E−05	1.16E-05	$75	167,742	64.5	0.038
200	0.05	15	4,500	5.96E−06	12.3	3.7	2.20E−05	1.65E−05	$107	83,871	32.3	0.054
500	0.05	15	4,500	5.96E−06	30.8	9.2	5.50E−05	4.13E−05	$268	33,548	12.9	0.134
1,000	0.03	9	2,700	3.57E−06	61.6	18.5	6.60E−05	4.95E−05	$322	16,774	6.5	0.161
2,000	0.03	9	2,700	3.57E−06	123.2	36.9	1.32E−04	9.91E−05	$644	8,387	3.2	0.322
4,000	0.005	1.5	450	5.96E−07	246.3	73.9	4.40E−05	3.30E−05	$215	4,194	1.6	0.107
8,000	0.005	1.5	450	5.96E−07	492.6	147.8	8.80E−05	6.60E−05	$429	2,097	0.8	0.215
Total	1	300	90,000	1.19E−04			4.48E−04	3.36E−04	$2,185			1.092

populated areas even though the tank spends little time in those areas. This is driven by the number of fatalities and the value of saving a life.

19. CRITICAL TIME FOR TP

We can calculate the number of km S or hours of driving per week HPW in a given population zone that gives a cost-benefit ratio CBR = 1. The relations are shown below:

$$S_{ZC} = \frac{TPC}{F_{HB} \cdot 52 \cdot PD_2 \cdot A_{HZ} \cdot EXP \cdot RR \cdot VSL},$$

$$HPW_{ZC} = \frac{TPC}{F_{HB} \cdot 52 \cdot V \cdot PD_{HZ} \cdot EXP \cdot RR \cdot VSL},$$

where V is the average truck speed in the zone.

These equations give the S or HPW of tank exposure for a specific population density that gives a cost-benefit ratio for TP of unity, assuming the truck spends all of its time in that specific zone. Fig. 1 shows the result for HPW.

Most trucks will have some kind of distribution of km or time over a range of population densities. Table IV includes this calculation for our sample case. The fraction of HPW or km/year exposure for each population density is calculated and then summed for all the population densities considered. If the total of those fractions is >1, then TP is justified by the cost-benefit ratio CBR <1.

This approach can be used by operators or regulators to identify tank truck operations that should have TP.

20. SENSITIVITY ANALYSIS

This analysis is driven by the key inputs including the Hot BLEVE frequency F_{HB}, the value of human life VSL, the human exposure to the hazards EXP, and the cost of the thermal protection TPC. Sadly, we do not have precise values for any of these. Any and all of these factors could easily be adjusted up or down by a factor of 2 or more. In other words, with different input assumptions, this CBA could easily result in cost-benefit ratios of between 0.2 and 5. Recall that we did not apply any aversion factor to account for the loss of many lives. Aversion factors can easily exceed a value of 10 or more. In other words, a CBR of 5 when life is at risk is probably acceptable.

In NA, rail tank cars were thermally protected in the 1980s without a cost-benefit study. Because of a series of major incidents, transportation regulators decided it was in the public's best interest to have TP.

21. CONCLUSIONS

A simplified CBA is presented for the application of TP on propane and LPG tanker trucks operating in NA. The results show that TP is cost effective if the tanker truck spends enough time operating in areas of moderate or high population density. The analysis is very sensitive to a number of inputs, including:

(i) value of life,
(ii) Hot BLEVE frequency,
(iii) public exposure to severe hazards, and
(iv) cost of TP.

This analysis has only considered the benefits of TP for on-road incidents. If other incidents were included such as:

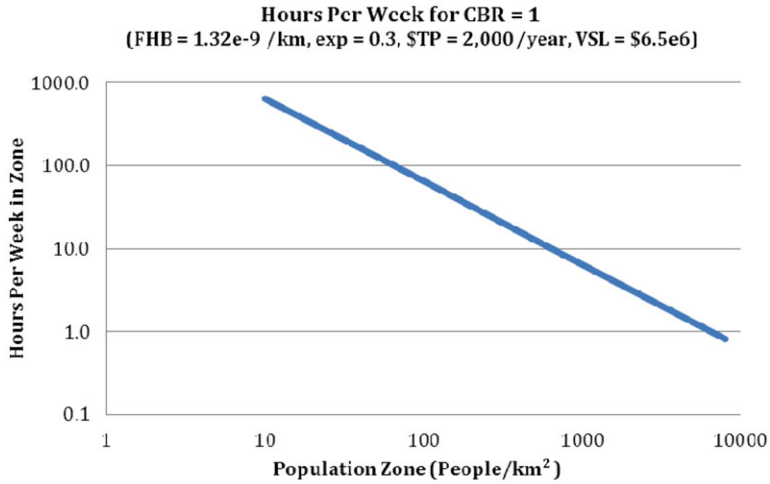

Fig. 1. Driving hours per week (HPW) exposure to the public versus population density that gives a cost-benefit ratio of unity for application of thermal protection. Assumed Hot BLEVE frequency is 1.32e−9/km.

(i) loading/unloading, and
(ii) while parked (on-site fire domino effects),

the cost-benefit ratio would be even more favorable for TP.

This analysis includes many uncertainties, the most important being the frequency of on-road Hot BLEVEs and the number of fatalities resulting from an incident. There is very limited data on both.

ACKNOWLEDGMENTS

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CHAPTER 22
MODELING AND UNDERSTANDING BLEVEs

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22.1 INTRODUCTION

Boiling liquid expanding vapor explosions (BLEVEs) are one of the most severe accidents that can occur in the process industry or in the transportation of hazardous materials. Strictly speaking, these explosions do not necessarily imply thermal effects. However, in most cases the substance involved is a fuel that causes a severe fireball after the explosion. Usually BLEVE refers to the combination of these two phenomena, BLEVE and fireball, i.e., to an accident simultaneously involving mechanical and thermal effects.

BLEVEs occur with a certain frequency: the substances that can lead to them (butane, propane, vinyl chloride, chlorine, etc.) are relatively common in the industry, as well as the installations in which they can happen (tanks and tank cars). They can have diverse origins, such as runaway reactions and collisions, but the most frequent one is the action of fire on a container. Table 22.1 (Prugh, 1991) shows a list of the most significant BLEVEs that have occurred between 1926 and 1986. As can be seen, most of these involved fatalities. Another source (Londiche and Guillemet, 1991) mentions 900 fatalities and over 9,000 injured in 77 BLEVEs occurring between 1941 and 1990.

In this section, the main features of BLEVEs and fireballs are discussed and a practical methodology for estimating their effects is described.

The information provided in Table 22.1 allows a simple statistical analysis to determine the most frequent external causes of BLEVEs. The results obtained can be seen in Table 22.2.

Some of these causes can occur simultaneously (for example, among the 18 cases of fire, 5 happened immediately after a derailing). Nevertheless, this information shows the importance of fire, overfilling, and runaway reactions both in fixed installations and during transport.

22.2 DESCRIPTION OF THE PHENOMENON

If a tank containing a pressurized liquid is heated—for example, due to the thermal radiation from a fire—the pressure inside it will increase. At a certain moment, its walls will not be

TABLE 22.1 The Most Significant BLEVE Accidents from 1926 to 1986

Date	Place	Cause	Material	Tons	Fatalities
12/13/1926	St. Auban, France	Overfilling	Chlorine	25	19
5/28/1928	Hamburg, Germany	Runaway reaction	Phosgene	10	10
5/10/1929	Syracuse, NY, U.S.A.	Explosion (H_2)	Chlorine	25	1
12/24/1939	Zarnesti, Rumania	Overfilling	Chlorine	10	60
7/29/1943	Ludwigshafen, Germany	Overfilling	Butadiene	16	57
11/5/1947	Roemo, Finland	Overfilling	Chlorine	30	19
7/28/1948	Ludwigshafen, Germany	Overfilling	Ethyl ester	33	209
7/7/1951	Port Newark, NJ, U.S.A.	Fire	Propane	2,600	0
4/4/1952	Walsum, Germany	Overfilling	Chlorine	15	7
1/4/1954	Institute, WV, U.S.A.	Runaway reaction	Acrolein	20	0
1/8/1957	Montreal, Quebec, Canada	Fire	Butane	?	1
1958	Alma, MI, U.S.A.	Overfilling	Butane	55	1
6/28/1959	Meldria, U.S.A.	Derailing	Propane	55	23
8/18/1959	Kansas City, U.S.A.	Fire	Gasoline	20	5
4/17/1962	Doe Run, MO, U.S.A.	Runaway reaction	Ethylene oxide	25	1
1/4/1966	Feyzin, France	Fire	Propane	1,000	18
1/1/1968	Donreith, U.S.A.	Derailing (fire)	Ethylene oxide	2	0
8/21/1968	Lieven, France	Mechanical	Ammonia	20	5
1/2/1969	Repcelak, Hungary	Overfilling	Carbon dioxide	35	9
1/25/1969	Laurel, MS, U.S.A.	Derailing (fire)	Propane	65	2
2/18/1969	Crete, NE, U.S.A.	Derailing	Ammonia	65	8
4/29/1969	Cumming, IA, U.S.A.	Derailing	Ammonia	?	?
9/11/1969	Glendora, CA, U.S.A.	Fire	Vinyl chloride	55	0
6/21/1970	Crescent City, IL, U.S.A.	Derailing (fire)	Propane	275	0
1/19/1970	Baton Rouge, LA, U.S.A.	Overpressure	Ethylene	4	0
10/19/1971	Houston, TX, U.S.A.	Derailing (fire)	Vinyl chloride	50	1
2/9/1972	Tewksbury, MA, U.S.A.	Collision	Propane	28	2
3/30/1972	Rio de Janeiro, Brazil	Fire	Propane	1,000	37
9/21/1972	Turnpike, NJ, U.S.A.	Collision	Propylene	18	2
11/2/1972	San Antonio, TX, U.S.A.	Corrosion	Carbon dioxide	0.01	0
7/5/1973	Kingman, AZ, U.S.A.	Fire	Propane	100	13
1/11/1974	West St. Paul, MN, U.S.A.	Fire	Propane	27	4
2/12/1974	Oneonta, NV, U.S.A.	Derailing (fire)	Propane	288	0
7/29/1974	Pueblo, CO, U.S.A.	Fire	Propane	80	0
4/29/1975	Eagle Pass, TX, U.S.A.	Collision	Propane	18	16
12/14/1975	Niagara Falls, NY, U.S.A.	Runaway reaction	Chlorine	20	4
5/11/1976	Houston, TX, U.S.A.	Collision	Ammonia	20	6
8/31/1976	Gadsden, AL, U.S.A.	Fire	Gasoline	4	3
1977	Cartagena, Colombia	Overpressure	Ammonia	7	30
2/22/1978	Waverly, TN, U.S.A.	Derailing	Propane	45	12
7/11/1978	Els Alfacs, Spain	Dilatation/overpressure	Propylene	23.6	216
5/30/1978	Texas City, TX, U.S.A.	Fire	Butane	1,500	7
8/30/1979	Good Hope, LA, U.S.A.	Ship collision	Butane	120	12
8/1/1981	Montonas, Mexico	Derailing	Chlorine	110	29
1/19/1982	Spencer, USA	Overheating	Water	0.3	7
12/11/1982	Taft, LA, U.S.A.	Runaway reaction	Acrolein	250	0
7/12/1983	Reserve, LA, U.S.A.	Runaway reaction	Chlorobutadiene	1	3
10/4/1983	Houston, TX, U.S.A.	Overfilling	Methyl bromide	28	2
11/19/1984	Mexico City, Mexico	Fire	Propane	3,000	500
1/28/1986	Kennedy Space Center, FL, U.S.A.	Fire	Hydrogen	115	7

Source: Prugh, 1991.

TABLE 22.2 The Most Frequent External Causes of BLEVEs

Cause	%
Fire	26
Derailing	20
Overfilling	18
Runaway reaction	12
Collision	10
Overpressure	6
Other	8

able to withstand the high stress and they will collapse (the steel typically used for the construction of LPG vessels may fail at pressures of about 15 atm, when the temperature of the walls reaches approximately 650°C). This is most likely to occur in the top section of the container, where the walls are not in contact with the liquid and therefore not cooled by it; the temperature of the walls will increase and their mechanical resistance will decrease (Birk, 1995). Instead, the wall in contact with the liquid will transfer heat to the liquid, thus maintaining a much lower temperature. If a safety valve opens, the boiling liquid will have a stronger cooling action due to the heat of evaporation.

Upon failure, due to the instantaneous depressurization, the temperature of the liquid will be greater than that corresponding to the new pressure according to the saturation curve in the P-T diagram. In this unstable condition, it is called superheated liquid. Liquids normally can withstand a small amount of superheating, which in certain experimental conditions can be extended far above the atmospheric-pressure boiling point (Prugh, 1991). However, there is a limit to superheat, called the superheat temperature limit (different for each substance). Then, if the temperature of the liquid at the moment of depressurization is higher than the superheat temperature limit, a violent and instantaneous flash of a fraction of the liquid and a superheated liquid vapor explosion will take place; a biphasic liquid/vapor mixture will then be released. This phenomenon occurs in a very short time (1 ms). The significant increase in the liquid's volume when it vaporizes—1,750 times in the case of water and 250 times in the case of propane—plus the expansion of the previously existing vapor, will give rise to a strong pressure wave (explosion, bursting of the container) as well as to the breaking of the container into several pieces, which will be propelled considerable distances. Experimental work performed with small 1-l vessels (Mcdevitt et al., 1990) has shown that when there is a break in the vessel, the pressure drops slightly and then rises up to a maximum; the initial depressurization brings the fluid near the break to a superheated state, thus causing a local explosion.

If the substance involved is not combustible, the pressure wave and the missiles will be the only effects of the explosion. This could happen if a steam boiler (water steam) exploded. If the substance is a fuel, however, as often happens in the process industry (for example, liquefied petroleum gas, such as ethylene or propane), the mixture of liquid/gas released by the explosion will probably ignite, giving rise to a fireball of approximately hemispherical shape, initially at ground level. The effect of the thermal radiation in this first stage, which is usually only a couple of seconds, is very important. The whole mass of fuel can burn only at its periphery because there is no air inside the mass (the mixture is outside the flammability limits).

In fact, not all the fuel initially contained in the tank is involved in this fire. Some of the fuel is entrained in the wake formed by the flying fragments. In one case (Mexico City, 1984), it has been suggested that a portion of the liquid was thrown significant distances without being ignited, which caused local fires (this effect has not been mentioned in any other case). This decreases the amount of fuel contained in the fireball and also affects its dimensions and the duration of the fire.

Later on, the turbulence of the fire entrains air into the fireball. Simultaneously, the thermal radiation vaporizes the liquid droplets and heats the mixture. As a result of these processes, the whole mass turbulently increases in volume, evolving towards an approximately spherical shape that rises, leaving a wake of variable diameter. Such fireballs can be very large, causing a very strong thermal radiation. The combined action of BLEVE and fireball can be summarized therefore in the following effects:

- Thermal radiation
- Pressure wave
- Flying fragments

The mode in which these effects actuate varies: punctual or directional in the case of projectiles, and zonal—covering a given surface—in the case of thermal radiation and blast.

It is worth noting that it is practically impossible to establish the exact instant at which the explosion will take place. Twenty years ago it was believed that once the emergency started—for example, from the instant in which fire started to impinge on a container—there was a certain time available before the explosion. It therefore seemed that diverse measures could be taken to prevent the explosion (for example, firefighters could refrigerate the tank with hoses). However, as more information was gathered on actual accidents, it became evident that this time could be extraordinarily short; in the San Juan Ixhuatepec accident in Mexico City (Pietersen and Cendejas, 1985), the time elapsed between the first explosion (which caused the fire) and the first BLEVE was only 69 seconds. The various stages of a BLEVE/fireball are shown in Fig. 22.1.

The instant at which a BLEVE can occur in a tank exposed to fire depends on the following factors:

1. Thermal flux from the fire, which will be a function of the distance from the flame to the tank and will depend on whether there is flame impingement and the type of flame (pool fire, torching, etc.)
2. Diameter of the tank
3. Tank fill level
4. Release capacity of safety valves
5. Existence of a layer (with a certain thickness) of isolating material (passive protection)

Theoretically, an insulated container should resist the effect of the flames from a pool-fire (thermal flux of approximately 100 $kW \cdot m^{-2}$) for two hours. In the case of a jet fire, the thermal flux increases significantly (up to 350 $kW \cdot m^{-2}$). In these conditions, some BLEVEs have occurred in the first minutes. For the development of this type of accident, the following times have been suggested (Nazario, 1988): flame impingement from a jet fire, 5 minutes; flame impingement with turbulent flames, 30 minutes (this value agrees with that proposed by ASTM (ASTM, 1983), 20 to 30 minutes). Although this time can vary with the features of the installation (insulating layer, cooling devices), it is evident that other factors can decrease it significantly (partial destruction due to impacts or pressure wave, for example). The most cautious practice, therefore, is to take into account that the explosion can occur at any moment from the beginning of the emergency. The exclusion area should therefore be rapidly evacuated.

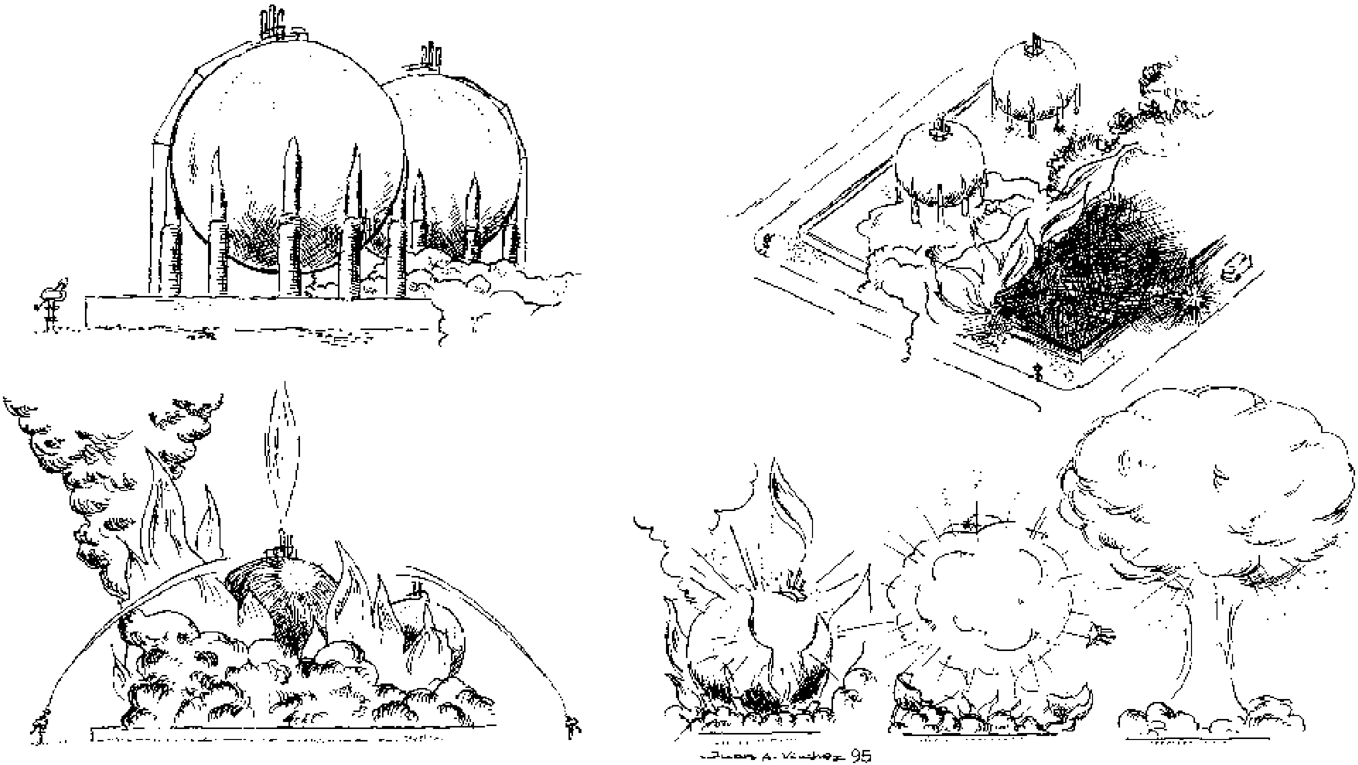

FIGURE 22.1 BLEVE-fireball: loss of containment, fire, tank heating, bursting and evolution of fireball.

22.3 CONDITIONS REQUIRED FOR A BLEVE TO OCCUR

22.3.1 Superheating and Depressurization

While the explosion of a tank containing a pressurized flammable liquid will almost always lead to a fireball, the explosion cannot always be considered strictly a BLEVE. To qualify as this type of explosion, the following conditions must be met (Reid, 1979; Mañas, 1984; Bestratén and Turmo, 1991a; Birk, 1995):

- Significant *superheating* of the liquid. Most liquefied gases under fire attack (LPG, ammonia, chlorine) fulfil this condition; it can also be fulfilled by other liquids contained in closed containers that undergo anomalous heating, for example due to a fire; and, as stated before, water can also be at this condition upon instantaneous depressurization.
- Instantaneous *depressurization*. This phenomenon is usually related to the type of failure of the vessel. The sudden pressure drop in the container upon failure causes the liquid superheat. If the liquid superheat is significant, the flashing may be explosive.

When these two conditions are met, a practically instantaneous evaporation of the contents takes place, with the formation of a large number of boiling nuclei in all the liquid mass (homogeneous nucleation). In these conditions, the velocity at which the volume increases is extraordinary and the explosion is therefore very violent. Strictly speaking, this is the phenomenon associated with the BLEVE explosion.

22.3.2 Temperature and Superheating Limit Locus

Diverse authors have suggested procedures to establish the superheat temperature limit and the superheating limit locus that determine, for each substance, the conditions under which a BLEVE can occur. Reid (1976, 1979) made a significant contribution to this field.

The theoretical superheating limiting conditions at which spontaneous homogeneous nucleation will exist in all the liquid mass can be established from the tangent line to the vapor pressure-temperature curve at the critical point. This represents the limit to which the liquid may be heated before spontaneous nucleation occurs with a vapor explosion (Reid, 1979). This is shown in Fig. 22.2.

The relationship between the vapor pressure and the temperature is established by the Antoine equation:

$$\ln P = -\frac{A}{T} + B \tag{22.1}$$

The tangent to the saturation curve at the critical point is obtained by calculating the derivative of pressure with respect to temperature:

$$\frac{dP}{dT} = A\,\frac{P}{T^2} \tag{22.2}$$

By applying this expression to the critical point,

$$\frac{dP_c}{dT_c} = \frac{P_c A}{T_c^2} = \operatorname{tg}\,\alpha \tag{22.3}$$

This expression gives the slope of the line tangent to the saturation curve at the critical point. The equation of this straight line is:

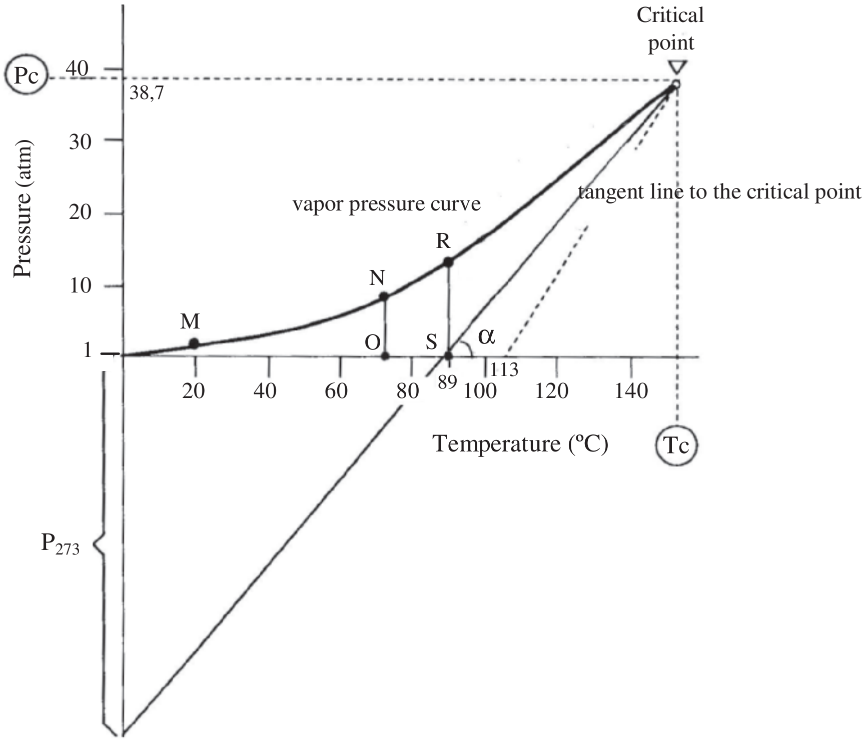

FIGURE 22.2 Saturation curve for butane and limiting conditions for BLEVE.

$$P = \mathrm{tg}\ \alpha \cdot T + b \tag{22.4}$$

An example will show how this expression can be used. The value of the superheating limiting value will be calculated for butane. The equilibrium data corresponding to the critical point and to atmospheric pressure are:

$$P_c = 38.7 \text{ atm} \qquad T_c = 425.8 \text{ K}$$

$$P = 1 \text{ atm} \qquad T = 272.5 \text{ K}$$

By introducing these data in the Antoine equation, the values of constants A and B for butane are found (the pressure expressed in atm and the temperature in K):

$$A = 2769 \qquad B = 10.16$$

The slope of the tangent to the saturation curve at the critical point is therefore:

$$\mathrm{tg}\ \alpha = 38.7 \cdot \left(\frac{2769}{425.8^2}\right) = 0.591$$

And the value of the ordinate at the origin, b, can be found by again introducing the values corresponding to the critical point, thus obtaining $b = -213$. In this way, the equation of the tangent line is obtained; its intersection with the horizontal line at $P = 1$ gives a temperature of 89°C (Fig. 22.2). Therefore, for a vessel containing liquid butane, the minimum temperature required to reach (upon vessel failure) a superheating degree causing spontaneous nucleation (and therefore BLEVE) is 89°C; at this temperature, the spontaneous nucleation would occur at 1 atm.

For the coordinate system used, with the origin at 0°C, the ordinate at the origin will be the pressure corresponding to 273 *K*,

$$P_{173} = 0.591 \cdot 273 - 213 = -52 \text{ atm}$$

Take for example a vessel containing butane at room temperature (20°C), in which liquid and vapor are at equilibrium at an absolute pressure of 2 atm (point M in Fig. 22.2). If, due to the thermal radiation from a fire, the temperature increases to 70°C, the pressure inside the vessel will be 8 atm (point N). If, at these conditions, the vessel bursts (due to the failure of the material or an impact, for example), there will be an instantaneous depressurization from 8 atm to the atmospheric pressure. At the atmospheric pressure, the temperature of the liquid-vapor mixture will be −0.5°C (point O in Fig. 22.2) and the depressurization process corresponds to the vertical line between N and O. As this line does not reach the tangent to the saturation curve at the critical point, the conventional theory states that there will be no BLEVE strictly speaking: although there will be a strong instantaneous vaporization and even an explosion, nucleation in all the liquid mass will not occur.

Instead, if during the heating process the liquid temperature reaches, for example, 89°C (point R in Fig. 22.2), during the depressurization the tangent line will be reached (point S in Fig. 22.2). In this case the conditions required (superheating) by the aforementioned spontaneous homogeneous nucleation would exist and a BLEVE explosion would occur.

However, this conventional theory, although accepted by many authors, fails to explain some of the BLEVEs that have occurred.

In fact, the use of the tangent line to the saturation curve at the critical point as the limiting value for the occurrence of a BLEVE implies a margin of safety. The experimental data seem to indicate that the difference between the real superheating limit required to originate a BLEVE and the value thus obtained is in the range of 15 to 25°C (Table 22.3). This is because when the substance is depressurized to reach the superheating limit line, there is also a slight decrease in its temperature. Thus, to reach the intersection point between the tangent at the critical point and the straight line at $P = 1$ atm (superheat temperature limit), it is necessary to start from a higher temperature. If, from this new temperature, a vertical line is drawn, a new limit temperature will be obtained for the line $P = 1$ atm. Connecting this new point to the critical point, a new superheating limit line will be obtained which is nearer to the real situation than the tangent line. The following expression has been suggested (Sigalés and Trujillo, 1990) for the calculation of this superheating limit:

$$T_g - T_s = 0.82206\, T_{cr} - 0.89485\, T_s \tag{22.5a}$$

where T_g is the superheat temperature limit and T_s is the saturation temperature corresponding to atmospheric pressure. For many organic and inorganic liquids the superheat temperature limit values are within 88 to 92% of the critical temperature. Reid (1976) used the equation-of-state of Redlich-Kwong to obtain the following expression:

$$T'_g = 0.895 \cdot T_{cr} \tag{22.5b}$$

The difference between the temperatures calculated from these two expressions ranges from −1°C to +8°C for the substances included in the table (Armet, 1997).

However, this is a theoretical treatment and some authors (e.g., Casal et al., 1999) doubt the applicability of these predictions to a real case. In the heating of a large vessel by a fire, there are probably some aspects that can make it difficult to predict the BLEVE occurrence. Amongst these we can cite the liquid temperature stratification, which can significantly affect the thermodynamic conditions inside the vessel. The content of a tank is not heated uniformly

TABLE 22.3 Calculation of the Superheat Temperature Limit for Diverse Substances

Substance	Formula	T_s, K	T_c, K	P_{cr}, atm	ln P_{cr}	A	B	tg α	b, atm	T_R, K	T_g, K	T'_g, K	$T'_g - T_g$, K
Water	H_2O	373	647	217.7	5.38312	4741	12.71	2.4656	−1378	559	571	579	8
Carbon dioxide	CO_2	195	304	73	4.29046	2333	11.96	1.8429	−487	265	270	272	2
Ammonia	NH_3	240	406	112.3	4.72117	2771	11.55	1.8878	−654	347	359	363	4
Phosgene	$COCl_2$	281	455	56	4.02535	2958	10.53	0.8001	−308	386	404	407	3
Methane	CH_4	111.5	191	45.8	3.82428	1024	9.18	1.2856	−200	156	169	171	2
Ethane	C_2H_6	184.4	305	48.8	3.88773	1812	9.83	0.9499	−241	255	270	273	3
Ethylene	C_2H_4	169.1	282.7	50.9	3.92986	1654	9.78	1.0534	−247	235	250	253	3
Propane	C_3H_8	231.1	369.8	43	3.76120	2317	10.03	0.7286	−226	312	328	331	3
Propylene	C_3H_6	225	365.3	45	3.80666	2230	9.91	0.7520	−230	307	324	327	3
n-Butane	C_4H_{10}	272.5	425.8	38.7	3.65584	2767	10.15	0.5906	−213	362	379	381	2
n-Pentane	C_5H_{12}	309.3	470.2	33	3.49651	3160	10.22	0.4717	−189	403	419	421	2
n-Hexane	C_6H_{14}	342	507.8	29.5	3.38439	3545	10.37	0.4056	−176	436	453	454	1
n-Heptane	C_7H_{16}	371.3	539.8	26.8	3.28840	3911	10.53	0.3597	−167	467	483	483	0
n-Octane	C_8H_{18}	398.8	569.2	24.7	3.20680	4272	10.71	0.3257	−161	497	510	509	−1
Ethyl eter	$(C_2H_5)_2O$	307.6	467	35.5	3.56953	3217	10.46	0.5236	−209	401	416	418	2
Chlorine	Cl_2	238.4	419	93.5	4.53796	2510	10.53	1.3368	−467	350	370	375	5

during a fire. In diverse fire tests conducted on LPG tanks (Towsend et al., 1974; Appleyard, 1980; Birk and Cunningham, 1996), the temperature of the liquid varies from the tank bottom (where the liquid is cooler) to the top (where the liquid is warmer) (Birk, 1995). This temperature stratification is due to buoyancy effects. Therefore, the temperature stratification plays an important role in case of an accident. This is a field in which there is still interesting research to be done.

22.4 ESTIMATION OF BLEVE EFFECTS

22.4.1 Thermal Radiation

When a BLEVE explosion involves a flammable substance, it is usually followed by a fireball and intense thermal radiation will be released. The thermal energy is released in a short time, usually less than 40 seconds (although this time is a function of the mass in the tank). The phenomenon is characterized from the first moments by strong radiation; this eliminates the possibility of escaping for the persons nearby (who also will have suffered the effects of the blast).

The parameters that must be evaluated for predicting the effects of a fireball are the diameter, duration, and thermal radiation at any given distance. In this section, a methodology is described to estimate these values.

Diverse authors have proposed correlations for the prediction of the diameter and duration of a fireball originated by a given mass of fuel (CCPS, 1994). Most of them have the following general expression:

$$D = a \cdot M^b \tag{22.6}$$

$$t = c \cdot M^e \tag{22.7}$$

where a, b, c, and e are empirical or semiempirical constants. A comparative study of 16 of these expressions has been made (Satyanarayana et al., 1991) (Table 22.4).

These authors compared the predictions from the diverse equations to real data from several explosions. The statistical analysis showed that the ‘‘best’’ correlation for the estimation of fireball diameter was the one proposed by Gayle (2), followed by those proposed by Marshall, Roberts, TNO, and High. Concerning the duration of the phenomenon, no comparison was possible, as there were no real data in the literature. Therefore, the expression proposed by Gayle could also be used. In another analysis carried out with 23 equations, Capdevila (1994) showed that the best ones were those proposed by Gayle (2), Marshall, Roberts, Moorhouse, TNO, High and Clay et al. Other authors (e.g., Lefin et al., 1993) found significant differences between the correlations published.

Therefore, it is rather difficult to establish which is really the best equation. Actually, and according to the previously comparative analysis, the diameter and duration of a fireball can be predicted using the following correlations:

$$D = 6.14 \cdot M^{0.325} \tag{22.8}$$

$$t = 0.41 \cdot M^{0.340} \tag{22.9}$$

where the units are m (D), kg (M), and s (t).

It is worth noting, however, that there are very few experimental data available to back up this type of comparative analysis. Furthermore, these data—obtained from real accidents in the case of large fireballs—are not always accurate, as often the films are incomplete or

TABLE 22.4 Equations for Estimating Fireball Diameter and Duration

Author	Reference	*a*	*b*	*c*	*e*
Gayle (1)	(Bagster and Pitblado, 1989)	3.68	0.326	0.245	0.356
Gayle (2)	(Bagster and Pitblado, 1989)	6.14	0.325	0.410	0.340
Brasie	(Bagster and Pitblado, 1989)	3.80	0.333	0.300	0.333
Marshall	(Bagster and Pitblado, 1989)	5.50	0.333	0.380	0.333
Roberts	(Lees, 1980)	5.80	0.333	0.450	0.333
SRD[a]	(Bagster and Pitblado, 1989)	6.00	0.333	0.005	—[c]
Fay-Lewis	(Fay and Lewis, 1977)	6.36	0.333	2.570	0.167
Hardee	(Bagster and Pitblado, 1989)	6.24	0.333	1.110	0.167
Hasegawa	(Bagster and Pitblado, 1989)	5.28	0.277	1.099	0.097
Hasegawa-Sato	(Hasegawa and Kato, 1978)	5.25	0.314	1.070	0.181
Moorhouse	(Bagster and Pitblado, 1989)	5.33	0.327	0.923	0.303
TNO	(Bagster and Pitblado, 1989)	6.48	0.325	0.852	0.260
Maurer	(Lihou and Maunde, 1982)	3.51	0.333	0.320	0.333
High	(Lihou and Maunde, 1982)	6.20	0.320	0.490	0.320
HSCC[b]	(Lihou and Maunde, 1982)	6.45	0.333	5.530	0.333
API	(Kayes, 1985)	5.33	0.327	1.089	0.327

[a] Safety and Reliability Directorate.
[b] Hot shell cold core model.
[c] M is used as $\log_{10} M$

bad. The significant difficulty involved in experiments on a large scale complicates the study of fireball accidents that happen from time to time in the industry or in the transportation of certain materials.

In fact, the lack of accuracy is not due only to the differences in the predictions from the diverse correlations. Another factor influencing it is the estimation of the fraction of the overall mass of fuel that really is involved in the fireball. As happens in many cases of risk analysis, the inaccuracy arises from the definition of the problem itself. It should be taken into account that some fuel has been leaving the vessel through the safety valves from the moment in which they opened; the amount released will depend on the time elapsed between this moment and that of the explosion. Furthermore, more fuel is entrained in the wake of the propelled fragments. The final result is that it is impossible to accurately establish the mass of fuel that will actually contribute to the fireball. This difficulty is found in the criteria recommended by different authors. Nazario (1988) suggests that the mass corresponding to the maximum capacity of the vessel should be used, and Pietersen and Cendejas (1985) recommend 90% of this value; other authors consider that only two-thirds or three-quarters of the initial fuel mass is finally involved in the fireball.

However, in calculating the fireball's diameter, most correlations include the mass of fuel affected by an exponent equal to one-third; this reduces considerably its influence on the value of D. This influence has been calculated (Calpe and Casal, 1989) for a maximum mass of 15,000 kg (Table 22.5).

Finally, the lack of accuracy is also due to the fire wake left by the fireball, which can reach a significant size; this modifies the flame surface and consequently the radiation that will reach a given point.

In any case, the correlations mentioned in the previous paragraphs allow an approximate estimation of the size of the fireball. It should be taken into account, furthermore, that its size and position change continuously; therefore, the thermal radiation is not constant. The available films of BLEVE accidents show that the fireball grows quickly up to its maximum diameter, remaining at this diameter for a short time and then dissipating. Usually the cal-

TABLE 22.5 Influence of the Initial Mass of Fuel on the Fireball Diameter (values corresponding to a maximum mass of 15,000 kg of LPG)

%	*D, m*
100	140
90	135
75	127
67	123

culation of the radiation received by a given target is performed supposing that the fireball reaches its maximum size immediately after reaching a certain height.

To estimate the radiation received by a surface located at a given distance, the solid body model can be applied:

$$I = \tau \cdot F \cdot E_p \tag{22.10}$$

It is necessary, therefore, to know the value of the emissive power (E_p), the view factor (F), the atmospheric transmissivity (τ), and the distance between the flame and the target. To know this distance, it is necessary to estimate the height at which the fireball is located. In fact, this height is a function of the specific volume and the latent heat of vaporization of the fuel; therefore, strictly speaking, it varies with the substance. This is not usually taken into account. Diverse correlations have been proposed to estimate this height; one of the most simple ones is the following:

$$H = 0.75\ D \tag{22.11}$$

where H is the height at which the center of the fireball is located (in m) and D is its diameter, calculated with Eq. (22.8).

Fay and Lewis (1977) proposed another correlation:

$$h = 12.7\ V_i^{1/3} \tag{22.12}$$

where h is the height of the top of the fireball (in m) and V_i is the initial volume of vapor in the fireball (in m^3).

Both expressions have been compared to the values corresponding to three real cases; the results can be seen in Table 22.6 (the heights correspond to the top of the fireball; note that if Eq. (22.11) is used, ($D/2$) must be added to obtain the value of h).

TABLE 22.6 Predicting the Height (Top of the Flame) of the Fireball

Accident	Fuel	Mass, kg	*h, m* (observed)	*h, m* (Eq. 22.11)	*h, m* (Eq. 22.12)
Crescent City[a]	Propane	35,000	230	230	315
Priolo	Ethylene	80,000	225	301	435
Priolo	Propylene	50,000	250	258	357

[a] Lewis, 1991.

The results from Eq. (22.11) are much closer to the observed values than those predicted by the other correlations; although only three cases imply a low statistical significance, this equation—which, furthermore, is the simplest one—can be recommended.

Once more, we do not know for certain what fraction of the energy released is emitted as thermal radiation. In fact, this is the most important uncertainty in the calculation of the thermal radiation from a fireball. The following correlation has been proposed (Roberts, 1982) to estimate this value:

$$\eta = 0.27 \cdot P_o^{0.32} \qquad \text{(maximum value of } \eta \text{ limited to 0.4)} \tag{22.13}$$

where P_o is the pressure (relative) in the vessel just before the explosion, in MPa. This expression was obtained from experimental data at laboratory scale, with amounts of fuel of a few kilograms; its validity at real scale has not been tested. The value of η ranges from 0.13 to 0.35, according to diverse authors, and from 0.24 to 0.40 according to others. In any case, its maximum value is 0.4. From this radiation coefficient and the overall combustion energy, the energy radiated can be deduced. Then, the emissive power can be calculated from the following equation:

$$E_p = \frac{\eta\, M\, H_c}{\pi\, D^2\, t} \tag{22.14}$$

where H_c is the combustion heat ($kJ \cdot kg^{-1}$) and t the time of duration of the fireball (s). Because the value of η can be inaccurate, another possibility is to use directly an arbitrary value of E_p in the range of 200 to 350 $kW \cdot m^{-2}$ according to some authors (Bagster and Pitblado, 1989) and 200 $kW \cdot m^{-2}$ according to others (Pietersen and Cendejas, 1985).

The maximum view factor is that corresponding to a sphere and a plane surface perpendicular to its radius. Due to the geometrical simplicity of this system, this factor can be calculated with a very simple equation:

$$F = \frac{D^2}{4r^2} \tag{22.15}$$

where r is the distance between the surface receiving the radiation and the center of the fireball [$(R + x)$ in Fig. 22.6]. For other positions of the surface of the target, the value of F must be corrected by using the angle formed by the surface and the surface perpendicular to the radius (see the example at the end of this section).

Finally, the atmospheric transmissivity can be estimated from the following equation:

$$\tau = 2.02 \cdot (P_w \cdot x)^{-0.09} \tag{22.16}$$

22.4.2 Mechanical Energy Released in the Explosion

When a vessel bursts in a BLEVE explosion, the mechanical energy contained inside is released (note that the units of pressure are energy per unit volume). The substance contained in the vessel instantaneously increases in volume due to the expansion of the vapor already existing in the vessel at the moment of the explosion and the superheated liquid, which undergoes a partial vaporization practically instantaneously (flash).

The energy released in a BLEVE explosion is distributed among the following:

- The energy of the pressure wave
- The kinetic energy of the projectiles

- The potential energy of the fragments (deformation plastic energy absorbed by the fragments)
- The heating of the environment

The relative distribution of the energy will change in relation to the particular conditions of the explosion. It is very difficult to establish accurately which amount of energy will contribute to the pressure wave (Capdevila, 1994). An important aspect is the type of failure (fragile or ductile). When the tank wall is heated and stressed, it may begin to thin due to plastic creep, and fissures may form. If this effect is very localized (for example, due to a jet fire) the fissure may stop growing as it enters thicker and stronger material (Birk, 1995). However, this fissure may result in sudden depressurization and a strong flashing effect in the liquid. As a result, pressure recovery can take place and this can restart the crack and finally lead to tank bursting and BLEVE.

If the thermal effects are more widespread (as for example, in the case of a tank engulfed in a pool fire (Planas-Cuchi et al., 1996)), then the fissure may continue to grow and the tank may burst, resulting in a BLEVE. The propagation speed will depend on the mode of failure. If the wall thickness has decreased by plastic creep, the crack speed will be different than if the crack must propagate through thicker material by shear failure (Birk, 1995). There is an upper limit to the crack velocity, related to the material yield strength and density (Baum, 1982). The actual propagating velocity is usually less than the limiting velocity; it can reach velocities up to 200 $m \cdot s^{-1}$.

It has been suggested that in a fragile failure of a vessel, 80% of the energy released contributes to the creation of the pressure wave. In the case of a ductile breaking—in which large fragments of the vessel are propelled—the energy in the pressure wave is only 40%. In both cases, the rest of the energy becomes kinetic energy of the fragments, as the fourth contribution (heating of the environment) is negligible.

In fact, most vessels or tanks are constructed with materials that are ductile at the operating conditions. A fragile failure is found only in very special conditions, when the stress reached by the material is much higher than its plastic limit. This only happens with tempered steel and glass. Therefore, BLEVE explosions usually consist of ductile breaking.

Concerning the vapor initially in the vessel, the energy released in its expansion (from the breaking pressure in the vessel up to the atmospheric pressure) is:

$$E_v = m(u_1 - u_2) \tag{22.17}$$

where E_v = the energy released in the expansion of the vapor (kJ)
m = the mass of vapor already existing in the vessel at the moment of the failure (kg)
u_1 = the internal energy of the vapor under the conditions at which the vessel bursts ($kJ \cdot kg^{-1}$)
u_2 = the internal energy of the vapor after the expansion up to atmospheric pressure ($kJ \cdot kg^{-1}$).

Supposing that the expansion is isoentropic—due to the velocity at which it takes place—and that the vapor behaves as an ideal gas, this energy is (Prugh, 1991):

$$E_v = 10^2 \cdot \left(\frac{P \cdot V}{\gamma - 1}\right) \cdot \left(1 - \left(\frac{P_a}{P}\right)^{(\gamma-1)/\gamma}\right) \tag{22.18}$$

where P_a is the atmospheric pressure (bar), V is the initial volume of vapor (m^3), γ is the ratio of specific heats, and P is the pressure (bar) in the vessel just before the explosion.

This energy can be expressed as TNT equivalent mass by using the adequate energy conversion factor (approximately 1,120 cal per gram of TNT),

$$W_{\mathrm{TNT}} = \left(\frac{0.021 \cdot P \cdot V}{\gamma - 1}\right) \cdot \left(1 - \left(\frac{P_a}{P}\right)^{(\gamma-1)/\gamma}\right) \tag{22.19}$$

where W_{TNT} is the equivalent mass of TNT (kg).

Furthermore, if the vessel contained superheated liquid—as in the case of a BLEVE explosion—the released energy can be estimated approximately by using the same method. In this case, it must be taken into account that the mass of liquid will partly vaporize suddenly when reaching atmospheric pressure. The volume of this vapor at the pressure in the vessel just before the explosion must then be calculated; adding this fictitious volume to the real one, the equivalent mass of TNT will be:

$$W_{\mathrm{TNT}} = \left(\frac{0.021 \cdot P \cdot V^*}{\gamma - 1}\right) \cdot \left(1 - \left(\frac{P_a}{P}\right)^{(\gamma-1)/\gamma}\right) \tag{22.20}$$

where V^* is the volume of vapor in the vessel plus the volume (at the pressure inside the vessel) of the vapor generated in the explosion, in m^3:

$$V^* = V + V_1\, f \left(\frac{\rho_1}{\rho_v}\right) \tag{22.21}$$

V is the volume of vapor inside the vessel before the explosion, V_1 is the volume of liquid in the vessel before the explosion (m^3), and f is the vaporization fraction (flash), i.e., the fraction of liquid which vaporizes in the depressurization; its value can be calculated with the following expression:

$$f = 1 - \exp\left(-2.63(C_p/H_v)(T_c - T_b) \cdot (1 - ((T_c - T_o)/(T_c - T_b))^{0.38})\right) \tag{22.22}$$

where T_c is the critical temperature of the substance (K), T_b is the boiling temperature of the substance at atmospheric pressure (K), T_o is the temperature of the substance in the moment of the explosion (K), and H_v is the enthalpy of vaporization of the substance (kJ $\cdot$ kg^{-1}).

In fact, the blast contribution from the liquid will be affected by the lack of homogeneity in liquid temperature, decreasing as liquid temperature stratification increases (since liquid stratification reduces the average liquid temperature, the vaporization fraction will decrease). However, the actual knowledge of the liquid stratification phenomenon does not allow the suitable correction to be introduced into the blast calculation.

22.4.3 Pressure Wave

The pressure wave generated by the explosion can be estimated from the equivalent TNT mass. This method implies a certain inaccuracy because in the BLEVE explosion of a vessel the energy is released at a lower velocity than in a TNT explosion and also because the volume of the vessel is much larger than that which would have the equivalent amount of a conventional explosive. Nevertheless, the method is simple and allows useful estimations.

Due to the fact that the volume initially occupied by the energy released in the explosion is much larger than that which would occupy the equivalent mass of TNT, a correction must be made on the distance from the explosion center to the place in which the pressure wave must be estimated.

This correction is carried out by using the scaled distance, d_n, based on the similitude principle proposed by Hopkinson in 1915, according to which when two explosive charges of similar geometry and of the same explosive but different sizes, detonate in the same atmosphere, similar pressure waves are generated at the same scaled distance. This principle can also be applied to two different explosives, taking into account the fact that two types of explosion with the same overpressure give rise to the same effects. Because overpressure is a function of the distance and two different explosions do not cause the same overpressure at the same distance from the center of the explosion, the scaled distance is defined as that at which the overpressure has the same value for both explosions. The scaled distance is related to the real distance and to the equivalent TNT mass by the cubic root law,

$$d_n = \frac{d}{(\beta \cdot W_{\mathrm{TNT}})^{1/3}} \tag{22.23}$$

where d_n = the scaled distance ($\mathrm{m \cdot kg^{-1/3}}$)
β = the fraction of the energy released converted in pressure wave
d = the real distance (from the center of the explosion) at which the overpressure must be estimated (m).

From the value of d_n it is possible to estimate the overpressure by using a graph as shown in Fig. 22.3.

22.4.4 Missiles

Because of their random behavior, projectiles from BLEVEs are one of the most difficult hazards to quantify (Birk, 1996). The fragments thrown by the explosion have a restricted and directional action, but with a larger radius of destructive effects than the pressure wave and the thermal effects of the fireball. These fragments can cause a domino effect if they destroy other tanks or equipment. The velocity required by a fragment to penetrate another similar tank ranges from 4 to 12 $\mathrm{m \cdot s^{-1}}$, and the maximum velocity that can be reached by the fragments in a BLEVE explosion—a function of the conditions at which the explosion occurs, the volume of vapor initially contained in the vessel, and the shape of the vessel—ranges from 150 to 200 $\mathrm{m \cdot s^{-1}}$.

There are basically two kinds of projectiles from BLEVEs, as in the case of conventional explosions of containers:

1. Primary projectiles, which are major pieces of the container
2. Secondary projectiles, which are generated by the acceleration of nearby objects (pipes, bars, bricks, etc.)

The number of primary projectiles (i.e., major pieces of the tank) will depend on the type of failure, the shape of the vessel, and the severity of the explosion. Typically, a BLEVE will involve a ductile failure; the cracks will propagate at lower velocity and without branching. The number of fragments will be less than if it were a fragile failure. The number of projectiles will be in the range of 2 to 15 (Baum, 1988, has reported that typically there will be less than five projectiles).

In the case of cylindrical tanks, the initial crack usually follows an axial direction and then changes and follows a circumference (following, for example, a welding); thus, the vessel is usually broken into two pieces: the bottom of the tank and the rest of the vessel.

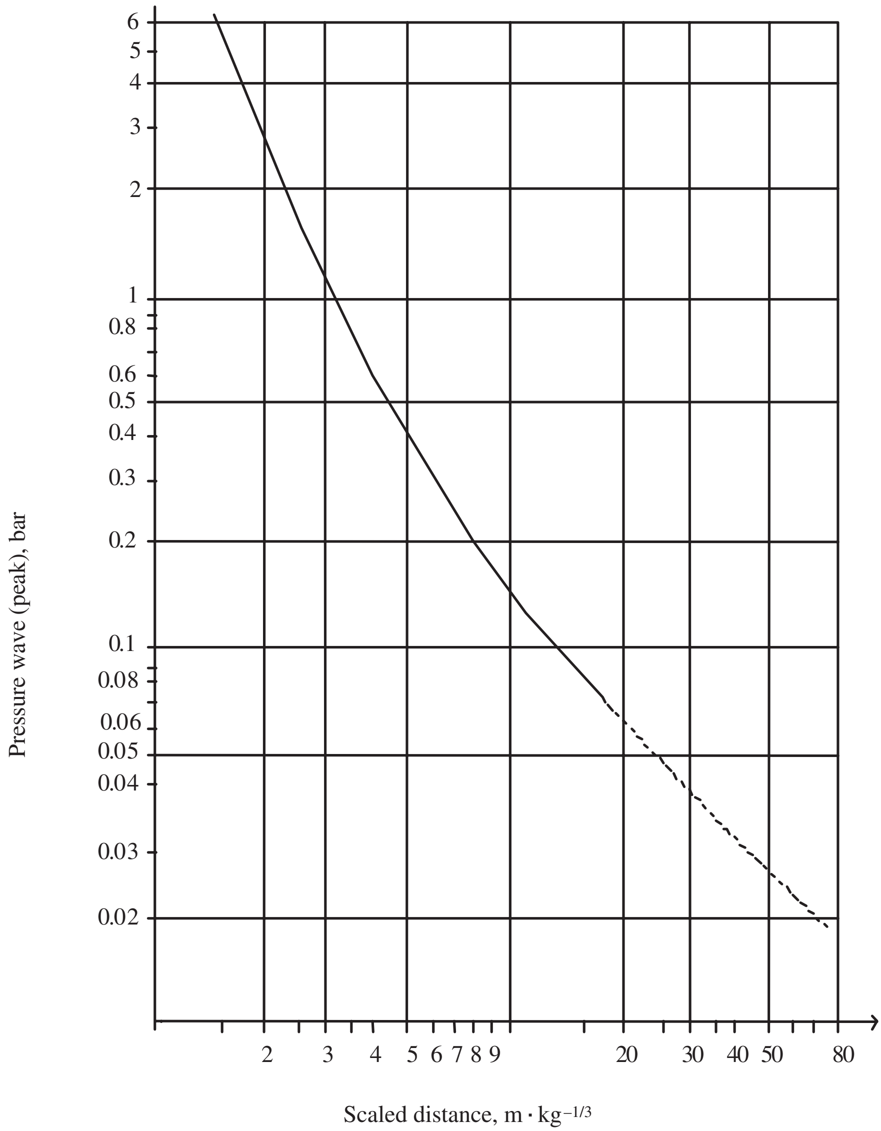

FIGURE 22.3 Pressure wave as a function of the scaled distance. (*Source: Van den Berg and Lannoy, 1993*)

The results of an analysis on 130 BLEVE cases, most of which occurred in cylindrical tanks, are shown in Table 22.7.

In cylindrical tanks there can also be three projectiles. If there are three fragments, there can be two types of failure. The vessel can be divided into two bottoms and the central body, or it can be first divided into two fragments, one bottom and the rest, and then this second fragment can be divided through the imaginary line that would separate the liquid and the vapor as shown in Fig. 22.4. The bottom usually breaks by the welding; if there is no

TABLE 22.7 Number of Projectiles in BLEVE Accidents

	With projectiles	Without projectiles
Due to fire	89	24
Without any fire	17	—

Source: Holden and Reeves, 1985.

welding, we can suppose that it will break at a distance from the end equal to 10% of the total length of the vessel.

Concerning the direction, the projectiles will probably follow the direction of the cylinder axis. Data from 15 accidents (Holden and Reeves, 1985) provided the data in Table 22.8, taking into account the 45° sectors at each side of the cylinder. Data obtained experimentally from 13 BLEVEs of 400-l vessels (Birk, 1996) gave somewhat different results; in some cases, this author observed that the tank remained flattened on the ground with both ends attached.

The distance reached by projectiles from cylindrical tanks is usually greater than that reached by fragments from spherical vessels. The following expressions have been suggested (Baum, 1988; Birk, 1995) for the prediction of the range of cylindrical tank projectiles (tube fragments):

$$\text{For tanks} < 5\ \text{m}^3 \text{ in capacity:} \qquad l = 90 \cdot M^{0.33} \tag{22.24a}$$

$$\text{For tanks} > 5\ \text{m}^3 \text{ in capacity:} \qquad l = 465 \cdot M^{0.1} \tag{22.24b}$$

where M is the mass of substance contained in the vessel (kg) and l is the range (m).

The difference between the two expressions is due to the reduced relative effect of drag (ratio of drag force to tank weight) as tank size increases (when tank capacities exceed 5 m^3, the tank size is increased by increasing the tank length, not the tank diameter) (Birk, 1995). These expressions were obtained assuming the tank is 80% full of liquid LPG at the time of failure, and for fragments launched at an optimum angle (45° to the horizontal). Because most fragments will not be launched at this angle, the real ranges will typically be less than those predicted by Eqs. (22.24*a*) and (22.24*b*).

Recently, Baum (1999) has proposed the following expressions, which agreed very well with his experimental data, to estimate the upper bond of velocities for fragments from horizontal pressure vessels containing a high-temperature liquid:

$$\text{End-cap missiles:} \qquad U = \frac{1.25 \cdot K^{0.375}}{\left(\dfrac{P_{\text{sat}}}{P_a}\right)^{0.085}} \cdot a_o \tag{22.25}$$

$$\text{``Rocket'' missiles:} \qquad U = \frac{I}{W} \tag{22.26}$$

where

$$K = \frac{P_o \cdot \pi \cdot R_f^3}{W \cdot a_o^2} \tag{22.27}$$

and I is the impulse (integrated pressure history of the closed end), which can be calculated as follows:

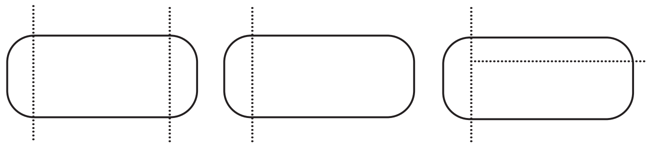

FIGURE 22.4 Common failure trends in cylindrical vessels.

$$\text{if } P_o > P_{\text{sat}} \qquad I = A \cdot (P_o \cdot t_w + P_{\text{sat}} \cdot (t_o - t_w) + P_{\text{sat}} \cdot t_e) \tag{22.28}$$

$$\text{if } P_o = P_{\text{sat}} \qquad I = P_{\text{sat}} \cdot A \cdot (t_o + t_e) \tag{22.29}$$

with

$$t_w = \frac{L}{a_w} \tag{22.30}$$

$$t_o = \left(\frac{W}{P_{\text{sat}} \cdot \pi \cdot R}\right)^{1/2} \tag{22.31}$$

$$t_e = \frac{L}{a_m} \tag{22.32}$$

Some years ago it was considered that projectiles could reach 500 m as a maximum; however, in the accident in Mexico City, a projectile from a large cylindrical vessel traveled 1,100 m and another one from a sphere went 600 m. The evacuation distance should therefore be 1,100 m for large cylindrical tanks.

In the case of spherical vessels, it is much more difficult to predict the number of fragments. The analysis of a reduced number of cases (Pietersen and Cendejas, 1985) gave an average of eight fragments per accident. This author obtained the following correlation:

$$N = -3.77 + 0.0096\ V_r \tag{22.33}$$

where N is the number of fragments and V_r is the volume of the spherical vessel, ranging from 700 to 2,500 m^3. However, taking into account the scattering of the data used by Pietersen, this correlation should not be considered very reliable.

The direction followed by the projectiles from a spherical vessel is practically impossible to predict. The analysis of several cases shows that the distribution is not symmetrical; this must be attributed probably to the special position of the contact flame/vessel in each case, although other aspects (construction details, for example) can also have an influence.

Finally, for projectile range, the analysis of 58 fragments from seven accidents has shown that 70% of the fragments reached distances less than 200 m. However, fragments from

TABLE 22.8 Probability of Projectile Launching in Cylindrical Vessels

Sector	Probability
1	0.62
2	0.38

Source: Holden and Reeves, 1985.

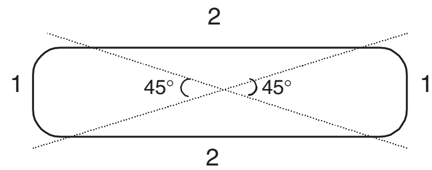

FIGURE 22.5 Distribution of projectiles from a cylindrical vessel.

spherical vessels have reached 600 m (Mexico City) and even 700 m. The distance reached is usually smaller in the case of fragments from spherical vessels because they are less aerodynamical than those from cylindrical tanks. Different theoretical models have been suggested for the prediction of these maximum distances, but they are not very practical, since to apply them the mass and shape of the fragment should be known.

22.5 PREVENTION MEASURES

In the case of an emergency that can lead to an accident of the BLEVE fireball type, it is very difficult to improvise adequate actions to control the situation. Any plan requiring the presence of people will be very dangerous because it is impossible to foresee when the explosion will occur. The actions should therefore be preventive and taken beforehand (Bestratén and Turmo, 1991b). The risk of a BLEVE can be reduced to tolerable levels if several of these measures can be taken at the same time. These are briefly discussed here.

22.5.1 Sloping Ground

The installation must be designed in such a way that any leak of a liquid (for example, liquefied petroleum gas) could be immediately removed from the area in which there is the tank that must be protected. The ground should be smooth and with a slope of 2.5% (1.5% minimum); a draining system must lead to a trench or a tank far enough away to avoid contact between the flames and the tank. It must be taken into account that in case of wind, the flames can have an inclination of 45° as well as a significant drag and that they can reach approximately twice the diameter of the trench (Kletz, 1977).

22.5.2 Thermal Insulation

If the walls of the tank are blanketed with a fireproof material (with a low thermal conductivity), the heating of the vessel, and therefore its pressure increase, by an eventual fire is significantly delayed. Furthermore, in long emergencies, thermal insulation reduces the heat flow to the system and makes it possible for the safety valve to prevent the explosion (fireproofing relies significantly on the correct operation of the safety valve). It must be taken into account that these valves are not designed to solve these types of emergencies on their own, as their cross section should be excessive. Fireproofing ensures protection for a limited time (usually four to five hours). It is the most suitable device for road or railway tanks (Londiche and Guillemet, 1991).

In any case, thermal insulation should be a complement, and other protective systems (for example, cooling of the vessel) should be installed. Another interesting point is that the structural elements (vessel legs) should also be insulated, to avoid the falling of the vessel under excessive heating (this is what happened with two of the spherical tanks in Mexico City; even after falling, however, surprisingly they did not explode). The thermal insulation should be installed in such a way that it could be effective in the event of a fire and also allow the tank surface and structural elements to be inspected periodically.

22.5.3 Cooling with Water

The usefulness of water sprinklers in protecting vessels exposed to the direct action of fire has been proven over many years. It is important to use the water from the first moments, with a layer of a certain thickness totally covering the wall to be cooled, especially those areas directly in contact with the flame. The required flowrate of water should be kept constant—in some cases, the action of firefighters and the consequent increase in water consumption have considerably decreased the pressure in the network and, thus, the water flowrate to the vessels—with a minimum value that will depend on the circumstances.

To protect a fire-engulfed tank, the water flowrate will depend on the circumstances. If the safety valve is correctly designed and works normally, the water rate (Londiche and Guillemet, 1991) should not be less than $8 \text{ l} \cdot \text{m}^{-2} \cdot \text{min}^{-1}$; however, diverse authors (Maddison, 1989) consider that reducing the water flowrate below $10 \text{ l} \cdot \text{m}^{-2} \cdot \text{min}^{-1}$ is dangerous if there is direct contact with the flame; a flowrate of $15 \text{ l} \cdot \text{m}^{-2} \cdot \text{min}^{-1}$ has been recommended (Londiche and Guillemet, 1991; Nazario, 1988; Vílchez et al., 1993) as a general criterion. To have an efficient cooling effect, water should be applied before the temperature of the wall reaches 80°C. If there is no flame impingement, only thermal radiation, smaller flowrates can be used.

If there is flame impingement on the wall, the thermal flux will depend on the type of flame (for a pool fire it can be approximately $100 \text{ kW} \cdot \text{m}^{-2}$, while for a highly turbulent flame it can reach $350 \text{ kW} \cdot \text{m}^{-2}$). In this case, for the zone of the wall located above the liquid surface, flowrates even larger than $25 \text{ l} \cdot \text{min}^{-1} \cdot \text{m}^{-2}$ may be required.

Another aspect to be taken into account is that all safety elements—valves, pipes, etc.—should be designed to resist the action of fire and the high temperatures that will be reached during the emergency; otherwise, they will collapse in the first moments, especially if there is direct contact with the flames.

22.5.4 Pressure Reduction

If pressure is reduced, the walls of the vessel will be exposed to less force and the risk of explosion if the temperature increases will be lower. As a general criterion, API recommends the installation of devices able to reduce the pressure up to approximately 7 bar (relative) or up to half of the design pressure in 15 minutes. If the ground is sloped and the vessel is thermally insulated, this time can be longer. The depressurization can require a remote control valve besides the safety valve. The released material should be eliminated in safe conditions (Shebeko et al., 1996), e.g., with a torch. It should also be taken into account that in some cases a strong depressurization can cause extremely low temperatures, leading to fragile conditions in the steel.

22.5.5 Mounding or Burying

The possibility of either totally or partially burying the vessel has been suggested. This provides good protection against thermal radiation for a very long time period, as well as against missiles impacts. However, this measure has many disadvantages, primarily the eventual corrosion in the tank walls.

22.5.6 Water Barriers

This is a relatively new system in which a set of sprayers generates curtains of fine water spray. The barriers retain the vapor released from the leak, thus reducing the possibility of ignition, and disperse them into the atmosphere.

22.5.7 Protection from Mechanical Impacts

Tanks containing materials stored at temperatures higher than their boiling temperatures at atmospheric pressure must be protected from impacts from cranes or other equipment or moving vehicles. A special case, not treated here, is the protection of tank cars.

22.5.8 Overflow

This is an incident that has caused a number of BLEVEs. Nowadays it is much less common, however, and adequate devices are installed to avoid it (level controls, safety valves).

22.5.9 Minimum Separation Distances

The minimum distances between vessels are usually established by regulations and will not be discussed here. They are important from the point of view of thermal radiation, and particularly to avoid direct contact between the flames from the fire in one piece of equipment and the wall of another vessel. They do not guarantee protection, however, in the case of an explosion (blast, projectiles).

22.5.10 Actuation on the Initiating Mechanisms

Diverse systems have been proposed to avoid homogeneous nucleation. These include installing aluminum mesh inside the tank and adding nuclei that initiate boiling. However, these systems are still being investigated, except for very specific applications.

22.6 EXAMPLE CALCULATION OF BLEVE FIREBALL EFFECTS

A tank with a volume of 250 m^3, 80% filled with propane (stored as a pressurized liquid at room temperature), is heated by a fire up to 55°C (~19 bar) and bursts. The thermal radiation and the pressure wave must be estimated at a distance of 180 m.

Data:
Room temperature = 20°C; H_R = 50% (partial pressure of water vapor, 1155 Pa); γ = 1.14; $H_v = 4.3 \cdot 10^5$ J · kg^{-1}; H_c = 46,000 kJ · kg^{-1}; $T_{cr} = 369.8\ K$; $T_{\text{boil. atm. p.}} = 231.1\ K$; $\rho_{\text{liquid, 20°C}} = 500$ kg · m^{-3}, $\rho_{\text{liquid, 55°C}} = 444$ kg · m^{-3}; $\rho_{\text{vapor, 55°C}} = 37$ kg · m^{-3}; Cp_{liquid} = 2,4 · 10^3 J · kg^{-1} · K^{-1}.

Solution:
First of all, the mass of propane involved is calculated:

$$M = V_l \cdot \rho_{\text{liq.,20°C}} = (0.8 \cdot 250\ \text{m}^3) \cdot 500\ \frac{\text{kg}}{\text{m}^3} = 100{,}000\ \text{kg}$$

Schematic diagram:

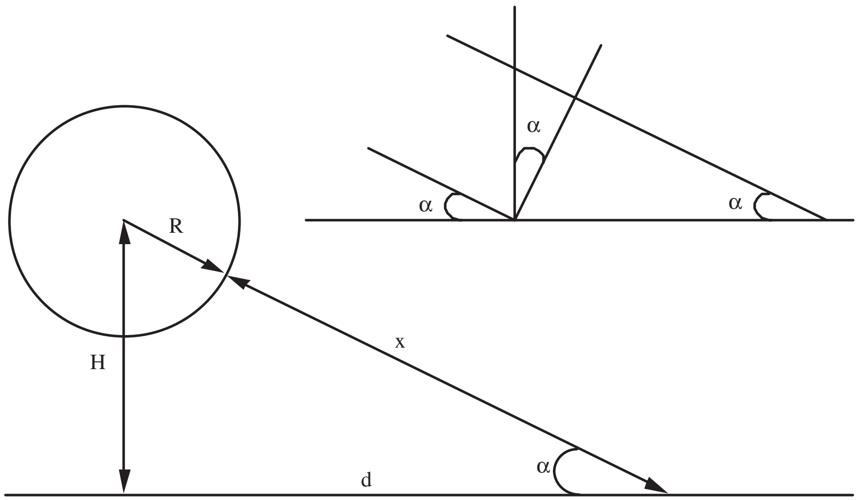

FIGURE 22.6 Position of fireball and target.

1. *Estimation of thermal radiation*
   By using Eq. (22.8), the fireball diameter is estimated:

$$D = 6.14 \cdot M^{0.325} = 6.14 \cdot 100000^{0.325} = 259 \text{ m}$$

Its duration is estimated with Eq. (22.9)

$$t = 0.41 \cdot M^{0.340} = 0.41 \cdot 100000^{0.340} = 20.5 \text{ s}$$

And the height reached by the fireball is estimated by Eq. (22.11):

$$H = 0.75 \cdot D = 0.75 \cdot 259 = 194 \text{ m}$$

The distance between the flame and the target, according to Fig. 22.6, can be calculated as follows:

$$x = \sqrt{H^2 + d^2} - R = \sqrt{194^2 + 180^2} - 129.5 = 135 \text{ m}$$

The atmospheric transmissivity will be, according to Eq. (22.16):

$$\tau = 2.02 \cdot (P_w \cdot x')^{-0.09} = 2.02 \cdot (1155 \cdot 135)^{-0.09} = 0.69$$

The view factor is calculated with Eq. (22.15):

$$F = \frac{D^2}{4\ r^2} = \frac{259^2}{4 \cdot 264.5^2} = 0.24$$

Taking a value of $\eta \approx 0, 25$, the emissive power is [Eq. (22.14)]:

$$E_p = \frac{\eta\ M\ H_c}{\pi\ D^2 t} = \frac{0.25 \cdot 100000 \cdot 46000}{\pi \cdot 259^2 \cdot 20.5} = 266 \text{ kW} \cdot \text{m}^{-2}$$

The radiation intensity on a surface perpendicular to the radiation will be:

$$I = \tau \cdot F \cdot E_p = 0.69 \cdot 0.24 \cdot 266 = 44 \text{ kW} \cdot \text{m}^{-2}$$

On a vertical surface,

$$I_v = I \cdot \cos \alpha = 44 \cdot 0.68 = 30 \text{ kW} \cdot \text{m}^{-2}$$

And on a horizontal surface,

$$I_h = I \cdot \sin \alpha = 44 \cdot 0.73 = 32.3 \text{ kW} \cdot \text{m}^{-2}.$$

2. *Estimation of pressure wave*
The overpressure is estimated from Eqs. (22.20), (22.21), and (22.22):

$$f = 1 - e^{(-2.63 \cdot C_p/H_v \cdot (T_c - T_b) \cdot (1-(T_c - T_o / T_c - T_b)^{0.38}))}$$

$$= 1 - e^{(-2.63 \cdot (2.4 \cdot 10^3/4.3 \cdot 10^5) \cdot (369.8-231.1) \cdot (1-((369.8-328)/(369.8-231.1))^{0.38}))} = 0.525$$

$$V^* = V + V_l \cdot f \cdot \left(\frac{\rho_l}{\rho_v}\right) = 50 + 200 \cdot 0.525 \cdot \frac{444}{37} = 1310 \text{ m}^3$$

$$W_{\text{TNT}} = \left(\frac{0.021\ P\ V^*}{\gamma - 1}\right) \cdot \left(1 - \left(\frac{P_a}{P}\right)^{(\gamma-1)/\gamma}\right) = \left(\frac{0.021 \cdot 19 \cdot 1310}{1.14 - 1}\right)$$

$$\cdot \left(1 - \left(\frac{1}{19}\right)^{(1.14-1)/1.14}\right) = 1133 \text{ kg}$$

admitting that 40% of the released mechanical energy is transformed in pressure wave (ductile breaking of the vessel):

$$(W_{\text{TNT}})_{\text{overpressure}} = \beta \cdot W_{\text{TNT}} = 0,4 \cdot 1133 = 453 \text{ kg}$$

d_n is calculated with Eq. (22.23):

$$d_n = \frac{d}{(\beta \cdot W_{\text{TNT}})^{1/3}} = \frac{180}{453^{1/3}} = 23.4 \text{ m} \cdot \text{kg}^{-1/3}$$

With the TNT equivalence diagram (overpressure versus d_n, Fig. 22.3), an overpressure of 0.05 bar is found.

22.7 NOMENCLATURE

- A Constant in the Antoine equation (–); in Eqs. (22.28) and (22.29) bore cross-section area (m^2)
- a Constant (−)
- a_o Sound velocity in the vapor ($\text{m} \cdot \text{s}^{-1}$)
- a_m Velocity of flow at exit plane ($\text{m} \cdot \text{s}^{-1}$)
- a_w propagation velocity of the initial rarefaction wave ($\text{m} \cdot \text{s}^{-1}$)
- B Constant in the Antoine equation (−)
- b Constant (−)
- c Constant (−)
- C_p Specific heat at constant pressure ($\text{J} \cdot \text{kg}^{-1} \cdot \text{K}^{-1}$)
- D Diameter of fireball (m)
- d Distance from the center of the vessel to the point at which the overpressure must be calculated (m)

d_n Normalized or scaled distance ($m \cdot kg^{-1/3}$)
E_p Emissive power ($kW \cdot m^{-2}$)
E_v Energy released in the vapor expansion (kJ)
e Constant (−)
F View factor (−)
f Vaporization factor (−)
H Height at which the center of fireball is located (m)
h Height at which the top of fireball is located (m)
H_c Heat of combustion ($kJ \cdot kg^{-1}$)
H_R Relative humidity (%)
H_v Enthalpy of vaporization ($kJ \cdot kg^{-1}$)
I Radiation intensity ($kW \cdot m^{-2}$). In Eqs. (22.26), (22.28), and (22.29) impulse applied to closed end ($bar \cdot m^2 \cdot s$)
l range of cylindrical tank projectiles (m)
L Length of "rocket" (m)
M Mass of fuel (kg)
m Mass of vapor existing initially (kg)
N Number of fragments (−)
P Vapor pressure (bar)
P_a Atmospheric pressure (bar)
P_c Critical pressure (atm)
P_o Relative pressure (bar). In Eqs. (22.28) and (22.29) rupture pressure (bar)
P_{sat} Liquid saturation pressure (bar)
P_v Partial pressure of water (Pa)
R Vessel bore radius (m)
R_f fragment radius (m)
r Distance from the center of fireball to the target (m)
T Temperature (K)
T_c Critical temperature (K)
T_b Boiling temperature (K)
T_g Superheat temperature limit at atmospheric pressure Eq. (22.5*a*) (K)
T'_g Superheat temperature limit at atmospheric pressure Eq. (22.5*b*) (K)
T_o Temperature of the substance at the moment of the explosion (K)
T_R Superheat temperature limit according to the tangent line to the vapor pressure-temperature curve at the critical point (K)
T_s Saturation temperature corresponding to atmospheric pressure (K)
t Time (s)
t_e Time required to expel the vessel content from the open end of the "rocket" (s)
t_o Time required to achieve a fully open breach (s)
t_w Time taken for the rarefaction wave to propagate from the break to the closed end of the "rocket" (s)
U Missile velocity ($m \cdot s^{-1}$)
u Internal energy of the vapor ($kJ \cdot kg^{-1}$)
V Volume of vapor in the vessel (m^3)
V_i Initial volume of vapor in the fireball (m^3)
V_r Volume of the spherical vessel (m^3)
V_l Volume of liquid in the vessel just before the explosion (m^3)
x Distance between the flame surface and the target (m)
W missile mass (kg)
W_{TNT} Equivalent mass of TNT (kg)
α Angle formed by the abscissa axis and the tangent to the saturation curve at the critical point (°); also, angle formed by the radius of the fireball and the horizontal (°)
β Fraction of the energy released converted in pressure wave (−)
γ Ratio of specific heats (−)

η Radiation coefficient ($-$)
ρ Density ($kg \cdot m^{-3}$)
ρ_l Liquid density ($kg \cdot m^{-3}$)
ρ_v Vapor density ($kg \cdot m^{-3}$)

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RAIL TANK CAR TOTAL CONTAINMENT FIRE TESTING: RESULTS AND OBSERVATIONS

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ABSTRACT

The frequent incidences of Non-Accident Releases (NARs) of lading from tank cars have resulted in an increasing interest in transporting hazardous materials in total containment conditions (i.e., no pressure relief devices). However, the ability of tank cars to meet thermal protection requirements provided in the Code of Federal Regulations under conditions of total containment has not been established. The intent of this effort was to evaluate through a series of third-scale fire tests, the ability of tank cars to meet the thermal protection requirements under total containment conditions, with a particular focus on caustic ladings. A previous paper on this effort described the test design and planning effort associated with this research effort.

A series of seven fire tests were conducted using third scale tanks. The test fires simulated fully engulfing, hydrocarbon fueled, pool fire conditions. The initial tests were conducted with water as a lading under jacketed and non-jacketed conditions and also with different fill levels (98% full or 50% full). Additionally, two tests were conducted with the caustic, Sodium Hydroxide as the lading, each test with a different fill level. In general, the tanks with water were allowed to fail or reach near-failure conditions, whereas, the tests with the caustic lading were not allowed to proceed near failure for safety reasons. This paper describes the results and observations from the fire tests, and discusses the various factors that affected the fire test performance of the test tanks.

Review of results from the one-third scale tests, and subsequent scaling to full-scale suggest that a full-scale tank car filled with 50% NaOH solution is unlikely to meet the 100-minute survival requirement under conditions of total containment.

[1] - Dr. Birk is also affiliated with Thermdyne Technologies Ltd., Kingston, ON

INTRODUCTION

Tank cars carrying hazardous materials are required to survive a 100-minute, fully engulfing pool fire, without catastrophic failure [1]. Fluid pressure buildup under fire conditions combined with loss of strength in the steel due to elevated temperatures can lead to catastrophic failure of tank shells under certain circumstances. In general, the 100-minute requirement is met through the use of thermal protection and pressure relief devices (PRDs). PRDs help to limit the pressure buildup in tank cars, thereby reducing the potential for a tank explosion. The expectation is that PRD use will result in smaller quantities of hazardous material being released while avoiding the potential for a catastrophic failure. In some cases, PRDs alone may not be sufficient to meet the 100 minute test requirement and thermal protection, which is 'high-performance' insulation that is designed to survive fire conditions, is required. Thermal protection, which in most cases is applied between a steel jacket and the tank, will reduce the heat input to the commodity tank and thus lessen both the temperature rise and pressure rise, thereby helping the commodity tank meet the 100-minute test requirement.

PRDs have a history of leaking and releasing product under nominal operating conditions for a range of reasons that are unrelated to an accident, and reducing the incidences of these frequent Non-Accident Releases (NARs) has been a significant industry focus. Reducing NARs associated with PRDs have resulted in an increasing interest in transporting such materials without the use of PRDs, i.e., conditions of total containment. Total containment is common practice in Europe where there is no 100-minute fire survival requirement. While that solution would help avoid NARs, the downside is that the only mechanism to relieve pressure buildup under fire conditions has been lost. The question then becomes whether a tank car carrying caustics can survive a 100-minute fire under total containment conditions without tank shell failure.

Caustics such as NaOH and KOH are currently transported in non-pressure (DOT111) tank cars that are insulated, but not thermally protected. The tank car industry currently uses a computer program called AFFTAC (Analysis of Fire Effects on Tank Cars) to determine the behavior of tank cars subjected to fire conditions [2]. Simulations conducted by the industry using AFFTAC have indicated that tank cars carrying caustic materials could survive a 100-minute fire under total containment conditions.

AFFTAC was originally developed to determine if tank car designs meet the requirements for thermal protection as provided in the Code of Federal Regulations Title 49, Section§179.18, with an inherent assumption that PRDs would be used to relieve pressure. With the above simulations, the application of the program has been expanded to analyze the behavior of tank cars without thermal protection, as well as, tank cars without pressure relief devices. Further, the program was initially developed to consider pure substances. Over time subroutines have been developed to allow for the analysis of solutions. These applications are founded on the principles of chemistry, thermodynamics, and material science. However, use of this program to underpin a tank car design and or the use of an existing design in a new way requires validation by experiment.

The overall goal of this research project was to conduct a series of 1/3 scale fire tests that can:

- address whether a full-scale tank car carrying NaOH or KOH can survive 100 minutes in a full engulfment fire under total containment conditions
- gather data for verification of the fidelity and performance of the AFFTAC model

Full-scale fire testing of tank cars can be a very expensive proposition (and one reason why the last full scale tank car test [3] was done more than 40 years ago). Given the number of tests that would be needed to fully characterize the behavior under fire conditions to address the issues raised, a series of full-scale tests would be cost prohibitive. On the other hand, given the number of physical properties that would affect the results of this study, their interactions, and non-linear behavior, scaling the results from a small scale test (1/10 scale or less) would require the use of many assumptions of material behavior that cannot be easily substantiated. A medium scale (1/3 scale) test was therefore chosen as a reasonable compromise between cost and scalability of results. The methodology for the tests follows the outline described by Birk in Ref. [4].

One of the challenges of the project was to develop reasonable methods for extrapolating the failure pressures and failure times for tanks that did not fail, and further to scale the results from the one-third scale tests to full-scale results. These elements are discussed further elsewhere in this paper.

Prior to the main tests however, there was a need to design and build the third-scale test tanks and to develop and confirm the performance of a test fire that effectively simulated the pool fire conditions outlined in 49CFR179.18. This effort is described in an earlier paper on this effort [5]. This paper describes the main tests and the results and observations.

MAIN TESTS - SETUP

Long duration fire testing is very complex and challenging. Minor and often, unexpected issues can lead to loss of test data or a poor test outcome. Therefore, researchers on this project spent considerable initial effort on ensuring that several key issues were reviewed and addressed.

One key issue for this testing was dealing with hazardous materials for the caustic tests. The hazardous materials of interest in this test program are NaOH and KOH at 50% solution in water. NaOH and KOH are both highly corrosive. They will burn skin, eyes, and mucous membranes on contact. Breathing vapor is very dangerous. These materials are incompatible with organic materials, aluminum, tin, zinc, wood, paper, and glass. They are both non-flammable, but they will produce other hazardous materials if exposed to fire. Protective clothing/equipment is needed to protect skin, eyes, and breathing passages.

Chlorine Institute and their partners, Dow-Germany, were critical to and prepared for the effective handling of the hazardous materials. Dow worked with the researchers and the team at BAM to ensure that the hazardous materials were delivered in time for the tests, ensured that the right fittings were available to load and unload the test tanks. And also, they were ready with emergency response and personnel, including fire trucks, if such equipment were needed.

Upon completion of the calibration tests [5], the main tests were conducted. Instrumentation for the main tests consisted of:

1. 2 pressure transducers
2. 27 lading thermocouples
3. 11 jacket thermocouples
4. 22 wall thermocouples
5. 10 directional flame thermometers

The pressure transducers were connected to the bottom of the tank either on the dump pipe or on separate pressure tubes. These connections were insulated from the fire to minimize boiling in these pipes/tubes. In addition to these tank and fire instruments, the following were recorded:

1. wind speed and direction
2. burner fuel pressure and mass flow
3. still and video images
4. thermal imager (for some fire calibration tests)
5. high speed images

All data was acquired and recorded using a digital data acquisition system. All tank and fire data were recorded at 1 second intervals.

For tests with significant saved energy or hazardous materials use, it was critical to ensure that the fire could be stopped and the tank contents dumped to a secure container in a safe manner. Therefore, for all the tests, one or more mechanisms to stop the fire and to dump the contents was developed and implemented. The fire was always under the control of the BAM staff with the necessary computer control and manual override. For the water tests, a remotely controlled ball valve was implemented to assist with dumping the fluid. For the caustic tests, a pressure relief valve that was preset to release at 8 Barg provided dump functionality, and allowed for the released contents to be captured by specifically designed containers.

Given that these tests are done outdoors, weather has the potential to influence the test process. In particular, wind direction and magnitude can significantly alter the performance of the fire, with the result of reducing the 'fully engulfing' intent of the test approach, and critically, making the fire input inconsistent from test-to-test. Throughout the fire development and fire calibration process, the researchers collected weather data during the tests to give us assurance that the tests would maintain reasonable consistency at wind speeds up to 3 mph. In addition, the weather forecasts for the local region were reviewed before the test each morning to ensure that the conditions were favorable. Wherever appropriate, the tests were rescheduled to ensure reasonable weather conditions.

Finally, special care was taken to ensure that all instrumentation performed as intended in the fire. This required the use of high temperature instrumentation, the use of high temperature insulation to protect exposed cables, the need for cables to flow through the water bath at the base of the test tank, and several other minor, but critical details.

An exemplar test setup is shown in Figure 1.

1. The tank was supported above the water filled fire pan, on water-cooled supports. The ground level fire nozzles were immersed in a pool of water to protect the pipes and valves from the fire. The water was constantly replenished with fresh cold water to avoid boiling, as boiling has a tendency to affect fire performance (which is not desirable).
2. The elevated burner system was also engulfed in the fire, and thus the elevated burners were also kept cool with a water jacket with flowing water.
3. An emergency dump line was connected to the bottom of the tank at the west or east end. This dump line was connected to a pipe with a remote control valve (or a PRV) so that the pressure in the tank could be relieved if necessary. The dump line emptied into the fire pan for the water tests and into a container for the NaOH tests. This dump line above the water line was insulated.
4. A wind barrier wall (16 m long by 2.5 m high) was located 6 m from the tank centerline to the North.
5. All instrument lines and pipes were taken to the North though the fire pan water, under the wind barrier to the necessary connectors for data acquisition.
6. Thermal insulation was used to protect all of the tank penetrations, the support legs, the dump pipe and the pressure transducer lines.

Figure 1. Exemplar test setup (Test 4)

The fire test was controlled from a remote and safe bunker. Table 1 outlines the tests that were conducted as part of this test effort.

Table 1. Main Tests

Test #	Tank	Fill Level	Commodity
0	Bare (non-jacketed)	98%	Water
1	Insulated and Jacketed	98%	Water
1b	Jacketed (no insulation)	98 %	Water
2	Insulated and Jacketed	50%	Water
3	Insulated and Jacketed	98%	NaOH/KOH 50% solution
4	Insulated and Jacketed	50%	NaOH/KOH 50% solution
5	Insulated and Jacketed	98%	Water

OVERVIEW OF TEST RESULTS

Seven total containment tests were conducted as part of this research effort during the period of September to November 2014. Tables 2 and 3 present a brief summary of the key results from these tests. The fire tests lasted between 2 (Test 0) and 40 minutes (Test 5). The tanks employed in Test 0 (Figure 2) and Test 1b (Figure 3) ruptured and the tank in Test 1 suffered a minor fitting failure. Test 1b was done with a previously fire heated and damaged tank. Test 1 suffered a minor fitting failure. In all the other tests, the fire was shut down before tank rupture took place.

Figure 2. Test 0 - View of deformed and failed B-End

Figure 3. Test 1b - View of vessel rupture

As seen in Table 2, the fire heat flux was very consistent from test to test. Test 0 was very brief and the fire did not reach steady state. The heat flux calculated for that test may not be accurate because of the short fire duration. The heat flux to the jacket is the actual average heat flux absorbed by the tank and lading. As can be seen the improved insulation systems reduced the heat flux by a factor of around four. The original insulation system reduced the heat flux by a factor of around three. The jacket only reduced the flux by a factor of two.

The 1/3rd scale fire tests provided pressurization and temperature rise data of the tanks under fire conditions. This chapter describes how the raw test data was extrapolated and scaled to answer the question of whether a full-scale tank carrying NaOH could survive a 100-minute, fully engulfing, pool fire. The specific steps were to:

1. Extrapolate the 1/3rd scale tests that did not result in failure, to predict the expected failure times, and
2. Scale the tested and/or predicted 1/3rd scale failure times to full-scale failure expectations.

Table 2. Results Summary - A

Test Name	**Estimated Average Heat Flux[1] kW/m^2**	**Estimated Average Heat Flux to Jacket[2] kW/m^2**	**Test Duration (sec)**
Test 0	114 (approx.)	NA	130
Test 1	106	33.6	1540
Test 1b	98.3	55.2	362
Test 2	103	21.8	1250
Test 3	96.4	22.7	1800
Test 4	103	22.2	1210
Test 5	99.1	29.7	2433

Table 3. Results Summary - B

Test Name	**Max Pressure Barg**	**Max Wall Temp Measured °C**	**Outcome**
Test 0	7.3	759	Rupture
Test 1	10.4	580	Minor failure and leak, near rupture
Test 1b	11	700	Rupture
Test 2	5.2	780	Test terminated at 5 Bar
Test 3	8.2	436 (wall wetting)	Test terminated with PRV activation at 8 Bar
Test 4	7.4	560	Test terminated at 7.4 Bar
Test 5	21	623	Test terminated at 21 Bar, near rupture

EXTRAPOLATION

Several of the tests were stopped before failure. This was done to reduce damage to the test facility and also to ensure a hazardous materials spill/explosion does not take place. In three of the five water tests the vessel did fail (i.e. Test 0, Test 1 and 1b – all 98% full). Tests 2, 3, 4 and 5 were stopped before failure. The two NaOH tests (3 and 4) were terminated well before failure (at or before 8 Barg) because the Test Facility had zero tolerance for a hazardous material spill. Thus, it was necessary to extrapolate the measured pressure and peak wall temperatures to estimate the time to failure.

We need to extrapolate both wall temperature and tank pressure to estimate time to failure. Pressure was extrapolated linearly from the point where the test was terminated. Peak wall temperature had to be extrapolated up to a time of around 2000 seconds or around 600°C whichever came first. This extrapolation was done manually to fit the shape of the curve.

[1] To a cool surface
[2] This is the average heat flux absorbed by the tank and lading.

Figures 4, 5 & 6 show the extrapolated plots for temperature, pressure, and the temperature pressure combination. Several independent extrapolations were attempted and failure times were calculated.

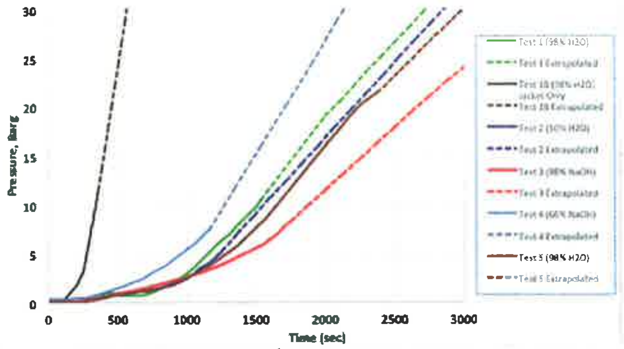

Figure 4. Extrapolated – 1/3rd scale – P Vs. Time

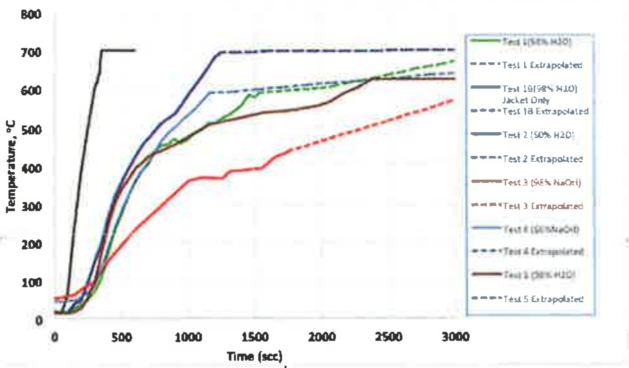

Figure 5. Extrapolated – 1/3rd scale – T Vs. Time

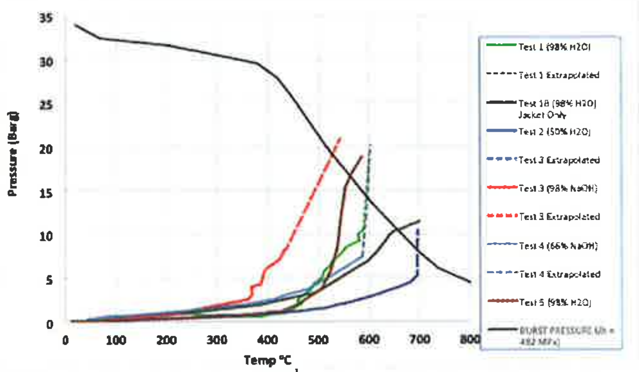

Figure 6. Extrapolated – 1/3rd scale – P Vs. T and Failure Line

Except for the non-insulated cases, it was seen that the NaOH 66% fill case pressurized the fastest and the 98% NaOH pressurized the slowest. Note that all the insulated and jacketed tanks are extrapolated to reach 20 Barg within 31-39 minutes. At that pressure, we expect tank failure with a wall temperature of around 550°C. All of the tests but one (NaOH 98% with wall wetting) achieved this temperature in less than 28 minutes.

SCALING

The test tank diameter was approximately 1/3rd that of the full-scale tank; to have the same burst pressure, the model tank also had a wall thickness about 1/3rd that of the full-scale system. The model tank volume was 1/27th that of the full-scale system while the tank surface area was 1/9th that of the full-scale system. The time for failure of the full-scale tank will be longer than that of the 1/3rd scale model, depending on the:

1. Tank material properties
2. Thermal protection system
3. Fire heat flux
4. Wall temperatures (and material degradation)
5. Pressurization (and stress)

The tank surface area (the area where heat enters the tank) to volume ratio (the part that is heated up by the fire) varies approximately with 1/D for a long cylinder. This means the bigger the vessel (the larger the D) the more slowly it will heat up. That suggests the 1/3rd scale tank should heat up approximately three times faster than the full-scale tank.

The tank wall material properties are nearly the same (density, specific heat, thermal conductivity, ultimate strength, yield strength, etc.).

The fire heat flux was similar to that expected in a full-scale engulfing liquid hydrocarbon pool fire (around 100 kW/m^2 to a cool surface). The heat flux to the 1/3rd scale model tank was slightly higher due to the jacket diameter ratio issue noted earlier. We estimated that the fire heat flux will be about 6% higher due to this.

The wall temperatures will rise more slowly in the full-scale tank because of the thicker wall. We are assuming surface emissivities and the convective heat transfer coefficients are nearly the same for the small and large-scale systems. The time to heat the wall will increase by a factor of the scale ($s = 3$).

As an approximation, the free convective boundary layer thickness on a vertical wall was used to scale the pressure results. In this case, the boundary layer thickness is proportional to the wall height (i.e. tank D) to an exponent of 0.6 (i.e. h^0.6) (Eckert and Jackson, [6]). This factor is 1.93 when the scale factor, $s = 3$. Therefore, we expect the full-scale tank to pressurize slower than the 1/3rd scale model by a factor of around 1.93.

The following method was used to scale failure times from 1/3rd scale to full-scale.

- The failure plot of burst pressure vs. wall temperature was plotted for each test using high temperature material tensile data for SA 455 steel. This material properties data proved to be quite accurate for the tests where failure did occur.

- Where tests were terminated early it was necessary to extrapolate the plots to failure.
- The scaling to full-scale required the adjustment of the time for reaching the specified wall temperature and tank pressure. A factor of s = 3 was used for the temperature time correction and a factor of s = 3^0.6 = 1.93 for the pressure time correction.

Note that the time is now different for the tank pressure and T. This means they can no longer be plotted together on the original failure plot of burst pressure vs. wall temperature. We must plot the P vs. T that occur at the same time. Note also that we have not accounted for continued insulation degradation for the much larger full-scale times. We have assumed the P and T are the same for small and full-scale tank. With continued insulation degradation we would expect higher P and T at later times.

Figures 7, 8, and 9 show the scaled plots for peak wall temperature vs. time; pressure vs. time; burst and tank pressure vs. peak wall temperature, for the full-scale tank. The failure plot curves for full-scale have also shifted due to the time change. From the figures, we see a cluster of failures for the full-scale tank around P = 17.5 Barg with wall temperatures around 540° C. This takes place around 3800 seconds or 65 minutes.

It must be noted that the scaling approach does not include the effects of liquid boiling, which could add another layer of complexity to the scaling effort, and potentially make the survival estimates non-conservative.

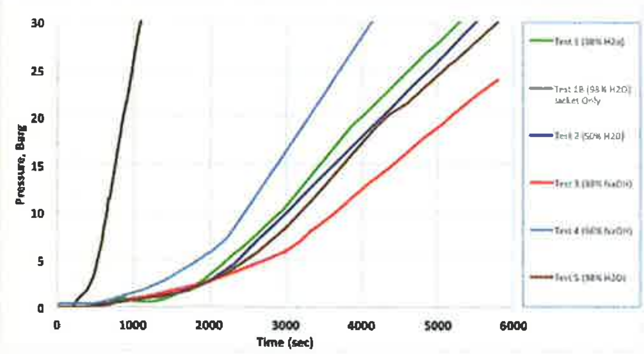

Figure 7. Extrapolated – Full scale – P Vs. Time

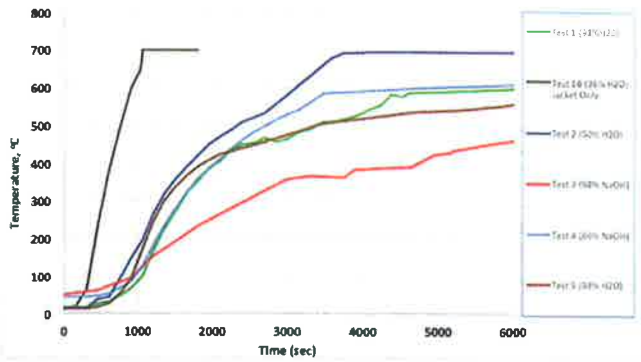

Figure 8. Extrapolated – Full scale – T Vs. Time

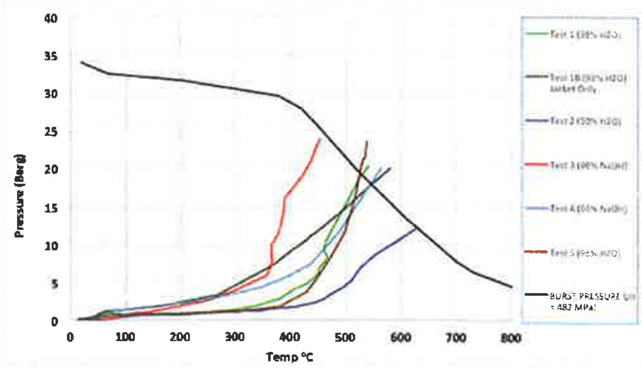

Figure 9. Extrapolated – Full scale – P Vs. T and Failure Line

Table 4 gives a summary of all the extrapolated test results and the calculated full-scale results based on the scaling method proposed here. The uncertainty in the results from the time scaling and time extrapolation is around ±10% on the failure times.

Note that the NaOH tests are considered to have better insulation conditions than what are nominally seen. The following can be seen from the results in the table:

1. Full-scale tank fails at a higher pressure and lower wall temperature than 1/3rd scale. This is because the wall temperature rises more slowly than the pressure at larger scale.
2. None of the estimated failure times is larger than 100 minutes for the full-scale system.
3. We are estimating full-scale tank failure with NaOH between 62 and 88 minutes. The longer time was for the 98% full case, where there was wall wetting in the vapor space.

Table 4. Summary of Extrapolated & Scaled Failure Times

Test	Test conditions	$1/3^{rd}$ scale failure P and T	$1/3^{rd}$ scale estimated failure time	Full-scale estimated failure P and T	Full-scale estimated failure time
0	98% water Bare tank	7 Barg 740 °C	120 sec 2 min	13 Barg 620° C	312 s 5 min
1	98% water Insulation and Jacket	15 Barg 630 °C	1750 s 29 min	19 Barg 560 °C	4250 s 71 min
1b	98% water Jacket only	12 Barg 670 °C	370 s 6 min	17 Barg 580 °C	790 s 13 min
2	50% water Insulation and Jacket	8 Barg 700 °C	1500 s 25 min	13 Barg 630 °C	3300 s 55 min
3	98% fill NaOH 50% solution Improved insulation and Jacket	20 Barg 560 °C	2700 s 45 min	26 Barg 460 °C	5300 s 88 min
4	50% fill NaOH 50% solution Improved insulation and Jacket	8 Barg 700 °C	2100 s 35 min	17 Barg 590 °C	3700 s 62 min
5	98% water Insulation and jacket (repeat of test 1)	17 Barg 560° C	2100 sec 35 min	20 Barg 540° C	4300 sec 72 min

SUMMARY

This research set out to answer a primary question: will the tank cars carrying caustics meet the 100-minute fire performance requirement under conditions of total containment? Review of the results from the one-third scale tests, and subsequent scaling to full-scale suggest that the failure time for a full-scale tank car filled with 50% NaOH solution to be in the range of 62-88 minutes. This estimation is strongly influenced by the insulation condition and tank fill (i.e. wall wetting). Clearly, these do not meet the 100-minute survival requirement.

This test effort generated significant amounts of data that can be used to estimate fire performance of tanks and particularly to validate the modeling of tank car performance. It also revealed the need for additional analysis and testing so that we can better understand and model the fire performance.

It is possible that the total containment system could survive 100 minutes in a fully engulfing liquid hydrocarbon pool fire if the insulation/thermal protection system was improved. It is believed a number of state-of-the-art thermal protection systems could protect the total containment system for 100 minutes. However, further analysis and fire testing is needed to confirm this.

The scaling from $1/3^{rd}$ scale to full-scale in these tests was not straightforward and required simplifying assumptions that are not fully validated. Further research is required to understand the complex 2-phase processes that influence the maximum wall temperatures and pressurization in total containment.

ACKNOWLEDGMENTS

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The staff at BAM, especially Mr. Jorg Borch and Dr. Christian Balke, deserve special thanks, as does Mr. Ian Bradley, a PhD student of Dr. Birk, for all their help with setting up the calibration tests.

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RAIL TANK CAR TOTAL CONTAINMENT FIRE TESTING: PLANNING & TEST DEVELOPMENT

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ABSTRACT

Given the frequent incidences of Non-Accident Releases (NARs) of hazardous materials from tank cars, there in an increasing interest in transporting hazardous materials in total containment conditions (i.e., no pressure relief devices). However, the ability of tank cars to meet thermal protection requirements provided in the Code of Federal Regulations under conditions of total containment has not been established. Also, the modeling tool commonly used by industry to evaluate thermal protection, AFFTAC, has not been validated under these conditions. The intent of this effort was to evaluate through a series of third-scale fire tests, the ability of tank cars to meet the thermal protection requirements under total containment conditions, and also, to validate AFFTAC for such conditions.

This paper describes the test design and planning effort associated with this research, including the design and evaluation of a fire test setup to simulate a credible, fully engulfing, pool fire that is consistent and repeatable, and the design and hydro-static testing of a third-scale tank specimen. The fire design includes controls on the spatial distribution and temperature variation of the flame temperature, the heat flux, and the radiative balance, to best reflect large liquid hydrocarbon pool fire conditions that may be experienced during derailment scenarios.

INTRODUCTION

Tank cars carrying hazardous materials are required to survive a 100-minute, fully engulfing pool fire, without catastrophic failure [1]. Fluid pressure buildup under fire conditions combined with loss of strength in the steel due to elevated temperatures can lead to catastrophic failure of tank shells under certain circumstances. In general, the 100-minute requirement is met through the use of thermal protection and pressure relief devices (PRDs). PRDs help to limit the pressure buildup in tank cars, thereby reducing the potential for a tank explosion. The expectation is that PRD use will result in smaller quantities of hazardous material being released while avoiding the potential for a catastrophic failure. In some cases, PRDs alone may not be sufficient to meet the 100 minute test requirement and thermal protection, which is 'high-performance' insulation that is designed to survive fire conditions, is required. Thermal protection, which in most cases is applied between a steel jacket and the tank, will reduce the heat input to the commodity tank and thus lessen both the temperature rise and pressure rise, thereby helping the commodity tank meet the 100-minute test requirement.

PRDs have a history of leaking and releasing product under nominal operating conditions for a range of reasons that are unrelated to an accident, and reducing the incidences of these frequent Non-Accident Releases (NARs) has been a significant industry focus. Reducing NARs associated with PRDs have resulted in an increasing interest in transporting such materials without the use of PRDs, i.e., conditions of total containment. Total containment is common practice in Europe where there is no 100 minute fire survival requirement. While that solution would help avoid NARs, the downside is that the only mechanism to relieve pressure buildup under fire conditions has been lost. The question then becomes whether a tank car carrying caustics can survive a 100-minute fire under total containment conditions without tank shell failure.

Caustics such as NaOH and KOH are currently transported in non-pressure (DOT111) tank cars that are insulated, but not thermally protected. The tank car industry currently uses a computer program called AFFTAC (Analysis of Fire Effects on Tank Cars) to determine the behavior of tank cars subjected to fire conditions [2]. Simulations conducted by the industry using AFFTAC have indicated that tank cars carrying caustic materials could survive a 100-minute fire under total containment conditions.

AFFTAC was originally developed to determine if tank car designs meet the requirements for thermal protection as provided in the Code of Federal Regulations Title 49, Section§179.18, with an inherent assumption that PRDs would be used to relieve pressure. With the above simulations, the application of the program has been expanded to analyze the behavior of tank cars without thermal protection, as well as, tank cars without pressure relief devices. Further, the program was initially developed to consider pure substances. Over time subroutines have been developed to allow for the analysis of solutions. These applications are founded on the principles of chemistry, thermodynamics, and material science. However, use of this program to underpin a tank car design and or the use of an existing design in a new way requires validation by experiment.

The overall goal of this research project was to conduct a series of 1/3 scale fire tests that can address:

- whether a full-scale tank car carrying NaOH or KOH can survive 100 minutes in a full engulfment fire under total containment conditions
- verification of the fidelity and performance of the AFFTAC model

A series of six third-scale fire tests were planned as part of this effort as shown in Table 1 below.

Table 1. Planned Tests

Test #	Tank	Fill Level	Commodity
0	Bare (non-jacketed)	98%	Water
1	Jacketed	98%	Water
2	Jacketed	50%	Water
3	Jacketed	98%	NaOH/KOH 50% solution
4	Jacketed	50%	NaOH/KOH 50% solution
5	Spare tank		

Full-scale fire testing of tank cars can be a very expensive proposition (and one reason why the last full scale tank car test [3] was done more than 40 years ago). Given the number of tests that would be needed to fully characterize the behavior under fire conditions to address the issues raised, a series of full-scale tests would be cost prohibitive. On the other hand, given the number of physical properties that would affect the results of this study, their interactions, and non-linear behavior, scaling the results from a small scale test (1/10 scale or less) would require the use of many assumptions of material behavior that cannot be easily substantiated. A medium scale (1/3 scale) test was therefore chosen as a reasonable compromise between cost and scalability of results. The methodology for the tests follows the outline described by Birk in Ref. [4].

Prior to the main tests however, there was a need to design and build the third-scale test tanks and to develop and confirm the performance of a test fire that effectively simulated the pool fire conditions outlined in 49CFR179.18. This paper describes the work that was conducted to design/build the test tanks and to develop the test fire system.

OBJECTIVES

The key objectives of this research phase were:

i) To design, validate and construct third-scale tanks that represent full scale tank cars
ii) To develop a fire system that meets the FRA requirements (i.e. simulate a credible large liquid hydrocarbon pool fire)
iii) To characterize the fire conditions under a limited range of wind conditions
iv) To prove by repeated calibration (three) tests that the fire system can consistently provide the fire characteristics required

SPECIMEN TANK DESIGN AND TESTING

Third-scale tanks and jackets were designed per the requirements outlined below and a total of seven tanks and five jackets were constructed. One tank was tested hydrostatically to failure to confirm that the strength requirements were met.

The design requirements for the specimen tanks were as follows: *The specimen tanks needed to be a third-scale in both diameter and length compared to DOT 111A100W1 tank cars used to transport NaOH and KOH solutions, with the thickness being scaled to meet the same burst pressure, and the material being the same as or similar to actual tank car steels. Additionally, a "full-scale" jacket (11 gauge steel) and insulation (4" of fiberglass) were required.*

The fiberglass insulation is not expected to last very long in a pool fire; it is primarily used to keep the transported commodity at an elevated temperature to assist with loading and unloading, and is not intended as thermal protection. Therefore, using a full scale (4") insulation was expected to have a minimal effect on the survivability of the tank and further using a full scale insulation would allow us to directly quantify any 'thermal protection' benefits that it might offer, rather than trying to scale another parameter that would affect only the first few minutes of performance in a fire. The degradation of the insulation system should be similar to that of the full-scale system.

As required, the specimen tank was designed to equate to third-scale of DOT111A100W1 tank car. Usual materials for tank car construction are TC-128 Gr. B or ASTM A-516 Gr 70. As neither material was available in the smaller thicknesses needed for appropriate stress/pressure scaling (1/3 scale model requires wall thickness from 0.10" to 0.13" depending on material strength), an alternate pressure vessel material, ASTM A414 Grade G was selected. This material has similar properties to standard tank car steels, including Yield, Tensile and chemical properties as shown in Tables 2 and 3 below.

Table 2. Material Strength Comparisons

Steel Type	Spec No.	Type/ Grade	Min Tensile (ksi)	Min Yield (ksi)
Carbon Steel	A-414	G	75	45
Carbon Steel	A 516	70	70	38
Carbon Steel	TC-128	B	81	50

Table 3. Chemical Property Comparisons

Element	Composition %			
	TC128 Gr B	SA-516-70	SA-414-G Spec.	SA-414-G Material Cert.
Carbon (max %)	0.26	0.25	0.31	0.24
Manganese (%)	1.00-1.70	0.79-1.26	1.35	1.11
Phosphorus (max %)	0.025	0.025	0.035	0.015
Sulfur (max %)	0.015	0.015	0.035	0.005
Silicon (%)	0.13-0.45	0.15-0.45	----	0.25
Vanadium (max %)	0.084	Per ASTM A20	0.04	<0.01
Copper (max %)	0.35	0.35	0.02	0.01
Nickel (max %)	No Limit	Per ASTM A20	0.43	<0.01
Chromium (max %)	No Limit	Per ASTM A20	0.34	0.03
Molybdenum (max %)	No Limit	Per ASTM A20	0.13	<0.01
Aluminum (%)	0.015-0.060	0.015-0.060		0.03

The specimen tank design is based on the DOT requirements for tank cars maintaining burst pressure of at least 500 psig based on the following equation [5]:

t=Pd/2SE

where:

t= shell thickness required
P= Burst pressure of the tank
d= outside diameter of tank
S= Minimum Tensile Strength
E= Weld joint efficiency

The thickness and diameter were further adjusted as per the actual strength values for the material used based on the mill certifications. The final design dimensions were:

Outside diameter =36"
Fabricated nominal shell design thickness: 0.121"
Fabricated head nominal thickness: 0.176"
Heads: 2:1 ellipsoidal
Shell Length: 136.2"
Jacket: 11 gauge ASTM A1011
Insulation: 4" fiberglass

Several ports were added to the tank to allow loading and unloading (2 top, 2 bottom), as well as to serve as feed-throughs for instrumentation (for thermocouples and pressure transducers), with the appropriate reinforcement. Figure 1 presents a photograph of one of the specimen test tanks.

Figure 1. Specimen test tanks (shown upside-down)

A hydrostatic test of one of the specimen tanks was conducted primarily as a quality control exercise. The objective of the test was to confirm that the as-built tank would meet the design requirements from a strength perspective. Figure 2 shows that the tank ruptured at about 575 psig; the theoretical burst pressure based on actual material properties and joint efficiency of 0.9 is 562 psi. The tank's behavior and rupture pressure demonstrated that the tank design, construction and material were acceptable for the fire testing. As intended, the rupture was a tensile failure of the parent material and did not occur at any of the weld seams or other discontinuities, such as the saddles or ports. Figures 3 & 4 presents photographs of the ruptured hydrostatic tested tank.

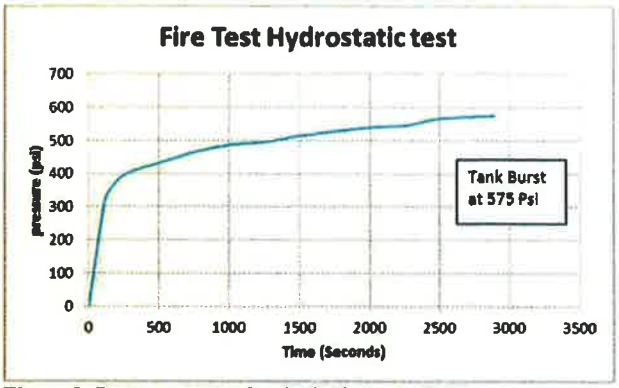

Figure 2. Pressure curve for the hydrostatic test

Figure 3. Burst hydrostatic tested tank – Overview

Figure 4. Burst hydrostatic tested tank – Tensile tear

FIRE TEST FACILITY

A fire test facility in Horstwalde (near Berlin), Germany was selected for the test effort based on the test equipment and facilities available. The test facility is owned and operated by BAM, the German Federal Institute for Material Research and Testing. BAM's strengths include an international reputation for fire testing of pressure vessels (including a full scale rail tank car in 1999), experienced fire test team, a ground level, burner based, fire system with the ability to add 'at-height' ring burners, remote data acquisition and control, and a safe in-ground bunker with viewing ports for test personnel.

SIMULATED POOL FIRE REQUIREMENTS

The fire used for the test needed to be a credible simulation of a large liquid hydrocarbon pool fire (relative to full scale fire test) with the following requirements [6]:

- Full engulfment (top, sides, bottom)
- Effective black body (bb) flame temperature between 816 – 927 degrees Centigrade (°C)
- Heat flux of around 100 kW/m^2
- Heat should be transmitted predominantly by radiation

The temperature range outlined above is as defined in the CFR [6], and the other requirements are consistent with behavior expected from large, liquid hydrocarbon pool fires. The above set represents a fairly tight set of requirements for a test fire, and one that is more highly constrained than most fire standards. For example, "ASTM E1529 14a Standard Test Methods for Determining Effects of Large Hydrocarbon Pool Fires on Structural Members and Assemblies"[7], specifies the temperatures and the temperature rise, but does not prescribe a heat flux or a radiation requirement. Underwriters Laboratories has a similar standard, UL 1709 [8], but with a faster rise time. The temperature rise requirements outlined in both ASTM E1529 and UL 1709 are expected to result in high heat flux levels (158 kW/m^2 and 200 kW/m^2 respectively), but this is not part of the test measurement or test requirement. It is also worth noting that both the above standards are usually implemented through a controlled furnace setup, not an actual pool fire, even though they are intended to simulate a hydrocarbon pool fire. For this project, we used the following targets:

- Heat transfer is mostly by thermal radiation (80%) and the remainder is convection (20%). This will vary over the tank surface and time period of heating.
- Blackbody fire temperatures are achieved on average on the tank top, sides and bottom 90% of the time during a steady burn period.
- Fire start-up and shut-down within 2 minutes.

CALIBRATION FIRE TESTS

After several initial rounds of experimentation, an appropriate burner and fuel flow configuration was selected for the fire tests. Using this configuration, a series of three fire tests were conducted in the period September 1-5, 2014. The objective of the tests was to demonstrate three consecutive, consistent fire burns.

The fire system consisted of 24 elevated rail burners, and 34 ground burners with 1.08 mm nozzles. The propane fuel flow rate was 4000 kg/hr. A test tank (filled with water) that was very similar in dimensions to the specimen tanks was used for these tests. Note that the test tank was vented to atmosphere and not allowed to pressurize. In addition a mechanical mixer was used to keep temperature stratification effects low, allowing the test tank to be used as a calorimeter. The blackbody temperature of the fire was measured using multiple Directional Flame Thermometers (DFTs), located at the top, bottom and sides of the test tank. In addition, the water temperature was measured using thermocouples. Nominal weather metrics, such as the wind speed, were also recorded.

The tests showed very good consistency and the temperatures measured showed that the fire met the requirements for the test fire. The results are summarized in Table 4. Based on the temperature rise of the water, the heat flux and the estimated radiation fraction were calculated. The calculation involved a detailed energy balance on the water tank and on the individual DFT devices. The energy balance considered conduction, convection and radiation heat transfer and heat storage in the affected materials. Variables in the analysis included surface emissivity, fire and surface temperatures, thermal conductivities, specific heat and convective heat transfer coefficients. A detailed presentation of this analysis will be given in future publications on this work.

Table 4. Summary of Fire Test Results

Test	DFT Temperature (°C)			Average Temp. (°C)	Aver. Heat Flux kW/m²	Est. Radiation Fraction
	Top	Bot.	Side			
Test 1	909	877	886	887 SD=112	103	81%
Test 2	914	885	868	888 SD = 78	108	77%
Test 3	937	900	892	907 SD = 55	111	81%
Average	920	887	882	896	107	80%

The target average black body radiating temperature for the fire was between 816 and 927°C (average 871°C). As can be seen all the test averages are in this range. The test repeatability was excellent, as seen in figure 5 below.

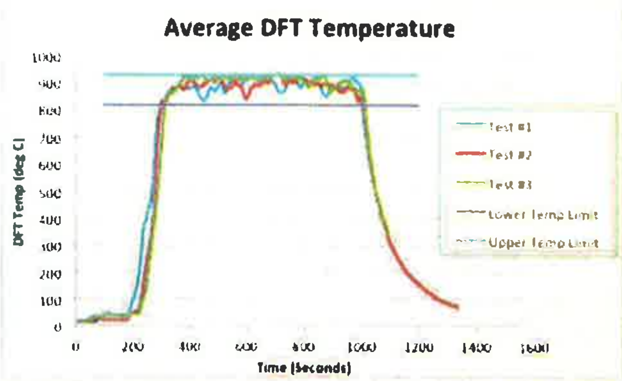

Figure 5. Plot of average DFT temperature vs time - three tests

Additionally, the following can be observed in figure 3.

i) Fast fire start (<2min)
ii) Fast fire shut down(<2min)
iii) Steady fire with minor fluctuations as would be expected in a turbulent diffusion flame of this scale.

The test-set-up calibration tank is shown in figure 6 and a picture of the fire from test #3 is shown in figure 7.

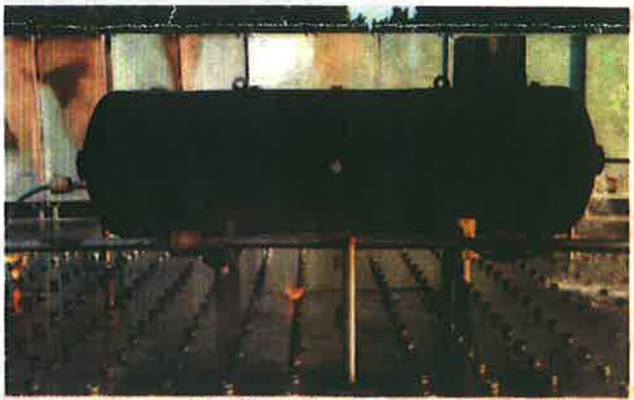

Figure 6. Picture of the fire calibration test tank and setup

Figure 7. Picture of the fire from Test # 3

SUMMARY

This paper describes the test design, setup and planning efforts associated studying the ability of railroad tank cars to meet the thermal protection requirements under total containment conditions, through a series of one-third scale fire tests, including the design and evaluation of a fire test setup to simulate a credible, fully engulfing, pool fire that is consistent and repeatable, and the design and hydro-static testing of a one-third-scale tank specimen.

A one-third scale specimen test tank was designed such that the failure under pressure induced conditions would approximate that of a full scale DOT111A100W1 tank-car. Further, multiple tanks were fabricated and one was destructively pressure tested to ensure that the strength capacity and failure mode were as expected.

A fire test setup that simulates a fully engulfing, non-jetting, credible pool fire was then developed at the BAM test facility in Horstwalde, Germany. Three successful calibration tests were conducted to demonstrate the effectiveness and consistency of the test setup.

In essence, a liquid propane burner based fire has been developed that simulates a liquid hydrocarbon pool fire and meets the FRA test requirement. The setup provides the desired temperature and heat flux conditions and is very consistent from test to test. The radiation fraction is very near the target of 80%. The fire starts were good and consistent and lasted less than 2 minutes. The fire was very steady and the shutdown was also fast (less than 2 minutes).

Based on the observed wind speeds during the tests, the testing using this fire should only be conducted with average wind speeds below an average of 2 m/s with peaks below 4 m/s.

In the next phase of this research project, the main tests outlined earlier will be conducted and the test results will be published through separate future papers.

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