PhD Dissertation

Experimental characterization and modeling of hazards: BLEVE and Boilover

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École des Mines d’Alès
Nothing in life is to be feared,
it is only to be understood.
Now is the time to understand more,
so that we may fear less

Marie Curie
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Acknowledgments

When I came back to the von Karman Institute in 2008, after one year as a customer engineer, I would have never imagined all the extraordinary events I would live in the next four years to come. First of all, I would like to thank the CEA Gramat, for the funding of the BUBLE research project, that allowed me to work freely, with all the financial support needed. I also thank them for the incredible experience I gained when I performed some field tests in their experimental facilities. Among the number of people I have met in the CEA, I would like to thank especially Antoine Osmont to have accepted to be a member of my jury, but also Emmanuel Lapebie, Laurent Munier and Christian Legallic. Finally, the regular visits to Gramat improved also my knowledge of french gastronomy, especially of duck products and Cahors wines!

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Last but not least, I would like to thank all my friends, that were joking about my explosion things, but were always there for a diner or a drink, to celebrate, or to encourage me. Thank for my family, that always believed in me. I know they are very proud of me, and this feeling makes me very happy. And last but not least, thank you to Marc, for being present for me in the good and bad moments, and to help me whatever it takes, love you...
Chapter 1.

Introduction

All around the world, refineries and chemical plants are storing their products close to the production site, in large scale reservoirs. Two main types of reservoir can be found in these storage sites, as observed in Fig. 1.1. Atmospheric reservoirs are used to store fluids in liquid state at atmospheric pressure, mostly hydrocarbon like crude oil, diesel, gasoline,... If the fluid needs higher pressure to be stored in liquid state at ambient temperature, pressurized reservoirs are used. In the last 50 years, guidelines and standards have been published to help companies to reduce the risks of potential accidents, like the American society of mechanical engineers (ASME) or the national fire protection association (NFPA). But accidents still occur.

In his book, Casal [20] states that major accidents “involve the release, instantaneous or over a relatively short period, of significant amount of energy or of one or more hazardous materials ”. Major accidents are associated with the occurrence of fire, explosions or atmospheric dispersion of hazardous materials; an explosion being defined by the CCPS [39] as “a release of energy that causes a blast ”. Major accidents can also be a combination of accidents. In 2011, Abdolhamidzadeh [2], based on an inventory of past accidents, concluded that domino events are mainly caused by explosions (mainly vapor cloud explosions) or fires (mainly pool fires). In this thesis, two types of accidents will be investigated: the BLEVE and the
Chapter 1. Introduction

boilover. The BLEVE is one of the most dangerous explosions involving a pressure liquefied reservoir, and Abdolhamidzadeh stated that BLEVEs are rarely the initiators of domino effects, but can lead to a powerful propagation of the accident chain. The boilover is the most dangerous consequence of a pool fire, and can be considered both as an initial event or as already a chain reaction.

BLEVE is an acronym for Boiling Liquid Expanding Vapor Explosion. This accident occurs when a reservoir is heated until rupture. Once the reservoir ruptures, the vapor expands and the fluid boils rapidly due to the pressure drop. A BLEVE also causes the total loss of containment of the reservoir, as schematized in Fig. 1.2. Three main hazards are generated by a BLEVE: a blast wave, projections of reservoir fragments and a fireball if the liquid is flammable.

In 2007, Abbasi [1] published a list of 120 BLEVE accidents that happened between 1926 and 2004. The BLEVE that involved the maximum quantity of liquefied gas happened in Montreal in 1957 where 5100 tons of butane ended in BLEVEs, due to a fire, killing one person. But the accident with the larger amount of fatalities happened in Mexico City in 1984, where 3000 tons of propane ended in BLEVEs.
due to a fire, killed 650 people and injured other 6400.

BLEVEs still happen nowadays, and the most recent BLEVE accident happened in Japan, following the Tsunami of March 2011. The event did not make any casualties, but destroyed completely a storage zone with 17 pressure liquefied spheres ranging from 1500 $m^3$ to 5500 $m^3$ in the Cosmo refinery. The tsunami created a leak in one of the refinery pipes, that ignited and propagated to the storage site. This fire resulted in several BLEVEs that destroyed completely the storage zone. The before-after study can be observed in Fig. 1.3 left and right respectively. Some of the BLEVEs generated a fireball, like the one of Fig. 1.4 right.

![Figure 1.4: Left: Fire in Cosmo refinery, Right: Fireball generated by a BLEVE in the Cosmo refinery](image)

The most dangerous accident that can happen in an atmospheric reservoir is the Boilover. Boilover happens when a fire starts at the fuel surface of a reservoir containing hydrocarbon. The storage reservoir contains most of the time a water layer due to condensation effects, drilling and transport or from the natural composition of the oil. The fuel burning progressively heats the fuel layer, and therefore the water layer. The water then vaporizes and pushes the remaining fuel, increasing considerably the flame size and radiation. The boilover phenomenology is schematized in Fig. 1.5.

Between 1892 and 1995, only 32 boilover accidents were reported by Gautier [44], which gives a boilover occurrence lower than for the BLEVE. The boilover is also rare compared to pool fires. Indeed, Persson resumed 480 tank fires [104], that happened between 1953 and 2004. And from this 480 tank fires, only 10 ended in boilover.

The Boilover that involved the maximum quantity of fuel happened in San Louis in USA in 1928 where two reservoirs of 100000 tons and 130000 tons of crude oil and a third reservoir of 160000 tons of fuel oil, all experienced a boilover. The reservoirs did not have a protection roof, and the fuel was ignited by a thunder. The whole series of boilover destroyed completely the storage site and killed two
Chapter 1. Introduction

Figure 1.5.: Schematic of boilover phenomenon

people. But the accident with the larger amount of fatalities happened in Tacoa Venezuela in 1982, where 15000 tons of fuel oil ended in a boilover as the protection roof was destroyed by an explosion. This accident killed 150 people and injured other 500.

An example of boilover accident is illustrated in Fig. 1.6. In 1983, a reservoir of 94110 m$^3$ filled with 47000 tons of light crude oil was ignited as the floating roof had been seriously damaged by periods of violent winds [100]. The reservoir started to burn as a pool fire, as observed in Fig. 1.6 left. After approximately 12-13h, a first boilover was observed (Fig. 1.6 right), followed by a second one, two hours later. During the boilover, a fireball of 90 m diameter was observed, rising up to 150 m high. Fortunately, nobody died during this accident, but 6 firemen were injured and the accident caused damages estimated to 11M$.

Figure 1.6.: Boilover at Milford Haven, United Kingdom, 1983

In the frame of these storage tank accidents, a research project has been funded by the CEA Gramat, and jointly performed by the von Karman Institute and the Ecole des Mines d’Alès. The objectives of this project were to better understand the conditions under which the BLEVE and Boilover phenomena appear, and to
model the hazards generated by these accidents: toxic cloud formation, blast wave propagation, fragment projection, fireballs, etc...

This thesis is part of this research project, focusing on the small scale experimental study of the BLEVE and Boilover phenomena, performed to achieve the objectives of the project. This thesis is divided in two parts, one for the BLEVE and one for the boilover. Each part starts with an introduction, giving a more precise description of the research objectives concerning the phenomenon, which is followed by a literature review, and a description of the experimental techniques. The part continues then on the analysis of the experimental results, ending in an application of the small scale experimental findings to a large scale accident. Each part ends with conclusions on the main findings of this study.
Part I.

BLEVE
Nomenclature

Roman Symbols

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<thead>
<tr>
<th>Symbol</th>
<th>Units</th>
<th>Description</th>
</tr>
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<tbody>
<tr>
<td>A</td>
<td>$[m^2]$</td>
<td>Area</td>
</tr>
<tr>
<td>a</td>
<td>$[m/s]$</td>
<td>Sound velocity</td>
</tr>
<tr>
<td>b</td>
<td>$[m]$</td>
<td>Groove depth</td>
</tr>
<tr>
<td>c</td>
<td>$[m]$</td>
<td>Half groove length</td>
</tr>
<tr>
<td>$C_p$</td>
<td>$[kJ/kgK]$</td>
<td>Specific heat capacity</td>
</tr>
<tr>
<td>D</td>
<td>$[m]$</td>
<td>Diameter</td>
</tr>
<tr>
<td>d</td>
<td>$[m]$</td>
<td>Thickness</td>
</tr>
<tr>
<td>E</td>
<td>$[kJ]$</td>
<td>Expansion energy</td>
</tr>
<tr>
<td>F</td>
<td>$[-]$</td>
<td>View factor</td>
</tr>
<tr>
<td>$F_0$</td>
<td>$[-]$</td>
<td>Fourrier number</td>
</tr>
<tr>
<td>f</td>
<td>$[-]$</td>
<td>Flashed fraction</td>
</tr>
<tr>
<td>$f_m$</td>
<td>$[m]$</td>
<td>Surface roughness</td>
</tr>
<tr>
<td>$f_r$</td>
<td>$[-]$</td>
<td>Surface slope</td>
</tr>
<tr>
<td>g</td>
<td>$[m/s^2]$</td>
<td>Gravity</td>
</tr>
<tr>
<td>H</td>
<td>$[m]$</td>
<td>Height</td>
</tr>
<tr>
<td>$H_u$</td>
<td>$[-]$</td>
<td>Humidity</td>
</tr>
<tr>
<td>h</td>
<td>$[W/m^2K]$</td>
<td>Conductance</td>
</tr>
<tr>
<td>$h_e$</td>
<td>$[kJ/kg]$</td>
<td>Enthalpy</td>
</tr>
<tr>
<td>I</td>
<td>$[MPa]$</td>
<td>Micro-hardness</td>
</tr>
<tr>
<td>J</td>
<td>$[-]$</td>
<td>Constraint factor</td>
</tr>
<tr>
<td>L</td>
<td>$[m]$</td>
<td>Length</td>
</tr>
<tr>
<td>M</td>
<td>$[-]$</td>
<td>Mach number</td>
</tr>
<tr>
<td>$M_F$</td>
<td>$[-]$</td>
<td>Folias factor</td>
</tr>
<tr>
<td>m</td>
<td>$[kg]$</td>
<td>Mass</td>
</tr>
<tr>
<td>N</td>
<td>$[-]$</td>
<td>Number of nodes</td>
</tr>
<tr>
<td>n</td>
<td>$[-]$</td>
<td>Strain-hardening exponent</td>
</tr>
<tr>
<td>P</td>
<td>$[MPa,kPa]$</td>
<td>Pressure</td>
</tr>
<tr>
<td>$\dot{Q}$</td>
<td>$[W/m^3]$</td>
<td>Heat flux coming from the resistor</td>
</tr>
<tr>
<td>$\dot{q}_r$</td>
<td>$[kW/m^2]$</td>
<td>Radiation received at distance $r$ from fireball</td>
</tr>
<tr>
<td>R</td>
<td>$[m]$</td>
<td>Radius</td>
</tr>
<tr>
<td>$R_{TNT}$</td>
<td>$[m/kg^{1/3}]$</td>
<td>TNT scaled distance</td>
</tr>
<tr>
<td>$R_{Sach}$</td>
<td>$[-]$</td>
<td>Sach’s scaled distance</td>
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</table>
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<table>
<thead>
<tr>
<th>Symbols</th>
<th>Units</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>( r )</td>
<td>[m]</td>
<td>Distance from source</td>
</tr>
<tr>
<td>( S_u )</td>
<td>[m/s]</td>
<td>Laminar burning velocity</td>
</tr>
<tr>
<td>( S_f )</td>
<td>[m/s]</td>
<td>Flame speed</td>
</tr>
<tr>
<td>( S_g )</td>
<td>[m/s]</td>
<td>Velocity of expanding gases</td>
</tr>
<tr>
<td>( S_E )</td>
<td>[kJ/kg]</td>
<td>Superheating energy</td>
</tr>
<tr>
<td>( SEP )</td>
<td>[kW/m²]</td>
<td>Surface emissive power</td>
</tr>
<tr>
<td>( s )</td>
<td>[kJ/kgK]</td>
<td>Entropy</td>
</tr>
<tr>
<td>( s_2 )</td>
<td>[m]</td>
<td>Factor in the definition of the rupture pressure for a cylindrical reservoir with a longitudinal surface crack</td>
</tr>
<tr>
<td>( T )</td>
<td>[K]</td>
<td>Temperature</td>
</tr>
<tr>
<td>( t )</td>
<td>[s]</td>
<td>Time</td>
</tr>
<tr>
<td>( U )</td>
<td>[kJ]</td>
<td>Internal energy</td>
</tr>
<tr>
<td>( u )</td>
<td>[kJ/kg]</td>
<td>Internal energy per unit mass</td>
</tr>
<tr>
<td>( V )</td>
<td>[m³]</td>
<td>Volume</td>
</tr>
<tr>
<td>( v )</td>
<td>[m/s]</td>
<td>Velocity</td>
</tr>
<tr>
<td>( W )</td>
<td>[W]</td>
<td>Power</td>
</tr>
<tr>
<td>( x )</td>
<td>[−]</td>
<td>Vapor fraction</td>
</tr>
<tr>
<td>( x_L )</td>
<td>[−]</td>
<td>Amount of a given gas in the pathlength</td>
</tr>
<tr>
<td>( Y )</td>
<td>[m]</td>
<td>Distance between 2 surfaces</td>
</tr>
<tr>
<td>( y )</td>
<td>[m]</td>
<td>Vertical coordinate</td>
</tr>
<tr>
<td>( Z )</td>
<td>[−]</td>
<td>Parameter for rarefied gas effect</td>
</tr>
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Greek symbols

<table>
<thead>
<tr>
<th>Symbols</th>
<th>Units</th>
<th>Description</th>
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<tbody>
<tr>
<td>( \alpha )</td>
<td>[m²/s]</td>
<td>Thermal diffusivity</td>
</tr>
<tr>
<td>( \alpha_1 - \alpha_5 )</td>
<td>[−]</td>
<td>Constants in Eq. 5.7</td>
</tr>
<tr>
<td>( \beta )</td>
<td>[−]</td>
<td>Constant</td>
</tr>
<tr>
<td>( \Delta H )</td>
<td>[kJ/kg]</td>
<td>Net available heat for radiation</td>
</tr>
<tr>
<td>( \Delta H_v )</td>
<td>[kJ/kg]</td>
<td>Enthalpy of vaporization</td>
</tr>
<tr>
<td>( \Delta t )</td>
<td>[s]</td>
<td>Time interval</td>
</tr>
<tr>
<td>( \Delta y )</td>
<td>[m]</td>
<td>Spatial resolution</td>
</tr>
<tr>
<td>( \gamma )</td>
<td>[−]</td>
<td>Ratio of specific heats</td>
</tr>
<tr>
<td>( \lambda )</td>
<td>[W/mK]</td>
<td>Thermal conductivity</td>
</tr>
<tr>
<td>( \mu )</td>
<td>[kg/mol]</td>
<td>Molar mass</td>
</tr>
<tr>
<td>( \nu )</td>
<td>[m³/kg]</td>
<td>Specific volume</td>
</tr>
<tr>
<td>( \phi )</td>
<td>[−]</td>
<td>Equivalence ratio</td>
</tr>
<tr>
<td>( \rho )</td>
<td>[kg/m³]</td>
<td>Density</td>
</tr>
<tr>
<td>( \sigma )</td>
<td>[W/m²K⁴]</td>
<td>Stephan-Boltzmann constant</td>
</tr>
<tr>
<td>( \sigma_F )</td>
<td>[MPa]</td>
<td>Flow Strength</td>
</tr>
<tr>
<td>( \sigma_{ult} )</td>
<td>[MPa]</td>
<td>Ultimate Tensile Strength</td>
</tr>
<tr>
<td>Symbol</td>
<td>Description</td>
<td></td>
</tr>
<tr>
<td>--------</td>
<td>-------------</td>
<td></td>
</tr>
<tr>
<td>$\sigma_{ys}$</td>
<td>[MPa] Yield Strength</td>
<td></td>
</tr>
<tr>
<td>$\tau$</td>
<td>[-] Transmissivity</td>
<td></td>
</tr>
<tr>
<td>$\chi$</td>
<td>[-] Coefficient = 0.65 for steel cylindrical vessels</td>
<td></td>
</tr>
</tbody>
</table>

**Subscripts and superscripts**

- *: That contributes to the blast wave generation
- 0: At beginning of reservoir heating
- 1: Before rupture
- 2: After rupture
- a: Air
- atm: Atmospheric
- b: At the boiling point
- B: Related to the fireball
- bt: burnt gases
- blast: Of the blast wave
- c: Contact
- cloud: Related to the fluid ejected cloud
- crit: Of the critical point
- exit: At the reservoir exit
- flame: Of the burning cloud
- fv: vapor of flammable material
- g: Interstitial gas
- growth: When fireball grows
- i: index of layer
- I: Internal
- init: Right after vessel rupture
- ignition: At ignition
- j: index inside a given layer
- l: Of the liquid
- m: Average between internal and external
- model: As found from the modeling approach
- n: time index
- non-notch: without any defect
- notch: with an external axial defect
- O: External
- open: Opened reservoir
- p: Liquefied fluid
- plate: Related to the plate
- $\pi$: Reservoir opened half-perimeter
- resistor: Of the resistor
Chapter 1. Introduction

rupt At reservoir rupture
s Overpressure
sat Corresponding to saturation conditions
test Experimental result
TNT TNT equivalent
tot Total
ubt unburnt gases
v Of the vapor
w Reservoir opened width
wv water vapor

Acronyms

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
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<tbody>
<tr>
<td>AIT</td>
<td>Auto-ignition temperature</td>
</tr>
<tr>
<td>ASSY</td>
<td>Average Shield Stress Yield</td>
</tr>
<tr>
<td>BABELs</td>
<td>BLEVE And Boilover Experimental setup</td>
</tr>
<tr>
<td>BLCBE</td>
<td>Boiling Liquid Compressed Bubble Explosion</td>
</tr>
<tr>
<td>BLEVE</td>
<td>Boiling Liquid Expanding Vapor Explosion</td>
</tr>
<tr>
<td>EES</td>
<td>Engineering Equation Solver</td>
</tr>
<tr>
<td>EOS</td>
<td>Equation of State</td>
</tr>
<tr>
<td>FEM</td>
<td>Finite element method</td>
</tr>
<tr>
<td>HFL</td>
<td>Upper limit of flammability</td>
</tr>
<tr>
<td>LFL</td>
<td>Lower limit of flammability</td>
</tr>
<tr>
<td>LPG</td>
<td>Liquefied petroleum gas</td>
</tr>
<tr>
<td>NIST</td>
<td>National Institute of Standard and Technology</td>
</tr>
<tr>
<td>NMSE</td>
<td>Normalized mean square error</td>
</tr>
<tr>
<td>NTG</td>
<td>Nipon Tansan Gas reservoir</td>
</tr>
<tr>
<td>SLT</td>
<td>Superheat limit temperature</td>
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<td>VCE</td>
<td>Vapor cloud explosion</td>
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Chapter 2.

Introduction and objectives

The objectives of this part of the thesis is to go further in the understanding and the modeling of the apparition conditions and consequences of the BLEVE phenomenon. The BLEVE is an acronym for Boiling Liquid Expanding Vapor Explosion, and is defined by the CCPS as an explosion resulting from the failure of a vessel containing a liquid at a temperature significantly above its boiling point at normal atmospheric pressure [39].

A lot of research studies have already been conducted on BLEVE phenomenon, both experimental and numerical. But the exact conditions for BLEVE apparition are still not clear. In addition, a large quantity of models for BLEVE hazards is available in literature, but very few studies, which compare the different models with experiments to determine the best prediction, have been found in literature. Therefore, the major objective of this study is to contribute to the understanding, the modeling and the validation of BLEVE apparition conditions and consequences.

The best way to understand a phenomenon is to make experiments at different scales. At large scale, the tested reservoirs are similar to the real storage conditions, but large scale experiments are very expensive and dangerous. Moreover, precise and repeatable experiments are more difficult to perform at these scales. By contrast, small scale experiments are very interesting due to their reduced cost and hazards. In addition, the small scales can also be controlled more easily and an exhaustive set of tests can be done for parametric analysis. Therefore, to model the BLEVE apparition conditions and consequences, small scale experiments have been performed, and the type of fluid, the size and the limit load of the reservoir have been varied. The results of these experiments are then compared to large scale experiments taken from the literature and to models available in the literature. The objective of this methodology is to use the experimental results performed at small scale to model the BLEVE apparition conditions and consequences for real industrial reservoirs.
Chapter 2. Introduction and objectives

Figure 2.1.: Methodology for the BLEVE apparition conditions

The modeling of the BLEVE apparition conditions can be divided in two categories. First, the change in the reservoir characteristics from the beginning of the thermal aggression to the rupture are analyzed. This analysis improves the understanding of how the pressure inside the reservoir is evolving with the temperature, and therefore with time. Second, the analysis of the rupture conditions gives a step further in the understanding of the BLEVE apparition conditions, mainly through a modeling of the rupture pressure. The whole modeling approach is resumed in Fig. 2.1. First, the geometrical and mechanical properties of the reservoir are used to model the rupture pressure, which lead to the rupture temperature, found by thermodynamic considerations. And finally, the knowledge of the rupture temperature determines the time to rupture through the thermal model.

The modeling of the consequences induced by the BLEVE rupture can be resumed in Fig. 2.2. A BLEVE has three main hazards, the fragment projection, the blast wave generation and if the liquid is flammable, a BLEVE can lead to a fireball. The main quantities usually modeled for the three types of hazards are listed in Fig. 2.2 and will be more detailed in chapter 3. In this thesis, the analysis focuses on the overpressure modeling, since no fireball or fragment projection have been observed in the present experimental study. The other two hazards are described and compared in the literature survey, without further analysis.

Figure 2.2.: Methodology for the BLEVE consequences
To be able to fulfill the objectives of this study and to finally come to the modeling approach described in Fig. 2.1 and 2.2, the BLEVE study is divided in 5 chapters, excluding this introduction. The chapter 3 gives a literature review on the different parameters used in Fig. 2.1 and 2.2. The chapter 4 describes the small scale experiments performed in this study. In addition, the experimental techniques and instrumentation used in this experiments is also described. The chapter 5 describes the experimental results, and compares them with existing or new models and correlations. Finally, the chapter 6 uses the correlations, that proved in chapter 5 to better model the phenomenon, on a large scale BLEVE experiment. The part ends with conclusions in chapter 7.
Chapter 3.

BLEVE literature survey

Since its denomination in 1957, a lot of studies about BLEVE have been published in literature. This bibliography focuses first on the BLEVE definition and mechanism, and on the physical principles behind the BLEVE. Then, a review of the BLEVE experiments performed at different scales is done, which will be used later to compare with the present experimental results. Finally, a description of the models elaborated for the fragment generation, overpressure and fireball are described and compared. A more extensive literature review has been published by Abbasi [1] in 2007, who gives more details on BLEVE mechanism, consequence assessment and management.

3.1. BLEVE definition and description

The BLEVE is an acronym for Boiling Liquid Expanding Vapor Explosion and this term was invented in 1957 by three researchers from the Factory Mutuals: James Smith, William Marsch and Wilbur Walls, following the rupture of a phenol reservoir, that was superheated. Later in 1979, Walls defined the BLEVE as a “major container failure in two or more pieces, at a moment in time when the contained liquid is at a temperature well above its boiling point at normal atmospheric pressure” [139]. In 1994, Birk and Cunnigham defined the BLEVE as the explosive release of expanding vapor and boiling liquid when a container holding a pressure liquified gas fails catastrophically. But the definition that is widely used about the BLEVE is the one given by the CCPS [39] and the TNO [40], saying that the BLEVE is “an explosion resulting from the failure of a vessel containing a liquid at a temperature significantly above its boiling point at normal atmospheric pressure” [39].

The principal types of aggression that lead to a BLEVE are listed by Abbasi [1] in Table 3.1 based on a compilation of 88 BLEVE events that occurred in the world since 1926. This table shows that BLEVE events happen mostly because of a fire or because of a mechanical damage. These two causes combined are investigated in this thesis.
Chapter 3. BLEVE literature survey

Table 3.1.: Frequency of causes for BLEVE events [1]

<table>
<thead>
<tr>
<th>BLEVE Cause</th>
<th>[%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fire</td>
<td>36</td>
</tr>
<tr>
<td>Mechanical damage</td>
<td>22</td>
</tr>
<tr>
<td>Overfilling</td>
<td>20</td>
</tr>
<tr>
<td>Runaway reactions</td>
<td>12</td>
</tr>
<tr>
<td>Overheating</td>
<td>6</td>
</tr>
<tr>
<td>Vapour space contamination</td>
<td>2</td>
</tr>
<tr>
<td>Mechanical failure</td>
<td>2</td>
</tr>
</tbody>
</table>

The typical BLEVE chronology can be resumed in a few steps, as sketched in Fig. 3.1 and presented in the literature survey of Abbasi [1]. A BLEVE happens when a reservoir containing a liquid (most of the time a pressure liquefied gas) is submitted to an aggression that results in the reservoir rupture. The rupture of the reservoir can be thermal due to wall thinning and/or fissures induced by an increase of the reservoir wall temperature and internal pressure, or mechanical due to an impact. When the reservoir opens due to rupture, the vapor expands and the liquid becomes superheated, triggering the liquid boiling. In addition, a blast wave is generated, and the loss of containment of the reservoir can lead to fragment projections. The reservoir rupture is not always leading to the formation of a fireball. But if the liquid is flammable, the fluid released, mixed with air can be ignited by a heat source and can form a fireball. If the ignition is delayed, it can also lead to a flash fire or a vapor cloud explosion (VCE). If the liquid is not flammable but toxic, the released content disperses in the atmosphere.

Figure 3.1.: BLEVE principle
3.2. BLEVE theory

The principal theory about BLEVE has been proposed by Reid in 1979 [110] and is based on the concept of superheat limit temperature (SLT), defined as the maximum temperature attainable by a liquid after which it must boil. The SLT is pressure dependent, so the superheat limit line represents the locus of all the SLT corresponding to internal pressures ranging from 1 bar to the critical pressure (see Fig. 3.2).

![Figure 3.2.: Propane saturated vapor pressure and superheat limit](image)

The BLEVE chronology described before can be expressed in terms of pressure and temperature evolution, as illustrated in Fig. 3.2. Initially, the reservoir is at atmospheric temperature and saturation pressure (A in Fig. 3.2). Due to a fire, or other thermal load, the pressure and temperature inside the reservoir increase, following the saturation line, the vapor and liquid parts being in equilibrium (B or C in Fig. 3.2). When the vessel ruptures, the pressure drops rapidly and equilibrium is lost, the liquid becomes superheated as its temperature is above the boiling point at atmospheric pressure (D or E in Fig. 3.2).

According to Reid, a BLEVE can happen only if the rupture pressure and temperature are high enough to reach the superheat limit temperature after depressurization (C to D in Fig. 3.2). In these conditions, the liquid experiences spontaneous homogeneous nucleation leading to a violent liquid flashing, which is part of what Reid calls a “superheated liquid-vapor explosion”. If the fluid temperature at rupture is lower (B to E in Fig. 3.2), the sudden depressurization leads to a temperature lower than the superheat limit temperature, which still causes violent
boiling but the flashing is not strong enough to generate an explosion. Therefore, this type of rupture is not considered as a BLEVE by Reid [110].

In 1993, Birk [14] showed that a BLEVE can also happen at a temperature lower than the SLT, and defined two types of BLEVE: a cold and a hot BLEVE. According to Birk, a cold BLEVE happens in a reservoir that fails before reaching the superheat limit temperature. The cold BLEVE happens then mostly to weaker reservoirs, that fail at lower pressures. The cold BLEVE results in a rupture where part of the liquid flashes to vapor and the remaining liquid is dispersed as droplets.
3.2. BLEVE theory

As opposed to a cold BLEVE, a hot BLEVE happens in a reservoir that fails at a temperature higher than the superheat limit temperature. The hot BLEVE is usually encountered by stronger reservoirs, where the initial failure is delayed from the total loss of containment and where almost all liquid flashes to vapor. The overpressure generated is stronger than for the cold BLEVE. Based on middle-scale experiments, Birk proposed a flow diagram, listing the conditions needed for the different types of BLEVE to happen, as showed in Fig. 3.3.

The theory of Reid has also been modified by Venart, introducing a new name for the BLEVE: the BLCBE or the Boiling Liquid Compressed/Collapse Bubble Explosion [136] and schematized in Fig. 3.4 left. This new phenomenon is close to the hot BLEVE defined by Birk [10]. The BLCBE starts with a small size failure in the vapor part of the reservoir. The energy of the expanding vapor is not sufficient to make this failure grow, so the crack propagation stops. But the failure has opened the reservoir to atmosphere, so the internal pressure drops and the vapor exit the reservoir as a superheated vapor jet. The pressure drop also causes a superheating of the liquid and the liquid starts to boil in a two-phase flow, which increases the internal pressure. The repressurization of the reservoir causes a bubble collapse, resulting in the formation of a power amplified liquid shock wave. The interaction of these waves with the reservoir walls causes the total and rapid vessel destruction, with an explosive mechanical distribution of the liquid contents as a finely divided aerosol.

![Diagram of BLEVE/BLCBE process in a vessel](image)

**Figure 3.4.:** Left: Schematic diagram of BLEVE/BLCBE process in a vessel [147]. Right: Pressure at top of vessel following rupture (60% water, 398K) [23]

This theory has been confirmed experimentally by the authors themselves [136]. In addition, the repressurization of a reservoir due to a two-phase flow have also be
Chapter 3. BLEVE literature survey

reported by other studies about liquefied gas, like Reinke [111], Hill [60] or Chen [23]. In 2008, Chen performed an experimental study in a vertical rectangular pressure reservoir containing pressurized water at 398\textdegree K as a fluid, and released to atmosphere through the opening of a rupture disk located on the upper part of the reservoir. Through high-speed visualization, Chen observed that right after the opening of the reservoir, the surface of the liquid boils (A in Fig. 3.4 right and Fig. 3.5). A few milliseconds later, a two-phase layer (observed like a mist in B) develops at the liquid surface and expands in both directions. The occurrence and the development of this two-phase layer is directly linked to the first pressure increase (B in Fig. 3.4 right). A second two-phase flow, more bubbly, has been observed (D in Fig. 3.4 right and Fig. 3.5), that develops after a few 100 milliseconds. This flow impacts on the pressure vessel and is responsible for the second pressure peak (D in Fig. 3.4 right).

![Flow visualization after opening the orifice, Water at 398\textdegree K](image)

3.3. Previous experimental research

Since the BLEVE has been defined, experiments have been performed to try to confirm the different theories about BLEVE, but also to create an experimental database of BLEVE experiments for comparison with correlations and models. Most of the experiments were performed at small scale, as it will be done in this thesis, or at middle scale, but two main experiments were performed at large scale.
3.3. Previous experimental research

3.3.1. Small scale

The first experimental study published in literature has been made by McDevitt [87] in the late 80s. She used 1 liter commercial reservoir filled either with propane or with R12. The reservoirs were heated from below by a propane torch, and the rupture was initiated by the impact of a 0.22 inch bullet. From a series of 47 experiments, the authors concluded that the superheat limit temperature is not a valid criteria for the apparition of a BLEVE, but observed that a minimum energy of the fluid is needed to initiate a BLEVE. Even if the tanks were heated with a propane torch, no fireball was observed. In 1997, Rirksomboon [113] performed a large series of BLEVE experiments using the same type of 1 liter commercial reservoir as McDevitt, but heated by a pool fire, until rupture. He used 10 different fluids and the rupture conditions were changed by drilling a weakness on the reservoir. Some reservoirs were also equipped with a pressure release valve. The author focused on developing a model for the thermal evolution of the reservoir, and on the calculation of the work to tear the tank, comparing them with theoretical works. Rirksomboon contributed to increase significantly the BLEVE experimental database, but did not conclude on major findings from his study. In 2003, Stawczyk [128] performed BLEVE with 5 and 11kg propane reservoirs heated from below by gas burners until rupture. The author is the first to publish the overpressure measured after the rupture of a small scale reservoir. He also measured the internal pressure of the reservoir, that was supercritical before rupture, i.e. above the critical point of the fuel. But the supercritical state is only mentioned and no attempt is done to explain the physics behind this type of rupture. Finally, Davison [24] in 2007 performed experiments with 440g commercial cylinders of LPG heated by a barbecue. He showed that the rupture pattern is different if the reservoir ruptures from a hydrostatic test or due to a BLEVE. No fireball was observed. A summary of the small scale experimental conditions and principal measured values can be found in appendix A section A.1.

3.3.2. Mid-scale

In 2007, Birk published a series of BLEVE experiments that were performed between 2001 and 2004 with 2000 l ASME code propane tanks [13, 12]. Internal pressure, temperatures at different positions inside the reservoir and overpressures at 10 m to 40 m both at side and end directions from the reservoir have been measured. Among the series of tests conducted, nine of them resulted in a BLEVE, with a failure pressure between 1.5 and 2 MPa. During heating of the reservoir, the pressure relief valve connected to the reservoir opened before the vessel rupture, once the internal pressure has exceeded the valve design pressure, set to 1.72 MPa. Therefore, a certain quantity of propane was ejected prior rupture, decreasing the percentage of the fluid remaining inside the reservoir to 15-60%, depending on
Chapter 3. BLEVE literature survey

the test. In the article [12], the author gives the conditions at rupture and the corresponding overpressures at different distances and directions.

Similar previous campaigns have also been performed by Birk, who derived the theory of hot and cold BLEVE explained above [14], but also studied the influence of the stratification of the fluid inside the reservoir [11], that showed to decrease the total energy available in the reservoir prior rupture, which leads to smaller fireball but more ground fire and less blast and projectiles.

3.3.3. Large scale

In 1991, 7 BLEVE experiments were performed by British gas in the frame of a European commission research project and published by Johnson [68, 69]. The reference case was a 5.6m³ reservoir, containing 2 tons of butane, that was heated until a pressure of 15 bar, and then ruptured by detonation of a linear shape explosive. A parametric analysis of the fluid mass (changed to 1 ton) or type (changed to propane), of the reservoir volume (doubled), and of the rupture pressure (halved) was performed. The overpressure was measured at 25, 50, 75, 100 and 150 m from the source, in different directions. An example of overpressure signal measured during these experiments is displayed later in this chapter, in Fig. 3.14. A few years later, the federal institute of material research and testing (BAM) in Germany performed a BLEVE test with a 45m³ reservoir, filled at 22% (5 tons) with propane [82]. It ruptured at 25 bar after being immersed in a hydrocarbon pool fire. The blast wave was recorded at 100, 150 and 200 m from the source.

3.3.4. Rupture conditions

The Figure 3.6 compares the rupture conditions (internal pressure and liquid temperature) of the different BLEVE experiments published in the literature and involving propane as it is the most used fluid in the experiments presented above, and the only fluid that can link experiments from different scales. Most of the large scale and mid-scale experiments have their rupture point located on the saturation line. Both hot and cold BLEVE were experienced at these scales: Johnson being a cold BLEVE [68], the BAM a hot BLEVE [82], and Birk experienced both types of BLEVE [12].

At small scale, Rirksomboon [113] and McDevitt [87] observed mostly hot BLEVEs. But a few experiments ruptured with a pressure and/or a temperature above the critical point. One test of Rirksomboon and one test of McDevitt have their rupture pressure higher than the critical pressure, but the rupture temperature lower. In these experiments, the fluid is at the state of a compressed fluid, but not yet supercritical. In other tests of Rirksomboon and in the test of Stawczyk [128], the rupture pressure and temperature are both higher than the critical point, meaning
3.4. BLEVE hazards

Figure 3.6.: Rupture pressure and temperature for the different scales of experiments

that the fluid state prior the rupture is supercritical. The supercritical fluid state at rupture mostly lies at the left of the pseudocritical line which is a line where each temperature corresponds to the maximum value of the specific heat for a given pressure. This line has a similar physical meaning as the saturation line. But the change of thermophysical properties is continuous when crossing this line, unlike the saturation line that experiences a discontinuous change. Therefore, being at the left of the pseudocritical line means that the fluid state is supercritical, with a liquid-like density. Only one test of Rirksomboon is on the right side of the pseudocritical line, meaning that the fluid state is supercritical, with a vapor-like density.

3.4. BLEVE hazards

BLEVE phenomenon has three major consequences: the formation and evolution of a fireball, the generation and propagation of a blast wave and the fragment projection. In this chapter, the modeling approaches for each hazard are resumed and compared.
Chapter 3. BLEVE literature survey

3.4.1. Reservoir rupture and fragments generation

3.4.1.1. Rupture pressure

No study has been found in literature about the modeling of the rupture pressure in BLEVE phenomenon. But a lot of studies about plastic collapse pressure of reservoirs, with and without defects have been published. An approach to determine the plastic collapse pressure of a reservoir is to calculate its limit load. When a pressure is applied on a structure, the displacement of the structure induced by the pressure load increases linearly with the pressure until reaching the yield value (see Fig. 3.7 left). Then, the displacements become non-linear with the applied pressure as the structure enters in the plastic zone. Coming closer to the limit load, the displacements induced by a small increase of the pressure load are getting larger and larger until becoming unbounded when the limit load is reached, as illustrated in Fig. 3.7 right.

The limit load of cylindrical reservoirs can be calculated with finite-element methods (FEM) or by using one of the numerous correlations that are already available in the literature. In 2011, Aseer Brabin performed a bibliographic study about the different published correlations that predict the burst pressure of a mild steel cylindrical reservoir [4]. These correlations are all expressed in terms of the mechanical properties of the metal (ultimate tensile strength $σ_{ult}$, yield strength $σ_{ys}$ or flow strength $σ_F = 0.5(σ_{ys} + σ_{ult})$) and the reservoir diameter (internal $D_I$, external $D_O$ or average $D_m = 0.5(D_I + D_O)$) and thickness ($d$). Some correlations use also the strain-hardening exponent, defined in Eq. 3.1. Aseer Brabin is also using a constant $χ$ which he sets to 0.65 for steel cylindrical vessels. The Table 3.2 lists all the correlations presented in the literature survey of Aseer Brabin [4], together with the correlation of Staat [127], that assumes a closed pipe, thick walled to derive a theoretical solution. The different correlations of Zhu are
3.4. BLEVE hazards

changing only by the yield criterion: Tresca, von Mises or in between, the average shear stress yield (ASSY) criterion. They have been developed from a theoretical solution, assuming an end-capped line pipe and an isotropic hardening material. The correlation of Subhananda Rao [129] comes from an experimental fit based on experiments on steel rocket motor (one side open and one side closed) with a diameter ranging from \( D = 0.09 \, \text{m} \) to \( D = 2 \, \text{m} \), and material with a yield strength ranging from \( \sigma_{ys} = 1646 - 2128 \, \text{MPa} \). The correlation of Faupel [33] is also empirical. The correlation of Svensson [130] comes from the theory of plasticity and finite strain, assuming a cylindrical vessel. Finally, the correlations of Xue and Aseer Brabin both come from a FEM analysis. Xue [144] performed a static, non-linear analysis. These correlations give a lower bound \( (P_{\text{min}}) \) by using the yield strength and a higher bound \( (P_{\text{max}}) \) by using the ultimate tensile strength. And Aseer Brabin [4] assumed thick and thin-walled cylindrical vessels, with ductile steel as material.

\[
n = 0.224 \left( \frac{\sigma_{ult}}{\sigma_{ys}} \right)^{0.604}
\]

(3.1)

<table>
<thead>
<tr>
<th>Author</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Zhu &amp; Leis (Tresca) [152]</td>
<td>( P_{\text{rupt}} = \left( \frac{1}{2} \right)^{n+1} \sigma_{ult} \frac{4d}{D_n} )</td>
</tr>
<tr>
<td>Zhu &amp; Leis (von Mises) [152]</td>
<td>( P_{\text{rupt}} = \left( \frac{1}{\sqrt{3}} \right)^{n+1} \sigma_{ult} \frac{4d}{D_m} )</td>
</tr>
<tr>
<td>Zhu &amp; Leis (ASSY) [152]</td>
<td>( P_{\text{rupt}} = \left( \frac{1}{2} + \frac{1}{2\sqrt{3}} \right)^{n+1} \sigma_{ult} \frac{4d}{D_m} )</td>
</tr>
<tr>
<td>Subhananda Rao [129]</td>
<td>( P_{\text{rupt}} = \left( \frac{4}{\sqrt{3}} \right)^{1+n} \sigma_{ult} \frac{d}{D_I} )</td>
</tr>
<tr>
<td>Svensson [130]</td>
<td>( P_{\text{rupt}} = (0.22 \frac{n+1}{n+2}) \left( \frac{2}{5} \right)^n \sigma_{ult} \ln \left( \frac{D_o}{D_I} \right) )</td>
</tr>
<tr>
<td>Faupel [33]</td>
<td>( P_{\text{rupt}} = \frac{2}{\sqrt{3}} \sigma_{ys} \left( 2 - \frac{\sigma_{ys}}{\sigma_{ult}} \right) \ln \left( \frac{D_o}{D_I} \right) )</td>
</tr>
<tr>
<td>Xue (minimum) [144]</td>
<td>( P_{\text{rupt}} = \frac{2}{\sqrt{3}} \sigma_{ult} \ln \left( \frac{D_o}{D_I} \right) )</td>
</tr>
<tr>
<td>Xue (maximum) [144]</td>
<td>( P_{\text{rupt}} = \frac{2}{\sqrt{3}} \sigma_{ult} \ln \left( \frac{D_o}{D_I} \right) )</td>
</tr>
<tr>
<td>Aseer Brabin [4]</td>
<td>( P_{\text{rupt}} = \frac{2}{\sqrt{3}} \sigma_{ys} \left( 1 + \chi \left( 1 - \frac{\sigma_{ys}}{\sigma_{ult}} \right) \right) \ln \left( \frac{D_o}{D_I} \right) )</td>
</tr>
<tr>
<td>Staat (Tresca) [127]</td>
<td>( P_{\text{rupt}} = \sigma_F \left( 1 + \frac{2d}{D_I} \right) )</td>
</tr>
<tr>
<td>Staat (von Mises) [127]</td>
<td>( P_{\text{rupt}} = \sigma_F \left( 1 + \frac{2d}{D_I} \right) )</td>
</tr>
</tbody>
</table>

The models presented in Table 3.2 are compared in Fig. 3.8. In that comparison, the reservoir properties are taken as the average of the experiments that will be presented in chapter 5 section 5.2 a reservoir with an outer diameter of 0.295 m, a thickness of 8.5 mm, \( \sigma_{ys} = 580 \, \text{MPa} \) and \( \sigma_{ult} = 725 \, \text{MPa} \). The influence of
Chapter 3. BLEVE literature survey

the reservoir thickness to diameter ratio is illustrated in Fig. 3.8 left (by variation of the thickness from 0 to 10% of the reservoir diameter). All the models are increasing in an exponential way as the reservoir thickness increases, only the exponential factors are changing from one model to another. The influence of the material properties (here assumed that the yield strength is 0.8 times the ultimate tensile strength) is shown in Fig. 3.8 right. The limit load increases with the strength of the material whatever the model, only the slope of the limit load increase change according to the model.

If the reservoir is weakened by the presence of a defect, the rupture pressure is affected by this defect and the prediction, using the previous correlations, overestimates the rupture pressure. Therefore, a more specific modeling is needed. Similarly to the burst pressure without defect, the rupture pressure of a reservoir with a longitudinal defect can be modeled by two approaches: through the use of FEM or with correlations. If the correlation approach is chosen, the effect of the defect on the rupture pressure is modeled by the Folias factor. This Folias factor is non dimensional and accounts for the stress amplification at the ends of axially oriented defects in curved shells caused by the outward deflection of the pipe. In 1973, Kiefner presented a correlation including the Folias factor ($M_F$) and Ewing proposed a correlation based in a slightly different expression of the Folias factor. The correlations of these two authors are taken from the article of Zarrabi [150] and are listed in Table 3.3, with the defect nomenclature illustrated in Fig. 3.9.

In 1991, Carter [19] developed formulations for the burst pressure of a thick pipe with an axial surface defect at the external wall, based on the assumption that

![Graph](image-url)
the reservoir stress field is piece-wise continuous. The pipe is divided in two parts: the part of the reservoir thickness with no defect, and the other part with a penetrating defect. The calculation of the stress field with the penetrating crack is also based on the use of the Folias factor. In 2007, Staat compared the formulation of Carter with a solution derived from FEM calculations. He found that the limit load of a cylinder with a deep axial crack is not only influenced by the defect itself, but also by the opposite unflawed section of the reservoir and therefore included this effect in the correlations. These formulations represent the global limit load of the reservoir. But a local limit can also be calculated, which corresponds to the yielding of the remaining ligament. The correlations of Carter [19] and Staat [126] can be used with both the von Mises and Tresca criteria. Staat states that the Tresca criterion is independent of the pipe end conditions but that von Mises does not apply for open pipe. As we are interested in a cylindrical reservoir, von Mises is more suited. In addition, Staat [126] and Zarrabi [150] have chosen the von Mises criterion. Therefore, the von Mises criterion will also be used in this study.

To compare the models, all the estimated rupture pressures are divided by their value for a reservoir without defect. The influence of the defect length on the rupture pressure is illustrated in Fig. 3.10 left. All the models show a decrease of the rupture pressure as the defect becomes longer, with the same curve shape. The influence of the defect thickness is illustrated in Fig. 3.10 right. The figure shows two categories of models, the ones that show a zero rupture pressure when the defect is completely open (thickness equal to the reservoir thickness), like the two local approaches and the model of Kiefner, and the ones that always give a non-zero rupture pressure even if the reservoir has an open defect, like the global approaches and the model of Ewing. Therefore, these last models are better suited as they are taking into account the residual rupture pressure.
### Chapter 3. BLEVE literature survey

#### Table 3.3.: Burst pressure modeling for a cylindrical reservoir with a longitudinal surface crack

<table>
<thead>
<tr>
<th>Author</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Kiefner [70]</td>
<td>[ P_{\text{rupt}} = \frac{\sigma_y d}{0.5(R_I + R_O)} \left( \frac{(d - b)/d}{1 - (d - b)/d} / M_F \right) ]</td>
</tr>
<tr>
<td></td>
<td>[ M_F = \sqrt{1 + 1.05 \left( \frac{c}{\sqrt{0.5(R_I + R_O)d}} \right)} ]</td>
</tr>
<tr>
<td>Ewing [31]</td>
<td>[ P_{\text{rupt}} = \left( \frac{0.5\sigma_y d}{R_I + R_O} \right) \left( 1 - \frac{b}{\sqrt{1 + 1.61c^2(0.5(R_I + R_O)b)}} \right) ]</td>
</tr>
<tr>
<td></td>
<td>[ M_F = \sqrt{1 + 1.61c^2} ]</td>
</tr>
</tbody>
</table>
| Carter [19]    | **Global estimate**<br>\[ P_{\text{rupt}} = \sigma_F J \left( \frac{b}{(R_O - b)M_F} \right) + \ln \left( \frac{R_O - b}{R_I} \right) \]
|                | \[ M_F = \sqrt{1 + 1.61c^2} \] |
|                | **Local estimate**<br>\[ s_2 = \frac{c b (1 - b/d)}{M_F R_O (R_O - b)} \left( \ln \left( \frac{R_O}{R_I} \right) - \ln \left( \frac{R_O - b}{R_I} \right) \right) - b \] |
| Staat [127]    | **Global estimate**<br>\[ P_{\text{rupt}} = \sigma_F J \left( \frac{1}{M_F} \ln \left( \frac{R_O}{R_O - b} \right) + \ln \left( \frac{R_O - b}{R_I} \right) \right) \]
|                | \[ M_F = \sqrt{1 + 1.25 \frac{c^2}{R_O^6}} \] |
|                | **Local estimate**<br>\[ s_2 = \frac{c \ln \left( \frac{R_O}{R_O - b} \right) (1 - b/d)}{M_F \ln \left( \frac{R_O}{R_I} \right) - \ln \left( \frac{R_O - b}{R_I} \right) - \ln \left( \frac{R_O}{R_O - b} \right)} \] |

#### 3.4.1.2. Rupture pattern

It is very difficult to predict the rupture pattern of a reservoir due to a BLEVE, as it depends on many factors like the cause of BLEVE, the shape of the reservoir, the location of the aggression, etc. So most of the authors used a statistical analysis of the past accidents and experiments to highlight the most probable rupture patterns. Holden and Reeves in 1985 [61] and Gubinelli in 2008 [49, 48] are the main contributors in this field. The thesis of Nguyen in 2009 includes an extensive literature survey about BLEVE fragment [97].
3.4. BLEVE hazards

Figure 3.10.: Comparison of the models to predict the burst pressure of a cylindrical reservoir with an external axial defect. Left: variation with the notch length. Right: variation with the notch thickness divided by the reservoir thickness ($b/d$)

Most of the authors consider a BLEVE rupture as ductile because this type of rupture induces a large plastic deformation, with a few or no crack branching. The rupture pattern also changes with the initial shape of the reservoir. For a horizontal cylindrical reservoir, Gubinelli performed a statistical analysis based on 100 BLEVE events due to an external fire and 19 BLEVE events not due to an external fire, and classified the rupture patterns based on their probability to occur. The most probable fragmentation patterns are illustrated in Fig. 3.11 where the percentage corresponds to the BLEVE induced by a fire, and the percentage in brackets to the BLEVE non induced by a fire.

<table>
<thead>
<tr>
<th>%</th>
<th>fragmentation</th>
<th>%</th>
<th>fragmentation</th>
</tr>
</thead>
<tbody>
<tr>
<td>6% (0%)</td>
<td></td>
<td>27.7% (8.3%)</td>
<td></td>
</tr>
<tr>
<td>55.4%</td>
<td></td>
<td>0% (12.5%)</td>
<td></td>
</tr>
<tr>
<td>10.8%</td>
<td></td>
<td>0% (4.2%)</td>
<td></td>
</tr>
</tbody>
</table>

Figure 3.11.: Probability of fragmentation pattern [49] for fire BLEVE and non-fire BLEVE (in parenthesis)
Chapter 3. BLEVE literature survey

If the reservoir is cylindrical, the rupture was shown by Gubinelli to start most probably at one of the circumferential welds or on the upper part of the reservoir. The cause of the rupture is either a decrease of the mechanical resistance of the metal due to the fire aggression, or a crack already present on the reservoir. If the rupture happens along one weld, the crack propagates along the weld, which represent 55.4% of BLEVE events (66.6% non fire), as observed in Fig. 3.11. If the rupture happens on the upper part of the reservoir, the crack can propagates axially along the whole reservoir length, like 6% of the BLEVE events, or can continue in a circumferential way once the crack has reached the ends of the reservoir, as 27.7% (8.3% non fire) of the BLEVE.

Once the reservoir started to rupture, Holden and Reeves [61] give a probability of 81% to generate at least one fragment. Gubinelli proposed a higher probability of 90% [49]. Holden and Reeves concluded from the past accidents involving cylindrical reservoirs that a BLEVE generates maximum 4 fragments (6 if the BLEVE does not involve fire). Gubinelli is close to this conclusion, showing that 5 fragments maximum are generated. But the two authors do not agree on the maximum probability of fragment number, as observed in Fig. 3.12. Gubinelli rely on more accidents than Holden and Reeves. Among the 100 fire and 19 non fire accidents, only 89 fire and 17 non fire accidents had a described fragmentation pattern, against 29 fire and 8 non fire accident for Holden and Reeves. Therefore, the results Gubinelli are more reliable.

Once the fragment is formed, it is propelled in different directions. Holden and Reeves [61], and Birk [9] published some results about the fragments directions. Both authors showed that the fragments are propelled in two main directions, along
the reservoir axis or perpendicularly, with a majority of the fragments propelled along the reservoir axis, as observed in Fig. 3.13 left. In this figure, a primary projectile is a major piece of the tank while a secondary projectile is generated by the acceleration of nearby objects.

3.4. BLEVE hazards

Finally, the fragment is propelled at a given distance. The modeling of the fragment distance has been investigated in the literature through different approaches. First, the fragment distance can be modeled with a statistical analysis, similar to the one made for the fragment number and rupture pattern. This approach gives a probability for a fragment to travel a given distance, or a correlation of the maximum distance of a fragment in function of its mass. This approach has been followed by Birk [9] and Holden and Reeves [61]. Birk grouped his experimental results with data from past accidents, including the ones from Holden and Reeves, and deduced two correlations, one for the end fragments and one for the side fragments, see Fig. 3.13 right. Hauptmanns [57] and Gubinelli [48] used a slightly different approach. Based on the statistical analysis on the fragment number and shape, the authors assume a statistical distribution for each parameter of the fragment (angle, direction, mass, number, ...). These statistical distributions are then used in the ballistic equations to calculate the fragment trajectory and therefore the travel distance. These results are finally used to compute a probability of a fragment to travel a given distance. This approach is more precise but needs more parameters like the velocity of the fragment, which is difficult to model.
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3.4.2. BLEVE overpressure

When a pressure vessel ruptures, the rapid expansion of the vessel content produces a blast wave. A blast wave is a transient change in gas-dynamic state parameters like pressure, density and particle velocity. For a gas-filled vessel rupture, the blast wave pressure at a fixed reference distance shows an initial peak, followed by a negative phase, and a second peak develops due to an over-expansion followed by a recompression of the released gas [6]. But when a pressure-liquefied gas reservoir ruptures, flash vaporization occurs in addition to vapor expansion. A blast wave signal recorded after a large scale BLEVE experiment [68] can be observed in Fig. 3.14. The physical explanation of the blast generation is still not clear. At first, it has been said that the first peak is linked to the vapor expansion and the second peak to the liquid flash vaporization. But then, Johnson proposed in his analysis that the first peak combines the liquid vaporization and the vapor expansion [68], as observed in Fig. 3.14. And recently, Birk [12] suggested that the liquid flashing is too slow to produce a blast wave and that the overpressure is mostly driven by the vapor energy. If the BLEVE leads to a fireball, a third overpressure peak is generated by the combustion (see Fig. 3.14). But the overpressure due to combustion is a mechanism that will not be considered in the present study.

![Figure 3.14.](image)

In literature, blast wave modeling is dedicated to the prediction of the first peak amplitude, and the overpressure is modeled using generalized methods based on thermodynamic equations. The procedure first consists in the calculation of the expansion energy based on the change in thermodynamic state of the substance stored in the vessel from the initial state, that is the moment before the explosion to the final state where the fluid is at boiling temperature and atmospheric pressure. The expansion energy (E) can be expressed differently depending on the model,
3.4. BLEVE hazards

but all the models are then using it in the calculation of a scaled distance ($\bar{R}$). This scaled distance can be expressed as the Sach’s scaled distance, linked to the distance from the source and the atmospheric pressure, and defined in Eq. 3.2. The peak side-on overpressure or the positive impulse can then be finally evaluated thanks to scaled peak or impulse overpressure curves (see Fig. 3.15 right).

\[
\bar{R}_{\text{Sach}} = r \left( \frac{P_{\text{atm}}}{2E} \right)^{1/3} \quad (3.2)
\]

\[
\bar{R}_{\text{TNT}} = \frac{r^{1/3}}{m_{\text{TNT}}} \quad (3.3)
\]

But the scaled distance can also be expressed in terms of the TNT equivalent mass (see Eq. 3.3), calculated as the ratio of the expansion energy $E$ with the energy of 1kg of TNT (equal to 4680kJ). The overpressure can then be determined from TNT equivalent curves, as shown in Fig. 3.15 left.

![Figure 3.15: Overpressure graphs. Left: TNT equivalent Right: Sach’s Scaled distance](image)

Figure 3.15.: Overpressure graphs. Left: TNT equivalent Right: Sach’s Scaled distance
Chapter 3. BLEVE literature survey

3.4.2.1. Literature review of BLEVE overpressure modeling

From the literature review performed in this study, three models based on the Sach’s scaled distance have been found: the Yellow book of TNO [40], Roberts [114] and Genova [45]. Four other models have been published, using the TNT equivalent mass scaling: Prugh [108], Planas-Cuchi [105], Casal [21], and Birk [12].

The TNO method [40] models separately the expansion energy of the liquid and vapor phases, each energy being defined as the product of the fluid phase mass with the difference in internal energies, assuming an isentropic expansion. The total expansion energy is expressed in Eq. 3.4 multiplied by a factor linked to the geometry of the reservoir (generally a factor two for ground effect), and used in the calculation of the Sach’s scaled distance. In near field i.e. close to the source, the blast wave generated after rupture of a pressurized reservoir differs greatly from the detonation of high explosives, showing smaller overpressures [12]. Therefore, the TNO has developed a special procedure that consists in the calculation of the initial overpressure and the initial distance. The initial distance is modeled as the radius of an hemispherical reservoir with a volume equivalent to the gas filled part of the actual reservoir, assuming that the blast wave is completely symmetrical. The initial overpressure and radius are then compared to a set of curves, and the closer curve is chosen to calculate the overpressure in near field (see appendix A section A.10 for details).

\[ E = m_l (u_{l1} - u_{l2}) + m_v (u_{v1} - u_{v2}) \]  

(3.4)

In 1999, Roberts [114] improved the TNO method, keeping a definition of the expansion energy as the difference in internal energies of the liquid and vapor phases, but he considers that at the final state, a part of the liquid has flashed to vapor. Both phase fractions are calculated from ratios of entropy differences (see appendix A section A.10).

Finally, in 2008, Genova [45] proposed a new model for the expansion energy, assuming that this energy is mainly due to the liquid flash that can be seen as a thermal phenomenon, linked to the excess of heat stored inside the liquid. The expansion energy is then modeled as Eq. 3.5 where \( \beta \) is an empirical coefficient, set to 7% following a comparison with experimental results.

\[ E = \beta m_l C_p (T_{rupt} - T_b) \]  

(3.5)
Concerning the models based on a TNT equivalent mass, Prugh [108] first defined the expansion energy by assuming an isentropic expansion and an ideal gas behavior (see Eq. 3.6). The volume $V^*$ used in Eq. 3.6 is the sum of the vapor volume and the volume occupied by the flashed fraction of liquid (see Eq. 3.7).

$$E_{TNT} = \left(\frac{P_{\text{rupt}}}{\gamma - 1}\right) \left(1 - \frac{P_{\text{atm}}}{P_{\text{rupt}}}\right)^{(\gamma - 1)/\gamma}$$

(3.6)

$$V^* = V_v + V_l \left(\frac{\rho_l}{\rho_v}\right)$$

(3.7)

In 2004, Planas-Cuchi [105] stated that an isentropic expansion as assumed by most of the previous models is an ideal case, thus overestimating the expansion energy. Planas-Cuchi proposed that the real expansion energy ranges between two values: the energy based on an isentropic expansion ($\Delta U$) and the energy based on an adiabatic and irreversible expansion where the only work performed is the one associated with the variation of volume ($\Delta V$). The expansion energy is found by iteration on the final vapor fraction until the value satisfies Eq. 3.8. The expansion energy from both Prugh and Planas-Cuchi is then multiplied by a factor, equal to 40% for ductile failure and 80% for brittle failure; the remaining energy being transformed into kinetic energy that propels the reservoir fragments.

$$-P_{\text{atm}} \Delta V = \Delta U$$

(3.8)

In 2006, Casal [21] introduced the liquid superheating energy (SE), defined as the difference between the enthalpy of the liquid prior rupture, and the enthalpy of the liquid at the saturation temperature corresponding the atmospheric pressure. The final TNT mass that contributes to the blast generation is supposed to be only a fraction: 5% for an irreversible expansion and 14% for an isentropic one.

And finally, in 2007, after analysis of mid-scale experiments, Birk [12] concluded that the liquid part does not contribute to the blast wave generation. His new model is based on the TNO model but using only the vapor part to calculate the expansion energy (see Eq. 3.9).

$$E_{TNT} = m_v (u_v1 - u_v2)$$

(3.9)

In this literature review, all the authors have developed models for an overpressure generated after the rupture of a liquefied gas reservoir at a pressure lower than the critical one, where the fluid follows the saturation line, and where both liquid and vapor phases are present. But in the present experiments, the fluid state prior
Chapter 3. BLEVE literature survey

rupture can be either a compressed fluid or a supercritical fluid. Therefore, all the models using an expansion energy calculated from a combination of the liquid and vapor energies cannot be used with these rupture conditions.

The overpressure following the rupture of a reservoir containing a supercritical fluid at rupture can be modeled by Prugh [108], Casal [21], Genova [45] and Birk [12]. For Prugh, the vapor volume is set equal to the volume of the reservoir (as the fluid occupies the whole reservoir in supercritical conditions). Birk, Casal and Genova models can be used directly, but by using the total fluid mass in the models instead of only using the liquid or vapor mass.

If the fluid temperature at rupture is not supercritical, but the rupture pressure is already supercritical, the fluid is a compressed fluid, without the presence of a vapor phase. To model this kind of rupture, Roberts and Planas-Cuchi cannot be used, as there is no more vapor space prior rupture, and therefore, two-phase calculations are impossible. Birk cannot be used either, because the model relies only on the vapor phase, which is not present with a compressed fluid. The TNO model can partially be used, as the liquid mass equals the total fluid mass, and the vapor mass is null. Similarly to the supercritical modeling, Prugh, Casal and Genova models can be used, considering that the liquid mass equals the total mass. For Prugh, the volume used is the total volume.

The comparison of the different models reviewed here with different scales of experiments, involving a mass of a few tens of grams to a ton, is presented in chapter 5 section 5.3.3

3.4.3. Fireball and vapor cloud explosion

If the liquid is flammable, the fluid ejected at reservoir rupture can be ignited by a heat source and generate a fireball, but it can also ignite after the rupture and lead to a vapor cloud explosion. This section first defines the basic principles of flame ignition and propagation, and then looks more into BLEVE fireballs and their modeling.

3.4.3.1. Vapor cloud explosion

According to the CCPS [39], a vapor cloud explosion (VCE) is “an explosion resulting from the ignition of a cloud of flammable vapor, gas or mist in which flame speeds accelerate to sufficiently high velocities to produce significant overpressure”. This definition gives two main conditions for a VCE to happen: the ignition of a vapor mixture and a sufficient flame acceleration. If the flame acceleration is not sufficient, the cloud ignition will lead to a deflagration.
3.4. BLEVE hazards

The ignition of a flammable vapor mixture is the process by which a rapid, exothermic reaction is initiated, which then propagates and causes the involved material to change, producing temperatures greatly in excess of ambient. There are two main types of ignition. First, the pilot ignition, where flaming is initiated by an external device such as an electrical spark, another flame or a hot source. The second type of ignition is the spontaneous ignition in which flaming develops spontaneously within the mixture. In BLEVE events, the fluid at rupture is usually at a temperature lower than the auto-ignition temperature (AIT) needed for a spontaneous ignition to occur. Therefore, the ignition of a vapor cloud following a BLEVE is a pilot ignition. In addition, the flammable vapor mixture has to be in a range of concentrations to be ignited. This range is bounded by the lower flammability limit (LFL) and the higher flammability limit (HFL). These limits are fuel dependents but also are changing with the ambient pressure and temperature. The Table 3.4 gives the flammability limits of a propane - air and a n-butane - air mixtures, at atmospheric pressure and 298 K.

![Figure 3.16: Temperature and concentration profiles through a plane combustion wave](image)

Once the vapor mixture has ignited, the flame propagates. The ignition of a vapor cloud leads to a premixed flame since the gaseous fuel and the oxidizer (usually the air) are already mixed before ignition. The flame can be decomposed in three distinct zones, as schematized in Fig. 3.16. First, the unburnt gases are heated from ambient to the temperature when they ignite in the pre-heat zone. Then, the combustion takes place in the reaction zone. This zone is usually very thin (less than 1 mm) and experiences high temperature and species gradients. This zone sees the destruction of fuel molecules due to the combustion, but also creation of other molecules. Finally, the post-flame region is characterized by high temperature and radicals recombinations occur.
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The linear rate at which a laminar combustion wave propagates relative to the unburned gas of a flammable mixture is the burning velocity ($S_u$). This velocity is a fundamental property of the mixture and depends primarily on the thermal diffusivity, the chemical reaction rate and the heat of combustion. This value can be found in literature for different vapor mixtures at different equivalence ratio. The equivalence ratio ($\phi$) is defined as the ratio of the fuel-to-oxidizer ratio to the stoichiometric fuel-to-oxidizer ratio. The table 3.4 gives the maximum laminar burning velocities for propane and butane and their corresponding equivalence ratios, taken from [46].

<table>
<thead>
<tr>
<th>Fuel</th>
<th>$S_{u_{\max}}$ [cm/s]</th>
<th>$\phi$</th>
<th>LFL [%]</th>
<th>HFL [%]</th>
<th>AIT [K]</th>
</tr>
</thead>
<tbody>
<tr>
<td>n-Butane</td>
<td>44.9 (1.03)</td>
<td>1.8</td>
<td>8.4</td>
<td>561</td>
<td></td>
</tr>
<tr>
<td>Propane</td>
<td>46.4 (1.06)</td>
<td>2.1</td>
<td>9.5</td>
<td>723</td>
<td></td>
</tr>
</tbody>
</table>

In opposite to the laminar burning velocity, the flame speed ($S_f$) is the rate of flame propagation relative to an observer. The flame speed is the sum of the laminar burning velocity and the gas velocity associated with the expansion and buoyancy of the product gases. The flame speed can also be calculated by multiplying the laminar burning velocity with the ratio of unburned to burned densities, which is usually around 7.5 under adiabatic conditions [75].

$$S_f = S_u + S_g = \left(\frac{\rho_{ub}}{\rho_{bt}}\right) S_u$$

Most of the time, the flame appears in an environment that is not quiescent and the turbulence of environment influences the flame. The turbulent flame speed depends also on the flow character, in addition to the thermal and chemical properties of the mixture (like the laminar burning velocity). Three regimes of turbulent flame speed exist: the wrinkled laminar flame, the flamelet in eddies and the distributed reaction [134]. The wrinkled laminar flame regime happens when the flame thickness is smaller than the kolmogorov length scale, which is the length at which the dissipation of the turbulence occurs. In this regime, the flame propagates at a velocity close to the laminar flame speed, but the effect of turbulence is wrinkling the flame and increases the flame area. The ratio of the turbulent to laminar flame speeds is equal to the ratio of the turbulent to laminar flame areas. The distributed reaction regime occurs when the flame thickness is higher than the integral length scale, which characterizes the large eddies. In this regime, all the turbulent length scales are in the reaction zone, which makes this regime very difficult to model. And finally, the flamelets in eddies regime lies between the previous two. In this regime, the rate of combustion is mainly influenced by the turbulent flow.
3.4. BLEVE hazards

3.4.3.2. Fireballs

A large quantity of models have been published to estimate the fireball geometrical parameters (radius and height) and radiation. These models can be grouped in three different approaches: the static models, the dynamic models and the model of Shield [122]. These three approaches are more detailed in the following paragraphs.

![Figure 3.17.: Fireball following rupture of 5 ton propane reservoir, BAM experiment](image)

The static models

These models do not take into account the temporal evolution of the fireball but give a single value for the fireball parameters over the total fireball lifetime. The main benefit of these models is that they allow a quick and fast estimation of the fireball, most of the time expressed by a function of the mass of gas. But the drawback is that they do not fit well with experimental results and they do not take into account the time evolution. In his literature review, Abbasi [1] listed all the published fireball static models (see Table 3.5). But the two most widely used are the two models coming from the CCPS and indicated by a dagger in Table 3.5.
### Chapter 3. BLEVE literature survey

#### Table 3.5.: Fireball static models review

<table>
<thead>
<tr>
<th>Source</th>
<th>Material</th>
<th>Diameter, $D_{\text{max}}$ (m)</th>
<th>Duration, $t_{\text{g}}$ (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Empirical correlations</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hardee and Lee [116]</td>
<td>Propane</td>
<td>$5.530^{323}$</td>
<td>$1.100^{197}$</td>
</tr>
<tr>
<td>Fay and Lewis [117]</td>
<td>Propane</td>
<td>$6.280^{333}$</td>
<td>$2.530^{167}$</td>
</tr>
<tr>
<td>Hasegawa and Sato [118]</td>
<td>Pentane</td>
<td>$5.280^{277}$</td>
<td>$1.100^{197}$</td>
</tr>
<tr>
<td>Hasegawa and Sato [119]</td>
<td>n-Pentane</td>
<td>$5.250^{204}$</td>
<td>$1.070^{181}$</td>
</tr>
<tr>
<td>Williamson and Mann [120]</td>
<td></td>
<td>$5.880^{233}$</td>
<td>$1.090^{167}$</td>
</tr>
<tr>
<td>Libou and Maund [121]</td>
<td>Butane</td>
<td>$5.720^{233}$</td>
<td>$0.450^{332}$</td>
</tr>
<tr>
<td>Libou and Maund [121]</td>
<td>Rocket fuel</td>
<td>$6.200^{320}$</td>
<td>$0.400^{320}$</td>
</tr>
<tr>
<td>Libou and Maund [121]</td>
<td>Propylene</td>
<td>$3.510^{333}$</td>
<td>$0.320^{333}$</td>
</tr>
<tr>
<td>Libou and Maund [121]</td>
<td>Methane</td>
<td>$3.650^{252}$</td>
<td>$2.570^{167}$</td>
</tr>
<tr>
<td>Moonhouse and Pritchard [122]</td>
<td></td>
<td>Flammable liquid</td>
<td>$1.090^{327}$</td>
</tr>
<tr>
<td>Libou and Maund [121]</td>
<td>Propane</td>
<td>$3.650^{333}$</td>
<td>$0.310^{333}$</td>
</tr>
<tr>
<td>Doktor [123]</td>
<td>Flammable liquid</td>
<td>$5.450^{130}$</td>
<td>$1.340^{167}$</td>
</tr>
<tr>
<td>Marshall [12]</td>
<td>Hydrocarbon</td>
<td>$5.500^{333}$</td>
<td>$0.380^{333}$</td>
</tr>
<tr>
<td>Gayle and Bannister [124] and Bagston and Poblad [125]</td>
<td></td>
<td>Flammable liquid</td>
<td>$6.140^{252}$</td>
</tr>
<tr>
<td>4. Reiner [104] and CCPS [1]</td>
<td>Flammable liquid</td>
<td>$6.480^{325}$</td>
<td>$0.850^{200}$</td>
</tr>
<tr>
<td>Martens and Marx [126]</td>
<td>Flammable liquid</td>
<td>$6.860^{353}$</td>
<td>$0.450^{333}$ ($M &lt; 3 \times 10^5$), $2.600^{147}$ ($M \geq 3 \times 10^5$)</td>
</tr>
</tbody>
</table>

Analytical models

Bader et al. [127], Hardee and Lee [116], LNG [200], $D_{\text{max}}$ mass of fuel in fireball (kg), $t$: time elapsed after BLEVE (s), $\rho$: density of fireball gas (lb/ft$^3$), $W$: mass of propellant (lb), $g$: acceleration due to gravity (m/s$^2$), $\beta$: entrainment coefficient, $\rho_c$: density of air (kg/m$^3$), $\rho_p$: density of product of combustion (kg/m$^3$).

$M$: mass of fuel in fireball (kg), $t$: time elapsed after BLEVE (s), $\rho$: density of fireball gas (lb/ft$^3$), $W$: mass of propellant (lb), $g$: acceleration due to gravity (m/s$^2$), $\beta$: entrainment coefficient, $\rho_c$: density of air (kg/m$^3$), $\rho_p$: density of product of combustion (kg/m$^3$).

Table 3.5.: Fireball static models review
3.4. BLEVE hazards

The radiation from the fireball received by an observer at a given distance from
the fireball is usually modeled through the solid flame model, where the fireball
is assumed to be a body of a simple geometry (cylinder, sphere,...) and where all
the radiations are emitted by the surface of the body. More information on this
modeling approach are given in the boilover part, chapter[9]. The most widely used
model for radiation is the TNO and expressed in Eq. [3.11]. This model calculates
the view factor $F$ by assuming that the fireball is spherical, and centered at a
height $H_{B}$ from the ground, as expressed in Eq. [3.12]. The target is assumed to
be oriented with a normal vector pointing directly at the center of the spherical
fireball, which gives the maximum value of the view factor, compared to target
oriented horizontally or vertically. The transmissivity $\tau_{atm}$ is expressed in Eq. [3.13]
and depends on the partial water vapor pressure $P_{wv}$ and on the distance from
the surface area of the flame to the observer. Finally, the flame surface emissive
power (SEP) is expressed in Eq. [3.14] and depends on the vapor pressure of the
flammable material inside the vessel $P_{fv}$, the net available heat for radiation $\Delta H$,
the initial mass of flammable material $m$ and the maximum diameter $D_{B_{max}}$ and
duration $t_{B}$ of the fireball.

\[
\dot{q}_{r} = SEP F \tau_{a} \quad (3.11)
\]
\[
F = \frac{(D_{B_{max}}/2)^2}{(r^2 + H_{B}^2)} \quad (3.12)
\]
\[
\tau_{atm} = 2.02(P_{wv}(r^2 + H_{B}^2)^{1/2} - D_{B}/2)^{-0.09} \quad (3.13)
\]
\[
SEP = \frac{0.00325P_{fv}^{0.32}m\Delta H}{4\pi(D_{B_{max}}/2)^2t_B} \quad (3.14)
\]
\[
H_{B} = D_{B_{max}} \quad (3.15)
\]

The dynamic models

These models have similar formulations as the static models, but they are modified
to take into account the temporal evolution, based on comparison with experimen-
tal results. The advantages of these models are that they are still quite easy to
calculate and they take into account the temporal evolution of the fireball, but
they don’t rely on a physical explanation. There are two published dynamic mod-
els: Martinsen [84] and Roberts [114], but they are very similar so only the model
of Martinsen will be presented here. The assumption is that the fireball grows
during the first third of its total lifetime with a 1/3 power law, and then stays
constant until it extinguishes.
Chapter 3. BLEVE literature survey

\[ t_B = 0.9m^{0.25} \]  
\[ t_{\text{growth}} = t_B / 3 \]  
\[ D_B = \begin{cases} 8.664m^{1/4}t^{1/3} & \text{if } t < t_{\text{growth}} \\ 5.8m^{1/3} & \text{if } t > t_{\text{growth}} \end{cases} \]

The radiation is also based on the solid flame model, but the expression of the surface emissive power is expressed differently, as observed in Eq. (3.19). The air transmissivity is taken from the model of Wayne [140], which uses the amounts of CO\(_2\) and H\(_2\)O in the path from the fireball to the observer, \(x_{LCO_2}\) and \(x_{LH_2O}\) respectively. They are expressed in Eq. (3.20) to (3.22).

\[ \text{SEP} = \frac{0.0133(0.27P_{\text{we}})^{0.32}m^{1/12}\Delta H}{4\pi(D_{\text{Bmax}}/2)^2t_B} \]  
\[ x_{LCO_2} = \frac{273r}{T_{\text{atm}}} \]  
\[ x_{LH_2O} = \frac{HucP_{\text{we, sat}}}{T_{\text{atm}}} \]  
\[ \tau_a = 1.006 - 0.001171 \log_{10}(x_{LH_2O}) - 0.02368 \log_{10}(x_{LH_2O})^2 \]  
\[ - 0.03188 \log_{10}(x_{LCO_2}) + 0.00164 \log_{10}(x_{LCO_2})^2 \]

The Shield model

Developed in 1993 [122], this model is a lot more complicated to implement and requires more physical parameters for a correct calculation. But the model equations are based on realistic physical assumptions. The whole fireball lifetime is illustrated in Fig. 3.18, but the model of Shield can be divided in three periods:

- **The expansion:** After the reservoir rupture, the vapor expands in the atmosphere and a part of the liquid flashes almost instantaneously, which induces a strong turbulence in the resulting cloud. The cloud then mixes with air, ignites, and the vapor starts to burn. During this period, Shield assumes a linear evolution of the fireball diameter and the surface emissive power.

- **The combustion:** Once the vapor fraction of the cloud has burnt, the fireball continues for burn from the combustion of the fuel droplets, and the fireball rises. The diameter during this period stays constant and the surface emissive power decreases until reaching 88% of its maximum value.

- **The extinction:** When all the fuel available in the fireball has burnt, the fireball progressively extincts. The diameter decreases until 0 but the surface emissive power stays constant.

The model is quite complex, and involves a lot of equations. For more details, please refer to the publication of Shield [122].
3.4. BLEVE hazards

Comparison of the three modeling approaches

First of all, a comparison of the time evolution of the fireball dimensions and emissive power between the three modeling approaches is given in Fig. 3.19.

To compare the performances of the different models, the 4th experiment among the series of large scale BLEVE experiments performed by Johnson (as explained in more details in section 3.3) is used. The experiment involved a $10.796m^3$ reservoir filled with 2 tons of butane and ruptured at 15 bar. The Fig. 3.20 shows the
comparison of the fireball diameter evolution obtained by image processing with the three types of model: the static model of TNO \( [40] \), the dynamic model of Martinsen \( [84] \) and the Shield model \( [122] \). The three models fit quite well the plateau for the fireball diameter. The initial fireball growth is better modeled by Shield, but the plateau value is better estimated by Martinsen. Finally, even if the TNO model is simpler to use compared to the dynamic models or the Shield model, it gives good estimates for the fireball diameter and duration.

![Figure 3.20.](image-url) Comparison of models with Johnson \( [68] \). Left: Fireball diameter, Right: Radiation at 50m from the fireball

The comparison of the radiation from the fireball generated by the Johnson experiment, and measured at 50m, 100m and 200m with the three types of model can be observed in Fig. \( 3.20 \) right and in Fig. \( 3.21 \). Close to the fireball (at 50m, Fig. \( 3.20 \) right), all the models are underestimating the measurements, the model of Martinsen being the closest model. But as the observer is placed further away from the fireball, the models are better estimating the experiments. Notice that even if the diameter and emissive power are initially increasing linearly in the Shield model, the radiation increases in a parabolic manner due to the evolution of the view factor, that is directly proportional to the square of the fireball diameter. In addition, even if the model of Shield is based on real physical assumptions, this model is not fitting better the experiment than the other types of models. Therefore, the modeling approach of Martinsen is recommended.

### 3.5. Conclusion

The objective of this chapter was to resume the different theories and models that have already been published in literature on the BLEVE, both about the
3.5. Conclusion

Figure 3.21.: Comparison of models with Johnson [68]. Radiation at 100m (left) and 200m (right) from the fireball

physical aspects of the phenomenon and about the hazards that this phenomenon generates.

From the literature review of the published BLEVE experiments, the fluid state prior to rupture is not always composed of liquid and vapor phases at saturation conditions; ruptures with compressed or supercritical fluids were observed. But the different theories that explain the physical process of the fluid evolution at rupture focused only on a fluid state at saturation. The authors that experienced compressed or supercritical fluid BLEVEs only mentioned it without further explanation. Therefore, one part of this study will be dedicated to a better understanding of the reservoir rupture with compressed or supercritical fluids.

Concerning the three main hazards of BLEVE, that are the fragments, the blast wave and the fireball (if the liquid is flammable) generations, a lot of modeling approaches have already been published. But publications focusing on the comparison of the different models with experimental or previous accident data are deficient. In this study, the results of the small-scale experiments in addition to experiments from literature will be compared with the different models to conclude on the best model to be used. This approach will be followed especially for the rupture pressure modeling and the overpressure modeling. The fireball and fragment generation will not be further investigated since the performed experiment did not generate any fireball and/or fragment.
Chapter 4.

Experimental setup

The most important cause of BLEVE is fire, as explained in chapter 2. The typical scenario for BLEVE apparition involves a reservoir engulfed in flames, causing an increase in liquid temperature and therefore in internal pressure, until rupture (see Fig. 4.1). This scenario is therefore reproduced at small scale in the experiments performed in this study. In addition, this scenario is combined with the second cause of BLEVE: a mechanical damage. This chapter describes the tested reservoirs, and the different measurement techniques used in this experimental study.

![Propane reservoir engulfed in flames](image)

Figure 4.1.: Propane reservoir engulfed in flames

4.1. BABELs

BLEVE experiments can generate a pressure wave, a fireball and project fragments. A dedicated safe facility called BABELs was built at the von Karman Institute to guarantee secure small scale experiments in a controlled environment. A general view of this facility is displayed in Fig. 4.2. BABELs is an acronym for Bleve A nd Boilover Experiments and consists of a cylindrical chamber of 2 m diameter, and 3 m high, with round-shaped flanges, and made out of steel with a
Chapter 4. Experimental setup

rated pressure of 0.5 MPa. The setup has 3 series of 7 optical accesses of 0.15 m in diameter separated by 90°, and an elliptical door of 0.57 m × 0.77 m. The setup allows air venting through openings in its bottom and upper parts, the last one being ended by an exhaust vent that can be used after each test to remove smoke or gas from the chamber. The setup also includes a ladder, and a circumferential walking area is located at mid-height for better accessibility to the upper optical accesses. All the small scale BLEVE experiments performed in this study have been done inside BABELs.

![Figure 4.2.: Picture and schematic of BABELs facility](image)

4.2. Reservoirs and heating system

The choice of the reservoirs used for small scale BLEVE has been influenced by different factors. First, the reservoir has to be geometrically similar to the large scale reservoirs. The storage of liquid gas at large scale can be designed using two types of shape: the spherical shape and the cylindrical shape. The cylindrical shape is chosen here as it is easily available at smaller scale, and because the majority of the large scale experiments published in the literature has been performed with cylindrical reservoirs. Second, the size of the reservoir has to be small enough compared to the size of BABELs (2m diameter) not to have the effects of a confined explosion. And for last, as BLEVE happens mostly with LPG
4.2. Reservoirs and heating system

type of liquids (usually propane or butane) and as the filling of reservoir is difficult and dangerous, the purchased reservoirs need to be already filled with the desired fluid. Therefore, two sizes of reservoir were tested: 5.67 g butane from Archer / RadioShack and 41 g propane from Nippon Tansan Gas (NTG), with the dimensions listed in Table 4.1 and sketched in Fig. 4.3. The constructor gives a composition of min. 95 % of propane in the total composition of the gas, but the composition of butane is not known. The NTG reservoirs are made of steel JIS G3141(96) SPCC (Japanese nomenclature, similar to AISI 1008 Steel). The material of the Archer reservoirs is not specified. As the reservoir fragmentation during rupture is a phenomenon difficult to predict and therefore to observe, the reservoirs were weakened prior the experiment at a precise location. A notch with the dimensions listed in Table 4.1 was made along the length of the reservoir, and was located on the top of the reservoir. The properties of propane and butane are listed in appendix A, section A.2.

Table 4.1.: Reservoir dimensions

<table>
<thead>
<tr>
<th>Constructor</th>
<th>Fluid</th>
<th>m [g]</th>
<th>Volume D₀ [ml]</th>
<th>d [mm]</th>
<th>2c [mm]</th>
<th>b [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Archer</td>
<td>Butane</td>
<td>5.67</td>
<td>13 20 1</td>
<td>~60 0.2:0.3</td>
<td></td>
<td></td>
</tr>
<tr>
<td>NTG</td>
<td>Propane</td>
<td>41</td>
<td>95 40 2</td>
<td>[10 : 80] 0.8</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Figure 4.3.: Reservoir dimensions nomenclature

The fire was simulated by an electrical resistor to guarantee a better control and reproducibility of the heat applied to the reservoir. The resistor consists of a spiral microheater GA-XP from Micropyretics Heaters International that can be supplied with varying electrical power, changing the heat flux applied to the reservoir by Joule effect. To protect the microheater from the reservoir rupture, the microheater was covered by a metal plate. The plate is then used to keep in position the reservoir, placed inside a cradle for a better heat distribution and stability of the reservoir on the plate, as shown in Fig. 4.4.
Chapter 4. Experimental setup

4.3. Instrumentation

During a BLEVE experiment, the external temperature, the internal pressure and the blast overpressure were recorded (see Fig. 4.4). In addition, visualization techniques were used: high-speed visualization and Edgerton direct shadowgraphy.

4.3.1. Temperature measurement

Temperature was monitored by three 220 µm diameter K type thermocouples. A thermocouple consists of two conductors of different materials joined together, so that an electrical potential difference generated between the points of contact is a measure of the temperature difference between these points. More information about thermocouples can be found in appendix B, section B.1.2. The thermocouples used here are located between the plate and the cradle, at the top, and at the bottom of the reservoir (see Fig. 4.4). The thermocouples were then connected to 3 m extension cables, that reached a NI9213 acquisition module, plugged into a National Instrument Compact-DAQ NI9178 and sampled at 3 Hz.

4.3.2. Overpressure measurement

The pressure wave generated at rupture was recorded by two types of sensors; three piezoelectric sensors from PCB and two piezoresistive sensors from Kistler. The piezoelectric sensors were of type PCB 106B50 and the piezoresistive sensors...
4.3. Instrumentation

of type Kistler 4043A2. Their specifications are listed in Table 4.2. The difference between these two types of sensors and the procedure for their respective calibrations is given in appendix A, section A.3.

Table 4.2.: Pressure sensor specifications

<table>
<thead>
<tr>
<th>Specification</th>
<th>PCB</th>
<th>Kistler</th>
<th>Kulite</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sensor Type</td>
<td>Piezoelectric</td>
<td>Piezoresistive</td>
<td>Piezoresistive</td>
</tr>
<tr>
<td>Pressure measurement</td>
<td>Relative</td>
<td>Absolute</td>
<td>Absolute</td>
</tr>
<tr>
<td>Measuring range [MPa]</td>
<td>0.034</td>
<td>0.2</td>
<td>0.35</td>
</tr>
<tr>
<td>Maximum Pressure [MPa]</td>
<td>0.69</td>
<td>0.5</td>
<td>1.05</td>
</tr>
<tr>
<td>Sensitivity [mV/kPa]</td>
<td>72.5</td>
<td>250</td>
<td>28.6</td>
</tr>
<tr>
<td>Resonance frequency [kHz]</td>
<td>&gt; 40</td>
<td>&gt; 30</td>
<td>300</td>
</tr>
<tr>
<td>Temperature range [K]</td>
<td>[-222; 394]</td>
<td>[-233; 343]</td>
<td>[-218; 526]</td>
</tr>
</tbody>
</table>

The acquisition of the overpressure measured by the PCB and Kistler sensors was done through a NI9323 acquisition module, sampled simultaneously at 250 kHz, and plugged in a National Instrument Compact DAQ NI9178. The acquisition was triggered by one of the PCB, plugged into a NI9205 module and also sampled at 250 kHz. The PCBs and one Kistler are located at 0.5, 0.6 and 0.7 meters from the reservoir and positioned on the grid floor approximately at the same height as the reservoir (see Fig. 4.5). The other Kistler sensor is positioned at 0.28 m from the reservoir, above the reservoir.

4.3.3. Internal pressure measurement

The pressure inside the NTG reservoirs was measured in two experiments, where the reservoir was connected to a pressure transducer. Two types of sensors have been used. The first sensor used was a variable reluctance differential pressure
Chapter 4. Experimental setup

transducer from Validyne, model DP15 with a 21 MPa membrane. More information about the working principle of this technology can be found in appendix A, section A.4. But after the first measurement, the range of the sensor appeared to be too small. Therefore, a low cost pressure transducer from GE, model PMP1400 was used, where the pressure is measured from the physical deformation of strain gages, that are bonded on the diaphragm of the pressure transducer and wired into a Wheatstone bridge, and that produces an electrical resistance change proportional to the pressure. The sensor has a pressure range of 60 MPa, but can reach 120 MPa without being damaged. The sensor operates in the temperature range of \[270 \leq T \leq 370 \text{ K}\].

![Figure 4.6: Internal pressure measurement setup](image)

Each sensor was connected to the reservoir with a tube. The pressure sensor was fixed to the BABELs grid floor, and the connecting tube between the pressure sensor and the reservoir was tightened in a vice. The vice was then fixed to the BABELs grid floor, as drawn in Fig. 4.6. The two sensors were calibrated with a dead weight tester 5020SMPR manufactured by the Desgranges & Huot company. The calibration curves can be found in appendix A, section A.4, in addition to the dead weight tester principle. The sensors were sampled at 100 Hz and recorded by a NI9205 acquisition module, plugged in the National Instrument Compact-DAQ NI9178.

4.3.4. Visualizations

As the BLEVE rupture is a very short time phenomenon, high-speed imaging was necessary. In this study, two types of imaging have been performed: shadowgraphy and simple visualization with halogen lamps. To ensure that the camera records the very short duration of the BLEVE rupture, the high-speed camera was triggered on the blast wave pressure measurement.

For observation of the pressure wave generated by the rupture, a technique called the “Edgerton’s direct shadowgraphy” has been used. The purpose of this technique, as showed in Fig. 4.7, is to illuminate an object S with a point source L.
4.3. Instrumentation

Figure 4.7.: Left: Principle of Edgerton direct shadowgraph technique [121], Right: Implementation of Edgerton direct shadowgraphy

and to look simultaneously at the object and its shadow on a retroreflective screen [121]. The main advantage of this technique is that it allows to observe a larger field of view than the one available for the camera. The major problem of this technique is the slight offset between the light source and the camera positions that causes double imaging. To minimize this problem, different optical configurations have been tested [79] and the best arrangement was found by placing the lamp perpendicular to the interesting area. The light was focused thanks to two spherical lenses of 60 mm and 100 mm focal length, and deviated by a 45° mirror, positioned as close as possible to the camera (Fig. 4.7). The lamp used was a H4 halogen lamp of 12V, 60/55W and the screen was made up with Oralite retroreflective film 5300. The camera used with the Edgerton shadowgraphy was the Phantom V7.1 high speed camera, combined with a 12 mm focal length objective. The specifications of the camera are also listed in Table 4.3.

Table 4.3.: Camera specifications

<table>
<thead>
<tr>
<th>Spec.</th>
<th>HS-1</th>
<th>HS-2</th>
<th>HS-3</th>
<th>IR</th>
</tr>
</thead>
<tbody>
<tr>
<td>Camera type</td>
<td>Phantom V7.1</td>
<td>Photron SA5</td>
<td>Photron SA3</td>
<td>FLIR SC4000</td>
</tr>
<tr>
<td>Image size</td>
<td>128x128</td>
<td>768x648</td>
<td>256x464</td>
<td>320x256</td>
</tr>
<tr>
<td>Color</td>
<td>No</td>
<td>Yes</td>
<td>No</td>
<td>-</td>
</tr>
<tr>
<td>Sampling [kHz]</td>
<td>14</td>
<td>7.5</td>
<td>7.5</td>
<td>max 0.42</td>
</tr>
<tr>
<td>Exposure [µs]</td>
<td>67</td>
<td>53</td>
<td>20</td>
<td>-</td>
</tr>
</tbody>
</table>

For the simple high-speed visualizations, the HS-1 camera with the 12 mm objective has been used alone, or combined with two other high-speed cameras, with the specifications listed in Table 4.3 and the spatial arrangement sketched in Fig. 4.8. When the Phantom V7.1 was used alone, it was placed in position HS-2. In this position a test was also performed with a FLIR SC4000 infra-red camera, with the specifications listed in Table 4.3.
Chapter 4. Experimental setup

4.4. Conclusion

To be able to perform small scale BLEVE experiments, different challenges needed to be accomplished. At first, a facility to host the small pressure reservoir, ensuring a secure environment without experiencing confined effects needed to be designed and built. Concerning this first challenge, the BABELs facility was designed during the first year of this study and available since January 2010.

Once the environment of the experiments was ready, the liquefied gas reservoir needed to be found and were ordered from the NTG company in Japan. The reservoir needed then to be heated until rupture. A microheater has been ordered from MHI company, that had high temperature capabilities, small size, and where a parametric analysis on the applied heat flux was possible.

Finally, instrumentation was necessary to correctly monitor the different parameters of a BLEVE experiments. In addition to the use of existing material like thermocouple and high-speed camera, different new techniques were developed. First, the piezoelectric pressure sensors were dynamically calibrated to be able to measure the blast wave produced by the BLEVE rupture. Then, a connection from the reservoir to a pressure sensor was especially designed to measure the
4.4. Conclusion

reservoir internal pressure. And finally, the Edgerton shadowgraphy technique was implemented to be used in BABELs.

All the experimental setup and instrumentation described in this chapter allowed to obtain a new experimental database of small scale reservoir ruptures, that will be analyzed in chapter \[5\] and which is detailed in appendix \[A\] section \[A.11\].
Chapter 5.

Experimental results

A more extensive comprehension of BLEVE events involves two aspects: the conditions leading to the reservoir rupture and the consequences of the rupture. Therefore, the presentation of the experimental results and their analysis is structured in three parts. First, the period from the beginning of the thermal aggression to the reservoir rupture is investigated, mainly with the study of the pressure and temperature evolution. The second part is dedicated to the reservoir rupture: the reservoir pressure at rupture, the time to rupture and the ruptured reservoir pattern. And finally, the last part deals with the BLEVE hazards. In this study, the fluid ejection and ignition, and the blast wave generation are studied.

5.1. Prior rupture

5.1.1. Temperature profiles

5.1.1.1. Experimental results

The time evolution of the measured temperature profiles for the two types of reservoirs is plotted in Fig. 5.1 left. As sketched in Fig. 5.1 right, the temperature was measured above the plate (positioned above the microheater), on the bottom and on the top of the reservoir. When the reservoir heating starts, the temperature increases very fast close to the microheater and rather slowly at the top of the reservoir. After this transitional period, the three temperatures rise almost linearly until the rupture of the reservoir; then the thermocouples are ejected from the reservoir or are broken by the violence of the rupture.

The influence of the reservoir size on the heating is illustrated in Fig. 5.1. The same heat flux is applied on both reservoirs, and the plate temperature evolution is similar for the two reservoir sizes. But the butane reservoir (Archer) has a smaller volume and a smaller wall thickness than the propane reservoir (NTG), which allows the heat from the microheater to be transferred more strongly inside the butane reservoir, decreasing the time to rupture.
Chapter 5. Experimental results

The Figure 5.2 shows the measured temperature evolution, plotted in a non-dimensional form. The non-dimensional form of the temperature is obtained by subtraction of the initial temperature \(T_0\), and by division of this difference by the difference between the temperature at rupture and the initial temperature \((T_{rupt} - T_0)\). The non-dimensional form of the time is obtained similarly by subtraction of the time at which the reservoir starts to be heated \(t_0\), and by division of this difference with the total duration of the reservoir heating (from the beginning to the rupture, \(t_{rupt} - t_0\)).

Figure 5.1.: Left: Temperature evolution for Archer & NTG reservoirs (same heat flux). Right: position of thermocouples

Figure 5.2.: Non-dimensional temperature evolution. Left: Archer, Right: NTG
5.1. Prior rupture

For the Archer reservoirs, the heat flux in the three tests displayed in Fig. 5.2 left was similar. And as the data obtained at the plate (position \( n^3 \)) and at the top of the reservoir (position \( n^1 \)) are superposing well, this suggests a good reproducibility of the three experiments. But the measured temperature under the reservoir (position \( n^2 \)) shows larger differences between the tests. These differences are probably due to a larger uncertainty about the thermocouple position under the reservoir. The effect of the thermocouple position is less pronounced for the NTG reservoirs heating due to their larger size, as observed in Fig. 5.2 right. For example, if the thermocouple is not positioned at the exact bottom of the reservoir but with a shift of 5 mm, this corresponds to an error angle of 28.6° for the butane reservoir (Archer) and only 14.3° for the propane reservoir (NTG). The temperature evolution for the position \( n^2 \) and 533 W as power is missing due to a wrong position of the thermocouple.

The three tests displayed in Fig. 5.2 right have been performed with NTG reservoirs weakened by the same groove length (\( 2c = 0.015m \)), but the heat flux has been changed by varying the applied electrical power. The non-dimensional temperature profiles are superposing well (see Fig. 5.2), so that the amplitude of the applied heat flux does not change the way the reservoir is heated. Only the time to rupture is changing, as shown in Fig. 5.3 left, where a linear influence of the electrical power on the time to rupture is observed, both for the Archer and the NTG reservoirs. If the time to rupture is divided by the ratio of the square of the reservoir thickness and of the material thermal diffusivity, the two non-dimensional times are similar, as observed in Fig. 5.3 right.

![Figure 5.3: Time to BLEVE: evolution with the electrical power of the micro-heater](image)

Left: dimensional, Right: non-dimensional
Chapter 5. Experimental results

5.1.1.2. Temperature modeling

Modeling the temperature evolution of a reservoir is strongly linked to the type of reservoir and thermal aggression. For example, a fire is mostly heating a reservoir by convection and radiation while an electrical resistor (like the microheater used in this study) is heating a reservoir mainly by conduction. Therefore, the objective of this section is only to model the temperature evolution for the thermal aggression used in this study: a reservoir heated by a microheater. This model will be combined with the model for the rupture pressure to predict the time to BLEVE occurrence, in section 5.2.

The thermal approach relies on a 1D thermal model, where the different parts of the experimental setup (the reservoir, the cradle, the plate and the microheater) are regarded as different layers. A schematic is proposed in Fig. 5.5. The main heat transfer mechanism that is heating the reservoir is the conduction between the different layers. But the measured temperatures of the plate, of the bottom, and of the upper parts of the reservoir experience larger differences than the ones induced only by the conduction, as observed in Fig. 5.4. Therefore, a contact conductance between each component of the model is added, as expressed in Eq. 5.3. Inside a given layer i, there is only conduction as expressed in Eq. 5.2. For the boundary conditions, the upper surface is subjected to convection loss with ambient (see Eq. 5.1) and the heat flux applied on the plate by the microheater experiences radiation losses (see Eq. 5.4).

Figure 5.4.: 1D model with only conduction heat transfer between layers
5.1. Prior rupture

Figure 5.5.: Schematic of the 1D unsteady temperature model

\[
\frac{-\lambda_1}{\partial y} \frac{\partial T_{1,1}}{\partial y} = \ h (T_{amb} - T_{1,1}) \quad (5.1)
\]

\[
\rho_i C_p \frac{\partial T_{i,j}}{\partial t} = \lambda_1 \frac{\partial^2 T_{i,j}}{\partial y^2} \quad (5.2)
\]

\[
\rho_i C_p \frac{\partial T_{i,N_i}}{\partial t} = \lambda_i \frac{\partial T_{i,N_i}}{\partial y} + h_{i,i+1} (T_{i,N_i} - T_{i+1,N_i+1}) \quad (5.3)
\]

\[
\rho_{i+1} C_{p,i+1} \frac{\partial T_{i,N_i+1}}{\partial t} = \lambda_{i+1} \frac{\partial T_{i,N_i+1}}{\partial y} + h_{i,i+1} (T_{i,N_i} - T_{i+1,N_i+1}) \quad (5.4)
\]

To evaluate the contact conductance between the different layers, the model developed by Yovanovich and Antonetti [146], resumed by Bejan [8] is used, and is expressed in Eq. 5.5. The different terms used in Eq. 5.5 are detailed in appendix A, section A.5 and the intersticial gas is assumed to be air. The Table 5.1 gives the values of the different contact conductances used in the temperature evolution model, calculated with Eq. 5.5.

\[
h_{i,i+1} = \left( \frac{1}{h_c} + \frac{1}{h_g} \right)^{-1} = \left( \frac{1.25 \lambda_{i,i+1} f_m}{f_r} \left( \frac{P_c}{T} \right)^{0.95} \right)^{-1} + \left( \frac{\lambda_g}{Y+Z} \right)^{-1} \quad (5.5)
\]

To solve this model numerically, the different material layers are discretized in a finite-differential form. In Eq. 5.1 to 5.4 the subscript \( i \) represents the layer.
number while the subscript $j$ represents the node number, inside a given layer $i$ and $N$ represents the number of nodes when a layer ends. The equations 5.1 to 5.4 are discretized (the discretized form is given in appendix A section A.5) and solved numerically with an explicit method and a forward difference approximation for the time derivative, and a central difference approximation for the spatial derivative. The time interval used to solve these equations is chosen to satisfy the stability criteria, that is the Fourier number of the different layers have to be smaller than 0.5, as expressed in Eq. 5.6. The spatial interval is set to $\Delta y = 0.2mm$, so the time interval is set to $\Delta t = 0.35ms$, which satisfies Eq. 5.6 for the three material layers.

\[ F_o = \frac{\alpha \Delta t}{\Delta y^2} < 0.5 \] (5.6)

Plots drawn in Fig. 5.6 show that the predictions fit very well the experiments if the applied heat flux is adjusted. In the experiment, the energy dissipated by unit time by Joule effect is calculated by multiplying the voltage and current monitored on the digital display of the power supply. But this value is too large to fit the experiments, and has to be reduced to 42%. The percentage of heat flux by Joule effect that is effectively heating the reservoir (according to our model) stays fixed whatever is the value of the heat flux applied. Indeed, in Fig. 5.6 right, the heat flux in the experiments has been decreased to 331W and the model still follows well the measured temperatures providing that the applied heat flux is decreased in the model from 462W to 331W (the percentage of heat flux effectively heating the reservoir stays unchanged: 42%). Therefore, this simple model is able to predict the heating of the NTG reservoir by a microheater, if the applied heat flux is known.

<table>
<thead>
<tr>
<th>Position</th>
<th>$h_{ij}$ [W/mK]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Between reservoir and cradle</td>
<td>$h_{12} = 19.5$</td>
</tr>
<tr>
<td>Between cradle and plate</td>
<td>$h_{23} = 18.3$</td>
</tr>
<tr>
<td>Between plate and microheater</td>
<td>$h_{34} = 27.2$</td>
</tr>
</tbody>
</table>

Table 5.1.: Contact conductance values calculated from Eq. 5.5

In the thermal model, the two main parameters that could be subjected to estimation errors are the heat flux and the contact conductances. First, the values of the contact conductance were estimated from the model of Yovanovich and Antonetti [146], but in the application of the model, some parameters were estimated. The sensitivity analysis of the contact conductance based on the different parameters of the model of Yovanovich and Antonetti is given in appendix A section A.5. The sensitivity of the thermal model on the values of the contact conductance can
5.1. Prior rupture

Figure 5.6.: Comparison of measured and modeled temperature evolution \( (2c = 0.015m, \text{NTG reservoir}) \). Left: Power = 462W, Right: Power = 331W

be observed in Fig. 5.7 left, and show that a wrong estimation of the contact conductance affect mostly the evolution of the upper part of the reservoir. Indeed, the contact conductance has an effect of adding a temperature gap between the different layers of the model. So the larger is the contact conductance, the smaller is the temperature gap between layers. And second, the heat flux applied in the model has been set to only 42% of the power calculated by multiplying the applied voltage and current. This percentage is only the result of a fit of the thermal model with the measurements. The sensitivity of the thermal model on the percentage of applied heat flux can be observed in Fig. 5.7 right, and show that the heat flux mostly influences the temperature evolution of the plate under the reservoir. Actually, the conduction and the contact conductances between the layers are damping eventual fluctuations of the heat source.

5.1.2. Pressure - Temperature evolution

When the reservoir is heated, the temperature increase results in an increase of the internal pressure, as the fluid is enclosed in a constant volume reservoir. Two measured internal pressure evolution are plotted in Fig. 5.8 versus the temperature measured at the top of the reservoir (position \( n^1 \) in Fig. 5.1 right). These measurements are compared with the experiment of Stawczyk [128] that generated a BLEVE with a 5 kg reservoir of propane, filled at 70%.

The pressure - temperature diagram in Fig. 5.8 shows that the reservoir heating is a two-step process. During the first step, the internal pressure follows the saturation line; the liquid and vapor phases inside the reservoir are in equilibrium, the liquid density decreases and the vapor density increases with time. And during
Chapter 5. Experimental results

Figure 5.7.: Sensitivity of the thermal model. Left: with the values of the contact resistances, Right: with the percentage of the applied heat flux by Joule effect

the second step, the measurements deviate from the saturation line and exhibit a quasi-linear increase of the pressure with temperature. The pressure - temperature relationship stays quasi-linear until rupture, even after having exceeded the critical point of the fluid. During the last part of the reservoir heating, the fluid is then supercritical, meaning that distinct liquid and gas phases do not exist anymore.

The modeling of the pressure - temperature evolution is done by using either the NIST thermodynamic database or the EES software (Engineering Equation Solver). These two softwares are designed to provide the thermophysical properties of a series of fluids in the conditions given by the user. For the thermophysical properties of propane, both softwares are based on the equation of state (EOS) developed by Miyamoto and Watanabe [90] and formulated only for propane in terms of the non-dimensional Helmholtz free energy. This equation of state has been developed from selected reliable measurements and is able to represent accurately most of the reliable experimental data in the range of validity from 85.48 K (the triple point temperature) to 623 K, at pressures to 103 MPa, and at specific volumes down to 0.00135 m$^3$/kg.

The pressure - temperature modeling, using the EOS of propane, is plotted together with the measurements in Fig. 5.8. The first step of the pressure - temperature evolution is modeled by the saturation line of the fluid. The second step, the quasi-linear increase, is modeled by fixing the specific volume. This specific volume is calculated as the volume of the reservoir divided by the total fluid mass, assuming that the fluid occupies the reservoir entirely. But the specific volumes are slightly different in the two experiments performed in this study. For these measurements, the tube connecting the reservoir and the pressure transducer, but
5.1. Prior rupture

Figure 5.8.: Pressure - Temperature diagram

also the pressure transducer itself introduce an additional volume that needs to be included in the calculation of the specific volume. In addition, between the two tests, the pressure transducer has been changed (see chapter [ ]). The slight difference of volume occupied by these two transducers can justify the difference in specific volume needed to fit the pressure-temperature evolution. Therefore, to predict the rupture pressure for a reservoir without a pressure transducer, the specific volume is calculated only from the volume of the reservoir.

When the pressure reaches the maximum stress sustainable by the reservoir, the reservoir fails. The change in the linear progression of the pressure - temperature curve close to the rupture for the measurement with $2c = 0.015m$ can be linked to the fact that the measured rupture pressure is in the range of the sensor proof pressure, causing a non-linear response of the sensor in that range. This explanation is strengthened by the response of the PMP transducer used in the test with $2c = 0.04m$, that has a pressure range of 60 MPa, and that does not show any deviation from the linear evolution of pressure and temperature until rupture.

To conclude this section, the pressure and temperature follow the saturation line until reaching the specific volume corresponding to the reservoir volume divided by the total fluid mass. This specific volume stays then fixed, and the fluid becomes supercritical. As the specific volume is constant, the pressure and temperature follow a quasi-linear evolution until the rupture pressure. The quasi-linear evolution of the pressure and temperature can be easily modeled through the calculation of the specific volume and the use of the thermodynamic properties database.
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5.2. Rupture

The study of the reservoir rupture is divided in three parts. The first part is dedicated to the modeling of the rupture pressure. The second part compares the measured time to rupture with the time to rupture obtained from the rupture pressure and the thermal models. The last part is dedicated to the ruptured reservoir: how the reservoir has ruptured and the influence of the rupture pressure on the fragmentation pattern.

5.2.1. Rupture pressure

The internal pressure was only measured in two tests with NTG reservoirs (see Fig. 5.9 left) due to the difficulty of measuring this pressure with a transducer that is kept in a leak-free system until rupture. Therefore, the rupture pressures of the other tests, performed with NTG reservoirs with varying groove lengths, but also with the Archer reservoirs have to be estimated. The rupture pressure is calculated from the equation of state of the fluid, knowing the rupture temperature (measured in every test) and the “specific volume”, as explained in the previous section. It can be observed that the calculated rupture conditions are in the same range of pressures as the measured points, but have smaller rupture temperatures. This can be explained by the difference in specific volume between the tests with internal pressure measurement and without. When the internal pressure is measured, the
5.2. Rupture

The pressure-temperature diagram plotted in Fig. 5.9 can also be used to learn more about the fluid state at rupture. As observed in Fig. 5.9, the fluid state at rupture differs between the Archer reservoirs and the NTG reservoirs. Concerning the Archer reservoir, the rupture pressure is higher than the critical pressure, but the rupture temperature is lower than its critical value. Therefore, the fluid is at the state of a compressed fluid, but not yet supercritical. As opposed to the Archer reservoirs, the fluid state prior the rupture of a NTG reservoir is supercritical. The supercritical fluid state at rupture always lies at the left of the pseudocritical line, meaning that the fluid state is supercritical, with a liquid-like density (see chapter 3, section 3.3 for more details). The analysis of the fluid state at rupture will be discussed further in section 5.3.1.

A large range of rupture pressures can also be observed on both types of reservoirs and is directly linked to the size of the groove lengths performed on the reservoir. The next part of this section is dedicated to the rupture pressure modeling. The rupture pressure can be modeled through a mechanical approach where the effect of heat on the mechanical properties of the reservoir can be neglected due to the low range of rupture temperatures. The rupture pressure modeling focuses on the NTG reservoirs, for which the notch dimensions have been correctly measured and the metal specifications are provided by the constructor. The rupture pressure modeling approach consists in a comparison of the experiments with models already published in the literature. The rupture pressure modeling is divided in two parts: the modeling of a reservoir without any defect and the modeling of a reservoir with an axial notch.

5.2.1.1. Reservoir without a notch

The objective of this section is to compare the correlations estimating the limit load of an horizontal cylindrical reservoir presented in chapter 3 with experimental data.

In the specifications of the NTG reservoir, the burst pressure is mentioned as 44 MPa for a reservoir made with the JIS G3141(96) SPCE steel. The constructor ensures that the material has a tensile strength of minimum 300 MPa and an elongation of 48%. But these data corresponds to the raw material only and the specifications are subjected to change during the production process. However, the company doesn’t know how much. Technical data sheets for the JIS G3141(96) SPCE steel are provided by steel making companies, but they usually are not complete and can vary from one company to another. Therefore, MatWeb, the internet material property database was used, as it contains over 91000 material

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Chapter 5. Experimental results

datasheets, with more than 5100 different steels. They also provide correspondance
between japanese and european standards for steel names, and the JIS G3141(96)
SPCC has been found to be similar to the AISI 1008 Steel CQ, DQ, and DQSK
sheet: 1.6-5.8 mm thick. The names SPCC and SPCE are related to metal grades,
the SPCE (deep drawing quality) being at bit more able to deform than the SPCC
(standard quality). For this metal, MatWeb proposes a range of ultimate tensile
strengths of 303 to 358 MPa and a range for the yield strengths from 180 to 240
MPa, with an elongation from 42 to 48%. In this study, the maximum values of
these ranges are taken.

Table 5.2.: NMSE validation factor of models compared with experiments

<table>
<thead>
<tr>
<th>Model</th>
<th>Experimental data</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Huang [64]</td>
</tr>
<tr>
<td>Faupel</td>
<td>0.0151</td>
</tr>
<tr>
<td>Svensson</td>
<td>0.0079</td>
</tr>
<tr>
<td>Subhananda Rao</td>
<td>0.0240</td>
</tr>
<tr>
<td>Zhu (Tresca)</td>
<td>0.0060</td>
</tr>
<tr>
<td>Zhu (ASSY)</td>
<td>0.0017</td>
</tr>
<tr>
<td>Zhu (von Mises)</td>
<td>0.0030</td>
</tr>
<tr>
<td>Xue ($P_{\text{min}}$)</td>
<td>0.0095</td>
</tr>
<tr>
<td>Xue ($P_{\text{max}}$)</td>
<td>0.0210</td>
</tr>
<tr>
<td>Aseer Brabin</td>
<td>0.0088</td>
</tr>
<tr>
<td>Staat (Tresca)</td>
<td>0.0094</td>
</tr>
<tr>
<td>Staat (von Mises)</td>
<td>0.0077</td>
</tr>
</tbody>
</table>

The rupture pressure of a cylindrical reservoir can be estimated by different cor-
relations, as detailed in 3 section 3.4.1. The rupture pressure provided by the
constructor of the NTG reservoir (44 MPa) is compared with the rupture pressure
estimations provided by the different correlations of Table 3.2. In addition, the
experimental results of Huang [64], Law [78] and Zheng [151] are also used in the
comparison. Law [78] has performed 5 burst tests on thin-walled end-capped steel
pipes, with a diameter range of 0.25-0.5m and a tensile strength range of 450-700
MPa. In another study, Huang [64] collected 32 burst tests from literature, with a
diameter range of 0.06-0.9m and a tensile strength range of 500-1000 MPa. Rajan
performed 14 burst test with the same reservoir size (0.2 m diameter) and material
(tensile strength of 1200 MPa). Finally, Zheng [151] has also performed 15 burst
tests with the same reservoir (0.04 m diameter and 375 MPa tensile strength).

The Figure 5.10 compares the rupture pressure of the different sets of burst tests
collected from the literature and the theoretical rupture pressure of the NTG. A
strong influence of the diameter ratio is clearly observed in Fig. 5.10 left while the
influence of the tensile strength on the rupture pressure is more difficult to
conclude. The different series of measurements are compared with the different
correlations resumed in Table 3.2 of chapter 3. The Table 5.2 shows the Normalized
5.2. Rupture

Mean Square Error validation metric (NMSE) between each series of experiments and the correlations. The NMSE express the mean scatter between the experiments and a given correlation, based on a linear scale, and is defined in appendix B section B.2.1. The average NMSE including all sets of measurements is also given in Table 5.2. In addition to the NMSE, the experimental burst pressure divided by the estimated pressure of the models of Zhu with the ASSY (average shear stress yield) criteria, the von mises criteria and the Xue model with $P_{\text{max}}$ are displayed in function of both the ultimate strength and the reservoir diameter in Fig. 5.11 to 5.13.

![Figure 5.10.](image1)

Figure 5.10.: Comparison of the rupture pressure from literature. Left: Influence of the diameter ratio, Right: Influence of the tensile strength

![Figure 5.11.](image2)

Figure 5.11.: Comparison of Zhu ASSY correlation with experimental results. Left: Influence of tensile strength, Right: influence of diameter
Chapter 5. Experimental results

First of all, the results of Rajan give an idea of the experimental scatter since the experiments were performed with the exact same reservoir design for the 14 tests. The scatter observed in Zheng is partly due to the experimental reproducibility, but also due to differences in the ratio of outer to inner diameter, as observed in Fig. 5.10. Second, a large discrepancy between the experiments of Huang and Law and the ones of Zheng and the NTG reservoir is observed. This discrepancy can first be explained by the difference in the scale of the reservoir. The experiments of Huang, Law and Rajan involved reservoirs from 0.06 to 0.9m as Zheng and the NTG have a diameter of only 0.04m. It can also be due to the different types of material used. Huang, Law and Rajan report rupture pressures from high strength steels, with a minimum tensile strength of 470 MPa and that can reach 1200 MPa, as the reservoirs tested by Zheng and the NTG are mild steels, with a tensile strength of 350 to 500 MPa. Finally, the difference between the series of tests can also be linked to different approaches in the design of the reservoir. The reservoirs of Zheng and the NTG are extruded, originally formed in a single piece as the reservoirs of Law and Rajan consists on a pipe with welded end-caps. The experiments reported by Huang are coming from other authors and the design of the reservoir is not mentioned.

In a tentative to find the best model to fit the different experiments, the best estimate for the tests of Huang and Law is the model of Zhu with the ASSY (average shear stress yield) criterion. But this model underestimates the tests of Zheng and the NTG (see Fig. 5.11). And the best model for Zheng and NTG is the $P_{\text{max}}$ modeled by Xue, but it overestimates the tests of Huang and Law, as observed in Fig. 5.12. The experiments of Rajan are lying between these two models. And the model that gives the best estimation whatever the design, size and material of the reservoir is the model of Zhu with von Mises criterion, as
5.2. Rupture

observed in Fig. 5.13.

Figure 5.13.: Comparison of Zhu von Mises correlation with experimental results.
Left: Influence of tensile strength, Right: influence of diameter

5.2.1.2. Reservoir with a notch

If the reservoir is weakened by the presence of a defect, the rupture pressure is affected too and the prediction of the rupture pressure with the previous correlations will overestimate the rupture pressure. Therefore, a more specific modeling is needed. Different correlations for estimating the rupture pressure of a cylindrical reservoir with an axial notch are detailed in chapter 3, section 3.4.1. The purpose of this section is to compare these correlations with experimental results.

Table 5.3.: NMSE validation factor of models compared with experiments

<table>
<thead>
<tr>
<th>Model</th>
<th>Experimental data</th>
<th>Staat [126]</th>
<th>Laboureur</th>
<th>All</th>
</tr>
</thead>
<tbody>
<tr>
<td>Kiefner [70]</td>
<td></td>
<td>0.2078</td>
<td>0.3267</td>
<td>0.2193</td>
</tr>
<tr>
<td>Ewing [31]</td>
<td></td>
<td>0.1159</td>
<td>0.394</td>
<td>0.1428</td>
</tr>
<tr>
<td>Carter Local (von Mises) [19]</td>
<td></td>
<td>0.0429</td>
<td>0.0837</td>
<td>0.0469</td>
</tr>
<tr>
<td>Carter Global (von Mises) [19]</td>
<td></td>
<td>0.0795</td>
<td>0.0614</td>
<td>0.0778</td>
</tr>
<tr>
<td>Staat Local (von Mises) [127]</td>
<td></td>
<td>0.04</td>
<td>0.0759</td>
<td>0.0435</td>
</tr>
<tr>
<td>Staat Local (von Mises) [127]</td>
<td></td>
<td>0.116</td>
<td>0.0536</td>
<td>0.11</td>
</tr>
</tbody>
</table>

The Table 5.3 lists the NMSE validation metrics for the local and global formulations of Carter and Staat with the von Mises criterion and for the correlations of Ewing and Kiefner. In addition to the NTG experiments, Staat made an extensive
Chapter 5. Experimental results

literature survey [126], and collected 293 burst tests, with varying type and size of defects, and also with varying reservoir dimensions and mechanical properties. Among this series of burst tests, the rupture of a reservoir with an axial external defect has been observed in 112 of the 293 tests. These 112 tests are then compared with the rupture pressure models. The first conclusion of the comparison is that there is a large improvement on the estimation of the rupture pressure with the models of Carter and Staat compared to the two other correlations. Zarrabi [150] already concluded that the models of Kiefner and Ewing were underestimating too much the rupture pressure compared to the FEM analysis and that they were not physically correct because the estimation of the rupture pressure for a defect free vessel was smaller than the rupture pressure expressed by the Xue model, $P_{\text{min}}$.

Comparing only the models of Carter and Staat, the error on the local estimate is better with the Staat model, and the error on the global estimate is better for the Carter model. This is mainly due to the fact that the model of Carter does not take into account the interaction between the bending and normal force, which results in a lower prediction. Therefore, the model of Staat globally underestimates the Laboureur database, but overpredicts the Staat database for the global approach.

![Figure 5.14: Predicted vs measured rupture pressure, function of the $b/c$ ratio, model of Staat with von Mises criteria. Left: Local estimate, Right: Limit load (global approach)](image)

The rupture pressures modeled with either the local or global estimates of Staat are compared with the Staat and NTG experimental results in Fig. 5.14 and 5.15. As already observed by Staat [127], the error between the model and the experiments increases when the ratio of the notch depth over half the notch length ($b/c$) decreases (see Fig. 5.14), and when the ratio of the notch depth over the reservoir thickness ($b/d$) increases (see Fig. 5.15). Larger errors for deep and/or
5.2. Rupture

Figure 5.15.: Predicted vs measured rupture pressure, function of the $b/d$ ratio, model of Staat with von Mises criteria. Left: Local estimate, Right: Limit load (global approach)

Long defects are partly due to the uncertainties on the exact dimensions of the reservoir thickness and groove dimensions. But it is also due to the model itself that is very sensitive for small $b/c$ and large $b/d$ ratios, as illustrated in appendix A section A.6. In addition, it can be observed that the global limit load overestimates the burst pressure, or that the reservoir collapses before reaching the global collapse pressure. The local estimate is fitting better the data but cannot be considered as a model for the burst pressure. Indeed, the local rupture pressure for a penetrating defect is estimated to zero. But in reality, a reservoir with a penetrating defect can still carry the global limit load. In conclusion, the best approach to predict the rupture pressure of a reservoir with an axial external defect is to use the local and global collapse as an interval.

Looking only at the results from this study, a part of the error between the models and the experiments is that the limit load of the NTG reservoir without defect is not correctly modeled by Staat. So to minimize this effect, the limit load modeled by the global approach of Staat is presented in a non-dimensional form. The limit load modeled by the global approach of Staat is divided by the limit load modeled by Staat for a reservoir without defect and the NTG experimental results are divided by the theoretical burst pressure (44 MPa). This non-dimensional rupture pressure is plotted in Fig. 5.16. There is an uncertainty on the depth of the grooves performed on the NTG reservoirs, and the groove depth is shown in Fig. 5.16 to have a strong influence on the rupture pressure. The average depth is 0.8mm, but the Fig. 5.16 shows that some tests have probably a higher or lower groove depths. It can also be observed in Fig. 5.16 that the hydraulic test performed with
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A groove length of $2c = 77\text{mm}$ shows a quite larger rupture pressure. This could tend to prove that even if the rupture temperatures are quite low, their values still influence the rupture pressure. Finally, it can also be observed in Fig. 5.16 that the value of $440\text{MPa}$ as given by the constructor for the rupture pressure of a reservoir without any defect seems to be an underestimation of the real burst pressure.

![Figure 5.16.: Reservoir rupture pressure in function of the groove length](image)

**5.2.2. Time to rupture**

The time to rupture can easily be determined by using the different models already described in this chapter. At first, the rupture pressure for a reservoir with an axial defect is estimated both with the global approach of Staat as defined in the Table 3.3 or with the global approach of Staat where the rupture pressure without defect is set to $44\text{MPa}$, as given by the constructor. For a NTG reservoir with a notch of $2c = 0.015\text{m}$ and $b = 0.8\text{mm}$, the rupture pressure from Staat is equal to $28.4\text{MPa}$ for the Staat Global approach and to $34.4\text{MPa}$ for the Staat global approach using the $44\text{MPa}$ rupture pressure. With the modeled rupture pressure, the rupture temperature can be found by using the quasi-linear pressure-temperature relationship, as the specific volume of the NTG reservoir is known. The rupture temperature is compared with the temperature evolution of the upper part of the reservoir, as modeled in section 5.1.1.2 to know the time to BLEVE.

The time to BLEVE obtained from this modeling approach is then compared to the Fig. 5.3 where the time to rupture has been observed to increase linearly when decreasing the applied heat flux. The Fig. 5.17 compares the estimated
times of rupture for the two modeling approaches of the rupture pressure, with the experimental values. The Global approach of Staat is underestimating the BLEVE rupture. A more reasonable fit is obtained by taking the rupture pressure from the NTG specifications, with an average error between the model and the experiments of around 5%. But the slope of the linear evolution of time is different from the experiments and the model. The reason of this difference is not clear. But it can be observed in Fig. 5.17 that, as the rupture pressure without defect increases, the slope of the time evolution increases also. One explanation of this slope difference would then be that the rupture pressure of 44MPa for a reservoir with no defect is an underestimation of the true rupture pressure, as the hydraulic test would tend to prove. Therefore, if the rupture pressure is higher, the time needed to reach this pressure is longer.

![Figure 5.17: Time to BLEVE, measured and modeled](image)

5.2.3. Ruptured reservoir pattern

The whole series of ruptured reservoirs (around 30 reservoirs were ruptured) took place in the same way. The rupture of the reservoir started at the groove length and propagated along the notch, and the sides of the notch opened, forced by the ejection of the fluid (see Fig. 5.18 left and middle). When the reservoir was connected to a pressure transducer, the rupture was slightly different, as observed in Fig. 5.18 right. The sides of the notch opened completely, ending in a final fragment that looks like a plate. This difference in the rupture pattern is probably due to the vice that forced the reservoir not to move during the rupture, which ended in a larger opening.
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Figure 5.18.: Reservoir rupture pattern: Left: $2c = 0.08 \text{ m}$, $P_{\text{rupt}} = 25.2 \text{ MPa}$, Middle: $2c = 0.01 \text{ m}$, $P_{\text{rupt}} = 39.2 \text{ MPa}$, Right: $2c = 0.01 \text{ m}$, $P_{\text{rupt}} = 39.4 \text{ MPa}$ with internal pressure measurement.

The time evolution of the rupture velocity has been calculated from the high-speed visualizations, by monitoring the opening or the reservoir from the side view, along the notch axis. At first, it can be observed in Fig. 5.19 that the rupture velocity is large at the beginning of the rupture, and then decreases quickly with time, until becoming null after around 0.5 ms, when the reservoir is opened. It can also be observed that the rupture velocity is higher when the notch length is longer. As the presence of a notch decreases the reservoir thickness in the region of the notch, the propagation of the rupture is easier than without the presence of the notch, and therefore is observed to be faster.

Figure 5.19.: Time evolution of reservoir opening velocity
5.2. Rupture

The rupture pattern observed in this experimental study is one of the four most probable rupture patterns according to Gubinelli [49], as observed in Fig. 5.20. In the present tests, CV1 was mostly observed, or CV7 if you consider one fragment at the end of the rupture. Therefore, even if the rupture pattern in the present experiments was intentionally repeatable, this pattern is among the most probable rupture patterns of a BLEVE, CV1 and CV7 representing almost 40% of BLEVE ruptures as shown in chapter 3. Therefore, even if this rupture has been induced by the groove, it is still interesting to analyze.

<table>
<thead>
<tr>
<th>ID</th>
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<tr>
<td>CV1</td>
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<td>CV3</td>
<td><img src="image" alt="CV3 Pattern" /></td>
</tr>
<tr>
<td>CV7</td>
<td><img src="image" alt="CV7 Pattern" /></td>
</tr>
</tbody>
</table>

Figure 5.20.: Left: Most probable rupture patterns according to Gubinelli [49]. Right: Legend for rupture pattern dimensions

There is a direct relationship between the groove length and the loss of containment; the less the reservoir is weakened, the larger is the reservoir opening, as observed in Fig. 5.18. Figure 5.21 shows a linear increase of the reservoir opening as the rupture pressure increases, and therefore as the groove length decreases, as observed in Fig. 5.16. Both the width of the opened reservoir and the half perimeter show a linear increase with the burst pressure. This behavior is also observed for the Archer reservoirs (see Fig. 5.21). This behavior can be explained by a larger energy contained in the reservoir before the rupture when the rupture pressure is increased. As more energy is available before rupture, the strength of the reservoir opening will be larger.
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Figure 5.21.: Evolution of rupture pattern dimensions with the rupture pressure. Left: NTG reservoirs, Right: Archer reservoirs

5.3. Rupture hazards

From the majority of the small scale experiments performed in this study, at rupture, the majority of fluid content was ejected from the reservoir in a few milliseconds; the fluid did not ignited and no fireball was noticed. The first part of the hazard description is therefore dedicated to the description of the fluid ejection. In a few tests, sparks were produced close to the reservoir, that ignited the cloud generated by the fluid ejection. The burning characteristics of the deflagration are also analyzed in this section. Finally, the reservoir rupture generated a blast wave, which is one of the main hazards linked to BLEVE. The blast wave characteristics are then analyzed and compared with existing overpressure models.

5.3.1. Fluid ejection

The analysis of the fluid ejection is divided in three parts. First, a qualitative analysis of the cloud generated by the fluid ejection is done through visualizations. Based on these visualizations, a quantitative analysis of the cloud is then performed through image processing. And finally, the thermodynamics states of the fluid before and after the rupture are compared to better understand the physical processes associated to the different cloud visualizations.
5.3 Rupture hazards

5.3.1. Fluid ejection: visualizations

The fluid state prior rupture differs with the type of reservoir: compressed fluid for the Archer reservoir type and supercritical fluid for the NTG reservoir type, as explained in section 5.2. Therefore, the analysis of the fluid ejected at rupture is done separately for the Archer and NTG reservoirs. The Fig. 5.23 left shows visualization of the cloud generated by the rupture of an Archer reservoir. Immediately after the reservoir rupture, the fluid expands in a spherical cloud of vapor and small droplets with blurry contours. After a few milliseconds, the spherical cloud with blurry contours transforms into a very bright column, surrounded by grey fog. This fluid structure is similar to a flashing liquid jet; the bright column being a dense accumulation of tiny droplets, and the grey fog being a cloud of droplets.

![Figure 5.22: Liquid core length, compared with Vieira [137]](image)

Flashing liquid jets have already been studied in 2007 by Vieira [137], using iso-octane. From the analysis of shadowgraphs and high-speed images of the flashing jets of iso-octane, Vieira concluded that the length of the liquid core depends on the pressure ratio between the injection pressure (which is here the rupture pressure) and the saturation pressure corresponding to the injection temperature (which is here the rupture temperature). The lengths of the bright column from the Archer reservoir ruptures have been determined from the high-speed images as the end of the white zone of the cloud. The opening diameters have been calculated as the diameter corresponding to a circle with the same perimeter as the final reservoir opening. As illustrated in Fig. 5.23 right, the lengths computed from the archer reservoir fluid ejection correspond to the observations of Vieira.
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As opposed to the Archer reservoirs, the fluid state at rupture of a NTG reservoir is supercritical. Depending on the rupture pressure, two cloud ejection patterns are observed. The first cloud ejection pattern, visible on high speed visualization of the rupture of a reservoir with a large groove length ($2c = 0.077 \, m$) is shown in Fig. 5.24. At rupture, the rapid expansion of propane results in a turbulent spherical shaped cloud of vapor and small droplets with blurry contours. The cloud grows with time and the two-phase ejection decreases (as well as the jet release angle). The cloud is finally diffusing once the reservoir is empty, and the droplets are evaporating until complete visual disappearance. The second cloud ejection pattern is somewhat different, as illustrated in Fig. 5.25 for a reservoir with a smaller groove length ($2c = 0.015 \, m$). At rupture, the cloud shape is almost perfectly spherical on its upper part, like a dome. This dome-like cloud increases until a given height where the cloud expansion is stopped. Finally, the dome disappear and the two-phase ejection (and consequently the jet release angle) decreases very fast until complete disappearance of the released fluid. It can also be observed by comparing Fig. 5.24 and Fig. 5.25 that the total cloud duration, defined as the time period during which the cloud is visible (i.e. has a non zero detected area as it will be shown later in the section), is smaller for the dome-like cloud than for the blurry cloud.

The NTG reservoir rupture has been recorded simultaneously by three high-speed cameras, in three directions: parallel to the reservoir axis (side), perpendicular to the reservoir axis (front) and above the reservoir (up), as shown in Fig. 5.26. The side and front views are very similar in the upper part of the cloud, which is confirmed by the upper view that shows a cloud with a slight elliptical shape due to the reservoir opening. The front and side views allows also to see that the jet angle increases up to $180^\circ$ and then decreases until the end of the ejection, similarly at the front and side.
5.3. Rupture hazards

Figure 5.23.: Visualization of the Archer reservoir opening ($\Delta t = 0.5\text{ms}$), $P_{\text{rupt}} = 5.7\text{MPa}$.
Chapter 5. Experimental results

Figure 5.24: Visualization of a NTG reservoir opening, $2c = 0.077 \text{ m}$, $\Delta t = 0.28 \text{ ms}$.
5.3. Rupture hazards

Figure 5.25.: Visualization of a NTG reservoir opening, \( z = 0.015 \text{ m}, \Delta t = 0.28 \text{ m/s}, P_{\text{rupt}} = 35.1 \text{ MPa} \)
Chapter 5. Experimental results

Figure 5.26: NTG reservoir opening: $\xi = 0.04\,m$, $\Delta t = 1.3\,ms$, $P_{\text{rupt}} = 30.5\,MPa$. Top: Side view. Middle: End view. Bottom: Upper view.
5.3. Rupture hazards

5.3.1.2. Fluid ejection: Image processing

The radius and area of the cloud can be determined by image processing. The cloud upper boundary has been calculated with a detection technique based on the positive gradient algorithm, available in the in-house LEDAR software (Level Detection and Recording) [106]. To determine the cloud radius, a circular fit centered on the NTG reservoir has been performed on the detected cloud boundary of each image. In addition, the cloud area has been calculated with a Matlab algorithm, using the high-speed images transformed into Black & White images, with an intensity threshold level of 5%. This results in the computation of the 2D area, as observed in the image, and not the total 3D cloud area. These two image processing techniques are further explained in appendix A, section A.7.

The time evolution of the blurry and dome-like clouds produced by the rupture of NTG reservoirs, but also for the cloud that follows the rupture of Archer reservoirs can be observed in Fig. 5.27. The evolution of the cloud radius is shown in Fig. 5.27 left and the cloud expanding velocity derived from the cloud radius is shown in Fig. 5.27 right.

![Figure 5.27: Time evolution of cloud. Left: Cloud radius, Right: Cloud expanding velocity](image)

In all the reservoir ruptures, the fluid is initially ejected with quite large velocity. But this speed is not sustained and rapidly decreases to considerably smaller values. Concerning the NTG reservoir rupture, both the blurry cloud (for \(2c = 0.077\) m) and the dome-like cloud (for \(2c = 0.015\) m) grows in a similar way during approximately 0.5 ms. But while the blurry cloud continues to increase, the dome-like cloud stabilizes. The dome-like shape of the cloud is slowing
down the cloud expansion, as observed in the time evolution of both the cloud radius in Fig. 5.27 left, and the cloud expanding velocity in Fig. 5.27 right. Once the dome-like shape of the cloud has disappeared (around 1.2 ms, see Fig. 5.27 left), the upper surface of the cloud increases again and accelerates until reaching an expansion velocity with the same order of magnitude as the blurry cloud. Concerning the Archer reservoir, the shape of the radius and expanding velocity time evolution is similar to the NTG reservoirs, but with a smaller amplitude. This can be explained by the smaller size of the reservoir and the smaller rupture pressures.

Coming back to the NTG reservoir ruptures, the Fig. 5.28 shows the radius evolution for a reservoir calculated from the three directions of visualizations. Just after rupture, the three directional cloud radii estimations are similar, indicating that the cloud is spherical. But as the cloud size increases, the radii from the side and front start to deviate from each other, reflecting the elliptical shape of the cloud observed in the upper view of Fig. 5.26.

![Figure 5.28: Cloud radius from images of the cloud processed in the three dimensions](image)

The influence of the groove length on the cloud can be observed in Fig. 5.29. During the initial evolution of the cloud, the groove length has no influence, all the clouds generated by the rupture of reservoirs of different groove lengths are growing in the same way, as observed in Fig. 5.29 right from 0 to 1 ms. The only difference between the different radius evolution is the presence of a dome, observed for groove lengths of $2c = 0.015 m$ and $2c = 0.01 m$ (see Fig. 5.29 right). The total cloud area, which is plotted in Fig. 5.29 left, is more suited to analyze the cloud evolution during the whole cloud duration. The total cloud area shows first that all the clouds are initially growing in a similar way, as already observed in Fig. 5.27 and 5.29 right. But the time evolution of the total area shows also that the cloud duration decreases with the groove length, the cloud duration being
5.3. Rupture hazards

defined as the time period during which the cloud is visible (i.e. has a non zero detected area in Fig. 5.29 left). The decrease of the cloud duration when the groove length decreases, or when the rupture pressure increases, has already been observed by looking at the cloud visualizations in Fig. 5.24 and 5.25. As the cloud initial growth is not influenced by the groove length, the cloud duration does not decreases because of an increase of the initial ejection velocity. It is more likely that the cloud duration decreases because the rupture pressure increases, and therefore the temperature, which leads to a higher degree of superheat, and a faster vaporization of the cloud.

Figure 5.29.: Left: Time evolution of cloud area, Right: Time evolution of cloud radius

5.3.1.3. Fluid ejection: thermodynamics

From the high-speed visualizations, it is difficult to conclude on the physical state of the fluid present in the different cloud patterns, and to understand why the shape of the cloud is changing when the rupture pressure increases. To explain the physics of an expanding supercritical or compressed fluid jet, it is interesting to analyze the thermodynamic transformations occurring within the jet. This approach has already been followed by Wu [143] and Lin [81], and resumed by Lamanna [76] who have performed a parametric study about downstream jets of supercritical fluids into sub-critical environments by varying the injection conditions. Moreover, Lin et al. [81] have shown that, if after the jet expansion assumed to be isentropic, the fluid conditions fall inside the two-phase region, the jet condensates.
Chapter 5. Experimental results

Following Lin et al. [81], the rupture conditions of the experimental results are plotted in a pressure - entropy diagram, in Fig. 5.30 left for the Archer reservoirs containing butane and in Fig. 5.30 right for the NTG reservoirs containing propane. The thermodynamic properties are extracted from the EES software. An attempt to compute the exit pressure, assuming that the fluid is choked at the reservoir exit is given in appendix A. The exit pressures computed range from 5 to 10 MPa, for initial rupture pressures of 25 to 40 MPa. If the sound velocity is computed, using the exit pressure and the rupture temperature, values ranging between 270 and 360 m/s are found. By comparing this range of speed of sound with the initial cloud expanding velocities, as displayed in Fig. 5.27, the assumption of a choked flow at the exit of the reservoir seems valid.

The rupture of the Archer reservoir happens at subcritical temperatures, as depicted in Fig. 5.30 left. Therefore, the fluid is mostly in liquid phase when it exits the reservoir and the flow remains overall subsonic. In addition, the saturated pressure corresponding to the rupture conditions is higher than the atmospheric pressure, and therefore, the isentropic expansion path ends into the liquid/vapor two-phase region. The fuel is discharged as a superheated liquid and near-critical flash vaporization occurs; as already observed in Fig. 5.23.

Concerning the experiments performed with NTG reservoirs (Fig. 5.30 right), all the tests show a supercritical fluid prior rupture. Once the fluid is out of the reservoir, the pressure decreases and the fluid state enters in the two-phase region. Therefore, the observed clouds of Fig. 5.24 and 5.25 are probably propane condensation. In addition, Lin et al. [81] have published shadowgraph images of condensing jets, having an opaque appearance. The shadowgraph images recorded
5.3. Rupture hazards

for the present experiments have also an opaque appearance, as observed in Fig. 5.33 another argument that proves the cloud condensation.

The pressure - entropy diagram can also provide explanation for the different cloud shapes observed after the rupture of NTG reservoirs. The fluid expansion that ended in a blurry cloud (as Fig. 5.24) has rupture conditions that end in the two-phase region, forming a two-phase mixture with a large liquid fraction. By contrast, the fluid expansion that forms a dome-like cloud (as Fig. 5.25) has an entropy larger than the critical entropy and the fluid state ends in a two-phase mixture with a larger fraction of vapor. Lamanna [77] states that supercritical expanding jets have similar shock structure as supersonic underexpanded gas jet, but this structure is hidden by the fluid condensation (see Fig. 5.31). This difference in the two-phase composition of the cloud can be used to explain the dome observed at higher rupture pressures. Indeed, the dome is observed only when the vapor fraction after rupture is larger than for the blurry cloud, so the proportion of the supercritical fluid that has condensed is smaller, and the shock structure of the cloud becomes visible.

Figure 5.31.: Condensing supercritical jet [77]
Figure 5.32: Infrared image of the cloud, $2c = 0.058\,mm$, $P_{\text{rupt}} = 28.3\,MPa$

The Figure 5.32 shows an infrared view of the cloud after the rupture of a NTG propane reservoir, with a notch length of $2c = 0.055\,m$. The temperature scale is based on black body radiation and therefore represents only qualitative information since the cloud emissivity is not known. Therefore, it can be qualitatively concluded that the cloud remains hotter than the environment, especially at the center and close to the reservoir.
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Figure 5.33.: High-speed visualization images (up, $P_{\text{rupt}} = 39.1\, MPa$) compared with shadowgraph images (down, $P_{\text{rupt}} = 35.1\, MPa$) ($2c = 0.01\, m$)
Chapter 5. Experimental results

5.3.2. Cloud ignition

Among the whole series of tests performed with both the NTG and the Archer reservoirs, no fireball was observed. In a few tests, after the reservoir rupture, and when the ejected fluid has vaporized, an ignition of the remaining propane-air mixture was observed in two high-speed visualizations; one of them is displayed in Fig. 5.34. This type of ignition is not a fireball as observed in large scale experiments and detailed in chapter 3, section 3.4.3, but more a deflagration, where the basic principles have been resumed in chapter 3, section 3.4.3. But it is not a vapor cloud explosion either, as the propagation velocity of the flame is a low slower in these experiments.

Figure 5.34.: Ignition ~ 260ms after NTG reservoir rupture ($2c = 0.077 m$), $\Delta t = 7.125 ms$

The different characteristics of the propane-air mixture ignition, given in Table 5.3, can help to better understand the ignition process. For a vapor mixture to be ignited, its temperature must be higher than the minimum auto-ignition temperature, which is equal to 723 K for propane. As observed in Table 5.4, the plate temperature at the reservoir rupture was measured with the same order of magnitude as the auto-ignition temperature and the microheater is heated to even higher temperatures. The ignition of the propane-air mixture by the plate or the microheater is then theoretically possible. But the propane-air mixture at rupture has a temperature smaller than the auto-ignition temperature and needs to be heated before ignition, which explains the delay of the ignition compared to the reservoir rupture. In addition, the vapor-air mixture ratio has to be between the flammability limits in order to self-support the flame. Therefore, the ignition of the vapor-air mixture happens only if the source is at a sufficient temperature and the induction period has to be small enough so that the mixture still lies between
5.3. Rupture hazards

The flammability limits. This set of conditions is so restrictive that it explains why the vapor mixture ignited only in a few tests.

<table>
<thead>
<tr>
<th>Test</th>
<th>$2c$ [m]</th>
<th>$T_{rupt}$ [K]</th>
<th>$T_{plate}$ [K]</th>
<th>$T_{amb}$ [K]</th>
<th>$t_{ignition} - t_{rupt}$ [ms]</th>
</tr>
</thead>
<tbody>
<tr>
<td>18-11-10-1</td>
<td>0.077</td>
<td>-</td>
<td>727</td>
<td>309.75</td>
<td>263</td>
</tr>
<tr>
<td>15-05-11-2</td>
<td>0.08</td>
<td>380.1</td>
<td>730.8</td>
<td>287</td>
<td>109</td>
</tr>
</tbody>
</table>

To ensure the ignition of the cloud, 3 tests with the Archer reservoirs were performed with the presence of sparks above the reservoir. As the time interval between two sparks is large (0.02s) compared to the cloud duration, the generation of the spark ignited the vapor cloud after the fluid ejection has already stopped. But the butane is still present, in a butane-air mixture, and the spark generates a transient plasma, rich in atoms and free radicals, that ignites rapidly the mixture. As observed in Fig. 5.35, the ignition of the cloud starts at the location of the spark and the flame initially propagates spherically. After a given time, the flame propagates faster upward than from the sides. This is probably due to the fact that more vapor in the flammability limits (between 1.9 and 8.4% for n-butane) is situated above the microheater than on the sides of it, but also because of the natural upward movement of the gas by buoyancy. In addition, the flame propagation is a lot slower than the cloud ejection, as observed by the time frame of Fig. 5.35 compared to Fig. 5.23.

Figure 5.35.: Vapour cloud burning following spark ignition ($\Delta t = 5\text{ms}$)
Chapter 5. Experimental results

To estimate the flame radius, the total burning area is calculated with a Matlab algorithm as explained in appendix A section A.7, using the high-speed visualization transformed into Black & White images, with an intensity threshold level of 20%. This area is then associated to a circle and the corresponding radius is computed. Therefore, the flame speed $S_f$ can be calculated from the slope of the initial radius increase versus time, and is equal to 3.33 m/s for the first test and 4.96 m/s for the second test, as displayed in Fig. 5.36 right.

Table 5.5.: Ignition of propane-air mixture with a spark

<table>
<thead>
<tr>
<th>Test</th>
<th>$t_{\text{ignition}} - t_{\text{rupt}}$ [ms]</th>
<th>$S_f$ [m/s]</th>
<th>$A_{\text{flame max}}$ [m$^2$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>05-10-10-1</td>
<td>35.9</td>
<td>3.33</td>
<td>0.024</td>
</tr>
<tr>
<td>05-10-10-2</td>
<td>27.3</td>
<td>4.95</td>
<td>0.042</td>
</tr>
</tbody>
</table>

The measured flame speeds ($S_f$) are then compared to the flame speed calculated by multiplication of the laminar burning velocity ($S_u$) taken from Glassman [46], with the ratio of fresh to burnt gas densities which usually around 7.5 [75]. More details about the flame speed can be found in chapter 3, section 3.4.3. As observed in Fig. 5.36 right, the measured flame speeds are higher than the flame speed calculated from the literature. This can be easily explained as the flame propagation is not perfectly laminar but is in the wrinkled laminar flame regime, where the remaining turbulence of the environment causes the flame to wrinkle, which increases its flame area and consequently its flame speed. More details are given on the different flame regimes in 3, section 3.4.3. The flame evolution for the first test is given in appendix A, section A.9.

Figure 5.36.: Left: flame radius evolution, Right: Flame speed, function of the stoechiometric ratio
5.3. Rupture hazards

5.3.3. Overpressure

5.3.3.1. Blast wave produced by a BLEVE

One of the main hazards of a BLEVE explosion is the generation of a blast wave. Overpressure measurements taken at 0.5 m and 0.6 m from the reservoir, and for two groove lengths ($2c = 0.04m$ and $2c = 0.01m$) are showed in Fig. 5.37 right for an overpressure measured at the side of the reservoir, and in Fig. 5.37 left for an overpressure measured at 0.28 m above the reservoir. The blast wave pressure at a fixed reference distance shows a first peak (A in Fig. 5.37), followed by a sharp drop with negative part, which is the typical shape of a blast wave, as explained in chapter 3 section 3.4.2.

![Figure 5.37: Overpressure signal: Left: above the reservoir, Right: at ground level, on the sides of the reservoir](image)

The blast wave generated by the reservoir rupture can also be observed on shadowgraph images (Fig. 5.38 and Fig. 5.39). The blast wave is spherical and appears ahead of the fluid cloud. The blast wave shows a higher contrast with the background in the upper part of the wave compared to the sides. This difference of contrast is directly linked to a stronger density gradient and therefore a stronger blast wave. As explained in the previous section, the cloud appears opaque in the shadowgraph, due to condensation. The value of the overpressure peak decreases with the distance from the reservoir as the blast is attenuated when traveling away from the reservoir (see Fig. 5.37). The value of the overpressure peak also increases when the groove length decreases. In section 5.2, it has been observed that when the groove length decreases, the rupture pressure increases (Fig. 5.16). Therefore, a higher rupture pressure increases the intensity of the generated blast wave.
Chapter 5. Experimental results

Figure 5.38: Shadowgraph visualization of the blast wave generated by the reservoir rupture, at 5 times indicated by letters A to E (see Fig. 5.37). Up: $t_c = 0.04 \text{ m}$, $P_{\text{rupt}} = 31 \text{ MPa}$. Down: $t_c = 0.01 \text{ m}$, $P_{\text{rupt}} = 44 \text{ MPa}$.
5.3. Rupture hazards

Figure 5.39.: Shadowgraph visualization of the blast wave generated by the reservoir rupture, for three different groove lengths.
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On the pressure signal measured above the reservoir, a second peak is observed (C in Fig. 5.37 left) for small groove lengths. This peak corresponds to the moment when the dome-like cloud touches the pressure sensor (which is located in the horizontal bar, as observed in Fig. 5.38). Finally, a last peak is visible both on the shadowgraph (E in Fig. 5.38) and on the pressure signals (E in Fig. 5.37), and observed after the rupture of NTG reservoirs with small groove lengths, corresponding to the apparition of the dome-like cloud. In the shadowgraph images (see Fig. 5.38), this second blast wave appears just after the cloud has disappeared, i.e. evaporated. And as the dome-like cloud vaporizes a lot faster than the blurry cloud, this fast vaporization seems to be the cause of the second blast wave generation.

The same image processing as performed for the cloud analysis has been applied on the shadowgraphs. In other words, the LEDAR software [106] has been used to detect the blast wave position in addition to the cloud upper boundary. A comparison between the radius evolution and the expansion velocity of both the cloud and the blast wave is illustrated in Fig. 5.40. The radius evolution of a spherical blast wave can be predicted by Eq. 5.7, where \( \alpha_1, \alpha_2, \alpha_3 \) and \( \alpha_4 \) are fitted parameters. The Eq. 5.7 has been developed by Sadek [116] and is a curve fit of experimental shock trajectory data of TNT, ANFO and propane-oxygen explosions. The quality of the fit using Eq. 5.7 is not guaranteed at higher distance than the measurements. But it can already be observed from the measured time interval that the blast wave travels faster and is much less slowed down than the cloud.

![Figure 5.40: Left: Cloud and blast wave radius. Right: Cloud and blast wave expanding velocity](image)

Figure 5.40.: Left: Cloud and blast wave radius. Right: Cloud and blast wave expanding velocity
5.3. Rupture hazards

\[ R_{\text{blast}}(t) = \frac{\alpha_1 \alpha_2 t^{\alpha_5}}{1 + \alpha_2 t^{\alpha_5}} + a_0 t + \alpha_3 \sqrt{\ln(1 + \alpha_4 t)} \]  

(5.7)

The fit of Sadek can also be used to calculate the time evolution of the overpressure generated by the reservoir rupture, by using one of the Rankine-Hugoniot equations expressed in Eq. 5.8. First, the blast wave velocity is derived from the blast wave radius. Then, the Mach number is obtained by dividing the blast wave velocity by the speed of sound of air at ambient temperature. Finally, the blast wave overpressure is calculated with the Rankine-Hugoniot equation that relates the blast overpressure \((P_s)\) to the Mach number \((M)\), as expressed in Eq. 5.8, where \(\gamma\) is the specific heat ratio of air.

\[ \frac{P_s}{P_{\text{atm}}} = \frac{2\gamma}{\gamma + 1} \left( M^2 - 1 \right) \]  

(5.8)

The comparison between the overpressure calculated with the Rankine-Hugoniot equation and the measured blast wave overpressure is showed in Fig. 5.41 for two groove lengths: \(2c = 0.040\,\text{m}\) and \(2c = 0.010\,\text{m}\), corresponding respectively to a blurry cloud and a dome-like cloud. The comparison shows a relatively good match with the overpressure measured above the reservoir. But the overpressures measured at the side of the reservoir, at further distance, are not fitting well the pressure derived from the Rankine-Hugoniot equation. Different explanations can be given to this difference. First, the fit following the equation of Sadek is not guaranteed at higher distances than the measured points (see the effect of changing by 5% the \(\alpha_i\) coefficients on the overpressure in appendix A section A.10). Second, the shadowgraph has showed that the blast wave seems to be higher on its upper part than on the side, but the directivity of the blast is not taken into account in the model of Rankine-Hugoniot.

A last explanation for the bad fit of the pressure calculated from Hugoniot with the measurements taken further from the reservoir is that the 3D effects of the propagation at these distances become non-negligible and are not taken into account by Hugoniot. But the Fig. 5.42 shows that the measurements at further distance from the reservoir are correctly fitted by assuming a spherical damping of the initial rupture pressure and volume: \(P_\text{rupt} \ast V/R^3\).

5.3.3.2. Comparison of overpressure models with experiments

In this section, the overpressure models defined in chapter 3 are compared with different scales of experiments, also resumed in chapter 3 section 3.4.2.1. The models are also compared with the present experimental results. Small scale is
Chapter 5. Experimental results

Figure 5.41.: Measured overpressure peak compared with overpressure derived from the blast wave radius: Left: $2c = 0.04\,m$, Right: $2c = 0.01\,m$, Temporal evolution

Figure 5.42.: Measured overpressure peak compared with overpressure derived from the blast wave radius: Left: $2c = 0.04\,m$, Right: $2c = 0.01\,m$, Spatial evolution

defined here as a size of reservoir that can be found in residential homes, with a maximal weight of several tens of kilograms. Large scale is similar to industrial containers, so with a fluid mass higher than a ton. And finally, mid-scale is lying in between. The different scales can also be expressed in terms of TNT equivalent mass; $m_{TNT} > 1$ for large scales, $m_{TNT} \approx 1$ for mid-scales and $m_{TNT} < 1$ for small scales. In addition, the experiments performed in this study are considered as a fourth scale, smaller than the small scale, that will be called lab scale.
5.3. Rupture hazards

Large scale experiments

As explained in the literature review (see chapter 3, section 3.4.2.1), two series of experiments have been performed at large scale: the experiments of Johnson and the test by the BAM.

Table 5.6.: NMSE validation factor of overpressure models compared with large scale experiments

<table>
<thead>
<tr>
<th></th>
<th>Prugh</th>
<th>Casal IS</th>
<th>Casal IR</th>
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<th>Roberts</th>
<th>Genova</th>
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<td>0.2857</td>
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</tbody>
</table>

Table 5.6 lists the NMSE validation factor, evaluated between the different model predictions and the measured overpressures (see appendix B, section B.2.1 for the definition). Each NMSE value stands for the different overpressures measured in one test. The general NMSE calculated from the entire measured points of the whole set of Johnson experiments is also presented. For the tests made by Johnson, the models of Casal with irreversible expansion, Genova and Birk show the best estimation with the data. For the BAM experiment, the prediction of Birk is not as good as Genova and Casal. Looking at the measurements scaled with the models of Casal and Genova (see Fig. 5.43), these models show a very slight underestimation for a few tests, that can be considered as acceptable. But if a fully conservative approach is preferred, the best model is Prugh.

Mid-scale experiments

The mid-scale experiments compared here with the overpressure models are the experiments of Birk, that are resumed in chapter 3.

The Table 5.7 presents, for each test, the NMSE validation factor between the different measurement points from the experiments of Birk performed in 2001-2002 (taken at 10-40m from the tank side, see chapter 3 for more details) and the model overpressure models. In addition, the NMSE of the whole test results is also presented. From Table 5.7 Casal assuming an irreversible expansion and Genova are the best models, considering that each model is associated with an uncertainty. But the possible underestimation of Casal is more pronounced in
Chapter 5. Experimental results

Figure 5.43: Comparison of large scale experiments with the models of Genova (left) and Casal with an irreversible expansion (right)

Table 5.7: NMSE validation factor of overpressure models compared with mid-scale experiments

<table>
<thead>
<tr>
<th>Model</th>
<th>Prugh</th>
<th>Casal IS</th>
<th>Casal IR</th>
<th>Planas</th>
<th>Roberts</th>
<th>Genova</th>
<th>TNO</th>
<th>Birk</th>
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<td>1.5772</td>
<td>0.3089</td>
<td>1.5329</td>
<td>0.7544</td>
</tr>
<tr>
<td>3</td>
<td>0.8625</td>
<td>0.1893</td>
<td>0.0951</td>
<td>0.3085</td>
<td>0.9728</td>
<td>0.0633</td>
<td>0.9105</td>
<td>0.924</td>
</tr>
<tr>
<td>4</td>
<td>1.348</td>
<td>0.5188</td>
<td>0.1266</td>
<td>0.5423</td>
<td>1.5547</td>
<td>0.1434</td>
<td>1.5037</td>
<td>1.1703</td>
</tr>
<tr>
<td>5</td>
<td>1.1312</td>
<td>0.2512</td>
<td>0.0482</td>
<td>0.4591</td>
<td>1.1768</td>
<td>0.0671</td>
<td>1.1021</td>
<td>1.2218</td>
</tr>
<tr>
<td>6</td>
<td>0.1187</td>
<td>0.0824</td>
<td>0.3453</td>
<td>0.112</td>
<td>0.1589</td>
<td>0.2502</td>
<td>0.1468</td>
<td>0.1646</td>
</tr>
<tr>
<td>7</td>
<td>1.5615</td>
<td>0.987</td>
<td>0.2812</td>
<td>0.7501</td>
<td>1.7221</td>
<td>0.4757</td>
<td>1.6554</td>
<td>0.4822</td>
</tr>
<tr>
<td>8</td>
<td>1.5137</td>
<td>1.077</td>
<td>0.3951</td>
<td>0.7512</td>
<td>1.7315</td>
<td>0.5163</td>
<td>1.6481</td>
<td>0.5681</td>
</tr>
<tr>
<td>9</td>
<td>0.9583</td>
<td>0.684</td>
<td>0.2174</td>
<td>0.4244</td>
<td>1.1828</td>
<td>0.2713</td>
<td>1.1311</td>
<td>0.2319</td>
</tr>
<tr>
<td>All</td>
<td>1.0201</td>
<td>0.5317</td>
<td>0.2403</td>
<td>0.4587</td>
<td>1.161</td>
<td>0.271</td>
<td>1.1088</td>
<td>0.6205</td>
</tr>
</tbody>
</table>

this scale, as shown in Fig. 5.44 left, where all measured points of a test are represented by their corresponding test number. Among the conservative models (Prugh, TNO, Roberts), Prugh is the one that gives the best agreement with the measurements (see Fig. 5.44 right).

Blast wave directivity

The overpressure, for the large and mid-scale experiments, has been measured at different directions from the reservoir, at equal distance from the reservoir. Both Birk and Johnson showed that the overpressure along the side of the reservoir (perpendicular to the reservoir longitudinal axis) is higher than the overpressure along the end of the reservoir (along the reservoir longitudinal axis), as observed in Fig.
5.3. Rupture hazards

Figure 5.44.: Comparison of mid-scale experiments with the models of Casal with an irreversible expansion (left) and Prugh (right)

Therefore, for a conservative approach, the overpressure measurement at the side have been used in the comparison with the overpressure models.

Figure 5.45.: Relationship between the overpressure measured at the tank sides and ends

From the experiments, when the shape of a reservoir gets away from the spherical shape, i.e. the ratio between the reservoir length and diameter increases, the directivity of the overpressure increases, as observed in Fig. 5.45 left. This effect of the reservoir geometry can be used to predict the end overpressure, by dividing the side overpressure by the ratio between half the reservoir length and diameter.
Chapter 5. Experimental results

difference \( ((L_{res} - D)/2) \) and the reservoir diameter, as observed in Fig. 5.45 right.

Small scale experiments

The small scale reservoirs are usually dedicated to a domestic use and therefore, are usually resistant to higher pressures than the large scale reservoirs, which causes the BLEVE to happen when the fluid is supercritical. In 2003, Stawczyk tested BLEVE phenomenon on 5 and 11 kg commercial propane reservoirs. The author has presented two blast wave signals recorded at 2 and 10 m from the source, with 11 kg bottle filled at 80\% \[128\]. As described before, supercritical BLEVE can only be modeled by Prugh, Casal, Birk and Genova so the experiments are only compared with these models.

![Figure 5.46: Comparison of overpressure models with the experiment of Stawczyk](128)

The different model estimations are compared with the measurements of Stawczyk in Fig. 5.46 and the Table 5.8 compares the discrepancy between models and experiments from Stawczyk \[128\], through the NMSE validation factor. The models that are estimating the best the measured overpressure are Prugh and Casal with an isentropic expansion. This conclusion differs greatly from the two larger scales so the lab scale analysis will help to better understand if this difference is due to the scale of the reservoir or to the large uncertainties of the measurements. Actually, the two measurement points of Stawczyk are related to two different experiments. So even if they have similar initial configuration, the rupture pressure could vary from one test to another. In addition, the fluid is not precisely written as the author tested both propane and propane-butane mixture. And finally, the rupture pressure related to the test were the overpressure was measured is not precisely given either. Stawczyk gave only the range of rupture pressure, from 7.5 to 12 MPa, and the higher rupture pressure (12 MPa) was used in the models.
5.3. Rupture hazards

Table 5.8.: NMSE validation factor of models compared with Stawczyk experiments

<table>
<thead>
<tr>
<th></th>
<th>Prugh</th>
<th>Casal IS</th>
<th>Casal IR</th>
<th>Genova</th>
<th>Birk</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1.0842</td>
<td>0.0879</td>
<td>0.3204</td>
<td>0.4143</td>
<td>0.6044</td>
</tr>
</tbody>
</table>

Lab scale experiments

In this section, only the measurements performed in this study are compared with the different overpressure models. The analysis is done in two parts, the rupture of NTG reservoirs and the rupture of the Archer reservoirs, since the fluid state at rupture is different in the two types of reservoir, which affect the expression of the overpressure models.

Concerning the rupture of the NTG reservoirs, the influence of the rupture pressure on the blast wave intensity measured at 0.5 m can be observed in Fig. 5.47. In this figure, the tests that ended in a blurry cloud (circular symbols in Fig. 5.47) are separated from the tests that ended in a dome-like cloud (square symbols in Fig. 5.47). All the models and experiments show a linear increase of the overpressure with the rupture pressure, but the Prugh model leads to a different slope compared to the other models. This is probably due to the assumption of ideal gas which is not postulated in the other models.

The Table 5.9 compares the discrepancy between models and experiments from the test performed with NTG and the Archer reservoirs, through the NMSE vali-
Chapter 5. Experimental results

Table 5.9.: NMSE validation factor of models compared with lab scale experiments

<table>
<thead>
<tr>
<th>Prugh</th>
<th>Casal IS</th>
<th>Casal IR</th>
<th>Genova</th>
<th>Birk*</th>
</tr>
</thead>
<tbody>
<tr>
<td>All</td>
<td>1.597</td>
<td>0.4868</td>
<td>0.1995</td>
<td>0.2588</td>
</tr>
<tr>
<td>BABELs experiments NTG reservoirs</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1.</td>
<td>0.221</td>
<td>0.05</td>
<td>0.4942</td>
<td>0.8368</td>
</tr>
<tr>
<td>2.</td>
<td>0.9044</td>
<td>0.1232</td>
<td>0.0405</td>
<td>0.0931</td>
</tr>
<tr>
<td>3.</td>
<td>2.9413</td>
<td>1.1566</td>
<td>0.29</td>
<td>0.2624</td>
</tr>
<tr>
<td>4.</td>
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<td>0.5648</td>
<td>0.052</td>
<td>0.019</td>
</tr>
<tr>
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<td>2.1735</td>
<td>0.6324</td>
<td>0.0837</td>
<td>0.0472</td>
</tr>
<tr>
<td>6.</td>
<td>0.5461</td>
<td>0.0212</td>
<td>0.1905</td>
<td>0.3647</td>
</tr>
<tr>
<td>7.</td>
<td>0.8542</td>
<td>0.0732</td>
<td>0.1286</td>
<td>0.259</td>
</tr>
<tr>
<td>8.</td>
<td>2.0757</td>
<td>0.651</td>
<td>0.0788</td>
<td>0.0335</td>
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<tr>
<td>9.</td>
<td>0.3274</td>
<td>0.0339</td>
<td>0.3599</td>
<td>0.6215</td>
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<td>10.</td>
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<td>0.7502</td>
<td>0.1319</td>
<td>0.0902</td>
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<tr>
<td>11.</td>
<td>3.3072</td>
<td>1.2978</td>
<td>0.3488</td>
<td>0.2195</td>
</tr>
<tr>
<td>All</td>
<td>1.597</td>
<td>0.4868</td>
<td>0.1995</td>
<td>0.2588</td>
</tr>
<tr>
<td>BABELs experiments Archer reservoirs</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1.</td>
<td>1.3991</td>
<td>2.3047</td>
<td>1.0258</td>
<td>0.598</td>
</tr>
<tr>
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<td>0.5082</td>
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<tr>
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<td>0.4014</td>
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<tr>
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<td>1.4329</td>
<td>1.653</td>
<td>0.6447</td>
<td>0.3608</td>
</tr>
<tr>
<td>All</td>
<td>1.2708</td>
<td>1.9314</td>
<td>0.8077</td>
<td>0.4671</td>
</tr>
</tbody>
</table>

* TNO for Archer reservoirs

dation factor. For these lab scale experiments, even if the fluid state prior rupture differs from the other scales of experiments, the models of Casal with an irreversible expansion and Genova show the best agreement with data. But they also underestimate a part of the measurements. Among the conservative approaches, Prugh do not underestimate any tests, as observed in Fig. 5.48.

Among the lab scale experiments performed in this study, the experiments with Archer reservoir showed (in section 5.2) that the fluid state prior rupture is a compressed fluid (with no vapor anymore). The models that can predict the overpressure generated by a compressed fluid are Prugh, Casal, Genova and the TNO, taking only the expansion energy of the liquid. The Table 5.8 compares the discrepancy between models and experiments with the Archer experiments, through the NMSE validation factor. For this type of rupture, all the models are overestimating the generated overpressure, and Genova shows the best agreement with data, as observed in Fig. 5.49.

Conclusions of the multi-scale comparison

From the different scales, Genova and Casal with an irreversible expansion are the two models that are fitting the best the measured overpressure, whatever the scale.
5.3. Rupture hazards

Figure 5.48.: Comparison of lab scale experiments (NTG reservoirs) with the models of Casal with an irreversible expansion (left) and Prugh (right)

Figure 5.49.: Comparison of lab scale experiments (Archer reservoirs) with the models of Casal with irreversible expansion

of the reservoir or the fluid state prior rupture, as observed in Fig. 5.50 left. But the models of Genova and Casal IR are for some experiments underestimating the predicted overpressure. Therefore, the model that fits the best the experiments in a conservative approach is Prugh (see Fig 5.50). But the model of Prugh is based on the assumption of an ideal gas, which is a bit too ideal. Therefore, Birk can be used for a conservative approach. But Birk cannot estimate the overpressure for a compressed fluid since the model is only based on the vapor phase. Therefore, even if the assumptions of Prugh are a bit too ideal, the model can be used with all reservoir scales and fluid states. These models are used to estimate the side
overpressure; the end overpressure linearly depends on the side overpressure and the reservoir length to diameter ratio.

![Graph showing experimental results and model comparison](image-url)

**Figure 5.50.:** Comparison of all the experiments with the models of Casal with an irreversible expansion (left) and Prugh (right)

### 5.4. Conclusion

The presentation of the experimental results and their analysis was structured in three parts: the period from the beginning of the thermal aggression to the reservoir rupture, the reservoir rupture and the BLEVE hazards.

The analysis of the temperature evolution of the reservoir has showed a good reproducibility of the experiments, and that the time to rupture depends linearly on the power of the heat source. A 1D thermal model has been developed, able to reproduce the temperature evolution of the reservoir whatever the power of the heat source. Then, the analysis of the measured internal pressure has shown that the reservoir heating can be divided in two steps. During the first step, the internal pressure and temperature follow the saturation line of the fluid. During the second step, the internal pressure and temperature increases quasi-linearly as the specific volume, calculated by dividing the volume of the reservoir by the total mass, is fixed.

When the reservoir ruptured, it started to open from the notch position, and propagated along the notch, opening the reservoir without any fragment generation. The rupture pressure of a cylindrical reservoir has been observed to be modeled
5.4. Conclusion

between the model of Zhu with ASSY criterion and the model of Xue $P_{max}$. If a notch is present on the cylindrical reservoir, the local and global estimates of Staat proved to give the best pressure range estimate.

Once the reservoir has opened, the fluid is ejected to the ambient environment. In the measurements with Archer reservoirs, the fluid state at rupture is a compressed fluid, and is expelled as a subsonic core of tiny droplets, with surrounding droplets. In the measurements with NTG reservoirs, the fluid state at rupture is supercritical. Two cloud shapes were observed. A cloud with blurry contours was observed when the entropy at rupture was smaller than the critical entropy, which is experienced by reservoirs with long groove lengths and therefore smaller rupture pressures. A dome-like cloud was observed when the entropy at rupture was larger than the critical one, experienced by reservoirs with small groove lengths and higher rupture pressures.

The cloud did not form a fireball, but ignited in a few case, from the plate under the reservoir, forming a small deflagration. If a spark was present during the experiment, the cloud ignited in all tests, and also formed a small deflagration.

Finally, overpressure was measured at different distances from the reservoir. A first peak was observed, corresponding to the initial blast wave that followed the reservoir rupture. In the dome-like cloud, a second blast wave was observed after vaporization of the cloud. After a multi-scale comparison, the models of Casal with an irreversible expansion or Genova are the two best estimations of the overpressure generated by a BLEVE, but Prugh is better suited for a conservative approach. A directivity of the overpressure is observed in cylindrical reservoirs. The side overpressure is modeled through the overpressure models, and the end overpressure is directly calculated from the side value and from the geometrical parameters of the reservoir.
Chapter 6.

Application of models to Large scale BLEVE

The objective of this chapter is to apply the models and correlations described in the bibliography (chapter [3]) and compared with experiments in the chapter [5]. Among the large scale experiments, only the tests of BAM can be used here as for the experiments of Johnson, the rupture pressure was chosen. The methodology followed in this chapter is similar to the one described in the introduction (in chapter [2]) and in the presentation of the results, and is divided in two parts: the BLEVE apparition conditions and the BLEVE hazards.

6.1. BAM large scale experiment

In 1999, the federal institute of material research and testing (BAM) in Germany performed a BLEVE test with a 45$\text{m}^3$ reservoir [82], filled at 20% with propane. Under the reservoir, a 10 m x 5 m x 0.5 m was poured with fuel oil, as observed in Fig. 6.1. The smaller yellow reservoir was a previously unused container for radioactive material, used only for a better understanding of the fire effect on that type of reservoir. At first, three plastic bowls with petrol were ignited. After 100s, the plastic bowls were destroyed and the petrol poured on the fuel that ignited. After 130s, the reservoir internal pressure started to rise and continued for 15 min until the rupture of the reservoir. The fuel tank tore open at the rear, generating 5 fragments. The liquid gas that was released evaporated and burned off in a fireball.

In this large scale experiment, temperature, internal pressure, radiation and over-pressure were measured, in addition of different angles of view of visualization. The temperature measurement consisted on K type thermocouples: 10 for the fire temperature, 11 for the wall temperature and 4 internal temperatures. The internal pressure was measured at three positions with P8AP 10 MPa sensors by HBM. The blast wave generated by the reservoir rupture was measured with three PCB 137A23, these were set up radially at distances of 100, 150 and 200 m from the tank. Finally, two sensors of the type Medtherm 64 were used to determine the heat flux of the fireball. The table 6.1 resumes all the parameters of this large scale experiment, that will be used in this chapter.
Chapter 6. Application of models to Large scale BLEVE

Figure 6.1.: Propane reservoir and pool fire, before the experiment [82]

Table 6.1.: BAM large scale experiment characteristics [82, 7]

<table>
<thead>
<tr>
<th>Reservoir</th>
<th>( \sigma_{ys} ) 420 N/m²</th>
<th>( \sigma_{ult} ) 550-660 N/m²</th>
<th>d 15 mm</th>
<th>D 2.9 m</th>
<th>L 7.6 m</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluid</td>
<td>Fluid type Liquid fill</td>
<td>propane</td>
<td>m</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Rupture</td>
<td>( P_{rupt} ) 2.5 MPa</td>
<td>( T_{rupt} ) 342 K</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Overpressure</td>
<td>100 m</td>
<td>150 m</td>
<td>200 m</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>2.5 kPa</td>
<td>1.4 kPa</td>
<td>1.2 kPa</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fireball</td>
<td>diameter 100 m</td>
<td>height 150 m</td>
<td>duration 7.2 s</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fragments</td>
<td>Number 4 (+1)</td>
<td>Distance 130 m, 155 m, 150 m, 200 m (145 m)</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

6.2. BLEVE apparition conditions

As already explained in the introduction, the methodology followed in this thesis to better understand and predict the BLEVE apparition conditions is schematized in Fig. 6.2. At first, the reservoir dimensions and shape are used to determine the rupture pressure. This pressure, in combination with the rules of thermodynamics, can allow to determine the rupture temperature. And finally, to determine the time to rupture, a heat model is needed.
6.2. BLEVE apparition conditions

Figure 6.2.: Methodology for BLEVE apparition conditions.

6.2.1. Modeling the rupture pressure $P_{\text{rupt}}$

In this study, the rupture pressure of a cylindrical reservoir was studied without any effect of the temperature on the material properties. The bibliography of chapter 3 section 3.4.1 has resumed the different correlations available in the literature to predict the limit load of a reservoir, and the chapter 5 section 5.2 compared them with different experiments from literature, in addition to the limit load of the NTG reservoir. This comparison showed that three models are estimating better the limit load: the correlation of Zhu with the average shear stress yield (ASSY) theory, the model of Xue, with the maximum pressure, and the model of Zhu with von Mises criterion.

<table>
<thead>
<tr>
<th>Temperature</th>
<th>Material properties</th>
<th>Zhu ASSY</th>
<th>Zhu von Mises</th>
<th>Xue</th>
<th>$P_{\text{max}}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>342 K</td>
<td>$\sigma_{ys} = 420$, $\sigma_{ult} = 600$</td>
<td>6.692</td>
<td>6.186</td>
<td>7.204</td>
<td></td>
</tr>
<tr>
<td>670 K</td>
<td>$\sigma_{ys} = 210$, $\sigma_{ult} = 300$</td>
<td>3.346</td>
<td>3.093</td>
<td>3.602</td>
<td></td>
</tr>
<tr>
<td>870 K</td>
<td>$\sigma_{ys} = 150$, $\sigma_{ult} = 215$</td>
<td>2.397</td>
<td>2.215</td>
<td>2.582</td>
<td></td>
</tr>
</tbody>
</table>

The three correlations are first compared, by using the material properties given by the authors [82] and are displayed in the first line of the Table 6.2. It can be observed that the three estimations are strongly overestimating the rupture pressure. But in the presentation of the results, the authors also give yield strengths at two other temperatures: $\sigma_{ys} = 210 M\text{Pa}$ at 670 K and $\sigma_{ys} = 150 M\text{Pa}$ at 870 K. And as the wall temperatures at rupture were in the range of 370 – 870 K, the influence of the heat on the rupture pressure should not be neglected. In addition, if the smaller values of the yield strength are used and the ultimate strength is calculated by keeping the ratio $\sigma_{ult}/\sigma_{ys}$ similar to the original, the Table 6.2 shows that the material properties at 870 K give a correct estimation of the rupture pressure, which was measured as 2.5 MPa.

To conclude on the rupture pressure estimation, the methodology of using correlations for the limit load of a cylindrical reservoir is a correct approach if the
Chapter 6. Application of models to Large scale BLEVE

material properties are known, in addition to the influence of heat on these properties. This influence has not been studied in this thesis and the publications about them in literature are difficult to find. Therefore, this subject should be studied more deeply for a better understanding of BLEVE rupture.

6.2.2. Modeling of the rupture Temperature $T_{rupt}$

In this study, the rupture pressure and temperature experienced by previous experiments in the literature, as shown in chapter 5 section 3.3 and the analysis of the pressure - temperature evolutions measured in the present study (see chapter 5 section 5.1.2), showed that for a rupture pressure below the critical pressure of the fluid, the fluid follows the saturation line of the fluid. Therefore, the rupture fluid temperature lies on the saturation line. The Figure 6.3 shows the measured internal pressure - temperature evolution. This evolution follows almost perfectly the saturation line of propane. The rupture temperature was measured equal to 342 K, and the temperature interpolated from the saturation line at 2.5 MPa is 341.4 K.

![Propane saturation curve with measurements](image)

Figure 6.3.: Internal pressure - temperature evolution in BAM experiment [82]

To conclude on the rupture temperature, the modeling approach proposed in chapter 5 section 5.1.2 is valid at all scales and can be used to estimate the rupture fluid temperature based on the rupture pressure.
6.3. BLEVE hazards

As already explained in chapter 2 and 3, a BLEVE has three main hazards: blast wave, fragments and fireball is the liquid is flammable. To model these three hazards, the initial parameters of the reservoir (dimensions, fluid type, etc..) but also the rupture conditions need to be known. This section divides the three hazards in three different subsections.

![Methodology for the BLEVE consequences](image)

6.3.1. BLEVE overpressure

As detailed in the chapter 3 section 3.4.2.1 the modeling of the blast wave over-pressure is focusing on the peak value of the overpressure. A series of overpressure models have been published in literature, and have been compared in chapter 5 section 5.3.3. The comparison of the different models with experiments at different scales showed that the models of Casal with an irreversible expansion and Genova are estimating the overpressure with the smallest error [21], and that the model of Prugh was the model to use in a conservative approach.

The application of these three models is compared with the three overpressure measurements in Fig. 6.5. The models of Casal with an irreversible expansion and Genova predict well the measured overpressures. The model of Prugh is indeed conservative, but considerably overestimates the measurements. Therefore, the model of Casal with an irreversible expansion or Genova allow a good estimation of the overpressures.

6.3.2. BLEVE fireball

From the literature survey about fireball models (in chapter 3 section 3.4.3.2), the dynamic model of Martinsen has showed a good qualitative estimation of the fireball geometrical and radiative properties, with an easy implementation and
Chapter 6. Application of models to Large scale BLEVE

Figure 6.5.: Overpressure models of Casal with irreversible expansion [21], Genova [45] and Prugh [108] compared with the overpressure measured after the BAM rupture [82]

taking into account the temporal evolution of the fireball. This model has been applied for the BAM experiment, involving 5 t of propane. The model gives a final diameter of 99.3 m as the fireball diameter from the BAM experiment has been estimated approximately to 100m. The total duration of the fireball is estimated by Martinsen as 7.56 s while the measured duration of the fireball is approximately 7.2 s. The model of Martinsen estimates well the two main parameters of the fireball.

The BAM experimentation report shows the time evolution of the fireball with pictures separated by 0.4 s, as showed in Fig. 4.1. A time evolution of the fireball diameter and height can then be retrieved, except that a spatial calibration of the field of view is not available. The calibration is then done a posteriori, by assuming that the largest fireball diameter is 100 m, as detailed in the BAM report [82]. The Figure 6.6 shows the comparison of the fireball diameter and height time evolution with the model of Martinsen. The diameter evolution is very well modeled by Martinsen, both for the initial increase and for the plateau value. The height evolution is correctly modeled at the beginning of the fireball lifetime, and the rate of increase of height is also correctly modeled. But the fireball starts to increase a bit later in the experiments than in the model of Martinsen.
6.3. BLEVE hazards

6.3.3. BLEVE fragments

At rupture, the reservoir started to fail approximately at 0.5 m left from the middle of the tank. The tank generated 4 fragments, in addition to a baffle plate that was located inside the reservoir. According to Gubinelli (see Fig. 3.12 of chapter 3), 30% of the analyzed accident generated 4 fragments, which is the second most probable fragment number, after 2 fragments. Holden and Reeves showed a smaller percentage of accident that generated 4 fragments, but this pattern is among the most probable ones also for them. Concerning the rupture pattern, the reservoir fragments consist of the two ends of the reservoir, and the shell that has been broken in two parts. This pattern corresponds to the pattern called CV7 by Gubinelli [49] (see Fig. 5.20 of chapter 5), and observed in almost 30% of the accidents.

The direction of the fragment is not precisely given in the report, but the distances between each fragment and the initial position of the reservoir has been measured. The two ends traveled up to 130 m and 155 m, and the two parts of the shell to 150 m and 200 m. The baffle plate traveled up to 145 m. Therefore, all the fragments traveled to a distance smaller than 5 fireball radii (the measured fireball radius was 50 m). In the Fig. 3.13 right of chapter 3, the probability of fragments traveling at a distance further than 5 fireball radii is about 5 %, which is consistent with the experimental results. But the shape of the curve presented by Birk in Fig. 3.13 right is not consistent with the experimental results, since all the fragments traveled a distance of 3 to 4 fireball radii. More precise estimation of the traveled distance based on ballistic equations can be found in the literature, like...
Chapter 6. Application of models to Large scale BLEVE

Hauptmanns [57] and Gubinelli [48], but this study did not focus on the fragment estimation, therefore the analysis here is kept simple.

6.4. Conclusion

The objective of this chapter was to apply the main conclusions drawn from the small scale BLEVE experiments performed in this study, to a large scale BLEVE.

In terms of BLEVE apparition conditions, the rupture pressure can be estimated if the mechanical properties of the reservoir are known. The effect of heat on these properties should also be known. The rupture temperature is easily deduced from the thermophysical properties of the fluid, once the rupture pressure is known.

In terms of BLEVE hazards, the BLEVE overpressure can correctly be estimated by the models of Casal with irreversible expansion or Genova. The fragment number is among one of the most probable, and the maximum distance is consistent with the observations of Birk. Finally, if the liquid is flammable, the model of Martinsen can correctly estimate the evolution of the fireball dimensions.

This thesis provides then a good modeling approach for the BLEVE apparition conditions and consequences. Nevertheless, further studies should focus on the thermal evolution of material properties and on the development of a more general thermal model.
Chapter 7.

Conclusion and recommendations

7.1. BLEVE modeling

The objective of the BLEVE part was to contribute to the understanding, the modeling and the validation of BLEVE apparition and consequences. Therefore, a literature review has been performed on the phenomenon to better understand what aspects of the BLEVE were not yet completely understood or modeled. Then, small scale experiments have been performed with butane and propane reservoirs. The BLEVE scenario studied here involves a cylindrical reservoir subjected to a thermal load. Finally, these experiments have been compared with larger scales of experiments and with models available in the literature and the modeling approach was applied to a large scale BLEVE.

When a reservoir containing a fluid is subjected to a heat aggression, the time evolution of the fluid temperature strongly depends on the configuration of the reservoir and on the type of thermal aggression. This study has shown that the heat source intensity is only influencing the reservoir heating by changing the time to rupture, providing that the metal properties are not affected. But the rupture conditions are not affected. A model for the fluid temperature has been developed in this study, but is only valid for the tested configuration: a reservoir heated by a microheater. The time evolution of the fluid pressure is directly linked to the fluid temperature. At the beginning of the fluid heating, the fluid pressure and temperature are both following the saturation line. And when the specific volume of the liquid reaches a given value defined as the volume of the reservoir divided by the total fluid mass, the pressure - temperature evolution becomes quasi-linear until rupture. The pressure evolution can then be easily modeled by knowing only the fluid type and mass, and the reservoir volume. This model proved to be valid whatever the scale of the reservoir.

When the internal pressure reaches the reservoir limit load, the reservoir ruptures. In this study, the rupture temperature range was too low to affect the mechanical properties of the reservoir material. Therefore, the rupture pressure can be modeled by calculating the limit load of a mild-steel cylindrical reservoir. The models of Zhu following the average shear stress yield (ASSY) theory and
Chapter 7. Conclusion and recommendations

the maximum pressure of Xue [144] can be used, giving lower and upper bounds for the modeling of the reservoir rupture pressure. At larger scale, this modeling approach is still valid, if the influence of heat on the mechanical properties of the reservoir is included. If the reservoir is weakened by an axial defect, as it has been the case in this study, the global and local approaches of Staat [127] with von Mises criterion are recommended to model the rupture pressure. But it has been showed that these models becomes very sensitive when the notch is deep and/or long, making a correct prediction more difficult.

Once the reservoir opens, the fluid is ejected into the atmosphere. The ejected fluid pattern depends on the state of the fluid prior rupture. In the present experiments, the fluid state prior rupture is not following the saturation line like previous studies about BLEVE experiments at larger scales. For the Archer reservoir, the fluid is in the state of a compressed fluid, with a supercritical pressure but a sub-critical temperature. If the expansion of the fluid content to atmosphere is assumed to be isentropic, the fluid falls in the two-phase region after depressurization and therefore forms a liquid core surrounded by a cloud of droplets. The liquid core length has been calculated to be in accordance with the results of Vieira performed with iso-octane [137].

For the NTG reservoirs, the fluid state prior rupture is supercritical so there is no distinction anymore between the liquid and vapor phase. Once in the atmosphere, the fluid state falls in the two phase region, if an isentropic expansion is assumed. A cloud of small droplets is therefore observed, as the fluid condenses when leaving the reservoir. Two types of cloud shapes have been observed. A blurry cloud is formed when the fluid falls after expansion in the two-phase region, with a quality closer to the liquid state. If the quality is closer to the vapor state, the cloud is observed with a smooth dome. An explanation for the dome-like cloud shape is that, as the condensation is reduced (because the fluid quality after expansion is closer to the vapor state), the shock structure of the supersonic underexpanded jet, as described by Lamanna, [77] becomes visible.

In the present experiments, the ejected fluid rarely ignites and when it ignites, it is after the fluid ejection, leading to a small deflagration. If a spark is produced after the reservoir rupture, the cloud ignites in every test, leading also to a deflagration. The propagation velocity of the flame is typical of a deflagration. In addition, the propagation velocity can be increased by the turbulence generated by the fluid ejection. At larger scale, the ejected fluid can lead to a fireball. The time evolution of the fireball diameter and height can be correctly predicted by Martinsen [84].

At the reservoir rupture, a blast wave is produced. The blast wave travels faster than the cloud and its velocity is less attenuated with time. A second blast wave has been observed in the case of a dome-like cloud, that is linked to the fast vaporization of the cloud. The blast wave has a spherical shape. In addition, large scale experiments from literature [68, 12] have showed that the blast wave intensity measured at the sides of the reservoir is higher than the blast at the ends of the
7.1. BLEVE modeling

reservoir, and that both intensities are correlated. A wide quantity of models for the first peak overpressure at ground level are available in the literature. But all the models have been developed for a rupture of a fluid following the saturation line. Therefore, the overpressure models have been adapted to compressed and supercritical fluids. When compared with different scales of experiments (with the side overpressure measurements for a conservative approach), the models of Casal with an irreversible expansion [21] and Genova [45] are fitting the best the experiments. But they underestimate the overpressure in several cases and therefore, if a conservative estimation is needed, the model of Prugh [108] is preferred.

For the small scale experiments performed in this study, at rupture, the reservoir opened from the defect and did not generated any fragment. But a rupture generating only one fragment is among the main rupture patterns following a BLEVE, according to Gubinelli [49]. In addition, the rupture pattern dimensions depend both on the rupture pressure and on the strength of the reservoir fixation. But as the experiments did not generate fragments, this study did not strongly focused on the fragment modeling.

To resume all these conclusions, the methodology presented in the introduction of chapter 2 is displayed again here, but completed with the different models that were either developed or chosen from the literature after comparison with experiments. The BLEVE apparition conditions methodology is represented in Fig. 7.1 while the BLEVE consequences methodology is represented in Fig. 7.2. For this model to be totally complete, future studies should focus on a general thermal model that could work for all types of heat source and reservoir scales, and consequently, on the evolution of the mechanical properties of materials with temperature.
Chapter 7. Conclusion and recommendations

Figure 7.2.: Methodology for the BLEVE consequences

7.2. Recommendations and perspectives

From this study, the first recommendation would be to use the different models presented here, that can give a first idea of the rupture conditions and consequences of a BLEVE, for a given reservoir configuration. The modeling approach presented in chapter 2 proved in chapter 6 to be efficient also at large scale. First, the rupture pressure can be evaluated if the reservoir dimensions and material are known. From the rupture pressure, the state of the fluid at rupture (sub or supercritical) but also the temperature can be determined through the use of thermophysical database if the fluid type and mass are known. The temperature at rupture can give an idea if the properties of the material could be affected by the thermal aggression or not, and if the rupture pressure has to be adapted accordingly. The rupture pressure, the reservoir dimensions and the fluid type and mass can be used to determine the peak overpressure at a given distance.

To improve this model, a more general temperature model should be developed. The development of such a model would require a specific study on the influence of the type of heat source and its position on the reservoir heating. Another improvement would be to include the modeling of the fragment projection. This would also require new experiments, focused in these aspects.

In addition to the BLEVE modeling, some recommendations can be proposed, based on the analysis of the experimental results. First, it has been observed that even if the cloud created by the fluid ejection does not ignite, it can be ignited after the rupture if a spark or another kind of ignition source is present. Therefore, limiting the presence of potential ignition source close to the reservoirs can allow to avoid the generation of a vapor cloud explosion. Then, it has been showed that the presence of a defect on the reservoir makes the rupture pattern predictable and decreases the rupture pressure. Therefore, drilling a known defect on the
reservoir allows a better estimation of the rupture and therefore of the fragment projection. The reservoir rupture has also been shown to depend on the way the reservoir is fixed. The reservoir fixations should also be taken into account. And finally, the blast wave has shown a directivity effect, created by the cylindrical shape of the reservoir. Therefore, the orientation of the reservoir has to be chosen in order to have the facilities that need to be protected located preferably on the ends of the reservoir where the overpressure is smaller than on the sides where the overpressure is larger.
Part II.

Boilover
## Nomenclature

### Roman Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Units</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$A$</td>
<td>$[m^2]$</td>
<td>Area</td>
</tr>
<tr>
<td>$Bi$</td>
<td>$[-]$</td>
<td>Biot number</td>
</tr>
<tr>
<td>$C_p$</td>
<td>$[kJ/K]$</td>
<td>Specific heat</td>
</tr>
<tr>
<td>$C_1$</td>
<td>$[-]$</td>
<td>Dimensionless effective soot concentration</td>
</tr>
<tr>
<td>$C_2$</td>
<td>$[mK]$</td>
<td>Plank’s second constant</td>
</tr>
<tr>
<td>$c_1$</td>
<td>$[W/mK]$</td>
<td>Conduction heat transfer term in Eq. 9.2</td>
</tr>
<tr>
<td>$c_2$</td>
<td>$[W/m^2K]$</td>
<td>Convective heat transfer term in Eq. 9.2</td>
</tr>
<tr>
<td>$c_3$</td>
<td>$[W/m^2K^4]$</td>
<td>Radiative heat transfer term in Eq. 9.2</td>
</tr>
<tr>
<td>Corr</td>
<td>$[-]$</td>
<td>Cross-correlation coefficient</td>
</tr>
<tr>
<td>Cov</td>
<td>$[-]$</td>
<td>Covariance</td>
</tr>
<tr>
<td>$D$</td>
<td>$[m]$</td>
<td>Diameter</td>
</tr>
<tr>
<td>$F$</td>
<td>$[-]$</td>
<td>View factor</td>
</tr>
<tr>
<td>$F_{boil}$</td>
<td>$[-]$</td>
<td>Criterion for boilover apparition</td>
</tr>
<tr>
<td>$Fr$</td>
<td>$[-]$</td>
<td>Froude number</td>
</tr>
<tr>
<td>$Fo$</td>
<td>$[-]$</td>
<td>Fourrier number</td>
</tr>
<tr>
<td>$f$</td>
<td>$[Hz]$</td>
<td>Flame Frequency</td>
</tr>
<tr>
<td>$g$</td>
<td>$[m/s^2]$</td>
<td>Gravity</td>
</tr>
<tr>
<td>$H$</td>
<td>$[m]$</td>
<td>Height</td>
</tr>
<tr>
<td>$H_u$</td>
<td>$[-]$</td>
<td>Humidity</td>
</tr>
<tr>
<td>$h$</td>
<td>$[W/m^2K]$</td>
<td>Convective heat transfer coefficient</td>
</tr>
<tr>
<td>$I$</td>
<td>$[-]$</td>
<td>Intermittency</td>
</tr>
<tr>
<td>$It$</td>
<td>$[-]$</td>
<td>Boilover intensity</td>
</tr>
<tr>
<td>$k$</td>
<td>$[m^{-1}]$</td>
<td>Extinction coefficient</td>
</tr>
<tr>
<td>$L$</td>
<td>$[m]$</td>
<td>Length</td>
</tr>
<tr>
<td>$L_p$</td>
<td>$[m]$</td>
<td>Path Length, mean beam length</td>
</tr>
<tr>
<td>$M$</td>
<td>$[m/s]$</td>
<td>Velocity magnitude</td>
</tr>
<tr>
<td>$m$</td>
<td>$[kg]$</td>
<td>Mass</td>
</tr>
<tr>
<td>$m_t$</td>
<td>$[kg/m^2s]$</td>
<td>Mass burning rate</td>
</tr>
<tr>
<td>$N$</td>
<td>$[-]$</td>
<td>Number of nodes</td>
</tr>
<tr>
<td>$n$</td>
<td>$[-]$</td>
<td>Infrared averaged optical constant</td>
</tr>
<tr>
<td>$pL$</td>
<td>$[Pa \cdot m]$</td>
<td>Path length</td>
</tr>
<tr>
<td>$P$</td>
<td>$[Pa]$</td>
<td>Pressure</td>
</tr>
</tbody>
</table>
Chapter 7. Conclusion and recommendations

\[ \dot{Q}_D \] kW Heat release rate
\[ \dot{Q}_F \] kW/m² Heat flux transferred from the flame to the fuel surface
\[ \dot{q}_r \] kW/m² Radiation received at distance r from pool fire
\[ \dot{q}_{r0} \] kW/m² Radiation heat flux at fuel surface
\[ \dot{q} \] kW/m² Radiation heat flux
\[ R \] m Radius
\[ r \] m Distance from source
\[ s \] [ ] Infrared averaged optical constant
\[ s_v \] [ ] Soot volume fraction
\[ SEP \] kW/m² Surface emissive power
\[ St \] [ ] Strouhal number
\[ T \] [K] Temperature
\[ t \] [s] Time
\[ U \] [m/s] Horizontal velocity
\[ u \] [m/s] Wind velocity
\[ u_c \] [m/s] Characteristic burning rate
\[ V \] [m³/s] Volumetric flow rate
\[ V \] [m/s] Vertical velocity
\[ V_a \] [m/s] Velocity of convective currents
\[ Vol \] [m³] Volume
\[ x \] [m] Vertical coordinate
\[ x_{lum} \] [ ] Luminous fraction of flame
\[ Z \] [ ] Non-dimensional number in Eq. B.18

Greek symbols

<table>
<thead>
<tr>
<th>Symbols</th>
<th>Units</th>
<th>Description</th>
</tr>
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<tbody>
<tr>
<td>( \alpha )</td>
<td>[m²/s]</td>
<td>Thermal diffusivity</td>
</tr>
<tr>
<td>( \beta )</td>
<td>[ ]</td>
<td>Radiative fraction</td>
</tr>
<tr>
<td>( \Delta H_c )</td>
<td>[kJ/kg]</td>
<td>Heat of combustion</td>
</tr>
<tr>
<td>( \Delta H_v )</td>
<td>[kJ/kg]</td>
<td>Heat of vaporization</td>
</tr>
<tr>
<td>( \Delta T )</td>
<td>[K]</td>
<td>Boiling temperature range</td>
</tr>
<tr>
<td>( \Delta t )</td>
<td>[ms]</td>
<td>Time interval</td>
</tr>
<tr>
<td>( \Delta y )</td>
<td>[m]</td>
<td>Spatial resolution</td>
</tr>
<tr>
<td>( \epsilon )</td>
<td>[ ]</td>
<td>Emissivity</td>
</tr>
<tr>
<td>( \eta )</td>
<td>[ ]</td>
<td>Constant in Eq. 9.4</td>
</tr>
<tr>
<td>( \gamma )</td>
<td>[ ]</td>
<td>Ratio of specific heats</td>
</tr>
<tr>
<td>( \kappa )</td>
<td>[ ]</td>
<td>Effective soot emission parameter</td>
</tr>
<tr>
<td>( \Lambda )</td>
<td>[mm/m²]</td>
<td>Initial fuel layer and pool area ratio</td>
</tr>
<tr>
<td>( \lambda )</td>
<td>[W/mK]</td>
<td>Thermal conductivity</td>
</tr>
<tr>
<td>( \mu )</td>
<td>[m⁻¹]</td>
<td>Average absorption coefficient</td>
</tr>
</tbody>
</table>
7.2. Recommendations and perspectives

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\nu$</td>
<td>Kinematic viscosity</td>
</tr>
<tr>
<td>$\phi$</td>
<td>Stoechiometric mass ratio of air to volatile</td>
</tr>
<tr>
<td>$\rho$</td>
<td>Density</td>
</tr>
<tr>
<td>$\sigma$</td>
<td>Stephan-Boltzmann constant</td>
</tr>
<tr>
<td>$\tau$</td>
<td>Transmissivity</td>
</tr>
<tr>
<td>$\chi$</td>
<td>Pre-boilover burnt mass ratio</td>
</tr>
<tr>
<td>$\omega$</td>
<td>Vorticity</td>
</tr>
<tr>
<td>$\Omega$</td>
<td>Velocity differences</td>
</tr>
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### Subscripts and superscripts

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>*</td>
<td>Non-dimensional</td>
</tr>
<tr>
<td>0</td>
<td>Before pool fire ignition</td>
</tr>
<tr>
<td>a</td>
<td>Air</td>
</tr>
<tr>
<td>atm</td>
<td>Ambient</td>
</tr>
<tr>
<td>b</td>
<td>At the boiling point</td>
</tr>
<tr>
<td>bo</td>
<td>Boilover (period or occurrence)</td>
</tr>
<tr>
<td>bub</td>
<td>At apparition of one large water bubble</td>
</tr>
<tr>
<td>CO$_2$</td>
<td>Of carbon dioxide</td>
</tr>
<tr>
<td>F</td>
<td>Of the flame</td>
</tr>
<tr>
<td>f</td>
<td>Of the fuel layer</td>
</tr>
<tr>
<td>fv</td>
<td>Fuel vapor</td>
</tr>
<tr>
<td>i</td>
<td>index of layer</td>
</tr>
<tr>
<td>j</td>
<td>time index</td>
</tr>
<tr>
<td>lip</td>
<td>Lip, distance between reservoir edge and fuel surface</td>
</tr>
<tr>
<td>lum</td>
<td>Luminous</td>
</tr>
<tr>
<td>mass</td>
<td>Relative to the mass loss</td>
</tr>
<tr>
<td>max</td>
<td>Maximum</td>
</tr>
<tr>
<td>mean</td>
<td>Average</td>
</tr>
<tr>
<td>Meas</td>
<td>Measured</td>
</tr>
<tr>
<td>min</td>
<td>Minimum</td>
</tr>
<tr>
<td>Mod</td>
<td>Modeled</td>
</tr>
<tr>
<td>p</td>
<td>Of the thermocouple rake</td>
</tr>
<tr>
<td>pf</td>
<td>Between thermocouple rake and fuel layer</td>
</tr>
<tr>
<td>pw</td>
<td>Between thermocouple rake and water layer</td>
</tr>
<tr>
<td>rad</td>
<td>Relative to the radiation</td>
</tr>
<tr>
<td>reread</td>
<td>Re-radiated</td>
</tr>
<tr>
<td>soot</td>
<td>Soot</td>
</tr>
<tr>
<td>st</td>
<td>During the quasi-steady period</td>
</tr>
<tr>
<td>t</td>
<td>At reservoir edge, with 0 lip height</td>
</tr>
</tbody>
</table>
Chapter 7. Conclusion and recommendations

\( w \) Of the water layer
\( \infty \) At infinite pool diameter

**Acronyms**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
</tr>
</thead>
<tbody>
<tr>
<td>DIP</td>
<td>Digital Image Processing</td>
</tr>
<tr>
<td>FB</td>
<td>Fractional Bias</td>
</tr>
<tr>
<td>FFT</td>
<td>Fast Fourier Transform</td>
</tr>
<tr>
<td>MEM</td>
<td>Maximum Entropy Method</td>
</tr>
<tr>
<td>MG</td>
<td>Geometric Mean</td>
</tr>
<tr>
<td>NMSE</td>
<td>Normalized Mean Square Error</td>
</tr>
<tr>
<td>PSD</td>
<td>Power Spectral Density</td>
</tr>
<tr>
<td>VG</td>
<td>Geometric Variance</td>
</tr>
</tbody>
</table>
Chapter 8.

Introduction

The objective of the second part of this thesis is to improve the modeling and the understanding of both the apparition conditions and hazards of the boilover phenomenon. A boilover is a violent ejection of fuel due to the vaporization of a water sublayer, resulting in an enormous fire enlargement and formation of fireball and ground fire.

A certain number of research studies have already been conducted on boilover phenomenon, mostly experimental. But the exact phenomenology of the water vaporization and the flame enlargement has not been widely studied. In addition, a large quantity of models for pool fire characteristics, or for quantification of the boilover intensity, is available in the literature. But few studies truly compare the different models with experiments in order to determine the best prediction. Therefore, the major objective of this study was to contribute to the understanding, the modeling and the validation of boilover apparition and consequences.

To achieve such a goal, an experimental database as large as possible helps to identify the dominant parameters that influence the boilover, and that should be involved in the modeling. Boilover experiments can be performed at different scales. A larger scale provides better similarity with the existing storage conditions. But large scale experiments are very expensive due to the large fuel quantity that is needed, and they can be strongly influenced by the weather conditions. On the other end, small scales experiments have a reduced cost and offer a better control on the operation conditions, which ease the parametric analysis. Therefore, small scale experiments have been performed in this study, and a parametric analysis on the fuel and water layer thicknesses, and on the reservoir diameter and material was done. The results of these experiments are then compared to large scale experiments and models available in the literature. If there is no existing model for a given parameter, or if the existing model does not satisfactory agree, new models are developed within this thesis.
Chapter 8. Introduction

As it will be explained in chapter 9, the boilover phenomenon can be divided in three periods: the quasi-steady period, the premonitory period and the boilover period. Therefore, the modeling follows this sequence, as presented in Fig. 8.1. The boilover apparition conditions are modeled through the quasi-steady period. Existing models developed for pool fires are used to predict the burning rate and the flame characteristics. In addition, a thermal model will be developed to predict the time evolution of the fuel and water layers temperature profiles. The thermal model, combined with the pool fire characteristics is then used to predict the boilover occurrence time. The boilover consequences are modeled through the boilover period. At first, the phenomenology of the water vaporization and the flame enlargement are investigated. The flame enlargement is then combined with radiation models to predict the radiation during boilover. Finally, other boilover quantities like the pre-boilover burnt mass ratio and the boilover intensity are also investigated.

Figure 8.1.: Schematic of Boilover modeling

To implement the methodology developed in this study, i.e. to model the boilover apparition conditions and consequences, this part of the thesis and divided in 6 chapters, the first chapter being this introduction. The chapter 9 is a literature review that resumes the existing theories and models already published about boilover. Then, the chapter 10 describes the small scale boilover experiments performed in this study. In addition, the experimental techniques and instrumentation used in these experiments are also described. The chapter 11 describes the experimental results, and compares them with existing or new models and correlations. Finally, the chapter 12 uses the correlations, that proved in chapter 11 to better model the phenomenon, on a large scale boilover experiment. The part ends with conclusions in chapter 13.
Chapter 9.

Boilover literature survey

Most of the hydrocarbon atmospheric reservoirs contain a water layer due to condensation effects, drilling and transport or from the natural composition of the oil. If by accident, a fire starts at the fuel surface, the flame progressively heats the fuel and consequently the water layer until the water starts to boil and expels the fuel from the reservoir. Different names are given to the fuel ejection, as sketched in Fig. 9.1: a slopover is a discontinuous frothing over of the fuel on one side of the tank and a frothover is a continuous and low-intensity frothing of the burning material. But the most dangerous fuel ejection is the boilover, that induces violent fuel ejections, flame enlargement and possible formation of fireballs.

Figure 9.1.: Different types of fuel ejection from pool fire burning [17]

As the boilover is the most dangerous, this study will focus only on this type of fuel ejection. The literature review presented in this chapter focuses first on the basic principles of boilover, and then presents in more details the modeling of the pool fire, the comprehension of the fuel heating until the boilover apparition, and finally the understanding and modeling of the boilover phenomenon.

9.1. Basic principle

The boilover phenomenon is defined as a violent ejection of fuel due to the vaporization of a water sub-layer, resulting in an enormous fire enlargement and formation of fireball and ground fire, as schematized in the Fig. 9.2.
Chapter 9. Boilover literature survey

Figure 9.2.: Boilover principle

The conditions for boilover occurrence have first been set up by Hall in 1925 [52]. A boilover appears with the burning of oil viscous enough to enable formation of froth, the presence of a water sub-layer, and the apparition of a zone of nearly constant temperature inside the fuel. This so-called hot-zone (more details in section 9.2) has an expansion rate higher than the burning rate of the hydrocarbon free surface and therefore enhances the heat propagation downwards the water layer, decreases the time of boilover apparition, and increases the intensity of consequences which mainly consist in flame enlargement and radiation increase. If the fuel nature or the configuration of the reservoir do not lead to the formation of a hot-zone, the temperature gradient at the fuel-water interface is decreased and a larger part of the fuel is burnt before water vaporization. Consequently, lower intensity of consequences (i.e. flame enlargement and radiation increase) are observed. But even if the consequences of a thin-layer boilover (boilover without hot-zone) are of lower intensity, the impact in terms of flame enlargement and radiation is also important.

Based on the conditions for boilover apparition, Mickaelis [89] has developed a semi-empirical criterion for the boilover apparition based on the fluid properties: the value of $F_{boil}$, calculated according to the Eq. (9.1), has to be higher than 0.6. In other words, this criterion states that the average boiling temperature of the fuel ($T_b$ in Eq. 9.1) has to be above the water boiling temperature, usually set as 393 K. In addition, the boiling range of the fuel ($\Delta T_b$ in Eq. 9.1) has to be at least 60 K, and that the fuel viscosity ($\nu$ in Eq. 9.1) has to be higher than the viscosity of the kerosene ($\nu > 0.73 \times 10^{-6} m^2/s$); this last condition guarantees a minimum impervious aspect of the fuel. The criterion of Mickaelis is easy to calculate and therefore is widely used for a first estimation of the boilover occurrence probability even if it is not very precise.

$$F_{boil} = \left(1 - \frac{393}{T_b}\right) \left(\frac{\Delta T_b}{60}\right) \left(\frac{\nu}{0.73}\right)^{1/3} > 0.6 \quad (9.1)$$
The boilover phenomenon can be divided in three different periods, from the ignition of the fuel surface to the end of the fuel burning after boilover. This description of the phenomenon in periods has been proposed by Fan and Hua [32, 63] from temperature measurements around the fuel - water interface and from sound levels. An example of such temperature evolution is shown in Fig. 9.3. The three periods are described as follow:

1. **Quasi-steady period:** This period starts after the ignition of the fuel surface and a small induction period where the flame propagates along the whole fuel surface. The pool fire is burning in a regular way, with very few influence of the water sub-layer. The flame is stable and the fire properties like the burning rate or the flame size are constant with time. During this period, the fuel layer in combustion progressively heats the water sub-layer. In the Fig. 9.3, this period last from 0 min burning time (not visible in the figure) to a bit more than 17 min of burning time.

2. **Premonitory period:** Once the water layer temperature is getting close to the boiling point, water bubbles develop at the fuel - water interface. They escape from the interface, pass through the fuel layer, and erupt from the fuel surface into the flame zone as oil-water dissolution droplets. The combustion of these oil-water bubbles emits a typical crackling sound. This
sound can be observed in Fig. 9.3 as peaks in signal 4, between 17 min and 19 min of burning time.

3. **Boilover period**: When the water vaporization is strong enough to push the fuel layer, the boilover starts, the burning fuel is sprayed out of the tank and the flame height increases significantly and quickly. The flame increase and the violent water vaporization also emit noise, with stronger amplitude than the micro-explosion noise. This period can be observed between 19 and 20 min of burning time in Fig. 9.3.

In literature, studies can be found both on the quasi-steady period and on the boilover period; the premonitory period being only a transition between the two other periods. The next sections give more details about the quasi-steady and boilover periods.

### 9.2. Quasi-steady period

The quasi-steady period lasts from the ignition time to the onset of water boiling. During this period, the fuel layer is progressively heated by the combustion of its surface, and progressively heats the water layer. The discussion concerning the quasi-steady period can be divided in two parts. First, a review of the models involving the different parameters of the pool fire is made. And as the pool fire induces the fuel and water layer heating, the second part of the literature review resumes the models developed to predict the time to boilover, based on a thermal simulation of the fuel and water heating.

#### 9.2.1. Pool fire

The fuel burning during the quasi-steady period is similar to a pool fire. A pool fire is a fire in an open-topped, circular flammable liquid tank, which forms a diffusion flame. The fuel and oxidizer (air) are initially separated and the combustion occurs in the zone where the gases mix [29]. Hydrocarbon flames have a characteristic yellow luminosity coming from the carbon based incandescent soot particles formed within the flame.

The heat flux transferred from the flame to the fuel surface has first been expressed by Hottel [62]. In 1959, the author analyzed the results of a systematic study on pool fires, with the liquid surface at the rim of the container (no lip height) and with pool diameters ranging from $3.7 \cdot 10^{-3}$ m to 22.9 m, performed by Blinov and Khudiakov [15]. From his analysis, Hottel assumed that the total heat flux transferred from the flame (at a temperature $T_F$) to the fuel surface (at a temperature $T_f$) is the sum of three heat transfer mechanisms: conduction through the
9.2 Quasi-steady period

reservoir walls, convection to the fuel surface and radiation from the flame to the fuel, as expressed in Eq. 9.2. In this equation, \( c_1 \) incorporates a number of heat transfer terms linked to conduction, \( c_2 \) is the convective heat transfer coefficient and \( c_3 \) contains the Stefan-Boltzmann constant \( \sigma \) and the view factor. The term \( (1 - \exp(-kD)) \) is the emissivity of an isotherm volume of luminous gas, where the soot particles are uniformly distributed in a non-radiating carrier gas.

\[
\dot{Q}_F'' = 4c_1 \frac{(T_F - T_f)}{D} + c_2 (T_F - T_f) + c_3 (T_F^4 - T_f^4) (1 - \exp(-kD)) \quad (9.2)
\]

The relative importance of the different heat transfer mechanisms changes with the size of the reservoir, as observed by Hottel in the plot of the fuel burning rate in function of the pool diameter, reproduced in Fig. 9.4. This plot reveals three flame regimes. The laminar flame regime characterizes small scale reservoirs \( (D < 0.03 m) \). In this regime, the burning rate decreases as the diameter increases because the conduction through the reservoir, which is the dominant heat transfer at small scale, becomes less significant at larger diameters. The flame is turbulent for large pool sizes \( (D > 1 m) \) where the burning rate is independent of the diameter and mainly controlled by radiation. For the middle range \( (0.03 < D < 1 m) \), the regime is transitional, evolving from laminar to turbulent with the diameter, and therefore with the importance of the radiation term compared to the other two mechanisms.

\[
\dot{n}_t = \frac{\dot{Q}_F''}{\Delta H_v} = c_3(T_F^4 - T_f^4)(1 - \exp(-kD)) = \dot{n}_t\infty(1 - \exp(-kD)) \quad (9.3)
\]
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Small scale pool fires are of less interest except for academic purpose. For that reason, the total heat flux is usually approximated by only the radiation term; the conduction and convection terms being neglected. By assuming that the flame view factor and temperature do not vary with the diameter, the burning rate of a radiation-dominated pool fire can be expressed as Eq. 9.3. More details about the derivation of Eq. 9.3 can be found in [22].

The majority of the semi-empirical correlations available in the literature follow the Eq. 9.3. Only the burning rate at infinite diameter \( \dot{m}_\infty \), and the extinction coefficient \( k \), change from one model to another, as observed in Fig. 9.5 left. These two parameters also vary with the type of fuel and are only valid for a reservoir with no lip height. The Table 9.1 lists the values of \( \dot{m}_\infty \) and \( k \) for diesel, used by different authors. Chatris [22] and Munoz [95] have derived their model based on diesel fires of 1.5 to 6 m diameter. The parameters of Rew [112] are coming from the POOLFIRE6 database, which groups 31 datasets with 24 different fuels and pool diameters ranging from 0.6 to 35 m. Finally, the correlation of Babrauskas has been developed for crude oil, but has been used by Chatris [22] with diesel pool fires. The correlation of Babrauskas is a fit of pool fires from 0.3 to 6 m diameter.

Table 9.1.: Burning rate correlation coefficients (Diesel oil)

<table>
<thead>
<tr>
<th>Author</th>
<th>( \dot{m}_\infty ) [kg/m(^2)s]</th>
<th>( k ) [m(^{-1})]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Babrauskas</td>
<td>0.034</td>
<td>2.8</td>
</tr>
<tr>
<td>Rew</td>
<td>0.054</td>
<td>1.3</td>
</tr>
<tr>
<td>Chatris</td>
<td>0.057</td>
<td>0.57</td>
</tr>
<tr>
<td>Munoz</td>
<td>0.062</td>
<td>0.63</td>
</tr>
</tbody>
</table>

If the fuel surface does not fit with the reservoir edge, so if a non-zero lip height is present while the fuel is burning, the correlations given in Table 9.1 are no longer valid. The lip height effect has not been widely studied in literature, compared to the pool diameter effect. In 2000, based on experiments, Dlugogorski [28] shows that the relation between the fuel burning rate and the lip height initially follows an exponential decline, as depicted in Fig. 9.5 right. Dlugogorski correlated the decay of the burning rate by the empirical Eq. 9.4. In this correlation, \( \dot{m}_t \) is the burning rate when the liquid level corresponds to the burner rim, \( \eta \) is a fitted parameter and \( H^* = H_{lip}/D \) is the lip height to the reservoir diameter ratio [28].

\[
\dot{m} = \dot{m}_t e^{\eta H^*} \tag{9.4}
\]

A flame from a pool fire is a diffusive flame. It can be decomposed in three regions as explained in Fig. 9.6: the base flame, the intermittent flame (where the flame oscillations are visible) and the plume region at the top [16]. The frequency of
9.2. Quasi-steady period

The oscillations, controlling the rate of air entrainment into the flame, is called by Weckman [141] the puffing frequency. The principal geometrical quantities that characterize the diffusion flame are the length, the tilt and the oscillation frequency of the flame. The flame tilt is only function of the wind speed. In this study, the experiments are performed in a calm environment (without wind) so the flame tilt will not be investigated here. On the other end, the radiation emitted by the flame is also analyzed.

As pool fire flame oscillates; the flame length is changing with time. To determine an average flame length \( L_{mean} \), Zukoski [154], and Ferrero [36] used the inter-
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mittency \( I(L) \), which is the fraction of time in which the flame is higher than \( L \), as illustrated in Fig. 9.6 right. They defined the average flame length as the length for which the intermittency \( I(L) \) is equal to 0.5. Likewise, the minimum flame length is defined with an intermittency of 0.95 and the maximum flame length with an intermittency of 0.05. But the majority of the correlations published in the literature focus on modeling the average flame length.

\[
\frac{L}{D} \propto \left( \frac{\dot{V}^2}{gD^5} \right) \propto \left( \frac{\dot{m}^2}{\rho g D^5} \right) \tag{9.5}
\]

Among the correlations that predict the average flame length, most of them show a dependency with the reservoir diameter (\( D \)), the burning rate (\( \dot{m} \)) and the wind speed (\( u \)), as Eq. 9.6 to 9.8. This correlation has been introduced by Thomas [132] who assumes that buoyancy is the driving force for this type of fire. Based on a dimensionless analysis, Thomas derived that the flame length is proportional to the volume flow rate and the reservoir diameter as shown in Eq. 9.5. More information on the dimensionless analysis can be found in [132]. The proportionality can also be expressed in terms of the mass burning rate, by using the relation \( \rho \dot{V} \propto \dot{m} D^2 \) [131].

\[
\frac{L}{D} = a(m^*)^b(u^*)^c \tag{9.6}
\]

\[
m^* = \frac{\dot{m}}{\rho_\infty \sqrt{gD}} \tag{9.7}
\]

\[
\begin{cases}
    u^* = \frac{u}{u_c} = \frac{u}{(\frac{g \dot{m} D}{\rho_\infty})^{1/3}} & \text{if } u > u_c \\
    u^* = 1 & \text{if } u < u_c
\end{cases} \tag{9.8}
\]

From this dimensionless analysis, Thomas has derived his correlation from a fit with experimental results based on wood fire [131]. Some other authors proposed similar correlations based on their experimental results. The models are then only changing by the values of the parameters \( a, b \) and \( c \). The Table 9.2 resumes the parameter values of these models. The models of Thomas have been derived from wood cribs fires of 0.2 to 2 m diameter. The first model takes into account the wind effect, the second model not. The models of Moorhouse have been derived from experiments of rectangular LNG fires on land, from 6.1 to 13.7 m length. The first model considers a cylindrical flame while the second model considers a conical flame. Mangialavori performed experiments with heptane, hexane and toluene fires of 1 to 6 m diameter to derive his correlation. Finally, the correlation of Ferrero is an empirical fit from experiments of diesel fires of 1.5 to 6 m diameter,
9.2. Quasi-steady period

and Pritchard derived his correlation from LNG fires. The Fig 9.7 left compares these correlations in function of the non-dimensional burning rate \((m^*)\). The comparison shows first that the correlations gives similar flame lengths for small values of the non-dimensional burning rate, and that the difference between models increases strongly with the non-dimensional burning rate. The Figure 9.7 left shows also that two different slopes are followed by the models. The correlations of Thomas and Mangialavori predict much larger increase of the flame length with the non-dimensional burning rate than the correlations of Moorhouse, Pritchard and Ferrero.

<table>
<thead>
<tr>
<th>Author</th>
<th>a</th>
<th>b</th>
<th>c</th>
</tr>
</thead>
<tbody>
<tr>
<td>Thomas 1 [131]</td>
<td>42</td>
<td>0.61</td>
<td>-</td>
</tr>
<tr>
<td>Thomas 2 [131]</td>
<td>55</td>
<td>0.67</td>
<td>-0.21</td>
</tr>
<tr>
<td>Moorhouse 1 [91]</td>
<td>6.2</td>
<td>0.254</td>
<td>-0.044</td>
</tr>
<tr>
<td>Moorhouse 2 [91]</td>
<td>4.7</td>
<td>0.21</td>
<td>-0.114</td>
</tr>
<tr>
<td>Mangialavori [83]</td>
<td>31.6</td>
<td>0.58</td>
<td>-</td>
</tr>
<tr>
<td>Ferrero [36]</td>
<td>4.201</td>
<td>0.181</td>
<td>-0.082</td>
</tr>
<tr>
<td>Pritchard [107]</td>
<td>10.615</td>
<td>0.305</td>
<td>-0.03</td>
</tr>
</tbody>
</table>

The puffing frequency is mainly dependent on the pool fire diameter. In 1983, Emori and Saito [30] first emphasized the importance of Strouhal and Froude numbers in a dimensional analysis of the pool fire puffing frequency. The Strouhal number is a non-dimensional number describing oscillating flow mechanisms \((St = fD/V)\), and the Froude number is another non-dimensional number defined as the ratio of a body’s inertia to gravitational forces \((Fr = V^2/gD)\). In 1992, Hamins performed pool fire experiments [54], combined them with experimental data from the literature, and observed a relation between the Strouhal and Froude number, as shown in Fig. 9.7 right. Hamins also showed that the fuel velocity at the base of the flame \((V)\) has a weak influence on the puffing frequency \((f)\), compared to the pool diameter \((D)\). Therefore, the Strouhal - Froude relationship can be simplified into a frequency - diameter relationship, as expressed in Eq. 9.9.

In 1989, Pagni [102] proposed a correlation based on this frequency - diameter relationship, expressed in Eq. 9.10 and is still nowadays the most widely used.

\[
St \propto \left( \frac{1}{Fr} \right)^{1/2} \rightarrow \left( \frac{fD}{V} \right) \propto \left( \frac{gD}{V^2} \right)^{1/2} \rightarrow f \propto \sqrt{\frac{g}{D}} \tag{9.9}
\]

\[
f = \sqrt{\frac{2.3}{D}} \tag{9.10}
\]
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Due to the combustion process, a flame radiates. To evaluate the potential hazard of a given fire, the radiation flux received at a given distance from the flame is an important quantity to determine. The radiation flux received by an observer can be calculated by two approaches: the point source model and the solid flame model. Both approaches are sketched in Fig. 9.8.

The point source model, sketched in Fig. 9.8 left, considers the heat source (i.e. the flame) as a point that emits in all directions. The heat received by the observer is a fraction ($\beta$) of the heat of combustion ($\Delta H_c$) multiplied by the burning rate ($\dot{m}$). This heat decreases with the square of the distance from the source ($r$) and with the transmissivity of the ambient air ($\tau_a$), as expressed in Eq. 9.11.

$$\dot{q}_r = \frac{\beta \dot{m} \Delta H_c \tau_a}{4\pi r^2} \quad (9.11)$$

The $\beta$ parameter is difficult to model. Experimental values can be found in literature, but are fuel dependent and a few studies about diesel pool fire are available.
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According to the CCPS [39], the fraction of heat of combustion ranges between 0.1 and 0.4. Other authors have also proposed correlations, based on curve fitting of experimental data, and depending on the reservoir diameter. They are shown in Table 9.3.

Table 9.3.: Flame radiative fraction correlations

<table>
<thead>
<tr>
<th>Author</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mc Grattan</td>
<td>$\beta = 0.35 \exp(-0.05D)$</td>
</tr>
<tr>
<td>Sarofim</td>
<td>$\beta = \left(\frac{1-\exp(-kD)}{\sqrt{D}}\right)^{0.61}$</td>
</tr>
</tbody>
</table>

The solid flame model assumes a heat source as a solid body of simple geometrical shape (middle schematic in Fig. 9.8). The radiation is emitted by the surface of this body and is modeled as the product of the view factor ($F$), the atmospheric transmissivity ($\tau_a$) and the flame emissive power (SEP), as shown in Eq. 9.12.

$$\dot{q}_r = F \cdot SEP \cdot \tau_a$$

The geometric view factor is taken as if the flame is a vertical cylinder with a height equal to the flame height, and the observer is a vertical surface at ground level with the normal to the plane oriented to the centre line of the cylinder. The transmissivity is calculated with the model of Moorhouse and Pritchard [92]. More details on these equations can be found in appendix B section B.2.3.

Table 9.4.: Flame emissive power correlations

<table>
<thead>
<tr>
<th>Author</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mudan &amp; Croce</td>
<td>$SEP = SEP_{lum} \cdot \exp(-0.12D) + SEP_{soot} \cdot (1 - \exp(-0.12D))$</td>
</tr>
<tr>
<td>Shokri</td>
<td>$SEP = \frac{58 \cdot 10^{(-0.0082 D)}}{1+0.33 D}$</td>
</tr>
<tr>
<td>Duiser (TNO)</td>
<td>$SEP = \frac{0.35 \Delta H}{1+0.33 D}$</td>
</tr>
<tr>
<td>Munoz</td>
<td>$SEP = x_{tum} SEP_{tum} + (1 - x_{tum}) SEP_{soot}$</td>
</tr>
</tbody>
</table>

In the solid flame approach, the most difficult quantity to determine is the emissive power of the flame (SEP). Different models found in the literature are resumed in Table 9.4. Shokri [123] assumes that the flame can be modeled as a cylindrical, black body, homogeneous radiator (like middle schematic in Fig. 9.8). The correlation of Shokri is based on a least square fit of large scale experimental data obtained with different fuels. Duiser [40] calculates the emissive power from the rate of combustion and released heat, and assumes a radiative fraction of 0.35.
Finally, Mudan & Croce [94] and Munoz [96] divide the flame in two zones: a luminous (SEP\textsubscript{lum}) and a non-luminous part (SEP\textsubscript{soot}), as schematized in Fig. 9.8 right and expressed in Table 9.4. Mudan & Croce derived their expression from data of gasoil, kerosene and JP-5 pool fires. They model the luminous and non-luminous part as constants; only the luminous fraction is changing with the pool diameter. Also based on experimental observations, Munoz models the non-luminous part as constant, and the luminous part increasing with the diameter up to a pool diameter of 5 m and then decreasing with the diameter until reaching a plateau from 20 m of reservoir diameter. Munoz proposed several definitions for the luminous and non-luminous part and for the luminous fraction depending on the pool fire size and on the fuel type. The Table 9.4 gives the correlations for a diesel pool fire of less than 5 m diameter.

The comparison between the different models of emissive power presented in Table 9.4 with measurements is done in Fig. 9.9. The data of Shokri [123] are already a summary published experimental results (Yamaguchi [145], Seeger [119], Yumota [149], Dayan & Tien [25], May & Mc Queen [85], and Hagglund & Persson [50]) obtained with reservoir diameters varying from 1 m to 50 m. In the Figure 9.9, the experimental data points labeled as “non-LNG” group fuels like JP-4, JP-5, kerosene, gasoline, and fuel oil. The data from Munoz [96] are coming from pool fires of gasoline and diesel [96]. The comparison shows that the correlations of Munoz are in agreement with a major part of the experimental points, except for large pool diameter where Munoz proposes a plateau value while the experiments from Shokri [123] show that the emissive power continues to decreases. The large scatter between experimental points could be due to differences in fuel type. Notice
9.2. Quasi-steady period

that only Munoz and Duiser (TNO) show a dependency of the fuel type on the emissive power formulation.

9.2.2. Thermal evolution of fuel and water layer

Figure 9.10.: Temperature profiles in fuel. Left: without formation of a hot-zone, Right: with the presence of a hot-zone [17]

The heat flux from the flames expressed by Eq. 9.2 progressively heats the fuel layer. The main difference between the classical and the thin-layer boilover is the presence of a hot-zone; a zone of uniform temperature inside the fuel layer, with a thickness increasing with time, as represented in Fig. 9.10 right. The thin-layer boilover does not experience a hot-zone, and the temperature profile can be approximated by an exponential curve, as sketched in Fig. 9.10 left. Hot-zones have been clearly identified in published experiments [17, 71, 56]. The Fig. 9.11 shows a typical example of temperature profile during the quasi-steady period of a 5 m diameter crude oil pool fire that experienced a hot-zone.

The common explanation for the hot-zone formation is related to a distillation process; the different boiling points of fuel components create convection movements inside the fuel. However, the exact mechanism of hot-zone formation and growth is still a matter of investigation. Hall [52] has argued that the hot-zone starts with a distillation process in a thin layer of fuel located just under the fuel surface. From this distillation, the heavy ends of the hydrocarbon fall in the bottom of the fuel layer. The heat transported by the heavy ends vaporizes the light end fractions of the hydrocarbon, that rise to the fuel surface and vaporize. A slightly different theory has been developed by Burgoyne [18] who assumes that, at the fuel surface, there is no distillation, but not all the fuel fractions are vaporized into
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Figure 9.11.: Hot zone measured during large scale experiment (D=5m, Arabian-light oil, \( H_f = 0.45m, H_w = 0.1m \)) [72]

the flame. The non vaporized fractions have a larger density, and therefore sink to the bottom of the fuel layer, mix with the cold fuel and generate vapor from the fractions of the fuel with lower boiling points. These fractions rise to the fuel surface. From these two theories of hot-zone formation and propagation, the main criteria for a hot-zone to develop is the fuel, that needs to have a large boiling temperature range, as a result of a large number of components with different densities [17], which is one of the conditions expressed in the criterion of Mickaelis [89]. Hasegawa [56] concluded from mid-scale measurements that the hot-zone has uniform density and chemical composition, in addition to the uniform temperature. Hasewaga also added some conditions about the size of the reservoir. For reservoirs with a diameter higher than 0.9 m, the hot-zone apparition depends only on the fuel characteristics, as proposed by Hall [52] and Burgoyne [18]. But when the reservoir diameter is smaller than 0.3 m, the conduction through the wall is the most important heat transfer, and the heat exchange in the zone between the reservoir edge and the fuel surface contributes to the formation of the hot-zone. Finally, for a reservoir between 0.3 m and 0.9 m, both the fuel characteristics and the surface temperature (affected by the conduction with the tank wall) are contributing to the apparition of a hot-zone.

Whatever the presence of a hot-zone, it is interesting to model the thermal evolution of the fuel and water layers since it can lead to determine the boilover apparition time. In 1994, Koseki [71] measured the time to boilover for different sizes of reservoir and different crude oil thicknesses, were the presence of a hot-zone was observed. From these experiments, the time to boilover showed a
9.2. Quasi-steady period

linear evolution with the fuel thickness, whatever the diameter, as observed in Fig. 9.12 left. But the scatter is quite large. In 1996, Garo showed [41], from thin-layer boilover with 0.15 to 0.5m diameter reservoirs of crude oil, that the time to boilover has a linear evolution with the fuel thickness, but depends also on the reservoir diameter, as shown in Fig. 9.12 right.

Figure 9.12.: Boilover apparition time with crude oil. Left: with hot-zone, D=0.3-2.7m [71]. Right: without hot-zone, D=0.15-0.5m [42]

In literature, different thermal models have been developed to simulate the fuel and water layer heating prior boilover, and are listed in Table 9.5. In 1990, Arai [3] first modeled the fuel and water heating with a one-dimensional unsteady conduction equation. Compared to small scale experiments with pool fires of $D = 0.04 - 0.2m$ with toluene and ethylbenzene, the conduction model largely overestimates the time to boilover (in average, by 43% for toluene and by 35% for ethylbenzene). Arai concludes that the discrepancy in the estimation of the time to boilover is large because all the heat transfer mechanisms involved in the fuel and water layer heating have not been taken into account. In 1992, Inamura [65] assumes that the fuel and water layers are heated both by conduction and by absorption of the flame radiation by the fuel layer. The fit with experiments is better than Arai, but Inamura still experiences differences: underestimation for toluene and overestimation for n-decane. Inamura assumes that these differences are due to convection movements in the fuel layer, not taken into account in his model. Finally, in 2006, Kozanoglu [74] adds a convection term in the one-dimensional thermal equation. The convection is characterized by a velocity $V_a$ which varies with time. This velocity corresponds to the effective velocity generated by the downward movement of the higher density components, replacing the lower density components moving upward, as stated in the hot-zone theory of Burgoyne [18]. The initial value of the velocity $V_{a0}$ is first determined from experiments, but in 2007, Ferrero [35]
proposes a correlation to estimate this velocity, based on the fuel layer thickness and on the boiling temperature of the fluid. As the fuel burns, the current fuel density increases due to the vaporization of lower density components, and the convection velocity decreases according to the equation provided in Table 9.5.

### Table 9.5: One-dimensional thermal models for fuel layer

<table>
<thead>
<tr>
<th>Author</th>
<th>Fuel thermal equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Arai [3]</td>
<td>( \rho_f C_{pf} \frac{\partial T_f}{\partial t} = \lambda_f \frac{\partial^2 T_f}{\partial x^2} )</td>
</tr>
<tr>
<td>Inamura [65]</td>
<td>( \rho_f C_{pf} \frac{\partial T_f}{\partial t} = \lambda_f \frac{\partial^2 T_f}{\partial x^2} + \dot{q}_r \mu \exp(-\mu x) )</td>
</tr>
<tr>
<td>Kozanoglu [74]</td>
<td>( \rho_f C_{pf} \frac{\partial T_f}{\partial t} = \lambda_f \frac{\partial^2 T_f}{\partial x^2} + \dot{q}<em>r \mu \exp(-\mu x) + \rho_f C</em>{pf} V_a \frac{\partial T_f}{\partial x} )</td>
</tr>
</tbody>
</table>

\[ V_a = V_{a0} \left( \frac{\rho_0}{\rho} \right) \]

But all these models do not simulate the hot-zone formation and growth. In 1995, Broeckmann [17] distinguishes two models depending if a hot-zone is present or not. The model for a thin-layer boilover (without hot-zone) is close to the model of Kozanoglu [74]. The modeling of the hot-zone is based on two constant temperatures: one temperature for the hot-zone set as the vaporization temperature, and one temperature for the fuel outside the hot-zone which is equal to ambient temperature. The model proposes an expression for hot-zone expansion rate, based on the molar fluxes of the vaporized and remaining fractions of the fuel, the molar masses and the tank surface area. This calculation is possible only if a description of the fuel composition and the boiling point of each component are known. Details of this model are available in the publication of Broeckmann [17].

### 9.3. Boilover period

In literature, it is commonly accepted that the boilover starts with the vaporization of the water sub-layer that induces a piston effect due to the enormous volume increase resulting from the phase change. Garo and Hua [41, 63] performed visualizations of the fuel and water layers, and showed that bubbles appear at the fuel - water interface (Fig. 9.13 (b)), and increase in size and generation rate (Fig. 9.13 (c)) with time up to boilover apparition (Fig. 9.13 (d)). But these experiments use a thick water layer, not very representative of the real industrial boilover accidents, and they do not explain a clear phenomenology for the exact starting moment of boilover.

The flame enlargement generated by the fuel expulsion due to the water boiling, and the resulting drastic increase of the radiation, are the major hazards of a
boilover phenomenon. Few studies have been achieved on the flame enlargement, even if it is the boilover principal hazard. A few authors published pictures showing the different steps of the flame enlargement \[41\] \[32\] \[63\], like the one of Fig. 9.14. From the quasi-steady period (Fig. 9.14 (a)), the premonitory period that follows (Fig. 9.14 (b)) shows an increase of the flame size as some fuel is ejected, and micro-explosion noise is emitted. The boilover starts in Fig. 9.14 (c), where fuel is expelled from the whole reservoir diameter due to the violent water boiling. Some ejected fuel forms a ground fire around the reservoir and the flame is increasing in size as well as in radiation intensity (Fig. 9.14 (d)). When the boilover ceases, the flame size comes back to its original size (Fig. 9.14 (f)) while the projected fuel is still burning on the ground.

But even if the flame enlargement has already been observed, an analysis of the boilover intensity or of the radiation based on the flame size has never been proposed in literature. Instead, most of the authors characterize the boilover intensity by using either the mass loss rate ($I_{mass}$) or the flame radiation ($I_{rad}$). The intensity is defined by Koseki \[73\] \[71\] as a ratio between either the burning rate or the flame radiation, evaluated at the maximum of the flame enlargement com-
Chapter 9. Boilover literature survey

Figure 9.14.: Flame enlargement during Boilover of 200mm initial thickness of crude oil in a $D = 0.6$ m diameter reservoir [63]

pared with the same quantity evaluated during the quasi-steady period. The two definitions of boilover intensity ($I_{\text{mass}}$ and $I_{\text{rad}}$) show an influence of the initial fuel thickness and the reservoir diameter.

The boilover intensity increases with the initial fuel layer thickness, as shown in Fig. 9.15 left. When the initial fuel layer increases, it takes more time to reach the boiling temperature of the water (as observed in Fig. 9.12), and the superheated water layer is thicker. The boilover intensity also decreases when the reservoir diameter increases, as shown in Fig. 9.15 right. Indeed, when the reservoir diameter increases, the burning rate increases (see Fig. 9.5), the fuel and water layers are heated faster, the fuel-water interface reaches the boiling temperature sooner, and the superheated water layer is smaller, resulting in a smaller intensity of the boilover. In 2007, Garo also pointed out the influence of the fuel type; the intensity ($I_{\text{mass}}$) increasing with the boiling temperature of the fuel [42], see Fig. 9.16 left. According to Garo, when the fuel boiling point increases, the burning rate decreases. Therefore, it takes more time to reach the water boiling temperature,
9.3. Boilover period

and the thickness of superheated water increases, resulting in an increase of the boilover intensity.

Figure 9.15.: Left: Relationship between the two definitions of boilover intensity ($I_{\text{mass}} = \frac{V_{bo}}{V_{st}}$ and $I_{\text{rad}} = \frac{\dot{q}_{bo}}{\dot{q}_{st}}$) and initial fuel thickness in 1 m crude oil pool fire (left) [73]. Right: $I_{\text{mass}}$ evolution with the reservoir diameter, with an initial crude oil thickness of 20 mm [74].

Therefore, for a specified fuel type, the boilover intensity depends both on the initial fuel thickness and the reservoir diameter, as shown by Koseki [73] in Fig. 9.15. Therefore, Ferrero [38] and Chatris [22] have plotted the boilover intensity as a function of a new parameter $\Lambda$ which represents the fraction between the initial fuel layer and the pool area and expressed in $\text{mm/m}^2$. The Figure 9.16 right shows the evolution of the boilover intensity for diesel pool fires with $\Lambda$, where the average and maximum intensity are changing only with the definition of the burning rate used during the boilover period. The average burning rate is defined by Ferrero [38] as the ratio of the fuel mass burnt during the boilover period to the duration of the boilover period. The maximum burning rate is defined similarly, but with the subtraction of the amount of water evaporated during the boilover period. In this thesis, the definition of the boilover intensity based on the average burning rate is chosen as the calculation of the amount of evaporated water is not straightforward.

The boilover intensity can be calculated in an indirect way through the pre-boilover burnt mass ratio ($\chi$), that provides information on the quantity of fuel still to burn that could be projected out of the reservoir during the violent water vaporization.
Chapter 9. Boilover literature survey

Figure 9.16.: Left: Relationship between intensity of Boilover and fuel boiling temperature [42], determined from measurements in $D = 0.15m$ pool fire with an initial fuel thickness of 13 mm, Right: boilover intensity dependence with $\Lambda$ [38].

This ratio is defined at the fuel mass available before the boilover apparition divided by the initial fuel mass. The evolution of the pre-boilover burnt mass ratio with the pool diameter (see Fig. 9.17 left) and the fuel boiling point (see Fig. 9.17 right) are consistent with the boilover intensity evolutions with the same parameters. Indeed, a smaller pre-boilover burnt mass ratio means that more fuel is available at boilover occurrence and can lead to a larger boilover intensity. Surprisingly, $\chi$ increases with the fuel layer thickness. But even if $\chi$ increases with the fuel layer thickness, the remaining fuel layer thickness at boilover apparition for an initial large thickness is still larger than for an initial small thickness, leading to an increase of the boilover intensity. Looking at the Fig. 9.17 for a reservoir diameter of 1.5 m, an initial fuel thickness of 10 mm leads to a fuel layer thickness at boilover apparition around 5 mm. But an initial fuel thickness of 40 mm leads to a fuel layer thickness at boilover apparition of 8 mm, leading then to a higher boilover intensity.

9.4. Conclusion

The objective of this chapter was to resume the different theories and models that have already been published in the literature about boilover and pool fire, both about the physical aspects of the phenomenon and about the hazards that this phenomenon generates.
9.4. Conclusion

First, the boilover phenomenon can be decomposed in three periods: the quasi-steady period, the premontitory period and the boilover period. During the quasi-steady period, the fuel surface burns with all the characteristics of a pool fire, whereas the boilover period appears when the water layer starts to vaporize violently and the flame to enlarge consequently.

The pool fire characteristics during the quasi-steady period have already been widely studied in literature. Experiments in different scales, and with different fuel types have been performed, and semi-empirical correlations have been derived for different aspects of the pool fire like the burning rate, or the flame length, puffing frequency, and radiation. But few comparisons exist in literature between the different models. In chapter 11, the small scale experiments (detailed in chapter 10) are compared with the different semi-empirical correlations to determine the best modeling approach for each of the parameters.

The hot-zone phenomenon will not be studied here, since it was not observed in the small scale experiments performed in this thesis, involving diesel. The study will focus more on the thin-layer boilover.

Concerning the boilover period, the literature survey showed that studies about the boilover phenomenology, i.e. the water vaporization process or the physics behind the flame enlargement, are very rare. Therefore, a part of this thesis will be dedicated in a better understanding of the boilover phenomenology. In a second part, the boilover intensity will be investigated further, in addition to the flame radiation.
Chapter 10.

Experimental setup

In this research, the boilover develops in a reservoir where the fuel surface is burning, which is the main scenario leading to boilover apparition. Two types of experiments have been conducted. First, field tests have been carried out with reservoirs of large dimensions, but experienced a strong dependence on environmental conditions such as wind. Secondly, laboratory experiments have been performed to guarantee a calm and uniform environment.

Figure 10.1.: Schematic of Boilover experimental setup

In both types of experiments, different cylindrical reservoirs were used, with varying heights and diameters. The reservoir was filled with a water layer topped with a fuel layer. The fuel and water layer thicknesses were changed for parametric analysis. The fuel was ignited with a butane flame for the laboratory experiments and with a torch made of tissue for the field tests. Same instrumentation was used in field tests and laboratory experiments. The Fig. 10.1 shows the schematic of the two test setups; the liquid temperature was monitored with a rake of thermocouples, the burning rate was recorded through a load cell placed under the reservoir, and the flame radiation was measured with radiometers positioned at given distances from the reservoir. Flame visualization was also performed.
10.1. Laboratory Experiments

All the laboratory experiments were performed in the BABELs facility, which is described in more details in chapter 4, section 4.1.

10.1.1. Fuel and reservoirs

The fuel used in the experiments was a mixture of 30% Valvata Shell 460 oil and 70% diesel. Oil was added to increase the fuel viscosity, which is a favorable condition for boilover apparition, as explained in chapter 9, section 9.1. More information of the fluid properties can be found in appendix B, section . Three reservoirs were used in these experiments: a glass reservoir of 0.08 m diameter and two metal reservoirs of 0.08 m and 0.115 m diameters respectively. All reservoirs had a 2 mm wall thickness. The glass reservoir was a DURAN crystallizing dish and the metal reservoirs were made out of steel. In this experimental study, the influence of the fuel thickness, the ratio between the fuel and water thicknesses, the lip height, the reservoir diameter, and the reservoir material were investigated. Each configuration was performed twice for reliability check. The different configurations tested are described in Table 10.1.

<table>
<thead>
<tr>
<th>D [m]</th>
<th>H [m]</th>
<th>Material</th>
<th>( H_f/D )</th>
<th>( H_f/H_w )</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.08</td>
<td>0.045</td>
<td>glass</td>
<td>0.1, 0.2, 0.4</td>
<td>( H_f/H_w = 3 )</td>
</tr>
<tr>
<td>0.08</td>
<td>0.045</td>
<td>glass</td>
<td>0.1, 0.2, 0.4</td>
<td>( H_f/H_w = 6 \text{ mm} )</td>
</tr>
<tr>
<td>0.08</td>
<td>0.045</td>
<td>metal</td>
<td>0.1, 0.2, 0.4</td>
<td>( H_f/H_w = 3 )</td>
</tr>
<tr>
<td>0.15</td>
<td>0.065</td>
<td>metal</td>
<td>0.1, 0.2, 0.4</td>
<td>( H_f/H_w = 6 \text{ mm} )</td>
</tr>
<tr>
<td>0.15</td>
<td>0.065</td>
<td>metal</td>
<td>0.1, 0.2, 0.4</td>
<td>( H_f/H_w = 3 )</td>
</tr>
</tbody>
</table>

10.1.2. Instrumentation

10.1.2.1. Overview

During each laboratory boilover experiment, the mass loss was measured with a load cell positioned under the reservoir, the fuel and water layer temperatures were measured by means of thermocouples, the flame radiation was measured at given distances from the reservoir with radiometers, and the flame was monitored with a digital camera. An overview of the instrumentation is schematized in Fig. 10.2 and a photograph of the experimental setup is displayed in Fig. 10.3.
10.1. Laboratory Experiments

10.1.2. Temperature measurement

The time evolution of the fluid temperature was recorded at different heights from the fuel surface with a thermocouple rake positioned at the center of the reservoir, as observed in Fig. 10.3. The rake was composed of a series of 26 type K thermocouples of 1mm diameter, sheathed with stainless steel AISI310 and with an isolated junction. A thermocouple consists of two conductors of different materials joined together, so that a potential difference generated between the points of contact is a measure of the temperature difference between the points. More information about the thermocouple principle and the type of junction is given in appendix B section B.1.2.
The design of the thermocouple rake was an iterative process, and the previous designs that were tested can be found in appendix B, section B.1.3. All the thermocouples were placed together inside two steel clamps of 140 mm high, 15 mm long and 10 mm large, with a thickness of 2 mm, acting like a vice. The Figure 10.4 left provides a schematic of the thermocouple arrangement and Fig. 10.4 right shows a photograph of the rake, and its main dimensions. The spatial interval between the different positions of the thermocouples in the rake was chosen for the best space efficiency, considering the different configurations to be tested, resulting in spatial resolution that ranges from 1 to 5 mm. To measure a temperature independent from the clamps (i.e. minimize the conduction between the clamps and the thermocouples), the distance between the thermocouple junction and the vice was set to 10 mm. The part of the 600 mm long sheathed thermocouple which is not inside the clamps was protected with a silica fiber braided sleeve from Omerin, as schematized in Fig. 10.4 left, and showed as the white tissue above the thermocouple rake in Fig. 10.3. Each thermocouple was then connected to a 3 m extension cable, that reached a NI9213 acquisition module, plugged in National Instrument Compact-DAQ NI9178. All the thermocouple measurements were sampled at 10Hz.

### 10.1.2.3. Mass loss measurement

Simultaneously to temperature measurement, the mass loss due to the fuel combustion was monitored by a Futek LSB303 load cell, with a maximum output of
5 kg and a resolution of 0.2 g. The load cell was compensated in temperature in the $288 - 345 \, K$ range. The signal was filtered at 100Hz with a digital low-pass filter, and sampled at 10 Hz by using a NI9205 acquisition module. The load cell was sandwiched between two plates of 150 mm large and 300 mm long, linked together with a cone-point support, as schematized in Fig. 10.2. On one side of the support, the load cell was positioned between the two plates and on the other side, the tested reservoir was laid on the upper plate, made out of bakelite to reduce the heat transfer from the burning reservoir to the load cell. The load cell was calibrated every day of experiments, and an example of calibration curve is proposed in appendix B section B.1.4.

10.1.2.4. Radiation measurement

Two 64P type Medtherm radiometers recorded the pool fire radiation. They were positioned at a distance of $6D$ and $7D$ from the reservoir. They were both equipped with a KRS-5 window of 150° view angle to capture only the radiation part of the thermal flux emitted by the flame. Their signals have been calibrated with a LANDCAL P550P black body (see appendix B section B.1.5). They were filtered at 100 Hz with a digital low-pass filter, and the 10 Hz sampling (similar to temperature and mass loss) was performed with a NI9205 acquisition module plugged on the same NI Compact-DAQ NI9178 as the thermocouple and mass loss measurements.

10.1.2.5. Flame visualization and contours detection

To record the flame evolution, a Panasonic NV-GS400 camera was used, combined with a Raynox HD 3035 Pro Semi-Fisheye. The fisheye allowed the capture of the flame dimensions during boilover (up to 2-3m high), which are large compared to the distance between flame and optical accesses imposed by the design of BABELs.
Chapter 10. Experimental setup

(1.3m). The distortion created by the fisheye was corrected by means of a Digital Image Processing (DIP) algorithm on Matlab platform. The algorithm performs an image spatial transformation based on a calibration image representing a chessboard like in Fig. 10.5, which can be called the step 0.

Figure 10.6.: Illustration of the different steps of the flame detection algorithm

In addition to the distortion correction, the algorithm was also used for flame detection, with the help of the Matlab Image Processing Toolbox. The main steps of this algorithm can be observed in Fig. 10.6. The first step is to apply the distortion correction determined from the calibration, on the flame images (step 1 in Fig. 10.6). The second step is to apply a median filter on the image for noise reduction, but also to adjust the intensity values such that only 1% of the image is saturated at low and high intensities, which increase their contrast (step 2 in Fig. 10.6). This processing is specially needed at the beginning of the boilover period when the flame is very intense, and can cause halo on the images. The third step is to convert the resulting gray scale images into binary images, using a global image threshold based on the Otsu method (step 3 in Fig. 10.6). Finally, the pixels on the edges of the flame are extracted by means of the Moore-Neighbor tracing algorithm modified by Jacob’s stopping criteria (step 4). More details about the different algorithms used are given in appendix B section B.1.6. The computation of the flame area, length and width is readily accomplished. The flame length definition differs, depending if the flame is taken from the quasi-steady period or during the boilover period. Both situations will
10.2. Field tests

be explained later in chapter 11.

10.1.2.6. Fuel and water layer visualizations

Visualizations of the behavior of the water and fuel layers have also been conducted to better understand the water boiling phenomenology at boilover onset. In BABELs, the glass reservoir of 80 mm diameter was used, filled with a water layer of 6mm and a fuel layer of 16 mm. The fuel used was a mixture of lamp oil and Valvata Shell 460 oil, chosen because it produces less carbon residues compared to diesel, and therefore remains more transparent during the test. A Phantom V7.1 high-speed camera recorded the boilover phenomenon in series of 2048 images, with an acquisition frequency of 1 kHz. Two view angles were chosen (see Fig. 10.7): the side view, to give a visualization of the evolution of water and fuel layers, and the bottom view, to observe the evolution of both layers over the whole reservoir diameter. To improve the visualization quality, tracing paper was placed in front of the two halogen lamps to give a better light diffusion.

![Figure 10.7.: Camera and lighting disposition](image)

10.2. Field tests

For boilover field tests, two test campaigns were carried out at the von Karman Institute outdoor. From these experiments, four tests ended in a boilover with all the measured quantities recorded. The reservoir diameter, the fuel composition and the fuel thickness to diameter ratio were varied, as listed in Table 10.2. Two steel reservoirs of \( D = 0.15 \) m and \( D = 0.3 \) m were used (Table 10.2). The same diesel-oil mixture as for the laboratory experiments was used in the field tests.

The instrumentation was similar for both reservoirs. Pictures of the experimental setups used for the 0.15 m and 0.3 m reservoirs are displayed in Fig. 10.10 and
Chapter 10. Experimental setup

Table 10.2.: Field tests configurations

<table>
<thead>
<tr>
<th>( D [m] )</th>
<th>( H [m] )</th>
<th>( \frac{H_f}{D} [-] )</th>
<th>( H_w )</th>
<th>Oil Fraction [-]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.15</td>
<td>0.09</td>
<td>0.4</td>
<td>( H_f/H_w = 3 )</td>
<td>0.3</td>
</tr>
<tr>
<td>0.15</td>
<td>0.09</td>
<td>0.4</td>
<td>( H_f/H_w = 3 )</td>
<td>0.2</td>
</tr>
<tr>
<td>0.3</td>
<td>0.09</td>
<td>0.2</td>
<td>( H_f/H_w = 2.5 )</td>
<td>0.3</td>
</tr>
<tr>
<td>0.3</td>
<td>0.09</td>
<td>0.2</td>
<td>( H_f/H_w = 2.5 )</td>
<td>0.2</td>
</tr>
</tbody>
</table>

\( D = 0.15 \) respectively. The two thermocouple rakes were composed of a series 220\( \mu \)m diameter K type, and positioned at the center of the reservoir. Both rakes are showed in Fig. 10.8. Tests with \( D = 0.15 \) m used a rake of 10 thermocouples, each of them being protected by a small metal tube, as shown in Fig. 10.8 left. Only 6 of them were immersed in the fuel and water layers, and the spatial resolution was equal to 15 mm. But this design limited the spatial resolution and the number of measurement points. Therefore, the tests with \( D = 0.3 \) m used a rake composed of 17 thermocouples, each of them fixed in a small ceramic tube. All the tubes were glued together, and fixed inside a larger metal tube. The design allowed to immerse 9 thermocouples in the fuel and water layers, and to decrease the spatial resolution to 5 or 10 mm depending on the position.

The mass loss due to combustion was measured with a Futek load cell. The same load cell was used in all the field tests. The load cell was sandwiched by two...
10.2. Field tests

Figure 10.9.: Load cell used for the Field tests

circular plates; one of \( D = 0.15 \) m and another of \( D = 0.3 \) m, as observed in Fig. 10.9. All these measurements were acquired with a Keithley USB3100 of 12bit resolution at an acquisition frequency of 10 Hz. Finally, the Sony DCR-SR210 camera was also used to monitor the flame evolution during the experiments. No fisheye was needed in these experiments as there was no space limitations for the field of view.

Figure 10.10.: Field tests experimental setup: \( D = 0.15 \) m

In addition to these two field test campaigns, another field test was performed by the Ecole des Mines d’Alès (EMA), with a similar experimental setup as the field test with \( D = 0.15 \) m. More information about this test is given in appendix B section B.1.7.

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Chapter 10. Experimental setup

Figure 10.11.: Field tests experimental setup: $D = 0.3 \text{ m}$

10.3. Conclusion

In this study, small scale boilover were performed both as field tests or as laboratory experiments. In all the experiments, a pool fire with a diesel-oil mixture lying on top of a water layer was ignited, and burned until the boilover apparition. All the experiments performed in this study are detailed in appendix B, section B.2.6.

Concerning the tested configurations, the laboratory experiments used both glass and metal reservoirs, and different parameters like the fuel or water layer thickness, the lip height or the reservoir diameter or material were varied. The field tests used a bit larger scale metal reservoirs.

Finally, a whole instrumentation measured the different parameters of a boilover experiments. The thermocouple rake used to measure the fuel and water temperature and the load cell apparatus used to measure the mass loss had to resist and not to be affected by the high temperature environment induced by the flame. Therefore, sheathed thermocouples protected by a silica braided sleeve, and a load cell separated from the reservoir by a cone point support and a bakelite plate were used. In addition, the flame visualizations were processed with a DIP algorithm, specifically implemented in this thesis for fisheye distortion correction and for flame detection.

All the experimental setup and instrumentation described in this chapter allowed to obtain a new experimental database of small scale boilover with diesel as fuel, that will be analyzed in chapter 11.
Chapter 11.

Experimental results

11.1. Introduction

The boilover phenomenon can be divided in three periods: the quasi-steady period, the premonitory period, and the boilover period, as explained in chapter 9. The results of the laboratory experiments and field tests follow the same classification. The analysis of the quasi-steady period is dedicated to the pool fire characteristics such as the burning rate, the flame geometry and radiation. For each parameter, the small scale experimental results of this study are compared with other experiments available in literature and with existing semi-empirical correlations. As the premonitory period is a transition from the quasi-steady period to the boilover period, this period is included in the analysis of the boilover period. In the analysis of the boilover period, the water boiling and flame enlargement phenomena are first investigated. Finally, the boilover intensity is characterized in terms of mass loss, flame radiation and geometrical change.

11.2. Steady burning analysis

11.2.1. Burning rate

After ignition of the diesel mixture and a small induction period needed to reach a quasi-steady burning, the mass loss becomes constant with time. The burning rate is then computed as the slope of the linear mass loss evolution. A first analysis of the results shows that the burning rate is influenced by the distance between the top of the reservoir and the fuel surface $H_{lip}$ called lip height, but also by the reservoir material, and by the pool diameter $D$.

The lip height effect has not been widely studied in literature, compared to the pool diameter effect. In 2000, based on experiments, Dlugogorski \[28\] shows that the relation between the fuel burning rate and the lip height initially follows an exponential decline, correlated by Eq. 9.4 given in chapter 9 section 9.2.1.
Chapter 11. Experimental results

Figure 11.1.: Influence of lip height on burning rate

present experimental data confirm the correlation of Dlugogorski, as shown in Fig. 11.1. The values of the fitted parameters are given in Table 11.1.

Table 11.1.: Influence of lip height on burning rate, correlations

<table>
<thead>
<tr>
<th>Configuration</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>D = 80mm, glass</td>
<td>$\dot{m} = 0.0074 \exp(-0.9084H^*)$</td>
</tr>
<tr>
<td>D = 80mm, metal</td>
<td>$\dot{m} = 0.0066 \exp(-0.7998H^*)$</td>
</tr>
<tr>
<td>D = 115mm, metal</td>
<td>$\dot{m} = 0.0097 \exp(-1.177H^*)$</td>
</tr>
</tbody>
</table>

The influence of the reservoir material can also be observed in Fig. 11.1. Indeed, keeping the pool diameter constant, the burning rate in a reservoir completely filled with fuel (no lip height) is equal to $7.4 \times 10^{-3} \text{ kg/m}^2 \text{s}$ for a glass reservoir and $6.6 \times 10^{-3} \text{ kg/m}^2 \text{s}$ for a metal reservoir, which gives approximately 10% decrease. This conclusion is in accordance with the previous studies, resumed in Hall [51], where the burning rate was observed to decrease as the thermal conduction of the material increases. Indeed, when the wall thermal conductivity increases, a larger part of the heat flux from the flame goes in the wall heating and is lost by convection with the surrounding. And as the burning rate is directly linked to the heat flux from the flame, as shown in Eq. 9.3 in chapter 9, a decrease of the heat flux decreases the burning rate.
11.2. Steady burning analysis

Figure 11.2.: Diesel pool fire burning rate for different pool diameters

To model the influence of the pool diameter on the burning rate, the correlations of Babrauskas [5], Rew [112], Chatris [22] and Munoz [95], presented in chapter [9] and applied to diesel are presented in Fig. 11.2 together with the present experiments. As explained in chapter 9 section 9.2.1, these correlations are valid for a reservoir with no lip height. For the field test results ($D = 0.15\text{m}$ and $D = 0.3\text{m}$), the experimental burning rate is directly plotted, even if a small lip height of 1 cm was present. Concerning the laboratory experiments with the smaller metal reservoirs ($D = 0.08\text{m}$ and $D = 0.115\text{m}$), the burning rates plotted in Fig. 11.2 are calculated from the correlations of Table 11.1, evaluated with a zero lip height (i.e. $H^* = 0$). The burning rates measured by Munoz [95] for diesel pool fires of 1.5 to 6 m in diameter are also presented.

For a quantitative comparison of the burning rate correlations presented in Fig. 11.2 with the small (this study) and large (the results of Munoz) scale experimental data, four validation metrics are used. First, the normalized mean square error (NMSE) represents the mean scatter between the experiments and a given correlation, based on a linear scale. The fractional bias (FB) represents the mean bias between the experiments and a given correlation, based on a linear scale. Two more validation metrics are used: the geometric mean (MG) which is the mean scatter on a logarithmic scale and the geometric variance (VG); the mean bias on a logarithmic scale. These four validations metrics are better explained and defined in appendix B section B.2.1. The Table 11.2 gathers all these metrics, evaluated
Chapter 11. Experimental results

for both experimental data (the present study and the Munoz study [95]). The burning rates for small scale reservoirs measured in this study are well correlated with the models of Rew [112] and Babrauskas [5]. Rew being slightly better in all the metrics evaluations. For large scale reservoirs, the best agreement is obviously found with the correlation of Munoz since this model relies on a fit based on the same experimental data as presented here. Leaving aside the correlation of Munoz, the lower metrics values can be found with the model of Rew, which can be regarded as a good tool to predict the burning rate of diesel pool fire from small to large scale.

Table 11.2.: Validation metrics for the burning rate correlations

<table>
<thead>
<tr>
<th></th>
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<th></th>
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</thead>
<tbody>
<tr>
<td>Present experiments</td>
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<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NMSE</td>
<td>0.0336</td>
<td>0.0329</td>
<td>0.6937</td>
<td>0.4180</td>
</tr>
<tr>
<td>FB</td>
<td>-0.1204</td>
<td>0.0704</td>
<td>0.7409</td>
<td>0.5826</td>
</tr>
<tr>
<td>MG</td>
<td>0.8859</td>
<td>1.0735</td>
<td>2.191</td>
<td>1.8312</td>
</tr>
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<td>VG</td>
<td>1.034</td>
<td>1.0333</td>
<td>1.9157</td>
<td>1.492</td>
</tr>
<tr>
<td>Munoz experiments</td>
<td></td>
<td></td>
<td></td>
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11.2.2. Temperature profiles

This section investigates the behavior of the thermal field. The parameters are: the fuel and the water layer thicknesses, the diameter, and the material of the reservoir. The Figure 11.3 right highlights the difference in the boilover apparition time for a glass reservoir with or without the presence of the thermocouple rake. It can be noticed that the thermocouple rake affects the fluid heating. But the thermocouples rake is present in all the laboratory experiments, which allows a parametric analysis of the temperature evolution. Moreover, the temperature measurements support the development of a uni-dimensional unsteady model of the thermal field.

11.2.2.1. Influence of fuel layer thickness

The influence of the fuel layer thickness on the thermal field is shown in Figure 11.3 left. The vertical temperature profiles at three fractions of the total time before the boilover apparition, are plotted for three different fuel thicknesses. In
11.2. Steady burning analysis

Fig. 11.3 and later, the temperature profile is non-dimensionalized by the initial fuel layer thickness, and the zero position represents the fuel-water interface. The ratio between the fuel and water layer thicknesses is equal to 3. As expected, the propagation of heat is faster for a thinner fuel layer. The influence of the fuel thickness on the boilover apparition time is linear, as observed in Fig. 11.3 right. Such a finding is consistent with previous studies detailed in chapter 9, Fig. 9.12.

Figure 11.3.: Left: Influence of fuel layer thickness $H_f$: $D = 0.08 m$, metal reservoir, $H_f/H_w = 3$. Right: Boilover apparition time $D = 0.08 m$

11.2.2.2. Influence of water layer thickness

The influence of the water layer thickness on the fuel and water heating is emphasized in Fig. 11.4 left that presents vertical temperature profiles at three different fractions of the total combustion duration. The two experiments have a fuel layer thickness of 16 mm and the water layer thickness is either 23 mm or 6 mm. The influence of the water layer is negligible at the beginning of the combustion, but as the burning time increases, the cooling effect of a thicker water layer thickness becomes more and more visible on the temperature profiles.

In the case of the large water thickness ($H_w = 23 mm$), the boilover did not occur. As observed in Fig. 11.4 right, if the water to fuel thickness ratio is larger than 2, the pool fire extinguishes before the boilover apparition (characterized here by the maximum flame area during boilover). More details about the physics of this phenomenon will be given in section 11.3.1.2. The water layer effect is also visible on the boilover apparition time as it can be appreciated in Fig. 11.3 right. For
Chapter 11. Experimental results

11.2.2.3. Influence of reservoir material

The influence of the reservoir material on the fuel and water heating is illustrated in Fig. 11.5 left. While the heating of the water layer is similar for both materials, the heating of the fuel layer is stronger in the glass reservoir. This is due to the influence of the heat conduction inside the walls of the reservoir, which is more pronounced with a metal reservoir than with a glass. This can also explain the difference in the time for boilover apparition (see Fig. 11.3); for the glass reservoir, the water layer is heated only by thermal conduction and radiation from the flame. Therefore, the water heating takes more time than if there is conduction inside the walls (like with the metal reservoir).

Figure 11.4.: Left: Influence of water layer thickness $H_w$: $D = 0.08\ m$, metal reservoir, $H_f = 16\ mm$. Right: Maximum flame area during Boilover occurrence

a small fuel layer thickness (i.e. $H_f = 8\ mm$) and a small water layer thickness (i.e. $H_w = 3\ mm$), the boilover appears after around 280 s. If the water layer is increased by a factor 10 (i.e. $H_w = 31\ mm$), the pool fire does not lead to a boilover and extinguishes after around 670 s, which is approximately 2.4 times more. And if the water to fuel layer thickness ratio stays below 2, decreasing the water layer decreases the boilover apparition time. To illustrate this conclusion, the boilover of a reservoir with a fuel layer thickness of $H_f = 32\ mm$ and a water layer of $H_w = 10\ mm$ appears after approximately 860 s. And if the water layer thickness is decreased by 30%, the boilover apparition time is decreased by moreless 11%.
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11.2.2.4. Influence of reservoir diameter

The influence of the reservoir diameter on the temperature profiles can be observed in Fig. 11.5 right that compares the fuel and water layers heating of two reservoir diameters: 80 mm and 115 mm. The effect of the diameter is similar to the effect of the material: the heating of the water layer is similar for both diameters, but the heating of the fuel layer is stronger for the larger reservoir. This reflects again the conduction effect inside the walls, which has a smaller effect on the liquid when the reservoir size increases. Concerning the boilover apparition time, for the two diameters, with the same fuel layer to diameter ratio \((H_f/D = 0.4)\), the time increases from around 850 s to 1350 s. This means that, even if the burning rate increases with the diameter (as shown in section 11.2.1) and would tend to decreases the boilover apparition time, the conduction effect through the walls is stronger than the change in burning rates. Therefore, the boilover apparition time increases because, as already pointed out, the wall conduction effect is less pronounced larger diameter reservoirs.

11.2.2.5. Temperature modeling

To better understand how the heat transfer mechanisms influence the fuel and the water heating and to predict the time evolution of the temperature profiles, a one-dimensional unsteady model has been developed. A 1D model that predicts temperature variations along the vertical direction is a good assumption, since Ito
Chapter 11. Experimental results

[67] showed that the temperature field at one given fuel height is relatively uniform across the pool diameter. Concerning the heat transfer mechanisms to be modeled, previous authors showed that the conduction from the fuel surface and the absorption of the flame radiation through the liquid have to be taken into account [65]. In the present study, it has been showed that the thermocouple rake influences the heating of the fuel and water layer and accelerates the time of boilover apparition. Therefore, this effect is included in the model. But the conduction effect along the reservoir walls is neglected because the model is applied only to glass reservoirs.

Temperature modeling for a glass reservoir

To model the influence of the thermocouple rake, two sets of equations have to be defined: the equations modeling the fuel and water heating, and the equations modeling the thermocouple rake heating. For the fuel and water heating, the model of Inamura [65] is followed. It assumes that the heat transfer mechanisms that contribute to the fluid heating are the conduction and the radiation absorption from the surface. In addition, in the present model, the heat loss from the side of the reservoir to the ambient (at a temperature $T_{amb}$) is taken into account by means of a convective heat transfer coefficient $h$ and the exchange with the rake (at a temperature $T_p$) is taken into account by means of two convective heat transfer coefficients: $h_{pf}$ for the interaction between the rake and the fuel layer and $h_{pw}$ for the interaction between the rake and the water layer.

\begin{align}
\rho_f C_{pf} \frac{\partial T_f}{\partial t} &= \lambda_f \frac{\partial^2 T_f}{\partial x^2} + \frac{\partial \dot{q}_f}{\partial x} + \frac{4hD}{D} (T_{amb} - T_f) + \frac{4h_{pf}L}{D} (T_p - T_f) \quad (11.1) \\
\rho_w C_{pw} \frac{\partial T_w}{\partial t} &= \lambda_w \frac{\partial^2 T_w}{\partial x^2} + \frac{\partial \dot{q}_w}{\partial x} + \frac{4hD}{D} (T_{amb} - T_w) + \frac{4h_{pw}L}{D} (T_p - T_w) \quad (11.2)
\end{align}

The Eq. 11.1 and 11.2 represent the change of internal energy of the fuel (at a temperature $T_f$) and the water (at a temperature $T_w$) respectively. The first term on the right hand side of these equations is the conduction, the second term represents the in-depth radiation absorption, the third term is the heat loss by convection with the environment and the last term is the convective exchange between the thermocouple rake and the fluids.

The model is closed by three boundary conditions: a first one at the fuel surface, a second at the fuel - water interface and a last one at the bottom of the water layer. The boundary condition at the fuel surface is a Dirichlet condition (Eq. 11.3); the fuel surface is always at the same temperature, corresponding to the vaporization temperature, measured with thermocouples. To take the fuel combustion into account, the fuel layer thickness $H(t)$ decreases with time according to Eq. 11.4. The boundary condition at the fuel - water interface guarantees the continuity of the heat flux (Eq. 11.5). And finally, the boundary condition at the bottom is a
11.2. Steady burning analysis

convection surface condition (Eq. 11.6), that fits better the experimental profiles than an adiabatic boundary condition.

\[
T_{N_w + N_f - z} = T_b \quad (11.3)
\]

\[
H = \frac{\dot{m}}{\rho_f \pi (D^2/4)} j \Delta t \quad (11.4)
\]

\[
\lambda_f \frac{\partial^2 T_f}{\partial x^2} \bigg|_{x=N_w} = \lambda_w \frac{\partial^2 T_w}{\partial x^2} \bigg|_{x=N_w} \quad (11.5)
\]

\[
\lambda_w \frac{\partial T_w}{\partial x} \bigg|_{x=0} = h (T_{amb} - T_w) \quad (11.6)
\]

The radiation absorption, inserted in Eq. 11.1 and 11.2, is modeled by the Beer-Lambert law, as expressed in Eq. 11.7. This law assumes that the heat flux at the surface of the fuel is progressively absorbed by the fuel and decreases exponentially as the depth increases. The model is easy to implement, but requires the determination of the radiation absorption coefficient \( \mu \), both for fuel and water, as well as the radiation heat flux at the fuel surface \( \dot{q}_w \).

\[
\dot{q}^w = \dot{q}_w^0 \exp(-\mu x) \quad (11.7)
\]

Concerning the determination of the radiation absorption coefficients, Inamura [65] measured a value of \( \mu = 145 \, m^{-1} \) for toluene, and Garo [41] performed measurements on a range of fuels similar to diesel (octane, decane, etc...) and found radiation absorption coefficients within the range of \( \mu = 117 - 185 \, m^{-1} \). These authors have kept the radiation absorption constant with time in their model. But when burning, the diesel-oil mixture creates a lot of soot particles. Some soot is not evacuated through the flame, shrinks in the fuel and darkens the fuel color with time. This evolution of the fuel color with time can be observed in Fig. 11.6 left that groups pictures of the fuel layer after different combustion times.

An evolution of the radiation absorption coefficient with time can then be estimated from the fuel color change. Each fuel image color scale is first converted to gray scale. Then, the gray scale intensity range, which goes from 0 to 255, is converted to a radiation absorption coefficient range, from \( \mu = 140 \, m^{-1} \) to \( \mu = 300 \, m^{-1} \). The initial value of \( \mu = 140 \, m^{-1} \) is chosen for the initial radiation absorption coefficient, as measured by previous authors for similar fuels. The final radiation absorption coefficient is set to \( \mu = 300 \, m^{-1} \) because it is in the order of magnitude of more viscous and black fuels, like crude oil. The radiation absorption coefficient of water has already been measured by previous authors and shows a
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Figure 11.6.: Left: Evolution of fuel color with time. Right: Evolution of radiation absorption coefficient with time calculated from the color evolution.

The radiation heat flux at the fuel surface needs also to be determined. Garo [41] and Inamura [65] have used measured values. Garo measured the radiation heat flux at the fuel surface with a Medtherm radiometer with a sapphire window and a cooling system, positioned at the fuel surface of a reservoir with no lip height. From these experiments, the measured heat flux at the fuel surface ranges from 5.81 kW/m² for heating oil to 14.16 kW/m² for benzene. In 1999, Hamins [55] developed a model for the conduction, convection and radiation terms of the heat feedback from the flame to the fuel surface. The radiation term is regarded as the isotherm emission from gaseous CO₂ and H₂O and gray body emission from soot, as already proposed by Siegel and Howell [124]. Because of the relatively low boiling point of the diesel, the re-radiation from the pool surface to the ambient has to be included in the calculation of the radiated heat flux that penetrates the fuel layer. The model of the radiation heat flux is given in Eq. 11.8 to 11.10. The details of the calculation of the emissivities needed in Eq. 11.9 can be found in appendix B section B.2.2.
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\[ \dot{q}_{r0}^v = \dot{q}_{rad}^v - \dot{q}_{rerad}^v \]  
(11.8)

\[ \dot{q}_{rad}^v = \sigma T_f^4 (1 - 0.08) \left[ 1 - (1 - \epsilon_{soot}) (1 - \epsilon_w) (1 - \epsilon_{co_2}) \right] \]  
(11.9)

\[ \dot{q}_{rerad}^v = \sigma (T_b^4 - T_{amb}^4) \]  
(11.10)

The model of thermocouple rake is simpler. The heat transfer processes are reduced to the conduction along the thermocouple rake height, and the convection between the rake and the fluids. The model is expressed in Eq. 11.11 to 11.13.

The part of the rake above the fuel is subjected to convection with the flame.

\[ \rho_p C_{pp} \frac{\partial T_p}{\partial t} = \lambda_p \frac{\partial^2 T_p}{\partial x^2} + \frac{4 h_{pf}}{D} (T_F - T_p) \]  
(11.11)

\[ \rho_p C_{pp} \frac{\partial T_p}{\partial t} = \lambda_p \frac{\partial^2 T_p}{\partial x^2} + \frac{4 h_{pw}}{D} (T_w - T_p) \]  
(11.12)

\[ \rho_p C_{pp} \frac{\partial T_p}{\partial t} = \lambda_p \frac{\partial^2 T_p}{\partial x^2} + \frac{4 h}{D} (T_p - T_F) \]  
(11.13)

For the boundary conditions of the thermocouple rake, the upper part undergoes convection with the flame environment, as expressed in Eq. 11.14, and the bottom is considered adiabatic (Eq. 11.15).

\[ \lambda_p \frac{\partial T_p}{\partial x} \bigg|_{x=N_w+N_f-z} = h (T_F - T_w) \]  
(11.14)

\[ \lambda_p \frac{\partial T_p}{\partial x} \bigg|_{x=0} = 0 \]  
(11.15)

To solve this model numerically, the two sets of equations (for the fluids and for the thermocouples rake) are then discretized in a finite-differential form. The fuel and water layer, and the thermocouple rake are discretized in nodes with a spatial resolution of 1 mm, as schematized in Fig. 11.7. Before ignition, the fuel has \( N_c \) nodes and the water has \( N_w \) nodes. An explicit method is used with a forward difference approximation for the time derivative and central difference approximations for the spatial derivatives. The equations 11.1 to 11.6 and 11.11 to 11.15 are expressed in their discretized form in appendix B section B.2.2. The time interval used to solve these equations is chosen to satisfy the numerical stability; the Fourier numbers of the fuel, the water and the thermocouple rake, defined in Eq. 11.16 have to be smaller than 0.5.
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Figure 11.7.: Schematic of temperature modeling

\[ F_o = \frac{\alpha \Delta t}{\Delta x^2} < 0.5 \quad (11.16) \]

The comparison between the 1D model for a glass reservoir and the temperature measurements can be observed in Fig. 11.8. The values of the convective heat transfer coefficients, adapted to fit the model and the experiments, are showed in Table 11.3. The best fit between the model and the measured temperatures is obtained with a flame temperature of 1100 K. The experimental profiles are well modeled when the water layer thickness is relatively small, as illustrated in Fig. 11.8 left. The model only deviates from the experiments close to the boilover apparition time, where the experiments follow a different shape. This change in the experimental profile is probably due to the water boiling, as 0.95tbo is very close to the boilover apparition, and most on the time already in the premonitory period. If the water layer thickness is increased, as shown in Fig. 11.8 right, the model is underestimating the heating in the fuel-water interface region while the fuel surface and the water bottom regions are correctly simulated. Since the model represents rather well the experiments, it can be used to investigate the influence of the fuel and water layer thicknesses, and see if the model agrees with the conclusions drawn from the experimental profiles.

Table 11.3.: Convective heat transfer coefficient values

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<tr>
<th>Coefficient</th>
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<tr>
<td>( h_{pf} )</td>
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</tr>
<tr>
<td>( h_{pw} )</td>
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<td>( h )</td>
<td>10</td>
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</table>

180
11.2. Steady burning analysis

Concerning the influence of the fuel layer thickness, the experimental profiles have shown in Fig. 11.3 that when the fuel layer thickness decreases, heat inside the fuel layer thickness propagates faster. By looking at Fig. 11.9 right which represents the experimental profiles measured in a glass reservoir, the same conclusion can be drawn, except that this tendency is less clear for smaller fuel layer thicknesses, when approaching the boilover apparition time. The thermal model developed in this thesis follows well the conclusion drawn from the glass reservoir, as observed in Fig. 11.9 left. But the model diverges from the measurements as the intermediate fuel layer thickness seems to be heated faster than the smaller one. This difference could be due to an increased effect of the wall conduction when the fuel and water layer thicknesses are small, which leaves a large lip height heated by the flames.

Concerning the influence of the water layer thickness, the experimental profiles measured inside a metal reservoir and plotted in Fig. 11.4 showed that a larger water layer thickness has a cooling effect on the fuel layer. In a glass reservoir, as observed in Fig. 11.10 right, the cooling effect of the water layer is less clearly visible. But it affects the time to boilover; by increasing the water layer thickness from 5 mm to 23 mm, the time to boilover is increased by 43 %. In the glass reservoir, the water layer temperature evolution is proportionally similar for the two water layer thicknesses. But as the time to boilover increases with the water layer thickness, the fuel has more time to be heated. And as the conduction effect of the reservoir walls in a glass reservoir is negligible, the cooling effect of the water is affecting less the fuel layer heating. As observed in Fig. 11.10 left, the model follows well the glass reservoir conclusion for the fuel layer, but shows a stronger cooling effect of the water layer.

Figure 11.8.: Comparison between the thermal model and the experiments. Left: $H_f = 16\text{mm}$, $H_w = 5\text{mm}$, Right: $H_f = 16\text{mm}$, $H_w = 23\text{mm}$
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Figure 11.9.: Influence of \( H_f \). Left: Profiles divided by initial \( H_f \), Right: Profiles divided by current \( H_f \).

Figure 11.10.: Influence of \( H_w \). Left: Profiles divided by initial \( H_f \), Right: Profiles divided by current \( H_f \).

Conclusions on the temperature modeling

Based on the temperature measurements with a glass reservoir, a 1D thermal model has been developed. This model takes into account the conduction and the radiation absorption through the fuel and water layers of the heat flux at the fuel surface, the heat loss by convection with the environment and the convective exchange between the thermocouple rake and the fluids. In addition to the model
11.2. Steady burning analysis

developed by Inamura [65], the radiative heat flux at the fuel surface has been estimated by the model of Hamins [54] instead of using a measurement. The change of the radiation absorption coefficient of the fuel with time is also taken into account.

Providing a flame temperature of 1100 K and convective heat transfer coefficients between the rake and the fluids of 150 W/m²K, this new model show a good agreement with the experiments. The model is very efficient for large fuel thicknesses and small water thicknesses, which is the configuration encountered in most industrial sites. This model will be used further in this chapter to estimate the boilover occurrence time.

11.2.3. Flame shape

A flame from a pool fire is a diffusive flame and can be decomposed in three regions: the base flame, the intermittent flame (where the flame oscillations are visible) and the plume region, at the top, as shown in chapter 9 section 9.2.1. In the Figure 11.11, representing the average flame during the steady burning period, the two first regions are clearly visible. The base region, represented as the small white cone, is present in all the images. The intermittent flame is observed as the larger grey cone. The black line at the left of the flame is due to the presence of the thermocouple rake.

![Figure 11.11. Mean flame](image)

Figure 11.11.: Mean flame $D = 0.115$, $H_f/D = 0.4$, $H_f/H_w = 3$

The principal parameters of the flame are: the length, the puffing frequency and the radiation intensity. They are computed from the results of this experimental study, and are compared with the correlations discussed in chapter 9 section 9.2.1 and with other experimental data published in literature.
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11.2.3.1. Flame length: L

The flame length is calculated by analyzing its intermittency (see chapter 9 section 9.2.1). For each reservoir configuration, 999 images are taken during the quasi-steady period of the pool fire (1 to 2 minutes after ignition) which corresponds to almost 40 s. In each image, the flame length is computed by image processing, using the flame detection algorithm developed in Matlab, and described in chapter 10 section 10.1.2.5. The flame length is calculated as the distance between the reservoir edge and the upper pixel of the flame. Finally, the flame intermittency is calculated from the 999 detected flame lengths, using Matlab. The flame intermittencies (I) for $D = 0.115 \text{ m}$ metal reservoirs, with a constant fuel to water layer thickness ratio of 3 are shown in Fig. 11.12 left. They are normalized by the average flame length (with an intermittency I=0.5). It can be observed in Fig. 11.12 left that the normalized intermittency changes very slowly with the heat release rate, directly linked to the lip height, as already observed by Zukoski [154].

![Figure 11.12: Left: Normalized flame intermittency, Metal reservoir $D = 0.115\text{ m}$, $H_f/H_w = 3$, Right: Minimum, average and maximum flame length for metal reservoir with minimum lip height](image)

The minimum and maximum flame length can also be computed by using the intermittency values of respectively $I=0.05$ and $I=0.95$. Interestingly, Ferrero [36] indicated that the minimum and maximum flame lengths of diesel fires can be determined by multiplying the average flame length by respectively 0.633 and 1.397. As observed in Fig. 11.12 right where the flame lengths determined from the three definitions are plotted against the non-dimensional burning rate (defined in Eq. 9.7 of chapter 9), these multiplication factors agree very well with the
11.2. Steady burning analysis

minimum and maximum flame lengths determined from the intermittency curves of the present experiments.

![Flame length comparison](image)

Figure 11.13.: Flame length, compared with experimental data and correlations from literature

The correlations of Thomas (considering wind (2) or not (1)) \[131\], Moorhouse (with a cylindrical (1) or a conical (2) flame shape) \[91\], Mangialavori \[83\], Pritchard \[107\], and Ferrero \[36\] are compared in Fig. 11.13 and in Table 11.4. The correlations are compared with the average flame length for the different configurations tested in this study. The non-dimensional burning rate \( m^* \) of the measurements is calculated from the Dlugogorski relations developed in section 11.2.1, Table 11.1.

In addition, the experiment performed by the EMA is also displayed. Previous experimental data showed by Mudan \[93\], grouping 8 previous publications of pool fire experiments, with diameter ranging from 0.004 to 80 m, mostly higher than 1 m, with LNG, gasoline, kerosene, diesel or acetone as fuel, and experimental results of the POOLFIRE database detailed by Rew \[112\] are also plotted, in addition to the experiments of Ferrero \[38, 36\] performed with diesel as fuel (1.5 to 6 m diameter).

The models can be divided into two categories, depending on the slope of the correlation, as shown in Fig. 11.13. Thomas \[131\] and Mangialavori \[83\] are modeling a flame length that increases more strongly with the dimensionless burning rate than the other authors. These correlations also follow better the experiments. Looking at the validation metrics of Table 11.4, Mangialavori \[83\] gets the best estimates for the whole series of experimental data except for the data of Ferrero \[36\]. A systematic bias is also observed with the data obtained in the frame of this thesis, which could be due to the presence of the thermocouple rake or to an error in the definition of the flame base. The flame length calculated from the EMA
Chapter 11. Experimental results

The puffing frequency is calculated from the time evolution of the flame height. In 2007, Ferrero [36] calculates the flame puffing frequency in two steps. First, the time evolution of the flame height is smoothed and then, the puffing frequency is calculated as the ratio between the number of peaks in the flame length signal.

### 11.2.3.2. Flame puffing frequency: \( f \)

The puffing frequency is calculated from the time evolution of the flame height. In 2007, Ferrero [36] calculates the flame puffing frequency in two steps. First, the time evolution of the flame height is smoothed and then, the puffing frequency is calculated as the ratio between the number of peaks in the flame length signal,

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</table>
11.2. Steady burning analysis

divided by the average time duration of one oscillation. Because in this study, 999 images are used for each test configuration, the length of the signal used here is too long to apply this methodology.

![Figure 11.14.: Left: Calculated puffing frequency based on model order, Right: FFT versus MEM with order 20](image)

Figure 11.14.: Left: Calculated puffing frequency based on model order, Right: FFT versus MEM with order 20

Another approach is to calculate the FFT of the signal. However, Henriksen [59] explains that, due to the turbulent nature of the flame, the FFT is too noisy to identify clearly the frequency peak. Therefore, the power spectrum is calculated by using the Burg implementation of the maximum entropy method [59]. More details are given about the maximum entropy method in appendix B section B.2.3. The Burg method gives a power spectral density expressed as the distribution of power per unit frequency. This algorithm depends on the selection of an appropriate model order but Henriksen does not give any recommendation for a proper choice of the model order. Consequently, for one flame time evolution signal, the method has been applied with varying order and the frequency peak obtained was monitored. From Fig. [11.14] left, it is clear that an order higher than 10 is mandatory. In this study, an order of 20 is chosen. More details about this procedure are given in appendix B section B.2.3. Once the correct model order is chosen, the maximum entropy method (MEM) gives smoother results than the traditional FFT approach as observed in Fig. [11.14] right, as the high frequency noise is considerably reduced.

The resulting flame puffing frequency, calculated with the Burg method, is compared with the correlation of Pagni [102] (more details in chapter 9 section 9.2.1) in Fig. [11.15] left. The data presented by Pagni [102] and Ferrero [36] are also plotted. The experiments of this study follow well the correlation of Pagni, and show the same scatter of the data around the correlation than the previous exper-
Chapter 11. Experimental results

Experimental data. The validations metrics are reported in appendix B section B.2.3. The experiment performed by the EMA has not been used in this analysis since the strong effect of the wind did not allow a correct estimation of the flame puffing frequency.

Despite the scatter appreciated in Fig. 11.15 left, the present data point out an effect of the lip height on the puffing frequency. When the lip height increases, the flame puffing frequency decreases, as observed in Fig. 11.15 right, where the flame frequency is plotted in a non-dimensional form, as referred to Eq. 9.10 in chapter 9. In addition, it can be observed that as the lip height approaches to zero, the flame frequency is better predicted by the correlation of Pagni. Physically meaning, the rate at which the flame oscillates determines the rate at which air is entrained in the flame. In 1988, Weckman performed an experimental study on mid-scale pool fires and concluded that the flame oscillation is due to the formation of large structures around the flame, that control air entrainment into the flame. Weckman [141] also concluded that these structures are more due to a fluid dynamic instability external to the fire than the local accessibility of air at the base of the fire. But the results presented in Fig. 11.15 right tend to prove the opposite, since increasing the lip height is decreasing the air entrainment into the flame.

11.2.3.3. Flame radiation

Due to the combustion process, the flame radiates. In order to know the potential hazard of a given fire, the radiation flux received at a given distance from the flame
11.2. Steady burning analysis

is an important quantity to determine. The prediction of the point source model with the flame radiative fraction correlations of Mc Grattan [88] and Sarofim [117], and the solid flame model with the flame emissive power correlations of Mudan [94], Shokri [123], Duiser (TNO) [40] and Munoz [96], are compared to the present measured radiation in Table 11.5 and in Fig. 11.16. The experiments performed in this study are presented together with the experiment performed by the EMA. For each configuration, the radiation experimental value corresponds to a time average of the measured radiation during the whole quasi-steady period.

![Figure 11.16.: Comparison between model and experiments. Left: Point Source model (Sarofim [117]), Right: Solid flame model (Munoz [96])]()

Looking at the Table 11.5 where the validation metrics are presented, the point source model that reproduces the best the measured flame radiation during the quasi-steady period is the model of Sarofim [117]. The measured and predicted radiations using this model are compared in Fig. 11.16left. It can also be observed in Table 11.5 that the solid flame model that reproduces the best the radiation is the one with the emissive power modeled by Munoz [96]. This model is compared with the present experimental results in Fig. 11.16right, and shows much better validation metrics than the model of Sarofim. Recalling that Munoz worked with diesel pool fires of large scale, this is probably why this model agrees better since the same fuel has been used here.
Chapter 11. Experimental results

Table 11.5.: Validation metrics for the flame radiation modeling

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<tr>
<th></th>
<th>Munoz</th>
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<th>Duiser</th>
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11.3. Boilover analysis

11.3.1. Boilover phenomenology

11.3.1.1. Fuel and water layer visualizations

The main theory about boilover is that the flame enlargement is due to a piston effect generated by the enormous volume increase during the water vaporization. Although some visualizations have been performed by previous studies [41, 63], they do not highlight the exact starting moment of boilover and the piston effect has not been observed. In this study, high-speed visualizations have been performed to better understand this piston effect.

To illustrate each step of the water boiling, from the fuel ignition to the premonitory period, images of the fuel and water layers have been extracted from the high-speed visualization. They are displayed in Fig. 11.17. These images show that first some bubbles appear on the fuel surface close to the walls (Fig. 11.17. A). After about a third of the boilover occurrence time, some water vapor bubbles start to grow at the fuel-water interface and reach the top fuel surface with a size of about 5% of the reservoir diameter (Fig. 11.17. B). The bubble generation rate accelerates with time and bubbles in cluster are becoming visible at the fuel surface (Fig. 11.17. C). At that time, the initial bubble size at the fuel-water interface is big enough to be visible, showing columns of bubbles through the fuel thickness.
11.3. Boilover analysis

Figure 11.17.: Fuel & water visualization during the quasi-steady period (bottom and side view): A: \( t \approx 0.1 - 0.2t_{bo} \), B: \( t \approx 0.2 - 0.3t_{bo} \), C: \( t \approx 0.5 - 0.6t_{bo} \) and D: \( t \approx 0.7 - 0.8t_{bo} \)

(Fig. [11.17] D). Coming closer to the boilover occurrence, the fuel-water interface experiences a fast growth of bubbles of bigger size (15 to 20% of the reservoir diameter). That creates waves at the fuel-water interface and enhances the water boiling. These periods of strong boiling prior boilover event are characteristic of the premonitory period of boilover [63]. During this period, the larger water bubbles generated at the fuel-water interface are wrapped by a layer of fuel as they escape from the fuel-water interface. When they are projected into the flame zone, they burn and emit a crackling noise. The Figure 11.18 shows the evolution of the mass of fuel and water measured by the load cell (more details in chapter 10 section [10.1.2.3]) plotted together with the sound recorded by the microphone of the digital camera. The test configuration of the Fig. 11.18 is similar to the visualizations, except that the fuel is here the oil-diesel mixture. A coupling is visible between the sound and the mass and proves the presence of crackling noise.

The evolution of the fuel and water layer at boilover onset can be visualized in Fig. 11.19. The growth of one big bubble leads to a boiling front that propagates radially all along the fuel-water interface, ejecting the upper fuel layer and leading to the increase of flame size, characteristic of boilover phenomenon. During this period, the piston effect of the water boiling induces a strong force, which is visible on the mass loss measurement of Fig. 11.18 at \( t - t_{bo} = 0 \). The boilover onset produces also a larger sound than during the premonitory period. This description of the piston effect states that the vaporization of the water layer leading to the flame enlargement is homogeneous.

The boilover behavior can also be quantified using an observation from the side view of the reservoir. The fuel surface and the fuel-water interface positions are
Chapter 11. Experimental results

Figure 11.18.: Sound recorded by camera and mass loss around the boilover period

monitored by using an edge detection technique based on Forward Step filter algorithm, available in the LEDAR software (Level Detection and Recording) [106] developed at the von Karman Institute (more details about this software in appendix A, section A.7). The tracking of the two interfaces leads to the same conclusions drawn from the bottom visualizations. First, a big bubble appears at the fuel-water layer interface, that disturbs the fuel layer as shown in Fig. 11.20 for \( t = t_{\text{bub}} \). This flow perturbation lasts until a two-phase (boiling) front propagates along the entire reservoir (around \( t = t_{\text{bub}} + 0.12 \text{s} \) in Fig. 11.20).

Among the 10 tests performed for visualization purposes using the same configuration, the phenomenology presented in Fig. 11.19 have always been observed. But the boilover onset duration, estimated from the apparition of the first big bubble to the complete propagation of the boiling front, shows an average of 278.5 ms, with a large standard deviation of 154.5 ms.

11.3.1.2. Pool fire without boilover occurrence

The Fig. 11.21 helps to better understand the physical process that leads to a non occurrence of boilover. First, it can be observed in Fig. 11.21 left that the flame starts to decrease at the same time as the sound emitted by the pool fire increases. As observed in the previous section, an increase of the sound amplitude is typical of the micro-explosion noise, that is emitted during the premonitory period, when the water vaporization starts to increase. It can also be observed in Fig. 11.21 right that, during the premonitory period (that starts a bit before
11.3. Boilover analysis

Figure 11.19.: Bottom visualization of boiling front at boilover onset

Figure 11.20.: Time evolution of fuel-water interface & fuel surface at Boilover onset

500 s), oscillations appear at the fuel-water interface. From these figures, it can be concluded that, once the water starts to vaporize, it disturbs the fuel layer and decreases its temperature. But due to the large water thickness and the conduction along the walls, the decrease of temperature is too strong and the fuel layer extinguishes.
11.3.1.3. Temperature evolution

Temperature profiles measured during the experiments allow to see the evolution of the thermal fuel and water layer behavior during boilover. However, it is worthwhile to notice that the sampling frequency of the temperature (3 Hz) is small enough compared to the few seconds of the boilover duration. In terms of temperature evolution, the phenomenon can be divided in three steps: before, during, and after the boilover, as observed in Fig. 11.22 left. Before the boilover apparition, the temperature profiles have similar shapes than observed in the analysis of the quasi-steady period in section 11.2.2 and represented as blue profiles in Fig. 11.22 left. Once the boilover starts, the water boiling front expands the whole reservoir, expels the fuel, and induces a mixing that makes the temperature homogeneous along the reservoir height (red profiles in Fig. 11.22 left). Finally, the boilover ends when the water boiling stops. If some fuel remains in the reservoir, the temperature profile comes back to its initial shape, as before the boilover apparition like observed through the black profiles in Fig. 11.22 left. The only difference is the height of the profile which is lower due to the fuel expelled during the boilover. The fuel then starts to burn again in a quasi-steady way as before the boilover. If enough fuel remains in the reservoir, the premonitory and boilover periods can be repeated.

As the temperatures are measured along the entire fuel and water layer thicknesses, they allow to measure the fuel-water interface temperature at the boilover apparition. The boilover apparition time can be determined from the dramatic change in the temperature profile shape during the boilover. The time at which
11.3. Boilover analysis

The temperature profile starts to become uniform can be used as a definition for the boilover occurrence time. The fuel - water interface temperature at the boilover apparition is displayed in Fig. 11.22 right as function of the initial fuel layer thickness, for the different reservoir configurations tested in this study. In addition to the present experiments, the results of Garo, obtained with a 150 mm reservoir filled with heating oil (FOD), are also displayed in Fig. 11.22 right. When the reservoir is made of metal, the fuel thickness clearly influences the fuel-water interface temperature. Nevertheless, the reservoir diameter or the fuel type seem to have a small influence on the boilover fuel-water interface temperature. Moreover, the temperature at boilover apparition for glass reservoirs seems to be independent of the fuel thickness, showing a stable value of 393 K. This temperature is the one typically cited by other authors as the fuel-water interface temperature at boilover apparition [41, 17]. Therefore, the influence of the fuel-water interface with the fuel thickness in metal reservoirs is probably due to the conduction effect of the metal walls.

It is interesting to analyze why the boilover occurs when the fuel-water interface temperature is equal to 393 K. As detailed in chapter 9, this temperature has already been observed in previous studies [3, 41]. Garo [11] and Patej [103] have explained this temperature difference from the weight of the fuel layer that increases the pressure at the fuel-water interface. If this assumption is followed, a water would boil at 393 K if the pressure is increased up to 0.1977 MPa, which corresponds to a diesel height of 11.5 m (from the formula $P = P_{atm} + \rho_f \cdot g \cdot H_f$). The observation of a fuel-water interface temperature of 393 K at boilover occurrence, even for small fuel layer thicknesses proves that another mechanism causes the
Chapter 11. Experimental results

Figure 11.23.: Boiling curve for water at 1 atm: Surface heat flux as a function of excess temperature [66]

superheating of the water prior boilover, and can be linked to the boiling curve of water, as displayed in Fig. 11.23. This curve shows that a pool boiling experiences different regimes. For a superheat below 5 K, the regime is called free convection boiling, were no bubbles are created, showing only free convection effects. The 5 to 30 K superheat interval is called the nucleate boiling regime. Boiling appears first with isolated bubbles. As the temperature increases, the bubble formation increases and causes bubble interference and coalescence. From 10 to 30 K of superheat, bubbles forming jets or columns are observed. And when the critical heat flux is reached (in Fig. 11.23 for a superheat higher than 30 K), the boiling is in the transition regime, where bubble formation is so rapid that a vapor film forms on the surface. This description of the evolution of the water boiling with the degree of superheat is very close to the high-speed images as displayed in Fig. 11.17 and Fig. 11.19. The fuel-water interface temperature at boilover is
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then consistent with a superheat higher than the critical heat flux. In the Fig. [11.23] the critical heat flux is observed for a superheat of 30 K while the fuel-water interface temperature was measured closer to 20 K. But the boiling curve as displayed in Fig. [11.23] corresponds to water in contact with a solid interface, and not a liquid, as in the present experiments.

11.3.1.4. Flame enlargement

The major boilover hazard is the flame enlargement resulting from the piston effect induced by the water boiling, as observed from high-speed visualization performed in laboratory tests. A typical flame enlargement recorded with the high-speed camera is shown in Fig. [11.24]. The water boiling front is first pushing the fuel into the flame, which enhances the combustion and increases the flame size to form a fireball shape which evolves into a column. Once the water boiling stops, the flame decreases and comes back to its original flame size or extinguishes if the remaining quantity of fuel after the boilover is not sufficient to continue to burn as a pool fire. If the whole fuel layer is not participating to the flame enlargement, several successive boilovers can be observed.

Figure 11.24.: Flame enlargement during Boilover apparition (\(D = 0.115\, m\) metal reservoir, \(H_f/D = 0.2\), \(\Delta t = 0.11\, s\) between images

A quantitative analysis of the flame images recorded during boilover has been performed by using the LS-PIV technique (Large Scale Particle Image Velocimetry), developed at the von Karman Institute. With this technique, the flow velocity can be calculated from the movement of large scale structures instead of from individual particles. Practically, the high-speed flame images are processed with an algorithm for the interrogation of PIV images, based on cross-correlation [118]. This image processing results in a velocity field only inside the flame. Two examples are shown in Fig. [11.26] and [11.27] middle image. To determine the velocity field around the flame and in the plume region, another technique like the classical PIV technique is needed.
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For each image, a vertical velocity profile at the centerline of the reservoir and an horizontal profile at about $4.6D$ above the reservoir are extracted, as shown in Fig. 11.25 left. These profiles are then averaged spatially and the evolution of these two spatial averages is compared in Fig. 11.25 right with the total flame area, calculated by using the flame detection algorithm explained in chapter 10 section 10.1.2.5.

The LS-PIV analysis highlights a correlation between the flame size and the flame velocity magnitude, as illustrated in Fig. 11.25. The correlation coefficient between two variable is defined Eq. 11.17 and evaluates the correlation between two parameters $a$ and $b$, with a result of 1 in case of a perfect correlation. If this equation is applied between the evolution of the vertical velocity profile and the flame area, the global cross-correlation is equal to 0.855, which rises up to 0.985 for the period between the beginning of the signal and the maximum flame area. If this equation is applied between the evolution of the horizontal velocity profile and the flame area, the global cross-correlation is equal to 0.882, which rises up to 0.936 for the period between the beginning of the signal and the maximum flame area.

$$\text{Corr}(a,b) = \frac{\text{Cov}(a,b)}{\sqrt{\text{Cov}(a,a) \cdot \text{Cov}(b,b)}} \quad (11.17)$$
11.3. Boilover analysis

Once the fuel is ejected from the reservoir, the flame size and velocity magnitude increase until reaching a maximum at about \( t = 0.91 \) s (red line in Fig. 11.25 right). After reaching the maximum area, the flame size decreases until reaching almost its original size. When the flame size decreases, a deviation from the flame size and velocity magnitude correlation is observed. In Fig. 11.25 a second peak in the velocity evolution is visible at about \( t = 1.2 \) s (blue line in Fig. 11.25 right), while the flame size is decreasing. Finally, the flame area and velocity increases together a second time (after \( t = 1.5 \) s). This is not a second boilover but more a pool fire aside of the reservoir created by the ejection of fuel during the boilover.

To go deeper in the understanding of the flame size and velocity evolution, the analysis focuses on the two velocity peaks at \( t = 0.91 \) s and \( t = 1.2 \) s, showed in Fig. 11.25 right (red and blue lines respectively). For these two specific times, the flame image recorded with the high-speed camera is compared to the velocity field obtained from the LS-PIV technique, and to the vorticity field, derived from the velocity field through the Eq. 11.18. This equation has been implemented in Matlab with a finite difference scheme proposed in \[109\]. Details of this implementation can be found in appendix B section B.2.5.1.

\[
\omega = \frac{\partial V}{\partial X} - \frac{\partial U}{\partial Y} \quad (11.18)
\]

The Fig. 11.26 shows the flame image, and velocity and vorticity fields at the time of the maximum flame area. It can be observed in the velocity field that the movement of the flame is mainly coming from the flame centreline and from the vorticity field, shear is visible on the flame boundary. Therefore, the flow characteristics of the flame at maximum area are similar to a round jet. The Fig. 11.27 represents the flame at the second velocity peak (\( t = 1.2 \) s) and shows that, even if the flame is smaller, the upper part is moving faster than the larger flame. In addition, the shear is increased.

\[
\dot{Q}_D = V \Delta H_c \rho_f v \left( \pi D^2 / 4 \right) \quad (11.19)
\]

\[
\dot{Q}_D^* = \frac{\dot{Q}_D}{\rho_a C_p a T_a \sqrt{g D D}} \quad (11.20)
\]

A general correlation between the flame size and velocity has already been observed and different authors have proposed a relationship between the flame height and the Froude number, which is a measure of the relative importance of inertia and buoyancy \[153, 58, 27\]. The value of the Froude number is then used to classify the different types of fire, ranging from buoyancy driven flame for low Froude numbers, to fully turbulent jet fires for high Froude numbers. In an attempt to find a correlation independent on fuel type, Zukoski \[153\] uses instead a non dimensional heat release rate \( \dot{Q}_D^* \), which is similar to the square root of a Froude
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Figure 11.26.: Left: flame visualization, Middle: flame velocity field, right: flame vorticity field. ($D = 0.115 \text{ m}$ metal reservoir, $H_f/D = 0.2$), $t = 0.91 \text{ s}$

Figure 11.27.: Left: flame visualization, Middle: flame velocity field, right: flame vorticity field. ($D = 0.115 \text{ m}$ metal reservoir, $H_f/D = 0.2$), $t = 1.2 \text{ s}$

number and expressed in Eq. [11.20]. The dimensional heat release rate $\dot{Q}_D$ used in Eq. [11.20] is expressed in [11.19] and assumes a circular bed of diameter

200
D, vapor fuel density $\rho_{fv}$ and a heat of combustion of the fuel vapors $\Delta H_c$. The velocity V used in Eq. (11.19) is the initial velocity of the fuel vapors. In this study, this velocity is determined for each image as the average of the vertical velocity component along the horizontal profile measured just above the reservoir.

Figure 11.28.: Flame height correlated with a non-dimensional heat release rate

The time evolution of $\dot{Q}_D$ allows to decompose the boilover phenomenon in three steps, as already observed in the temperature profiles. Before the apparition of the boilover (light grey squares in Fig. 11.28), the flame height corresponds to the buoyancy dominated region II, dependent both on the heat release rate and the pool diameter $[153]$. Delichatsios defines also the interval $0.23 < \dot{Q}_D < 1.9$ as typical for intermediate scale pool fire $[26]$. The region I is typical for large scale pool fires $[26]$, where the burning surface is decomposed in flamelets with a height independent of the pool diameter $[153]$. During the boilover (light grey circles and triangles in Fig. 11.28), the flame height increases to the region III, where the dependency on the pool diameter is negligible compared to the heat release rate. Delichatsios defines flames with $\dot{Q}_D > 1.9$ as buoyant jet flames $[26]$. This regime is consistent with the boilover phenomenon, since the velocity and the flame size are increasing while the reservoir size is kept constant. In addition, an increase of $\dot{Q}_D$ is similar to an increase of the Froude number, and therefore a stronger importance of the momentum, created by the piston effect of the water boiling, compared to the buoyancy, which is normally dominant for this pool fire configuration. When the boilover ends, the flame comes back to regime II, as
Chapter 11. Experimental results

before the boilover apparition. This quantitative analysis of the flame enlargement confirms that during the boilover period, the flame size rises because the piston effect from the violent water vaporization creates a significant momentum.

11.3.2. Boilover modeling

11.3.2.1. Boilover occurrence time

The moment at which the boilover appears is influenced by many parameters like the fuel and water layer thicknesses, the diameter and material of the reservoir, the lip height, etc. Therefore, modeling the boilover occurrence time based on a semi-empirical correlation seems very difficult. But the temperature model developed in section 11.2.2 can be used to determine the boilover occurrence time, provided that the fuel-water interface temperature is known.

![Figure 11.29.: Boilover apparition time, comparison of measured and modeled values](image)

For a glass reservoir, it has been observed in Fig. 11.22 that the fuel-water interface temperature at boilover onset is constant with the fuel thickness, and has been measured around 390 – 400 K. Therefore, the temperature of 393 K is chosen in this model, as it has already been chosen by other authors as the water boiling
11.3. Boilover analysis

The temperature in hydrocarbon reservoirs [41]. By using this temperature as the boilover onset fuel-water interface temperature, the thermal model can predict the boilover apparition time for a glass reservoir with the presence of the thermocouple rake with an average error of 10%, as observed in Fig. [11.29]. If the influence of the thermocouple rake is removed, the boilover apparition time can be predicted with an average error of only 3%. In addition, it can also be observed that the model also experiences a linear evolution of the boilover apparition time with the fuel thickness. This proves that the combination of conduction heat transfer and radiation absorption is needed to correctly simulate the fuel and water heating; modeling only the conduction heat transfer would have led to a square relationship between the fuel thickness and the time.

11.3.2.2. Pre-Boilover burnt mass ratio

The ratio between the mass of fuel burnt before the occurrence of Boilover and the initial mass of fuel is an important parameter since it provides information on the available quantity of fuel that can be ejected during the boilover period (see chapter 9 section 9.3).

![Figure 11.30.: New correlation for the pre-Boilover burnt mass ratio $\chi$](image)

The pre-boilover burnt mass ratio was shown in chapter 9 section 9.3 to depend both on the fuel initial thickness and on the pool diameter. Therefore, the Fig. 11.30 presents the variation of $\chi$ in function of the initial volume of fuel. The
Chapter 11. Experimental results

data of the present study (white points) are presented together with the results of Garo (using heating oil, black points \[41, 42\]) and Ferrero (using diesel, light grey points \[38\]), and the result of the experiment performed by the EMA (see appendix B, section B.1.7). The correlation in the form of a power law, as expressed in Eq. 11.21, estimates very well the configurations with a large fuel thickness. A discrepancy is observed only for smaller values of the fuel volume, and especially smaller values of the fuel thickness. This is probably due to the influence of the wall conduction in the heating process of the small scale reservoir, which is not present at larger scales. Indeed, the data of the glass reservoir points of this study are following a lot better the correlation than the metal reservoir points, as glass is not a good heat conductor. Notice also that the thermocouple rake introduces a bias in $\chi$, as the thermocouple rake accelerates the fuel heating and therefore the boilover occurrence.

\[
\chi = 93.57 \left( \pi \left( \frac{D}{2} \right)^2 H_f \right)^{0.0752}
\]  

(11.21)

11.3.2.3. Boilover intensity based on mass loss

As defined in chapter 9 section 9.3, the boilover intensity is defined by Koseki \[73\] as the ratio of the mass burning rate during the boilover period over the average burning rate during the quasi-steady period. Ferrero gives a slightly different definition of the boilover intensity \[38\], as expressed in Eq. 11.22, that will be used here.

\[
It = 100 \frac{\dot{m}_{bo} - \dot{m}_{st}}{\dot{m}_{st}}
\]  

(11.22)

The quasi-steady burning rate can be easily measured or modeled, as explained in section 11.2.1. But the boilover burning rate is more difficult to determine experimentally due to the short duration of the boilover period and therefore the short duration of the measured mass loss signal. In addition, the periods of strong water vaporization prior the boilover are affecting the mass loss measurement, as observed in Fig. 11.13. So the boilover intensity is then computed here following Ferrero \[38\] who defined the boilover burning rate as the ratio of the mass of fuel burnt during the boilover period by the boilover duration (see chapter 9 section 9.3). The boilover duration is determined from the temperature measurement as the difference between the time when the temperature comes back to the quasi-steady temperature profile and the time when the temperature starts to become uniform (due to water boiling, as shown in Fig. 11.22). The quasi-steady burning rates are calculated with the correlations derived from the Duglogorski analysis, and listed in Table 11.1.
11.3. Boilover analysis

Figure 11.31.: Boilover intensity evolution with $\Lambda$

The boilover intensities from the present experiments are displayed in Fig. 11.31 together with the data of Ferrero [38], Chatris [22], and the result of the experiment performed by the EMA (see appendix B, section B.1.7). The main difference between the data of this thesis and the previous authors is the order of magnitude of $\Lambda$, which has been defined in chapter 9 section 9.3 as the fraction between the initial fuel layer and the pool area and expressed in $\text{mm/m}^2$. Chatris and Ferrero have tested large scale reservoirs (from 1.5 or 6 m diameter) with a very small fuel thickness (maximum 30 mm), which results in small $\Lambda$ values. In our case and for the EMA, the reservoir diameters are a lot smaller and the fuel thicknesses are higher. Therefore, $\Lambda$ is higher. In order to compare the two scales, a logarithmic plot is needed. The correlation proposed by Chatris from his measurements and expressed in Table 11.6 is not fitting well the data of this thesis. So a new correlation is developed to account better both scales of data and is also presented in Table 11.6.

Table 11.6.: Correlations for the boilover intensity based on mass loss

<table>
<thead>
<tr>
<th>Author</th>
<th>Correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Chatris</td>
<td>$I_t = 29.6 \cdot \Lambda - 30.9$</td>
</tr>
<tr>
<td>Laboureur</td>
<td>$I_t = 24 \cdot \Lambda^{0.87}$</td>
</tr>
</tbody>
</table>

The validation metrics between the two correlations and the two scales of experiments (the small scale data of this thesis or the large scale data of Chatris [22]
Chapter 11. Experimental results

and Ferrero [38] are listed in Table 11.7. As already depicted in Fig. 11.31, the new correlation agrees now better with the present experimental results and the large scale data.

Table 11.7.: Validation metrics for the Boilover intensity modeling

<table>
<thead>
<tr>
<th></th>
<th>Chatris</th>
<th>New correlation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Present study</td>
<td>NMSE</td>
<td>4.307</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>-1.076</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>0.265</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>10.12</td>
</tr>
<tr>
<td>Chatris</td>
<td>NMSE</td>
<td>-10.11</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>-0.977</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>2.398</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>2.824</td>
</tr>
<tr>
<td>Ferrero</td>
<td>NMSE</td>
<td>33.012</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>0.863</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>2.589</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>23.1</td>
</tr>
</tbody>
</table>

11.3.2.4. Boilover intensity based on flame enlargement

As mentioned in the literature survey of chapter 9, very few studies have been dedicated to the flame enlargement that is caused by the water violent vaporization during the boilover period. In the present experiment, the flame has been monitored during the whole duration of boilover experiments, and therefore, it is possible to calculate a boilover intensity based on the flame enlargement. The flame images recorded during the boilover period are processed with the DIP algorithm described in chapter 10, section 10.1.2.5, which calculates for each image, the maximum length of the flame. To derive a correlation, the ratio of the maximum flame length observed during the boilover period over the average flame length observed during the quasi-steady period is plotted against the Λ parameter, as displayed in Fig. 11.32. In this figure, only the results performed with the metal reservoirs, and that have lead to a boilover apparition, are plotted. In addition, the flame enlargement observed by Garo with a 150 mm reservoir filled with FOD is also plotted [41].

The flame enlargement is observed in Fig. 11.32 to increase with Λ until reaching a plateau. The correlation that fits the best the present experiments is displayed in Eq. 11.23:

\[
\frac{L_{bo,max}}{L_{mean}} = 15 \left( 1 - e^{\exp(-9 \cdot 10^{-4} \Lambda)} \right)
\] (11.23)
11.3. Boilover analysis

11.3.2.5. Flame radiation

The flame radiation can be modeled by two types of approaches: the point source model and the solid flame model, as explained in chapter 9 section 9.3. During the Boilover period, the flame size increases as well as the radiation, as shown in Fig. 11.33 left. Therefore, the point source model cannot be used since it is independent of the flame size; only the solid flame model is used for the boilover radiation prediction. The radiation models defined in chapter 9 section 9.3 are used: Munoz [96], Mudan & Croce [94], Shokri [123] and Duiser [40]. The calculations for the transmissivity and the surface emissive power are similar than the ones from the quasi-steady period. But as the flame size is changing during the boilover, the view factor needs to follow this evolution accordingly.

The flame is assumed to be a cylindrical object so the view factor is calculated with the height and width of the flame (see Eq. B.42 in appendix [5]). The flame detection algorithm allows the determination of the flame height (or width). Only one flame height (or width) per image is chosen by the algorithm, which is the largest value of all the detected flame lengths (or widths). These values are taken as the characteristic height and width to build the corresponding cylinder representing the flame. This approach works well when the flame looks like a column, as shown in Fig. 11.34 left. But when the flame has a shape different from a cylinder, the choice of the largest flame length and width leads to an overestimation of the view factor, as shown in Fig. 11.34 right. Therefore, another approach is
Chapter 11. Experimental results

Figure 11.33.: Left: Radiation and Flame height during Boilover, Right: Boilover peak radiation modeling of Munoz [96] compared with experiments proposed. The flame length is taken as the largest of the image, and the flame width is taken as the flame area divided by the flame length. This approach gives an average width, which models better the real flame.

The validation metrics are resumed in Table 11.8 that compares the different radiation models with the peak radiation of the biggest boilover of each test (indeed, some test have led to several boilover). The validation metrics in Table 11.8 indicate that the model of Munoz [96] predicts the best the flame radiation, both during the quasi-steady and the boilover periods. The comparison of the radiation modeled by Munoz and measured is shown in Fig. 11.33.

11.4. Conclusions

The presentation of the experimental results and their analysis was structured in two parts: the quasi-steady period and the boilover period.

During the quasi-steady period, the pool fire characteristics are constant. The burning rate can be correctly modeled by Rew [112] if the fuel surface is at the edge of the reservoir. If there is a lip height, the burning rate has been shown to decrease exponentially with the lip height. A new 1D unsteady thermal model has been developed for the fuel and water layers heating, including the conduction and radiation absorption through the fuel from the flame, the convection loss with environment, and the convection heat transfer with the thermocouple rake. This
11.4. Conclusions

Figure 11.34.: Left: Column-like flame, Right: Ball-like flame

Table 11.8.: Validation metrics for boilover radiation modeling

<table>
<thead>
<tr>
<th></th>
<th>Munoz</th>
<th>Mudan &amp; Croce</th>
<th>Shokri</th>
<th>Duiser</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>D=80mm, glass reservoir</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NMSE</td>
<td>0.31</td>
<td>5.13</td>
<td>1.33</td>
<td>8.72</td>
</tr>
<tr>
<td>FB</td>
<td>-0.24</td>
<td>-1.41</td>
<td>-0.85</td>
<td>1.52</td>
</tr>
<tr>
<td>MG</td>
<td>0.773</td>
<td>0.16</td>
<td>0.39</td>
<td>8.55</td>
</tr>
<tr>
<td>VG</td>
<td>1.34</td>
<td>35.29</td>
<td>3.11</td>
<td>151</td>
</tr>
<tr>
<td><strong>D=80mm, metal reservoir</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NMSE</td>
<td>0.4</td>
<td>6.32</td>
<td>1.76</td>
<td>6</td>
</tr>
<tr>
<td>FB</td>
<td>-0.4</td>
<td>-1.5</td>
<td>-0.99</td>
<td>1.21</td>
</tr>
<tr>
<td>MG</td>
<td>0.65</td>
<td>0.14</td>
<td>0.32</td>
<td>5.39</td>
</tr>
<tr>
<td>VG</td>
<td>1.44</td>
<td>64.92</td>
<td>4.22</td>
<td>45.42</td>
</tr>
<tr>
<td><strong>D=115mm, metal reservoir</strong></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>NMSE</td>
<td>0.18</td>
<td>3.19</td>
<td>0.69</td>
<td>5.55</td>
</tr>
<tr>
<td>FB</td>
<td>0.031</td>
<td>-1.25</td>
<td>-0.62</td>
<td>1.35</td>
</tr>
<tr>
<td>MG</td>
<td>1.036</td>
<td>0.22</td>
<td>0.52</td>
<td>6.02</td>
</tr>
<tr>
<td>VG</td>
<td>1.19</td>
<td>11.89</td>
<td>1.87</td>
<td>40.14</td>
</tr>
</tbody>
</table>

model proved to correctly model the fuel and water layer heating, especially for large fuel thicknesses.

The average flame length is best modeled by Mangialavori [83] and the minimum and maximum flame length can be deduced with the multiplication factors of Ferrero [36]. The flame puffing frequency can be predicted with the correlation of Pagni [102], and the flame radiation is best modeled with the solid flame model, where the flame emissive power is modeled by Munoz [96].
Chapter 11. Experimental results

At boilover onset, high-speed visualizations showed that the fuel is expelled from the reservoir due to a two-phase front that propagates radially along the whole reservoir diameter. This two-phase front is generated from an enhanced water boiling due to the generation of a large water bubble at the fuel-water interface. When the water experiences this violent vaporization, the flame size increases considerably. The flame starts from a small scale pool fire dominated by buoyancy, up to a buoyant jet flame, due to the increased momentum generated by the water vaporization. The fuel-water interface temperature at boilover onset depends on the fuel thickness for pool fire with a metal reservoir. For a glass reservoir, the fuel-water interface temperature is not dependent on the fuel thickness, and lies around 393 K, as observed by other authors. The thermal model developed in this study allows to predict the boilover occurrence time with an error of 3% of 10% for a pool fire without or with the presence of the thermocouple rake.

Finally, a new correlation has been proposed for the pre-boilover burnt mass ratio, based on the initial volume of fuel. A new correlation was also developed to predict the boilover intensity based on mass loss, valid for diesel fires. Finally, the radiation during the boilover period is correctly modeled by the solid flame model, with the correlation of Munoz [96], providing that the flame dimensions are known.
Chapter 12.

Application of models to large scale boilover

As it has been done for the BLEVE phenomenon in chapter 6, the objective of this chapter is to compare all the models that have been chosen or developed after comparison with experiments, in order to predict the boilover apparition conditions and consequences. In real industrial sites, the hydrocarbon reservoirs can have a diameter up to more than 150 m, but it is very difficult to find detailed experiments at so large scales. In addition, it has been shown in chapter 11 that the modeling of the boilover is strongly dependent on the type of fuel, and this study focused on diesel. Therefore, the more documented large scale experiments involving diesel as fuel are the ones of Ferrero, as already used in chapter 11.

Figure 12.1.: Experimental setup for large scale diesel experiments [22]

The experimental setup used by Ferrero consisted of concentric circular pools made of reinforced concrete (between 1.5 and 6 m diameter), as shown in Fig. [12.1] The fuel and water temperature were measured with 10 K type thermocouples fixed at the reservoir centreline, with a spatial resolution of 2 mm. The pool fire burning rate was calculated from the variation of the fuel level, by using a system of communicating vessels, as shown in Fig. [12.1] right. The flame height was monitored with two video cameras. Finally, two heat flux sensors were used
Chapter 12. Application of models to large scale boilover

to measure the radiation from the pool fire at a specified distance. Among the 15 large scale experiments performed by Ferrero, the author only published one temperature profile. Therefore, that test will be used here for the large scale modeling. The principal measurements for the test FOC3 − 05 − D3 are listed in Table 12.1.

Table 12.1.: Boilover experimental results from test FOC3 − 05 − D3

<table>
<thead>
<tr>
<th>Reservoir</th>
<th>D</th>
<th>H_f</th>
<th>H_w</th>
<th>H</th>
<th>fuel</th>
<th>material</th>
</tr>
</thead>
<tbody>
<tr>
<td>3 m</td>
<td>24.9 mm</td>
<td>0.275</td>
<td>0.3 m</td>
<td>diesel</td>
<td>concrete</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Flame</th>
<th>Period</th>
<th>L_{min}/D</th>
<th>L_{mean}/D</th>
<th>L_{max}/D</th>
<th>u</th>
</tr>
</thead>
<tbody>
<tr>
<td>quasi-steady</td>
<td>1.22 m</td>
<td>1.86 m</td>
<td>2.57 m</td>
<td>0.49</td>
<td></td>
</tr>
<tr>
<td>boilover</td>
<td>1.83 m</td>
<td>2.48 m</td>
<td>3.16 m</td>
<td>0.34</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Pool fire</th>
<th>Period</th>
<th>\dot{q}_r \text{ at } 5.33D</th>
<th>\dot{q}_r \text{ at } 7.33D</th>
<th>\dot{m}</th>
</tr>
</thead>
<tbody>
<tr>
<td>quasi-steady</td>
<td>1.27 kW/m²</td>
<td>0.47 kW/m²</td>
<td>0.06 kg/m²s</td>
<td></td>
</tr>
<tr>
<td>boilover</td>
<td>2.39 kW/m²</td>
<td>1.08 kW/m²</td>
<td>0.073 kg/m²s</td>
<td></td>
</tr>
</tbody>
</table>

\[ H_{fbo} \text{, } H_{rad} \text{, } H_{mass} \]

\[ 4.75 \text{ mm } 1.88 \text{ } 22.03 \% \]

The methodology that will be used in this chapter to predict the boilover apparition conditions and consequences follows the methodology presented in the chapter 8 and schematized in Fig. 12.2. The apparition conditions are modeled through the quasi-steady period parameters, such as the burning rate and the boilover apparition time, but also the flame characteristics and radiation. All these parameters are then used to model the boilover period parameters such as the pre-boilover burnt mass ratio, the boilover intensity and the boilover radiation, that help to better understand the boilover consequences.

12.1. Quasi-steady period modeling

In the quasi-steady period modeling, different parameters can be estimated: the burning rate, the flame length, frequency and radiation, and the time to boilover.

As concluded in chapter 11, section 11.2.1, the semi-empirical correlation that estimates the best the burning rate of diesel pool fires is the correlation of Rew [112]. If this correlation is applied for a diameter of 3 m, as shown in Eq. 12.1, it gives an estimation of 0.0529 kg/m²s. The burning rate during the steady period is mentioned in [38] as \( \dot{m} = 0.06 \text{ kg/m}²\text{s} \) (as shown in Fig. 12.3 left). Therefore,
12.1. Quasi-steady period modeling

The prediction of the pool fire burning rate is done with an error of 12%. But in the same article, Ferrero gives also the time evolution of the burning rate in \( \text{mm/s} \), as shown in Fig. 12.3 right. The average burning rate in this figure, calculated from the end of the induction period (around 100s) until the boilover occurrence, is calculated as 0.0514 \( \text{mm/s} \), which corresponds to a burning rate of 
\[
\dot{m} = 0.044 \text{ kg/m}^2\text{s},
\]
which is then predicted by Rew with an error of 20%.

\[
\dot{m} = 0.054 (1 - \exp(-1.3 \cdot 3)) = 0.0529 \text{ kg/m}^2\text{s} \tag{12.1}
\]

The average flame length correlation depends both on the non-dimensional burning rate and on the wind velocity, as showed in chapter 9 section 9.2.1. From chapter 11 section 11.2.3 the average flame length is best estimated, whatever the fuel type, by Mangialavori [83], which does not depend on the wind velocity. For this estimation, the air density is assumed here equal to \( \rho_a = 1.2 \text{ kg/m}^3 \). If the flame length is calculated with the measured burning rate as given in Fig. 12.3 left, the...
estimation gives $L_{\text{mean}}/D = 2.083$, which estimates the measured flame length with an error of 12%. And if the burning rate is taken from the Fig. [12.3] right, the correlation of Mangialavori estimates $L_{\text{mean}}/D = 1.748$, which gives an error of only 6%.

\[
m^* = \frac{0.06}{1.2\sqrt{9.81 \cdot 3}} = 0.0092 \quad (12.2)
\]

\[
L_{\text{mean}}/D = 31.6 \cdot (0.0092)^{0.58} = 2.083 \quad (12.3)
\]

\[
m^* = \frac{0.044}{1.2\sqrt{9.81 \cdot 3}} = 0.0068 \quad (12.4)
\]

\[
L_{\text{mean}}/D = 31.6 \cdot (0.0081)^{0.58} = 1.748 \quad (12.5)
\]

In addition, Ferrero showed that the minimum and maximum flame lengths can be estimated by multiplying the average flame length by 0.633 and 1.397 respectively. If the measured flame length is multiplied by these coefficients, the minimum flame length is estimated as $L_{\text{min}}/D = 1.177$ (with an error of 3.5%) and the maximum flame length is estimated as $L_{\text{max}}/D = 2.599$ (with an error of 1.1%).

The puffing frequency of this experiment is not explicitly given by the author. But Ferrero [36] plotted all the measured puffing frequency against the pool diameter. For a pool diameter of 3 m, the puffing frequency is plotted between 0.89 and 0.95 Hz. The correlation of Pagni gives $f = \sqrt{2.33} = 0.876$ Hz, which predicts the measured frequency with an error range of 1.6% to 7.8%.

The heat flux received at 5.33D and 7.33D can be estimated from the solid flame model. As showed in chapter [11] section [11.2.3], the transmissivity expressed by Moorhouse & Pritchard [92], the view factor expressed by assuming that the flame is a vertical cylinder, and the correlation of Munoz [96] for the flame emissive power give the best estimate for the heat flux received from the flame at a given distance. To estimate the transmissivity, only the distance between the flame and the receiver is needed and the calculated value is expressed in Eq. [12.6]. For the flame emissive power, the fuel is diesel and the pool diameter is smaller than 5 m. Therefore, the correlation given in Table [9.4] of chapter [9] can be used, and is expressed in Eq. [12.7]. Finally, as the quasi-steady radiation heat flux measured by Ferrero is an average value during the quasi-steady period, the flame length used in the calculation of the view factor is taken as the average flame length. The view factor is expressed in Eq. [12.12].
12.1. Quasi-steady period modeling

\[ \tau = 0.998^{5.33} = 0.9986 \]  \hspace{1cm} (12.6)

\[ E = 0.3 \cdot (28.03 \cdot 3^{0.877}) + 0.7 \cdot 40 = 50.038 \text{ kW/m}^2 \]  \hspace{1cm} (12.7)

\[ L^* = 2 \cdot 1.86 = 3.72 \]  \hspace{1cm} (12.8)

\[ R^* = 2 \cdot 5.33 = 10.66 \]  \hspace{1cm} (12.9)

\[ A = (10.66 + 1)^2 + (3.72)^2 = 149.79 \]  \hspace{1cm} (12.10)

\[ B = (10.66 - 1)^2 + (3.72)^2 = 107.15 \]  \hspace{1cm} (12.11)

\[ P = \frac{1}{\pi} \left( \frac{1}{10.66} \right)^2 \tan \left( \sqrt{\frac{(3.72)^2}{(10.66)^2} - 1} \right) + \frac{3.72(149.79 - 2 \cdot 10.66)}{10.66\sqrt{149.79 \cdot 107.15}} \tan \left( \sqrt{\frac{(3.72 - 1)149.79}{(10.66 + 1)107.15}} \right) - \frac{3.72}{10.66} \tan \left( \sqrt{\frac{10.66 - 1}{10.66 + 1}} \right) = 0.0206 \]  \hspace{1cm} (12.12)

\[ q = E \tau F = 50.038 \cdot 0.9986 \cdot 0.0206 = 1.029 \text{ kW/m}^2 \]  \hspace{1cm} (12.13)

The solid flame model using the correlation of Munoz to calculate the flame emissive power, estimates the heat flux received at a distance of 5.33D to 1 kW/m\(^2\), which gives an error of 21%. If the distance is increased to 7.33D, the heat flux is predicted as 0.533 kW/m\(^2\), which gives an error of 13%. The prediction is then getting better as the distance from the flame is increased.

Finally, the thermal model developed in this study can be applied on the experimental profiles measured by Ferrero [38]. The comparison between the model and the experiments can be observed in Fig. 12.4. There is a relatively good match between the experiments and the model, but a few changes from the initial model had to be performed. At first, the burning rate was not taken as the value given by Ferrero (i.e. \( \dot{m} = 0.06 \text{ kg/m}^2\text{s} \)). Indeed, the fuel thickness at boilover apparition (so 359 s after the induction period) was referred to 4.75 mm. But the use of a burning rate of \( \dot{m} = 0.06 \text{ kg/m}^2\text{s} \) (as given by Ferrero in Fig. 12.3 left) leads to a zero fuel thickness at boilover occurrence, which is not physical. Therefore, the average of the burning rate in mm/s, taken from the time evolution of the burning rate displayed by Ferrero in the same article was chosen (see Fig. 12.3 right), and is equal to 0.044 kg/m\(^2\text{s}\). Second, the boilover occurrence was calculated from the beginning of the quasi-steady period, after the induction period, when the flame occupies the whole reservoir diameter. And finally, the radiation heat flux at the fuel surface calculation was adapted. In chapter 11 section 11.2.2.5 the heat flux at fuel surface was calculated from Hamins [54], using a flame temperature of
Chapter 12. Application of models to large scale boilover

1100 \textdegree K. If the same flame temperature is used here, the calculated heat flux is equal to 70 \text{kW/m}^2, which is a lot higher than the heat flux measured by Ferrero, and equal only to 22 \text{kW/m}^2 \cite{34}. Therefore, the flame temperature in the model of Hamins was adapted to 850 K. Such a flame temperature leads to an estimate of the radiation heat flux equal to 21.4 \text{kW/m}^2, which is a lot closer than the measured heat flux.

![Comparison between profiles measured by Ferrero and the model of chapter 11](image)

Figure 12.4.: Comparison between profiles measured by Ferrero \cite{38} and the model of chapter 11

12.2. Boilover modeling

From the thermal model used in the previous section, the time to boilover can be calculated. The time to boilover occurence is defined here at the time when the fuel-water interface reaches 393 \textdegree K. If the induction period after ignition, observed as 100 s is removed, the thermal model gives a boilover occurence time of 328.3 s. In the measurements, the boilover occurence time was of 459.25 s. If the induction period is removed, this gives 359.25 s, which is then predicted with a bit less than 6\% error. But this corresponds to the time when the temperature profiles become uniform, not to the time when the interface temperature is equal to 393 \textdegree K. This happens at 307.2 s (with the induction period removed), which gives then an estimation error of 17\%.

The chapter section 11.3.2.2 proposed a correlation to estimate the pre-boilover
12.2. Boilover modeling

burnt mass ratio, which is the ratio between the mass of fuel burnt before the boilover occurrence and the initial mass of fuel, expressed in Eq. 11.21. If this correlation is applied to the experiment of Ferrero, the Eq. 12.14 gives a pre-boilover burnt mass ratio of 76.03%. Ferrero measured a fuel thickness at boilover occurrence of 4.75 mm, which gives burnt thickness over initial thickness ratio of 80.92%. By assuming that the fuel density does not change over the quasi-steady period, the fuel thickness ratio is equal to \( \chi \), and therefore is estimated by the correlation of Eq. 11.21 with 6.4% error.

\[
\chi = 93.57 \left(24.9 \cdot 10^{-3} \cdot \pi \cdot (3/2)^2\right)^{0.1184} = 76.03\% \tag{12.14}
\]

As shown in chapter 11 section 11.3.2.3, the boilover intensity based on mass loss can be estimated with a new correlation, based on the \( \Lambda \) parameter which is the ratio of the fuel initial thickness over the pool surface area. The correlation is expressed in Table 11.6 of chapter 11. In the experiment of Ferrero, \( \Lambda = 24.9/(\pi \cdot (3/2)^2) = 3.523 \, mm/m^2 \). If the burning rate is taken as the value indicated by Ferrero (\( \dot{m} = 0.06 kg/m^2s \)), the correlation overestimates considerably the observed intensity, which is only \( I_{mass} = 22.03\% \), giving an error of almost 225%. But if the burning rate is taken as \( \dot{m} = 0.044 kg/m^2s \) (from Fig. 12.3 right instead of Fig. 12.3 right), the measured intensity is then equal to \( I_{mass} = 65.9\% \), which is a lot closer to the estimates. The error is then reduced to less than 9%.

\[
I_{mass} = 24 \cdot 3.523^{0.87} = 71.77\% \tag{12.15}
\]

Finally, the radiation emitted by the flame during boilover is estimated similarly to the quasi-steady radiation. But here, the flame height is chosen as the maximum flame height, defined as the flame length that has an intermittency of \( I=0.95 \). The solid flame model with the correlation of Munoz [96] for the flame emissive power estimates the heat flux received at a distance of 5.33\( D \) to 1.483 kW/m\(^2\), which gives an error of 38%. If the distance is increased to 7.33\( D \), the heat flux is predicted as 0.837 kW/m\(^2\), which gives an error of 22.5%. Again, the prediction is then getting better as the distance from the flame is increased, but the error between the estimation and the measurement is larger than for the quasi-steady period estimations.

Several reasons can be given to explain this larger error. At first, the heat flux measured during boilover by Ferrero [37] is defined as “the average during this period and the maximum value”. Therefore, the flame length that should have been used is an average between the average flame length during the boilover and the maximum flame length observed. But the flame length that has been used here is the flame length with an intermittency of \( I=0.95 \). This choice was a trade off as the true maximum flame length was not available, but could not be consistent. Second, Ferrero [36] does not give any information about the increase
of the flame diameter during boilover. But is the present study, the flame width was observed to increase as well as the flame length, which influences the value of the view factor. Finally, Ferrero [37] observed that, during the boilover, the view factor increases, but also the flame emissive power, which has not be taken into account. The author showed that for a pool diameter of 3m, the flame emissive power during boilover is approximately 1.7 times larger than the flame emissive power during the quasi-steady period.

12.3. Conclusion

The objective of this chapter was to apply all the models and correlations outlined in chapter 11 to a large scale boilover. The large scale experiment chosen here is a thin-layer boilover with diesel as fuel in a 3 m diameter pool fire, performed by Ferrero [38, 37, 36].

The burning rate measured for this experiment is ambiguous, since the author gives two different values as observed in Fig. 12.3. The calculations have been performed with the two values, but the burning rate derived from Fig. 12.3 right is more precise, and is estimated by Rew [112] with an error of 20%. If the measured burning rate is used, the average flame length can be estimated by Mangialavori [83] with an error of 1.6-7.8%. The flame radiation can be estimated by the solid flame model, using the correlation of Munoz [96] for the flame emissive power, with an error of 21% at 5.33D and 13% at 7.33D. The thermal model developed in the section 11.2.2.5 of chapter 11 fits correctly the experimental data, if the flame temperature is reduced to 850K, compared to the 1100K used in section 11.2.2.5. The thermal model allow a prediction of the boilover occurrence time with an error of 17%.

The pre-boilover burnt mass ratio can be estimated with the correlation developed in this study with an error of 6.4%. This study also developed a correlation for the boilover intensity based on the mass loss, which gives an error of 9%. Finally, the radiation emitted by the flame during boilover, and received at a distance of 5.33D is estimated with an error of 38% and with an error of 22.5% at 7.33D.

From this resume of all the estimations performed in this chapter, most of them agree with the measured data by less than 10% error. The larger error are experienced for the burning rate, and the heat flux estimations, especially during boilover occurrence. Therefore, a more precise estimation of the flame emissive power during boilover and on the flame enlargement should be done in future studies.
Chapter 13.

Conclusions and recommendations

13.1. Boilover modeling

The objective of the Boilover part was to contribute to the understanding, the modeling and the validation of Boilover apparition and consequences. Therefore, a literature review has been performed on the phenomenon to better understand the different theories and modeling approaches formulated by other authors. Then, small scale experiments have been performed with glass and metal reservoirs filled with a diesel-oil mixture. The effect of the fuel and water thicknesses, of the lip height, and of the reservoir material and diameter were investigated. Finally, these experiments have been compared with larger scales of experiments and with models available in the literature and the models that proved to best estimate the phenomenon have been applied to a large scale boilover.

The prediction of the boilover apparition and consequences is structured in three steps. The first step is to model the pool fire characteristics to predict the reservoir conditions prior the boilover. The second step is to model the boilover starting conditions and the final step is the modeling of the boilover period and therefore consequences.

The major pool fire characteristics during the quasi-steady period can be divided in two classes of parameters: the ones linked to the fuel and the ones linked to the flame. The major pool fire characteristics linked to the fuel are the burning rate and the temperature profile. The burning rate for a diesel reservoir is correctly modeled by Rew [112] if the fuel surface is at the edge of the reservoir. For a reservoir with a lip height, the correlation of Dlugogorski [28] has been adapted to the experiments performed in this study.

In this experimental study, no hot-zone was observed with small-scale reservoirs, and therefore, the modeling has focused on thin-layer boilovers. An increase of the fuel layer thickness decreases the speed of heating of the water layer and therefore increases the time to boilover. An increase of the water layer thickness decreases the boilover apparition time due to its cooling effect. At small scale, the conduction inside the reservoir walls plays a role in the fuel heating, accelerating the boilover apparition time, compared to a glass reservoir, where the wall conduction can be
Chapter 13. Conclusions and recommendations

neglected. Finally, an increase of the reservoir diameter decreases the conduction effect, which becomes negligible at large scales. A thermal model that predict the fuel and water heating, with negligible wall conduction has been developed. The comparison with both the small scale experiments of this study, but also from the large scale experiments of Ferrero is good, providing a change in the flame temperature. In the future, more attention on the evolution of the flame temperature with the pool diameter should be drawn.

The pool fire characteristics linked to the flame are the flame puffing frequency, length, and radiation. During the quasi-steady period, the pool fire oscillates and the puffing frequency can be predicted with the correlation of Pagni [102]. An effect of the lip height has been observed, decreasing the puffing frequency as the lip height increases. The average flame length can be modeled with the correlation of Mangialavori [83] and the minimum and maximum flame length can be calculated with multiplication factors determined by Ferrero [36]. Finally, knowing the flame length, the radiation emitted by the flame can be predicted by the solid flame model, using the correlation of Munoz [95] to model the flame emissive power.

The boilover starts when the water boiling develops in a two-phase front that propagates radially through the whole reservoir. This boiling front pushes the fuel out of the reservoir, resulting in the increase of the flame size. The flame increases size with time following a gaussian shape. As the water boils, the flame increases due to a stronger importance of the momentum induced by the piston effect of the water. The flame increases until a maximum, and then comes back to its original size when the water boiling stops. Some conditions for boilover occurrence have already been proposed in literature; the presence of a fuel with a smaller density and a larger boiling point compared to water, a minimum viscosity to guarantee the piston effect of the water boiling, and a heat source that increases the water temperature until boiling (most of the time, a pool fire). In addition to these conditions, the experiments performed in this study showed that a too large water layer thickness prevents the boilover apparition.

For a reservoir where the wall conduction is negligible, the fuel-water interface temperature that triggers the apparition of the water boiling front is around 393 K. When the wall conduction is influencing the fuel and water heating, the fuel-water interface temperature follows a power law, depending on the initial fuel thickness. As the fuel-water interface temperature is known, and the fuel and water heating with time has been modeled, the boilover apparition time can be predicted within 3% (without the influence of the thermocouple rake).

When modeling the boilover consequences, a first indication of the boilover intensity is the pre-boilover burnt mass ratio; when the ratio increases, more fuel is likely to be expelled from the reservoir and lead to a strong flame increase and/or to several boilovers. The pre-boilover burnt mass ratio depends both on the reservoir diameter and the fuel thickness and a new correlation has been developed to
model it. The mass boilover intensity quantifies also the boilover hazard. The mass boilover intensity depends on both the fuel thickness and the reservoir diameter and a new correlation that improves the prediction at small scale, compared to the correlation of Chatris [22], has been developed. Finally, the radiation emitted during the boilover is directly linked to the flame size increases, and therefore the solid flame model with the emissive power predicted by Munoz [95] (like in the quasi-steady period) can be used, changing only the flame size evolution instead of a constant value. Nevertheless, it has been observed at larger scales that the surface emissive power of the flame could also change during the boilover period. Therefore, a study to measure the flame emissive power during boilover would be interesting.

![Figure 13.1.: Schematic of boilover apparition conditions modeling](image)

To resume these conclusions, the methodology for predicting the boilover apparition conditions and consequences presented in the introduction of chapter 8 is redrawn, including all the models that were either developed or chosen from literature after comparison with different scales of experiments. The Fig. 13.1 represents the methodology for the boilover apparition conditions, i.e. the analysis of the quasi-steady period, and the Fig. 13.2 represents the methodology for the boilover consequences or the analysis of the boilover period.

To complete this model, future studies should focus on other fuels like crude oil, because most of the models presented in this methodology are fuel dependent. A crude oil should be used to try to better understand the hot-zone formation and development, and try to model it. Finally, more attention should be drawn on the evolution of the flame emissive power during boilover.
Chapter 13. Conclusions and recommendations

13.2. Recommendations

The main conclusions of the literature survey and the experimental study performed in this thesis allow to suggest some recommendations to minimize the boilover consequences. The reservoir configuration can increase the boilover apparition time, leaving more time for the emergency response. To increase the boilover apparition time, it is interesting to: increase the lip height (decreasing the burning rate), increase the fuel thickness, decrease the wall conduction (by increasing the size of the reservoir or by using less conducting materials) and avoid to place some metal structure inside the reservoir (like the thermocouple rake that is heating the fuel).

Concerning the reduction of the boilover consequences, increasing the boilover apparition time as proposed consequently decreases the fuel quantity available for the flame enlargement. Also, minimizing the $\Lambda$ parameter helps in the reduction of the boilover intensity.
Chapter 14.

Conclusions

The objectives of this thesis were to better understand the conditions under which the BLEVE and Boilover phenomena appear, and to model the hazards generated by these accidents: toxic cloud formation, blast wave propagation, fragment projection, fireballs, etc... To fulfill these objectives, the methodology applied for both phenomena was similar, and described schematically in Fig. 14.1.

![Figure 14.1.: Methodology applied in this thesis](image)

At first, an extensive literature survey has been performed on both phenomena, that helped to identify the main theories already formulated to explain physically the BLEVE and the boilover, but also to model the different aspects of these phenomena. A first modeling of the apparition conditions and the consequences was elaborated.

In parallel to that theoretical aspect of the research, small scale experiments were performed. Large scale experiments, even if they show a better similarity with the real storage conditions, are too expensive and dangerous. By contrast, small
scale experiments are very interesting due to their reduced cost and hazards. In addition, the small scales can also be controlled more easily and an exhaustive set of tests can be done for parametric analysis. For the BLEVE phenomenon, 6g butane reservoirs and 41g propane reservoirs were tested. For the last reservoirs, the limit load was varied by making a notch along the reservoir axis. For the boilover phenomenon, reservoirs of 0.08 to 0.3 m diameter, both in metal and glass, containing a mixture of 70% diesel and 30% oil were tested. The fuel and water layers were varied, for the parametric analysis. In addition to the small scale experiments, large scale experiments were taken from the literature. For the BLEVE, the experiments of Johnson, the BAM or Birk were used while for the boilover, mainly the experiments of Ferrero were used.

Finally, the experimental database has been compared with the models collected from the literature survey. If there was different models available, they have been compared and commented. If no model was available, new ones have been formulated. These models were also structured in a modeling procedure, as detailed in the conclusions of chapter 7 and 13.

In addition to the model approach, a better understanding of the physical phenomenon behind the BLEVE and the boilover have been achieved for some aspects of the phenomena. Concerning the BLEVE, the fluid state prior rupture in the NTG is supercritical. Once in the atmosphere, the fluid state falls in the two phase region, if an isentropic expansion is assumed. A cloud of small droplets is therefore observed, as the fluid condenses when leaving the reservoir. Two types of cloud shapes have been observed. A blurry cloud is formed when the fluid falls after expansion in the two-phase region, with a quality closer to the liquid state. If the quality if closer to the vapor state, the cloud is observed with a smooth dome. An explanation for the dome-like cloud shape is that, as the condensation is reduced (because the fluid quality after expansion is closer to the vapor state), the shock structure of the supersonic underexpanded jet, as described by Lamanna, becomes visible. At the reservoir rupture, a blast wave is produced. The blast wave travels faster than the cloud and its velocity is less attenuated with time. A second blast wave has been observed in the case of a dome-like cloud, that is linked to the fast vaporization of the cloud.

Concerning the boilover, it starts when the water boiling develops in a two-phase front that propagates radially through the whole reservoir. This boiling front pushes the fuel out of the reservoir, resulting in the increase of the flame size. The flame increases size with time following a gaussian shape. As the water boils, the flame increases due to a stronger importance of the momentum induced by the piston effect of the water. The flame increases until a maximum, and then comes back to its original size when the water boiling stops. The experiments performed in this study also showed that a too large water layer thickness prevents the boilover apparition.
Part III.

Appendix
Appendix A.

Appendix linked to BLEVE

A.1. BLEVE bibliography

The Tables A.1 and A.2 are listing the principal characteristics of the small scale BLEVE experiments that have been published in literature. The Table A.1 gives the main dimensions of the reservoir used, indicates if the pressure (P) and/or the temperature (T) were measured, the fluid type and mass, and gives the type of rupture used: either a fire until rupture and/or an impact that breaks the reservoir at a given time.

Table A.1.: Small scale experiments: Comparative review of the experimental setup

<table>
<thead>
<tr>
<th>Source</th>
<th>Date</th>
<th>D</th>
<th>L</th>
<th>d</th>
<th>P</th>
<th>T</th>
<th>Type</th>
<th>m</th>
<th>Fire</th>
<th>Impact</th>
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<td>260</td>
<td>0.64</td>
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<td>/</td>
<td>Propane,</td>
<td>0.36-1.1</td>
<td>X</td>
<td>X</td>
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<tr>
<td>[87]</td>
<td></td>
<td></td>
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<td></td>
<td></td>
<td>R12</td>
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<td></td>
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</tr>
<tr>
<td>McDevitt</td>
<td>1990</td>
<td>75</td>
<td>260</td>
<td>0.64</td>
<td>X</td>
<td>X</td>
<td>R22</td>
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<td>X</td>
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<td>[86]</td>
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<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Rirksomboon</td>
<td>1997</td>
<td>75</td>
<td>260</td>
<td>0.64</td>
<td>X</td>
<td>X</td>
<td>10 fluids</td>
<td>0.1-1.16</td>
<td>X</td>
<td>/</td>
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<td>[113]</td>
<td></td>
<td></td>
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<td></td>
<td></td>
</tr>
<tr>
<td>Stawczyk</td>
<td>2003</td>
<td>300</td>
<td>450</td>
<td>1.9</td>
<td>X</td>
<td>X</td>
<td>Propane</td>
<td>2-4</td>
<td>X</td>
<td>/</td>
</tr>
<tr>
<td>[128]</td>
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<tr>
<td>Davidson</td>
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<td>/</td>
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</table>

The Table A.2 gives the main results of the experiments described in Table A.1 in terms of rupture pressure, overpressure at a given distance (r), rupture temperature, time to rupture and duration of the fluid ejection. Not all the experiments mentioned all these parameters, especially the overpressure that has been measured only by Stawczyk [128].
Chapter A. Appendix linked to BLEVE

Table A.2.: Small scale experiments: Comparative review of the experimental results

<table>
<thead>
<tr>
<th>Author</th>
<th>$P_{rupt}$ [MPa]</th>
<th>$P_s$ [kPa]</th>
<th>$r$ [m]</th>
<th>$T_{rupt}$ [K]</th>
<th>$t_{rupt}$ [s]</th>
<th>$\Delta t_{rupt}$ [ms]</th>
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<td>McDevitt</td>
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<td>/</td>
<td>/</td>
<td>308-359</td>
<td>/</td>
<td>/</td>
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<tr>
<td>McDevitt</td>
<td>2.7</td>
<td>/</td>
<td>/</td>
<td>338</td>
<td>/</td>
<td>&lt; 2</td>
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<td>Rirksomboon</td>
<td>1.2-8.1</td>
<td>/</td>
<td>/</td>
<td>327-507</td>
<td>36-552</td>
<td>/</td>
</tr>
<tr>
<td>Stawczyk</td>
<td>7.5-12</td>
<td>50-8.2</td>
<td>2-10</td>
<td>388-423</td>
<td>1500</td>
<td>4</td>
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<td>Davidson</td>
<td>/</td>
<td>/</td>
<td>/</td>
<td>348-378</td>
<td>540-852</td>
<td>/</td>
</tr>
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</table>

A.2. Fluid and material properties

The two fluids used where n-butane and propane. Their physical properties are resumed in Table A.3. In addition, the different material properties used in the thermal model of section are listed in Table A.4.

Table A.3.: Fluid physical properties

<table>
<thead>
<tr>
<th>Property</th>
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<th>Propane</th>
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<tbody>
<tr>
<td>Boiling point [K]</td>
<td>272.7</td>
<td>230.9</td>
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<tr>
<td>Critical temperature [K]</td>
<td>425.2</td>
<td>369.7</td>
</tr>
<tr>
<td>Critical pressure [bar]</td>
<td>37.97</td>
<td>42.49</td>
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<tr>
<td>Gas density at 1 bar [kg/m$^3$]</td>
<td>2.46</td>
<td>1.86</td>
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<tr>
<td>Liquid density at 293 K [kg/m$^3$]</td>
<td>584.49</td>
<td>509.27</td>
</tr>
<tr>
<td>Heat of combustion [J/kg]</td>
<td>4.586 $\cdot 10^7$</td>
<td>4.6013 $\cdot 10^7$</td>
</tr>
<tr>
<td>Molecular weight [kg/mol]</td>
<td>58.1 $\cdot 10^{-3}$</td>
<td>44.096 $\cdot 10^{-3}$</td>
</tr>
<tr>
<td>Poisson ratio [-]</td>
<td>1.1</td>
<td>1.13</td>
</tr>
<tr>
<td>Lower flammability limit [vol %]</td>
<td>1.9</td>
<td>2.1</td>
</tr>
<tr>
<td>Upper flammability limit [vol %]</td>
<td>8.4</td>
<td>9.5</td>
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Table A.4.: Materials properties

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<th>Plate</th>
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<td>Refractory steel</td>
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<td>Thermal conductivity [W/m$^2$]</td>
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<td>60</td>
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<tr>
<td>Specific heat [J/kgK]</td>
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<td>910</td>
<td>490</td>
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<td>Density [kg/m$^3$]</td>
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<td>Thickness [mm]</td>
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</table>
A.3. Overpressure measurement, piezoelectric sensor calibration

A piezoresistive sensor experiences a change in electrical resistance due to the deformation of the material when subjected to pressure. In a piezoresistive sensor, semiconductor materials are used as they can still observe a change in the resistance even for very small deformations. But they need to be supplied with voltage in order to measure the change in resistance (in opposite to piezoelectric sensors). Compared with piezoelectric sensors, piezoresistive sensors have to be supplied with voltage, but have a very high sensitivity and a better low frequency response, so that they can be used for static measurements.

A piezoelectric sensor produces a voltage when subjected to pressure. This effect is reversible and shows an excellent linearity over a wide amplitude range. The drawback of these sensors is that they are sensitive to temperature changes, the heat causing an expansion of the case surrounding the piezoelectric material, most of the time a crystal, here a quartz. Another drawback is that they cannot be used for static measurements because even if the piezoelectric material generates a charge due to an applied pressure, the leak of the charge through the lowest resistance path cannot be avoided, even with large electrical insulation.

At the von Karman Institute, the sensors available in the range expected for overpressure measurements (less than 1bar) where piezoelectric (PCB). Therefore, they were used to measure the blast. During the last series of BLEVE measurements, piezoresistive sensors (Kistler) were also used closer to the reservoir, due to their higher pressure range. The piezoresistive sensors can be calibrated statically with a pressure pump calibrator. But this type of calibration is hardly applicable to piezoelectric sensors, that are better calibrated dynamically, by comparison with a piezoresistive reference sensor. In this study, the reference sensor was a Kulite of type XTE-190-50A and its specifications are also listed in Table 4.2. In addition, a dynamic calibration is useful to determine the resonance frequency of a sensor, in order to correctly process the recorded signal (sampling frequency, filtering frequency,...).

One of the widely used methods to determine the frequency response of the sensors and the sensitivity of the piezoelectric sensors is the use of a shock tube. The shock tube facility of von Karman Institute \[138,135\] consists of two tubular chambers of 100 mm internal diameter: a driver (high pressure) and a driven (low pressure) chambers of respectively 3 m and 4.3 m long (see Fig. [A.1]). The two chambers are mechanically coupled by a bolted flange which allows insertion of a membrane as separator. Plastic membranes were used for this type of calibration, as the pressure range is relatively low.

The shock tube principle is based on the piston effect. After pressurization of the driver chamber, an horizontal metallic needle with knife edge situated at the
center of the driver chamber is manually displaced to rupture the membrane. The sudden release of the pressurized air from the high pressure chamber into the low pressure one produces a supersonic shock wave front that impinges the end plate where the transducers to be calibrated are flush mounted. The shock wave is then reflected several times, but only the first shock wave passage is used for calibration. A typical signal recorded by the transducers is reported in Fig. A.2 left. Before the shock, the pressure inside the driven chamber is constant, so the signal is flat. Once the shock wave impinges on the sensors, the pressure increases until reaching a plateau of about 16ms, which is consistent with the characteristics of the shock tube, before decreasing back to the driven chamber pressure.

![Figure A.1: VKI shock tube facility](image)

The shock wave signal as observed in Fig. A.2 left is similar to a pressure step, if the decreasing part of the signal is cut. Two types of oscillations can be observed on the measured signals (see Fig. A.2 left). First, constant amplitude oscillations can be observed on the whole duration of the signals, and are due to the vibration of the shock tube end plate. The frequency of these oscillations has been estimated in the range of 4 to 5 kHz by Van de Wyer [135], and a peak in this range can be observed in Fig. A.2 right. In addition, larger oscillations are visible at the beginning of the piezoelectric signal, and decrease with time. These oscillations are linked to the sensor itself: vibration of the membrane, resonance in the sensor cavity. The sensor resonance frequency can be determined by converting the step signal into frequency domain and by identifying the resonance frequency as the main peak, as displayed in Fig. A.2 right. The piezoresistive sensor (Kulite) taken as reference has a resonance frequency of about 380kHz and the signals were acquired with a sampling frequency of 250kHz. Therefore, the Kulite resonance frequency is higher than the sampling frequency and does not affect the measurement, showing no oscillations in the shock tube signal. But the resonance frequency of the piezoelectric sensor (PCB) is around 50kHz (as observed in the FFT of the shock tube signal, Fig. A.2 right) and is consequently lower than the sampling frequency. The influence of the resonance frequency is therefore visible on the recorded signal. The piezoelectric sensors need then to be filtered to remove the influence of the resonance frequency. A low-pass analog filter was used for each PCB. Two filter frequencies have been tested: 25kHz and 50kHz. From Fig. A.2 right, it can be observed that the resonance frequency peak is still visible with a filter frequency of 50kHz, so that a 25kHz filter frequency is needed.
A.4. Internal pressure measurement

A.4.1. Variable reluctance differential pressure transducer

This type of pressure transducer is composed mainly on a diaphragm or membrane of magnetically permeable stainless steel, positioned between two inductance coils, as observed in Fig. A.4. When the pressure on the two sides of the membrane...
Chapter A. Appendix linked to BLEVE

Figure A.3.: Left: PCB dynamic calibration, Right: Kistler static calibration

is similar, the membrane is perfectly centered with the two coils, and the magnetic reluctance is similar on both sides. But if a pressure difference is applied through the pressure ports, the diaphragm deflects toward the cavity with the lower pressure, decreasing one gap and increasing the other. As the magnetic reluctance varies with the gap and determines the inductance value of each coil, the diaphragm deflection increases the inductance of one coil and decreases that of the other. The coils are then connected to a Wheatstone bridge, and a change in the inductance creates a voltage in the bridge, which allow this type of sensor to be used to measure pressure easily.

Figure A.4.: Schematic of Variable reluctance differential pressure transducer (left, from Validyne online operating instructions) and dead weight tester (right, from sensorland.com)
A.4.2. Dead weight tester

Dead weight testers are a piston-cylinder type measuring device. The principle of this device is based on the well known formula: Pressure = Force over Area. A pressure is applied (through a pump in $n^\circ1$ on Fig. A.4 right) on a known area of a sealed piston ($n^\circ5$), that generates a force. This force is then compared to the force applied by calibrated weights ($n^\circ3$) positioned on the weight support ($n^\circ4$). The main advantage of this type of device is that it can generate stable pressure for calibration purpose, by placing the sensor to calibrate in $n^\circ2$ and can also reach high pressures, up to 100 MPa.

A.4.3. Sensor calibration curves

The Figure A.5 left gives the calibration curves of the two sensors used to measure the internal pressure of the NTG reservoir.

Figure A.5.: Calibration curves. Left: Variable reluctance pressure transducer, Validyne DP15, Right: Pressure transducer, GE PMP 1400

A.5. Thermal modeling

A.5.1. Contact resistance modeling

As part of the temperature model for the heating of the reservoirs, a contact conductance is used between each part of the model (reservoir, cradle, plate). The
contact conductance equation, as defined by Yovanovich and Antonetti \[146\], resumed by Bejan \[8\], is detailed in Eq. A.1.

\[
h_{i,i+1} = \left( \frac{1}{h_c} + \frac{1}{h_g} \right)^{-1} = \left( 1.25 \lambda_{i,i+1} \frac{f_m}{f_r} \left( \frac{P_c}{T} \right)^{0.95} \right)^{-1} + \left( \frac{\lambda_g}{Y + Z} \right)^{-1}
\]  

(A.1)

The Eq. A.1 assumes that the total contact conductance between two surfaces consists on two conductances: the solid-to-solid conductance \(h_c\) and the gap conductance \(h_g\). The solid-to-solid conductance depends on \(\lambda_{i,i+1}\) the harmonic mean thermal conductivity for the two surfaces, \(f_r\) the effective absolute surface slope, \(f_m\) the contact pressure and \(I\) the microhardness of the softer material. The gap conductance depends on \(\lambda_g\) the thermal conductivity of the gap fluid (which is considered here as air), \(Z\) a gas parameter used to account for rarified gas effects, and \(Y\) the distance between the mean planes. All these parameters are defined through the expressions listed in Eq. A.2 - A.6.

\[
\lambda_{i,i+1} = \frac{2 \lambda_i \lambda_j}{\lambda_i + \lambda_j}
\]  

(A.2)

\[
f_m = 0.125 \left( f_r \cdot 10^6 \right)^{0.402}
\]  

(A.3)

\[
P_c = \frac{mg}{A_c}
\]  

(A.4)

\[
I = 3 \sigma_y
\]  

(A.5)

\[
\frac{Y}{f_r} = 1.185 \left( - \ln \left( 3.132 \frac{P_c}{T} \right) \right)^{0.547}
\]  

(A.6)

The sensitivity analysis of the contact conductance with the parameters that can suffer from a wrong estimation is presented in Fig. A.6.

A.5.2. Discretization of thermal model

Equation at top of reservoir (Eq. 5.1 in chapter 5):

\[
T_{1,1}^{n+1} = T_{1,1}^{n} (1 - 2Fo_1) + 2Fo_1 \left( T_{1,2}^{n} + \frac{h \Delta x}{\lambda_1} (T_{amb} - T_{1,1}^{n}) \right)
\]  

(A.7)
A.6. Sensitivity of the notch dimensions on the rupture pressure modeling

Equation inside layer “i” (Eq. 5.2 in chapter 5):

\[ T_{i,j}^{n+1} = T_{i,j}^n + 1 - 2F_{o,i} + 2F_{o,i} \left( T_{i,j-1}^n + T_{i,j+1}^n \right) \]  
(A.8)

Equation between layer “i” and “i+1” (Eq. 5.3 in chapter 5), discretized in two equations, one for each material layer:

\[ T_{i,N_i}^{n+1} = T_{i,N_i}^n \left( 1 - F_{o_i} - \frac{h_{i,i+1}\Delta x}{\lambda_i} F_{o_i} \right) \]
\[ + F_{o_i} T_{i,N_i-1}^n + \frac{h_{i,i+1}\Delta x}{\lambda_i} F_{o_i} T_{i,N_i+1}^n \]  
(A.9)

\[ T_{i+1,N_i}^{n+1} = T_{i+1,N_i}^n \left( 1 - F_{o_{i+1}} - \frac{h_{i,i+1}\Delta x}{\lambda_i} F_{o_{i+1}} \right) \]
\[ + F_{o_{i+1}} T_{i+1,N_i-1}^n + \frac{h_{i,i+1}\Delta x}{\lambda_i} F_{o_{i+1}} T_{i+1,N_i+2}^n \]  
(A.10)

Equation under the plate (Eq. 5.4 in chapter 5):

\[ T_{3,N_3+2}^n = T_{3,N_3+1}^n + \frac{\Delta x}{\lambda_3} \left[ Q - \sigma (T_{3,N_3+1}^4 - T_{amb}^4) \right] \]  
(A.11)
A.6. Sensitivity of the notch dimensions on the rupture pressure modeling

The sensitivity of parameters to the Staat global approach [127] is presented here. The Fig. A.7 left presents the influence of the defect length, illustrated by the $b/c$ ratio, for different defect depths. The Fig. A.7 right presents the influence of the defect depth, illustrated by the $b/d$ ratio, for different defect lengths.

A.7. Image processing techniques

The determination of the cloud edge radius starts with the use of the LEDAR software (Level Detection and Recording) [106]. Once opened, the first step is to define the image to process by introducing different informations like the file name, extension, image number, etc... (A in Fig. A.8). Once the image is loaded in the software, a region where the algorithm will try to detect the cloud edge has to be defined (B in Fig. A.8). The purpose of this step is to remove from the field of view any disturbances that could end to a bad detection like a cable, a shadow, an instrument, etc... Then, the algorithm used to detect the cloud edge needs to be selected. There are 8 algorithms implemented in LEDAR, and after trials, the positive gradient algorithm showed to fit the best the cloud edge (C in Fig. A.8). Finally, once the cloud edge detection is satisfying, the detected pixels are saved in a separate file (D in Fig. A.8).

The detected points are then plotted in Matlab and a fitting algorithm is applied, to fit a circle on the cloud edge, as observed in Fig. A.9 left. The center of the circle is chosen to be at the upper part of the reservoir, where the reservoir opens. This position is clearly visible on the high-speed images. Then, the calculated circle
A.7. Image processing techniques

radius is finally converted from pixel to meters, thanks to the image calibration. One example of calibration image is displayed in Fig. A.9 right.

The cloud area is calculated with the same image processing algorithm as the one used for the detection of flame contours during boilover (see chapter 10). The only difference is that the level of image intensity used to convert the gray scale image into black and white is chosen for every image of every test as 5%. The result of
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this image processing algorithm is illustrated in Fig. A.10 in red contours (the green contours are not used).

![Image](image.png)

Figure A.10.: Left: Original image, Right: Detected contours

### A.8. Exit pressure at reservoir rupture

In this section, a procedure to calculate the pressure at the exit of the reservoir, for a fluid initially at supercritical state, and proposed by Lamanna [77] is presented. The objective of this procedure is to know if the fluid is still supercritical at the exit of the reservoir or if its state has already changed to a two-phase flow. It is a zero dimensional assumption allowing for subsonic and sonic exit velocities. The procedure is divided in several steps:

- Calculate the enthalpy $h_{e,rupt}$ at the rupture pressure $P_{e,rupt}$ and for the reservoir specific volume (reservoir volume divided by the total fluid mass).

- Make a first guess for the exit pressure. For the calculations to be easy, the exit pressure has to be higher than the critical pressure (so the fluid is not in the two-phase region). For example, a pressure close to the critical one can be chosen.

- Calculate the enthalpy $h_{e,exit}$ and the speed of sound $a_{exit}$

- Calculate the exit velocity $v_{exit}$ with the following equation:

$$u_{exit} = \sqrt{2 (h_{rupt} - h_{exit})} \quad (A.12)$$

- Calculate the exit Mach number $M_{exit} = \frac{v_{exit}}{a_{exit}}$
A.9. Vapor Cloud Explosion

• If $M_{\text{exit}} < 1$, the jet is subsonic. But if $M_{\text{exit}} > 1$ the jet is supersonic. But we set as an assumption that the maximum exit velocity cannot exceed the speed of sound. In this situation, an iteration on $P_{\text{exit}}$ is needed until reaching a unity Mach number $M_{\text{exit}} = 1$

Concerning the NTG reservoir rupture, if taking an exit pressure around the critical pressure ($P_{\text{exit}} = 4.3MPa$), all the experiments are giving an exit Mach number higher than 1. This means that for all the tests, if the reservoir rupture can be assumed to have similar properties than a jet, the pressure at the exit of the reservoir is still supercritical. They are indicated in addition to the rupture pressure in Fig. A.11.

![Entropy vs. Pressure diagram NTG reservoirs (propane)](image)

Figure A.11.: Entropy vs. Pressure diagram NTG reservoirs (propane)

A.9. Vapor Cloud Explosion

The Fig. A.12 shows the flame temporal evolution following the ignition of the butane-air mixture by an electrical spark. The initial flame speed is calculated from image processing as 3.33 m/s.
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Figure A.12.: Vapour cloud burning ($\Delta t = 5ms$)

A.10. BLEVE overpressure

A.10.1. Sensitivity of Sadek fit coefficients

Figure A.13.: Influence of 5% change in the coefficients of the Sadek fit for $c = 10\, mm$. Left: on the radius evolution, Right: on the pressure evolution
A.10. BLEVE overpressure

A.10.2. Overpressure modeling based on TNT equivalent mass

Overpressure modeling by Prugh in 1991 [108]

Model based on the TNT equivalent mass. The expansion energy is calculated with Eq. A.13. The overpressure is found by using $\bar{R}_{\text{Prugh}}$ from Eq. A.14 in Fig. A.2 left of chapter 2. The factor 0.4 in Eq. A.14 is because we consider the rupture to be ductile. If the rupture is considered as brittle, this factor is changed to 0.8.

$$E_{\text{TNT-Prugh}} = \left( \frac{0.021P_{\text{rupt}}V^*}{\gamma - 1} \right) \left( 1 - \frac{P_{\text{atm}}}{P_{\text{rupt}}} \right)^{(\gamma - 1)/\gamma}$$

(A.13)

$$\bar{R}_{\text{Prugh}} = \frac{r}{(0.4E_{\text{TNT-Prugh}})^{1/3}}$$

(A.14)

$$V^* = V_v + V_l f \left( \frac{\rho_l}{\rho_v} \right)$$

(A.15)

$$f = 1 - \exp \left( -2.63 \left( \frac{C_p(T_b)}{H_v} \right) (T_{\text{crit}} - T_b) \left( 1 - \frac{(T_{\text{crit}} - T_{\text{rupt}})(T_{\text{crit}} - T_b)^{0.38}}{T_{\text{rupt}}} \right) \right)$$

(A.16)

$$H_v = h_{e,v}(T_b) - h_{e,l}(T_b)$$

(A.17)

$$V^* = \begin{cases} 
V_v + V_l f \left( \frac{\rho_l}{\rho_v} \right) & \text{if } P_{\text{rupt}} < P_{\text{crit}} \\
V_l & \text{if } P_{\text{rupt}} > P_{\text{crit}} 
\end{cases}$$

(A.18)

Overpressure modeling by Planas-Cuchi in 2004 [105]

First, determine the quantities before rupture, expressed in Eq. A.19 - A.20

$$U_1 = m_v u_{1v} + m_l u_{1l}$$

(A.19)

$$V_1 = V$$

(A.20)

Then, make an iterative procedure to find $x_2$ that satisfies Eq. A.21 - A.25. The factors in Eq. A.25 are used to find the units of MJ.
Chapter A. Appendix linked to BLEVE

\[
U_2 = x_2 m_{\text{tot}} u_{2v} + (1 - x_2) m_{\text{tot}} u_{2l} \quad (A.21)
\]

\[
V_2 = x_2 \frac{m_{\text{tot}}}{\rho_v(P_{\text{atm}})} + (1 - x_2) \frac{m_{\text{tot}}}{\rho_l(P_{\text{atm}})} \quad (A.22)
\]

\[
\Delta U = U_2 - U_1 \quad (A.23)
\]

\[
\Delta V = V_2 - V_1 \quad (A.24)
\]

\[
-0.1 P_{\text{atm}} \Delta V = 10^{-3} \Delta U \quad (A.25)
\]

Model based on the TNT equivalent mass. The expansion energy is then calculated with Eq. (A.26). The overpressure is found by using \( \hat{R}_{\text{Planas}} \) from Eq. (A.27) in Fig. A.2 left of chapter 2.

\[
E_{\text{TNT-Planas}} = 0.214 \Delta U \quad (A.26)
\]

\[
\hat{R}_{\text{Planas}} = \frac{r}{(0.4 E_{\text{TNT-Planas}})^{1/3}} \quad (A.27)
\]

Overpressure modeling by Casal in 2006 [21]

The expansion energy is calculated from the superheated energy defined in Eq. (A.28). The expansion energy is then calculated from Eq. (A.29). The coefficient \( \beta \) is equal to 0.14 for an isentropic expansion and 0.07 for an irreversible expansion. The overpressure is found by using \( \hat{R}_{\text{Casal}} \) from Eq. (A.30) in Fig. A.2 left of chapter 2.

\[
SE = h(T_{\text{rupt}}) - h(T_{\text{amb}}) \quad (A.28)
\]

\[
E_{\text{TNT-Casal}} = \begin{cases} 
0.214 \cdot 10^{-3} \beta m_{l} SE & \text{if } P_{\text{rupt}} < P_{\text{crit}} \\
0.214 \cdot 10^{-3} \beta m_{\text{tot}} SE & \text{if } P_{\text{rupt}} > P_{\text{crit}}
\end{cases} \quad (A.29)
\]

\[
\hat{R}_{\text{Casal}} = \frac{r}{(E_{\text{TNT-Casal}})^{1/3}} \quad (A.30)
\]

Overpressure modeling by Birk in 2007 [12]

Birk considers that only the vapor is contributing to the overpressure, and that the expansion is isentropic. The expansion energy is then calculated from Eq.
A.10. BLEVE overpressure

A.31. The overpressure is found by using $\bar{R}_{\text{Casal}}$ from Eq. A.32 in Fig. A.2 left of chapter 2.

\[
E_{\text{TNT-Birk}} = \begin{cases} 
  m_e (u_{e1} - u_{e2}) & \text{if } P_{\text{rupt}} < P_{\text{crit}} \\
  m_{\text{tot}} (u_1 - u_2) & \text{if } P_{\text{rupt}} > P_{\text{crit}}
\end{cases}
\]  

(A.31)

\[
\bar{R}_{\text{Birk}} = \frac{r}{(E_{\text{TNT-Birk}})^{1/3}}
\]  

(A.32)

A.10.3. Overpressure modeling with the Sach’s scaled distance

Overpressure modeling by TNO in 1997 [40]

The expansion energy is calculated from Eq. A.33 considering isentropic expansion. The overpressure is found by using $\bar{R}_{\text{TNO}}$ from Eq. A.34 in Fig. A.2 right of chapter 2 if $\bar{R}_{\text{TNO}} > 2$.

\[
E_{\text{TNO}} = m_l (u_{l1} - u_{l2}) + m_e (u_{e1} - u_{e2})
\]  

(A.33)

\[
\bar{R}_{\text{TNO}} = r \left( \frac{100P_{\text{atm}}}{2E_{\text{TNO}}} \right)^{1/3}
\]  

(A.34)

For near field, i.e. $\bar{R}_{\text{TNO}} < 2$, there is a special procedure to calculate the overpressure, due to the fact that a BLEVE rupture has not the same behavior at near field as a TNT explosion. Therefore, the initial overpressure at the reservoir is calculated from the implicit Eq. A.36. The overpressure at the reservoir and the initial radius of the blast (Eq. A.37) are plotted in Fig. A.14. The curve the closest to the point $(R_{\text{init}}, P_{\text{init}})$ is then used to determine the overpressure at $\bar{R}_{\text{TNO}}$.

\[
ap = \sqrt{\frac{\gamma_p T_{\text{rupt}} \mu_a}{\gamma_a T_{\text{amb}} \mu_p}}
\]  

(A.35)

\[
P_{\text{rupt}} \over P_{\text{amb}} = (P_{\text{init}} + 1) \left[ 1 - \frac{(\gamma - 1) (a_a/a_p) P_{\text{init}}}{\sqrt{2\gamma_a (2\gamma_a + (\gamma_a + 1)) P_{\text{init}}}} \right]^{-2\gamma/(\gamma-1)}
\]  

(A.36)

\[
R_{\text{init}} = 0.782 V_0^{1/3}
\]  

(A.37)
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Figure A.14.: Overpressure versus Sach’s scaled distance for near field modeling ($\bar{R}_{TNO} < 2$) [40]

Overpressure modeling by Roberts in 1999 [114]

The expansion energy is calculated from Eq. A.38 considering isentropic expansion. The overpressure is found by using $\bar{R}_{Roberts}$ from Eq. A.39 in Fig. A.2 right of chapter 2. This model is similar to the TNO, except that the final vapor and liquid fractions are determined from ration of entropies (see Eq. A.40-A.43).

$$E_{Roberts} = m_{l2}u_{l2} + m_{v2}u_{v2} - m_{l1}u_{l1} - m_{v1}u_{v1} \quad (A.38)$$

$$\bar{R}_{Roberts} = r \left( \frac{100P_{atm}}{2E_{Roberts}} \right)^{1/3} \quad (A.39)$$
A.11. Experimental results overview

The Table A.5 resumes all the small scale BLEVE experiments that have been performed in this study. For each test, the type of reservoir, the power applied to the reservoir, the weakness (depth b for the Archer reservoir and length 2c for the NTG reservoir) are given. It is also mentioned if the microheater experienced a short cut during the test or if a spark ignition device was used to ignite the cloud. For each test, if the internal pressure (P), the overpressure (Dp), the temperature (T), the visualization (V) and/or the infrared camera (I) have been used, a cross is written in the corresponding column. In addition, for the visualization (V), a cross stands for a normal high-speed visualization, a S stands for a visualization with shadowgraphy, and a D stands of the use of the laser diffraction technique. In terms of experimental results, the time to rupture (t\textsubscript{rupt}), the overpressure peak at different distances, the rupture temperature (T\textsubscript{rupt}) at different positions (pos3 = plate, pos2 = bottom of reservoir, pos1 = top of reservoir), the rupture pressure (P\textsubscript{rupt}) and the reservoir opening dimensions (open) are given.

Overpressure modeling by Genova in 2008 [45]

The expansion energy is calculated from Eq. A.45. The overpressure is found by using $\bar{R}_{\text{Genova}}$ from Eq. A.46 in Fig. A.2 right of chapter 2.

\begin{align*}
x_{l2} &= \frac{s_{l1} - s_{l2}}{s_{v2} - s_{l2}} \quad (A.40) \\
x_{v2} &= \frac{s_{v1} - s_{l2}}{s_{v2} - s_{l2}} \quad (A.41) \\
m_{l2} &= (1 - x_{l2}) m_l + (1 - x_{v2}) m_v \quad (A.42) \\
m_{v2} &= x_{l2} m_l + x_{v2} m_v \quad (A.43)
\end{align*}

\[C_p = 0.5 \left( C_p(T_{\text{rupt}}) + C_p(T_b) \right) \quad (A.44)\]

\[E_{\text{Genova}} = \begin{cases} 
0.07 m_l C_p (T_{\text{rupt}} - T_b) & \text{if } P_{\text{rupt}} < P_c \\
0.07 m_{\text{tot}} C_p (T_{\text{rupt}} - T_b) & \text{if } P_{\text{rupt}} > P_c
\end{cases} \quad (A.45)\]

\[\bar{R}_{\text{Genova}} = r \left( \frac{100 P_{\text{atm}}}{2E_{\text{Genova}}} \right)^{1/3} \quad (A.46)\]
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<th>Spark</th>
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<th>Dp (kPa)</th>
<th>T&lt;sub&gt;exp&lt;/sub&gt;</th>
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<td>x</td>
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<td>674</td>
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<td>x</td>
<td>x</td>
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<td>160</td>
<td>674</td>
<td>561</td>
<td>456</td>
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<td></td>
</tr>
</tbody>
</table>
Appendix B.

Appendix linked to Boilover

B.1. Experimental setup

B.1.1. Fluid and metal properties

This section gives the diesel-oil mixture properties used in the thermal model, but also the properties of the thermocouple rake (the general properties of steel were taken) and the properties of water, all resumed in Table B.1. The evolution of the fuel viscosity with the burning time is also displayed in Fig. B.1.

<table>
<thead>
<tr>
<th>Properties</th>
<th>Diesel-oil</th>
<th>Rake</th>
<th>Water</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\rho$ [kg/m$^3$]</td>
<td>diesel: 835</td>
<td>oil: 902</td>
<td>7800</td>
</tr>
<tr>
<td>$\nu$ [m$^2$/s]</td>
<td>1.0024e-5</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>$\Delta H_v$ [MJ/kg]</td>
<td>0.24</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>$\Delta H_c$ [MJ/kg]</td>
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<td>-</td>
<td>-</td>
</tr>
<tr>
<td>$\lambda$ [W/mK]</td>
<td>$-7.167 \cdot 10^{-5}(T_{amb} - 273.15) + 0.1395$</td>
<td>60</td>
<td>0.59</td>
</tr>
<tr>
<td>$C_p$ [J/kgK]</td>
<td>3.693(T_{amb} - 273.15) + 1840</td>
<td>490</td>
<td>4190</td>
</tr>
</tbody>
</table>

B.1.2. Thermocouple principle

A thermocouple is schematized in Fig. B.2. The thermocouple principle is based on the Seebeck effect, which states that a temperature gradient in a metal induces a difference of potential in this metal. This potential difference depends on the temperature gradient and on the type of metal. Therefore, if two wires made of two different metals are in contact through a junction (A. in Fig. B.2), a voltage
will be experienced between the junction and the extremities of the wires (B. and C. in Fig. B.2).

The two wires can be directly connected to a voltmeter. But as the measured voltage is proportional to the temperature difference between the junction and the voltmeter, the voltage will be dependent of the temperature of the voltmeter. To avoid the voltmeter influence, a reference temperature area, characterized by a constant temperature, is included in the circuit. The extremities of the two wires are connected to identical material wires and these two new junctions are placed in the reference temperature area. The voltage is then measured at the extremities of these two new wires. As these two wires are composed of the same material and their extremities are at the same temperature (reference and voltmeter), their influence on the global voltage difference is negligible. Originally, the reference temperature area consisted on a ice point or a triple point. But nowadays, the reference temperature is electronically controlled.

![Figure B.2.: Thermocouple principle](image)

The thermocouples used in this thesis are K type thermocouples. The positive
wire consists of a nickel-chrome alloy, better known as chromel, and the negative wire consists of a nickel-aluminum alloy, better known as alumel. This type of thermocouple has a continuous temperature range of 273 – 1373 K and can be used until 93 – 1573 K \[115\].

Figure B.3.: Different positions for the junction (from Single iteration company)

In the boilover experiments, the thermocouples wires are placed inside a steel tube, filled with a ceramic powder, as schematized in Fig. B.3 left. This design was chosen in order to protect the thermocouple from the flames of the pool fire. Inside the steel tube, the junction is isolated from the sheath. The junction can also be grounded to the sheath, which is assumed to increase the response time, but leads to a more fragile design. In this study, a 1 mm thermocouple with a isolated and a welded junction were tested, and no significant improvement of the response time was observed. Therefore, a isolated junction was chosen, as this design is more robust.

In the BLEVE experiments, the thermocouple wires are protected by glass fiber. The junction is exposed to the atmosphere, as schematized in Fig. B.3 right. An exposed junction increases the response time of the thermocouple, but is more sensitive to the environment and can more easily be broken.

**B.1.3. Thermocouple rake design**

One of the main characterization of the boilover phenomenon is the measurement of the temperature evolution both of the fuel and the water layers. In literature, the previous boilover experiments measured the liquid temperature with thermocouples inserted horizontally in the reservoir. But as very small scale, this design can be spatially large, and strongly reservoir dependent. Therefore, another design was chosen in this thesis: a thermocouple rake that is inserted in the reservoir
from the upper part, and that can be very easily removed and replaced in another reservoir. The final design of the thermocouple rake, as presented in chapter 10, was the result of an iterative process. The first version of the thermocouple rake consisted on a steel pipe with a rectangular opening on one end. This opening was meant to place a series of type K thermocouples, non sheated, of 0.12 mm diameter. Each thermocouple was inserted in a ceramic tube. The series of ceramic tube were fixed to the steel pipe with a ceramic based cement. The thermocouple rake can be observed in Fig. B.4 left. During the first test of this thermocouple rake, the cement reacted with the fuel and the water of the reservoir. This reaction prevented the boilover to happen. Therefore, a resin was applied on the cement, to protect it from the liquids.

But after analysis of the temperature profiles measured by this thermocouple rake, the flame temperatures were really low and the water temperatures were really high. After investigation, it was found that the thermocouple junctions were too close to the steel pipe, and therefore were measuring the temperature of the pipe and not the temperature in the environment. This assumption has been proved by calculating the response time of the rake when the rake is changed from a hot oil bath (around 350K) to a water bath at ambient temperature. As observed in Fig. B.4 right, the response time of the thermocouple located in the rake is similar to the response time of the steel pipe. Therefore, a second thermocouple rake was designed, by keeping a distance of 5 mm between the thermocouple junction and the ceramic tube, each thermocouple being distant of 2.5 mm. The thermocouples were assembled on a 6 mm diameter steel pipe. This rake was then tested in a thermostatic bath and showed a measurement fairly independent from the pipe. This new design is displayed in Fig. B.5 left. But during the first boilover test with the use of this new rake, the cement used to fix the tubes did not resist to the heat, and also the thermocouple wires were burnt by the flame, as observed in Fig. B.5 right.

In conclusion, the sheathed thermocouples were found to be the solution to protect
B.1. Experimental setup

The thermocouple wire from the flame. Then, a numerous series of cement were tested to see if they could resist both to heat and to diesel and water. But no cement were found to perfectly resist. This impossibility of fixing the thermocouples with a cement induced the final design, which fixes the thermocouples by clamping them instead of gluing them.

B.1.4. Load cell calibration

The load cell was calibrated by placing different known weights, always at the same position on the bakelite plate (see more details in chapter 10). The voltage given by the load cell was monitored for each of the weights, showing a linear increase of the voltage with the applied weight. The calibration was performed every day. One example of calibration is shown in Fig. B.6.

B.1.5. Medtherm calibration

The Medtherm radiometers were calibrated with a black body. The main advantage of the black body is that its emissivity can be considered to unity. Therefore, the temperature inside the black body is directly proportional to the heat flux emitted by this black body. Each of the radiometers were placed in the entry of the black body hole, and the temperature was varied from 323 to 773\,K by steps of 50\,K, which corresponds to a heat flux range of 0.6 – 20\,kW/m². The calibration
Chapter B. Appendix linked to Boilover

Figure B.6.: Load cell calibration, 8th March

Figure B.7.: Medtherm calibration curves: Left SN 94481, Right: SN 95461

curves of the two radiometers used in this study are given in Fig. B.7. The two radiometers were amplified 2000 times with an analog amplifier.

B.1.6. Algorithms used in the Flame detection

B.1.6.1. Otsu method

The Otsu method determines a threshold to convert a grey scale image into black & white. The principle of the method is to iterate through all the possible threshold values (for a typical greyscale, from 0 to 255) and to calculate for each of them a measure of the spread for the pixel levels on each side of the threshold, i.e. the pixels that either fall in foreground or background. The threshold that will
B.1. Experimental setup

be chosen by the Otsu method is the value where the sum of foreground and background spreads is at its minimum.

Figure B.8.: A 6-level greyscale image and its histogram (from labookpages.co.uk)

The algorithm will be demonstrated using the simple 6x6 image shown in Fig. B.8 together with the grayscale histogram. To simplify the explanation, only 6 greyscale levels are used instead of the 256 levels of a typical greyscale image. In this case the threshold value is 3. The calculations for finding the foreground and background variances (the measure of spread) for a single threshold are displayed in Eq. B.1 to B.7. From the weight of each class (foreground (subscript f) and background (subscript b) and their mean ($\mu$) and variance ($\sigma^2$), the Within-Class variance is calculated in Eq. B.7. This is simply the sum of the two variances multiplied by their associated weights.

\[
W_b = \frac{8 + 7 + 2}{36} = 0.4722 \quad (B.1)
\]

\[
\mu_b = \frac{(0 \cdot 8) + (1 \cdot 7) + (2 \cdot 2)}{17} = 0.6471 \quad (B.2)
\]

\[
\sigma^2_b = \frac{(0 - 0.6471)^2 \cdot 8 + (1 - 0.6471)^2 \cdot 7 + (2 - 0.6471)^2 \cdot 2}{17} = 0.4637 \quad (B.3)
\]

\[
W_f = \frac{6 + 9 + 4}{36} = 0.5278 \quad (B.4)
\]

\[
\mu_f = \frac{(3 \cdot 6) + (4 \cdot 9) + (5 \cdot 4)}{19} = 3.8947 \quad (B.5)
\]

\[
\sigma^2_f = \frac{(3 - 3.8947)^2 \cdot 6 + (4 - 3.8947)^2 \cdot 9 + (5 - 3.8947)^2 \cdot 4}{19} = 0.5152 \quad (B.6)
\]

\[
\sigma^2_w = W_b \sigma^2_b + W_f \sigma^2_f = 0.4909 \quad (B.7)
\]

This same calculation needs to be performed for all the possible threshold values, i.e. from 0 to 5. The Table B.1.6.1 below shows the results for these calculations.
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It can be observed that the minimum within-class variance is obtained with the threshold 3. Therefore, this is the final selected threshold. All pixels with a level less than 3 are background, all those with a level equal to or greater than 3 are foreground.

\begin{table}[h]
\centering
\begin{tabular}{|c|c|c|c|c|c|}
\hline
Threshold & 0 & 1 & 2 & 3 & 4 & 5 \\
\hline
$\sigma^2_w$ & 3.1196 & 1.5268 & 0.5561 & 0.4909 & 0.9779 & 2.2491 \\
\hline
\end{tabular}
\caption{Within-class variance for different thresholds}
\end{table}

**B.1.6.2. Moore-Neighbor tracing algorithm modified by Jacob’s stopping criteria**

The Moore neighborhood of a pixel, $P$, is the set of 8 pixels which share a vertex or an edge with that pixel. These pixels are namely pixels $P_1$ to $P_8$, as shown in Fig. B.9 left. Therefore, the Moore-Neighbor tracing algorithm works as illustrated in Fig. B.9. Given a digital pattern, i.e. a group of black pixels (which are colored as pink in Fig. B.9) or a background of white pixels. The algorithm locates a black pixel and declare it as the starting pixel. This pixel is colored in green in Fig. B.9. The principle of the algorithm will be illustrated by a bug. The bug starts from a given direction (that need to be specified in the algorithm inputs), here assumed as west. The bug moves around all the moore-neighborhood pixels of the start pixels until the bug find another black pixel, as shown in Fig. B.9 right.

![Figure B.9.: Illustration of Moore-Neighbor tracing algorithm](image)

The general idea is: every time you hit a black pixel, $P$, backtrack i.e. go back to the white pixel you were previously standing on, then, go around pixel $P$ in a clockwise direction, visiting each pixel in its Moore neighborhood, until you hit a black pixel. The algorithm terminates when the start pixel is visited for a second time. The black pixels you walked over will be the contour of the pattern.

The main weakness of Moore-Neighbor tracing lies in the choice of the stopping criterion. In the original description of the algorithm used in Moore-Neighbor tracing, the stopping criterion is visiting the start pixel for a second time. But this...
idea fails to visit a large number of pixels of the pattern. Therefore, a better
criterion is to stop after entering the start pixel a second time in the same manner
you entered it initially. This criterion was proposed by Jacob Eliosoff and we will
therefore call it Jacob’s stopping criterion.

B.1.7. Experiments performed by the EMA

In August 2011, the Ecole des Mines d’Alès (EMA) performed a field test where
the objective was to try to reproduce test conditions as close as possible from the
field tests performed at the von Karman Institute. Therefore, a metal reservoir of
150 mm diameter and 90 mm height was filled with around 19 mm of water and 55
mm of a mixture of 70% diesel and 30% of Yacco LS200 80w90 oil. The reservoir
was placed inside a retention tank to gather the oil projections during the boilover
apparition, as observed in Fig. B.10 left. A series of K thermocouples were inserted
from the bottom of the tank at different height and positions along the reservoir
diameter and were recorded at 2 Hz, as observed in Fig. B.10 right. A load cell
monitored the mass loss due to combustion with an acquisition frequency of 5 Hz.
Two Captec radiometers positioned at 1 m and 1.56 m from the reservoir center
monitored the flame radiation at a frequency of 2 Hz. Finally, two normal digital
camera and one high-speed camera recorded the flame evolution. The high-speed
camera was a Photron SA3, with a 512x768 field of view, and 250 Hz as sample
frequency.

Figure B.10.: Reservoir used in EMA field tests. Left: global view, Right: view of
thermocouples and fuel layer
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B.2. Experimental results

B.2.1. Validation metrics definition

According to Oberkampf \cite{99, 98}, a validation is a process of determining the degree to which a model is an accurate representation of the real world, from the perspective of the intended uses of the model. One approach for validating a model is to look at validation metrics. In this study, four validation metrics were used: the normalized mean square error (NMSE), the fractional bias (FB), the geometric mean (MG) and the geometric variance (VG). They compare $O$, the observed value from experimental data and $P$, the output from a numerical model or a correlation. The NMSE gives the mean scatter between $O$ and $P$ on a linear scale, FB gives the mean bias between $O$ and $P$ on a linear scale, and MG and VG gives the mean scatter and bias respectively, on a logarithmic scale. These four validations metrics are defined as follow:

\[
NMSE = \frac{(O - P)^2}{OP} \quad (B.8)
\]
\[
FB = \frac{(O - P)}{0.5 (O + P)} \quad (B.9)
\]
\[
MG = \exp \left( \ln O - \ln P \right) \quad (B.10)
\]
\[
VG = \exp \left[ \frac{(O - P)^2}{(O + P)^2} \right] \quad (B.11)
\]

If a dataset has extremely high or low values, these values will affect the different validation metrics differently. The NMSE and FB will be more strongly affected by seldom high values while the MG and VG will be more affected by low values.

B.2.2. Temperature modeling

B.2.2.1. Absorption coefficient of water

The Fig. B.11 the variation of the water absorption coefficient with the wavelength, as measured by Segelstein \cite{120}.

B.2.2.2. Radiation heat flux from the flame to the fuel surface

As already expressed in chapter \cite{11} the modeling of the heat transfer to the pool surface by radiation is expressed by Eq. B.12.
B.2. Experimental results

\[ \dot{q}_{\text{rad}} = \sigma T_f^4 \left( 1 - 0.08 \right) \left[ 1 - (1 - \epsilon_{\text{soot}}) (1 - \epsilon_w) (1 - \epsilon_{\text{co}2}) \right] \] (B.12)

In this equation, the constant 0.08 is the surface reflectivity for liquid fuel, and estimated as 0.08 by Hamins \[53\]. In this same equation, \( \epsilon_{\text{soot}} \) is the spectrally average emissivity of soot. Yuen and Tien \[148\] have showed that the soot emission can be approximated by a gray emitter, expressed as Eq. B.13 where \( L_p \) is the path length and \( \kappa \) is the effective soot emission parameter, expressed in Eq. B.14.

In this equation, \( C_2 = 0.014388 \ mK \) is the Planck’s second constant and \( C_1 \) is the dimensionless effective soot concentration parameter, expressed in Eq. B.15.

\[ \epsilon_{\text{soot}} = 1 - e^{-kL_p} \] (B.13)

\[ \kappa = \frac{3.6C_1T_F}{C_2} \] (B.14)

\[ C_1 = \frac{36\pi s_vn^2s}{\left(n^2 - (ns)^2 + 2\right) + 4n^4s^2} \] (B.15)

In the expression of \( C_1 \), \( n \) and \( s \) are the infrared averaged optical constants of soot particles and are taken as \( n = 3.49 \) and \( s = 2.17 \) from Tien and Lee \[133\]. \( s_v \) is the soot volume fraction, and has been taken equal to the soot volume fraction of toluene (\( s_v = 6 \cdot 10^{-3} \)). Finally, the path length \( L_p \) is represented by the mean beam length, by making the assumption of homogeneous gray mixture, and the flame is assumed to be a right cylinder of height \( L \). Therefore, the mean beam length.
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length is expressed as Eq. [B.16] and the flame length is taken from the model of Heskestad [58] and expressed in Eq. [B.18].

\[ L_p = \frac{3.6 Vol_F}{A_F} = \frac{3.6\pi(D/2)^2 L}{2\pi(D/2)((D/2) + L)} \] (B.16)

\[ Z = \left( \frac{C_{pa} T_{amb}}{\rho a (\Delta H_c/\phi)^3} \right) \left( m \Delta H_c \right)^2 D^5 \] (B.17)

\[ L = D \left( -1.02 + 15.6 Z^{1/5} \right) \] (B.18)

The total emissivities of H\(_2\)O (\(\epsilon_w\)) and CO\(_2\) (\(\epsilon_{co2}\)) are calculated using the algorithm given by Leckner [80], and resumed in Eq. [B.19] to [B.23].

\[ \tau_F = T_F/1000 \] (B.19)

\[ pL_w = 0.127 L_p,100 \] (B.20)

\[ pL_c = 0.133 L_p,100 \] (B.21)

\[ \log(\epsilon_w) = (-2.1118 - 1.1987\tau_F + 0.035596\tau_F^2) + (0.85667 + 0.93048\tau_F - 0.14391\tau_F^2) \log(pL_w) + (-0.10838 - 0.17156\tau_F + 0.045915\tau_F^2) \log(pL_w)^2 \] (B.22)

\[ \log(\epsilon_c) = (-3.9781 + 2.7353\tau_F - 1.9882\tau_F^2 + 0.31054\tau_F^3 + 0.015719\tau_F^4) + (1.9326 - 3.5932\tau_F + 3.7247\tau_F^2 - 1.4535\tau_F^3 + 0.20132\tau_F^4) \log(pL_c) + (-3.5366 + 0.61766\tau_F - 0.84207\tau_F^2 + 0.39859\tau_F^3 + 0.063356\tau_F^4) \log(pL_c)^2 + (-0.080181 + 0.31466\tau_F - 1.9973\tau_F^2 + 0.046532\tau_F^3 + 0.0033086\tau_F^4) \log(pL_c)^3 \] (B.23)

B.2.2.3. Discretization of the Heat equations

Fuel layer heat equation:

\[ T_i^{n+1} = T_i^n \left( 1 - 2F_{of} - 4B_{if}F_{of} \frac{\Delta x}{D} - 4B_{if}F_{of} \frac{\Delta x}{D} \right) + F_{of} \left( T_i^{n+1} + T_i^{n-1} \right) + \left( \frac{q_{\mu f} \Delta t}{p_{if} C_{pf}} \right) \exp \left( -\mu f x_i \right) \frac{\Delta x}{D} T_{amb} + 4B_{if}F_{of} \frac{\Delta x}{D} T_{p,i} \] (B.24)
B.2. Experimental results

Water layer heat equation:

\[ T_{n+1}^i = T_n^i \left( 1 - 2F_{ow} - 4B_{iw}F_{ow} \frac{\Delta x}{D} - 4B_{pw}F_{ow} \frac{\Delta x}{D} \right) \]

\[ + F_{of} \left( T_{i+1}^n + T_{i-1}^n \right) + \left( \frac{\delta_{iw}}{\rho_{iw}C_{pw}} \right) \exp \left( -\mu_fH \right) \exp \left( -\mu_wx_i \right) \]

\[ + 4B_{iw}F_{ow} \frac{\Delta x}{D} T_{amb} + 4B_{pw}F_{ow} \frac{\Delta x}{D} T_{n,p,i} \] (B.25)

Boundary conditions for the fuel and water model:

Fuel surface:

\[ T_{N_w+N_c+1}^n = T_b \] (B.26)

Fuel - water interface:

\[ T_{N_w+1}^n = \frac{\lambda_f T_{N_w+2}^n + \lambda_w T_{N_w}^n}{\lambda_f + \lambda_w} \] (B.27)

Bottom (to include in Eq. B.25):

\[ T_{-1}^n = \frac{-2h\Delta x}{\lambda_w} \left( T_{amb} - T_0^n \right) + T_1^n \] (B.28)

Thermocouple rake heat equation in fuel:

\[ T_{p,i}^{n+1} = T_{p,i}^n \left( 1 - 2F_{op} - 4B_{pф}F_{op} \frac{\Delta x}{D} \right) \]

\[ + F_{op} \left( T_{p,i+1}^n + T_{p,i-1}^n \right) + 4B_{pф}F_{op} \frac{\Delta x}{D} T_i^n \] (B.29)

Thermocouple rake heat equation in water:

\[ T_{p,i}^{n+1} = T_{p,i}^n \left( 1 - 2F_{op} - 4B_{pф}F_{op} \frac{\Delta x}{D} \right) \]

\[ + F_{op} \left( T_{p,i+1}^n + T_{p,i-1}^n \right) + 4B_{pф}F_{op} \frac{\Delta x}{D} T_i^n \] (B.30)

Thermocouple rake heat equation in flame:

\[ T_{p,i}^{n+1} = T_{p,i}^n \left( 1 - 2F_{op} + 4B_{pф}F_{op} \frac{\Delta x}{D} \right) \]

\[ + F_{op} \left( T_{p,i+1}^n + T_{p,i-1}^n \right) - 4B_{pф}F_{op} \frac{\Delta x}{D} T_i^n \] (B.31)

Boundary conditions for the fuel and water model: the top rake condition needs to be included in Eq. B.31 and the bottom rake condition in B.30.

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B.2.3. Flame modeling

B.2.3.1. Flame frequency

As explained in chapter 11, the puffing frequency of the flame has been determined by the Maximum Entropy Method (MEM).

Usually, the frequency content of a signal is calculated with power spectral densities (PSD). Through the use of a Fourier transform, a signal initially in the time domain $x(t)$ is converted into the frequency domain $X(\omega)$, as expressed in Eq. B.34. The fast Fourier transform (FFT) is an efficient algorithm to compute the discrete Fourier transform, and is the most widely used. But in the case of flame frequency analysis, the FFT gives noisy signals, and it is difficult to identify clearly a frequency peak from it.

$$X(\omega) = \frac{1}{\sqrt{2\pi}} \int_{-\infty}^{\infty} x(t)e^{-i\omega t}dt$$ \hspace{1cm} (B.34)

Therefore, the maximum entropy method (MEM) has been used. This method is based on the axiom saying that: of all the possible solutions, we should use the one with the maximum entropy [142]. As showed in the BLEVE part, the entropy is a measure of the disorder of a system. If this concept is applied to signal processing, the entropy is the uncertainty of an information source. Therefore, the entropy $S$ is expressed in Eq. B.35, where $x(\omega)$ is the power spectral density of the signal, which is expressed in Eq. B.36. This equation is the solution of the inverse of an autoregressive moving average digital filter, where $p_O$ is the power of prediction error, $i$ is $\sqrt{-1}$, $a_l$ are the coefficients of the prediction error filter, and $O$ is the order of the filter [142].

$$S = \int \log(x(\omega))d\omega$$ \hspace{1cm} (B.35)

$$x(\omega) = \frac{p_O}{|\sum_{l=0}^{O} a_l e^{(-2\pi i l \omega)}|^2}$$ \hspace{1cm} (B.36)
B.2. Experimental results

The algorithm for Eq. (B.36) has already been implemented in Matlab. The only variable is the filter order $O$ that needs to be determined. The more the filter order increases, the more complex is the power spectral density, and less noise is eliminated. A good choice for the order is a trade-off between a correct determination of the puffing frequency and noise reduction. In chapter 11, an order of 20 has been chosen. Different power spectral densities are plotted in Fig. B.12 with varying filter orders.

![Power spectrum for different model orders](image)

Figure B.12.: Power spectrum for different model orders.

The Table B.2.3.1 shows the validation metrics of the different configurations performed in this study, in addition to the results of Mudan [94] and Ferrero [36]. Only one correlation is tested here, and has been proposed by Pagni [102].

### B.2.4. Flame radiation

The transmissivity is modeled by Moorhouse & Pritchard [12], and expressed only in function of distance from the source to the observer, in Eq. (B.37)

$$\tau = 0.998^r$$ (B.37)

The geometric view factor is taken as if the flame is a vertical cylinder with a height equal to the flame height, and the observer is a vertical surface at ground level with the normal to the plane oriented to the centerline of the cylinder, as
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Table B.3.: Validation metrics for the comparison of experimental data with Pagni

<table>
<thead>
<tr>
<th>Experiments</th>
<th>Metric Name</th>
<th>Evaluation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Mudan</td>
<td>NMSE</td>
<td>0.0321</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>-0.0642</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>0.9374</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>1.0327</td>
</tr>
<tr>
<td>Ferrero</td>
<td>NMSE</td>
<td>0.0550</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>-0.0125</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>1.0146</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>1.0570</td>
</tr>
<tr>
<td>D=80mm, glass reservoir</td>
<td>NMSE</td>
<td>0.0399</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>0.0862</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>0.916</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>1.03</td>
</tr>
<tr>
<td>D=80mm, metal reservoir</td>
<td>NMSE</td>
<td>0.0299</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>0.1565</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>0.857</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>1.0308</td>
</tr>
<tr>
<td>D=115mm, metal reservoir</td>
<td>NMSE</td>
<td>0.0886</td>
</tr>
<tr>
<td></td>
<td>FB</td>
<td>0.2631</td>
</tr>
<tr>
<td></td>
<td>MG</td>
<td>0.767</td>
</tr>
<tr>
<td></td>
<td>VG</td>
<td>1.091</td>
</tr>
</tbody>
</table>

schematized in Fig. B.13. It is expressed in Eq. B.42 where the different variables of this equation are defined in Eq. B.38 to B.41.

\[
L^* = \frac{2L}{D} \tag{B.38}
\]

\[
R^* = \frac{2r}{D} \tag{B.39}
\]

\[
A = (R^* + 1)^2 + (L^*)^2 \tag{B.40}
\]

\[
B = (R^* - 1)^2 + (L^*)^2 \tag{B.41}
\]

\[
F = \frac{1}{\pi} \left( \frac{1}{R^* \tan^{-1} \left( \sqrt{\frac{(L^*)^2}{(R^*)^2 - 1}} \right)} + \frac{L^* (A - 2R^*)}{R^* \sqrt{AB} \tan^{-1} \left( \sqrt{\frac{(R^* - 1) A}{(R^* + 1) B}} \right) - \frac{L^*}{R^* \tan^{-1} \left( \sqrt{\frac{R^* - 1}{R^* + 1}} \right)} \right) \tag{B.42}
\]
B.2. Experimental results

Figure B.13.: View factor of a cylindrical flame

B.2.5. Boilover phenomenology, flame enlargement

B.2.5.1. Vorticity definition and implementation

As already explained in chapter [11], the vorticity is defined by Eq. \[ \text{(B.43)} \]

\[
\omega = \frac{\partial V}{\partial X} - \frac{\partial U}{\partial Y} 
\]  

This equation has been implemented in Matlab, using finite differentiation described in [109]. The idea is to choose a small rectangular contour around which the circulation is calculated using a standard integration scheme. The local circulation is then divided by the enclosed area to arrive at an average vorticity in this area. The Eq. \[ \text{(B.45)} \] provides a vorticity estimate at point (i,j), based on a circulation estimated around the neighboring eight points.
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\[(\omega)_{i,j} \cong \frac{\Omega_{i,j}}{4\Delta X \Delta Y}\]  \hspace{1cm} (B.44)

\[\Omega_{i,j} = \frac{1}{2} \Delta X (U_{i-1,j-1} + 2U_{i,j-1} + U_{i+1,j-1}) + \]

\[\frac{1}{2} \Delta Y (V_{i+1,j-1} + 2V_{i,j} + V_{i+1,j}) - \]

\[\frac{1}{2} \Delta X (U_{i+1,j+1} + 2U_{i,j+1} + U_{i-1,j+1}) - \]

\[\frac{1}{2} \Delta Y (V_{i-1,j+1} + 2V_{i-1,j} + V_{i-1,j-1})\]  \hspace{1cm} (B.45)

B.2.6. Resume of boilover small scale experiments

The Table B.4 presents a resume of the small scale experiments, grouped by all the parameters linked to the quasi-steady period. This Table groups the test number, the reservoir material, the reservoir diameter \(D\), the initial fuel and water layer thicknesses \((H_f\) and \(H_w\)), the position of the radiometers \(r/D\), the average radiation at the first radiometer \(\dot{q}_{r,1}\), and at the second \(\dot{q}_{r,2}\), the initial total mass, the flame frequency \(f\) and minimum, average and maximum flame lengths \((L_{min}, L_{meam}, \textrm{and} L_{max} \textrm{respectively})\).

The Table B.5 presents a resume of the small scale experiments, grouped by all the parameters linked to the boilover period. This Table groups the test number, the time to boilover occurrence \(t_{bo}\), the number of boilovers \(N_{br_{bo}}\), the maximum radiation measured by the first radiometer \(\dot{q}_{r,bo1}\), and by the second radiometer \(\dot{q}_{r,bo2}\), the mass lost during the larger boilover \(m_{bo1}\), the mass lost during the whole boilover period \(m_{bo_{tot}}\), the mass before the boilover occurrence \(m_{pre-bo}\), the temperature of the fuel-water interface at boilover occurrence \(T_{int}\), the maximum flame area \(A_F\), and the maximum flame length and width (\(L_{bo_{max}}\) and \(H_{bo_{max}}\) respectively).

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<table>
<thead>
<tr>
<th>Test</th>
<th>Material</th>
<th>$D$</th>
<th>$H_f$</th>
<th>$H_w$</th>
<th>$r/D$</th>
<th>$\dot{m}$</th>
<th>$\dot{q}_{x1}$</th>
<th>$\dot{q}_{x2}$</th>
<th>$m_0$</th>
<th>$f$</th>
<th>$L_{min}$</th>
<th>$L_{mean}$</th>
<th>$L_{max}$</th>
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<td>09/02/11-1</td>
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<td>80</td>
<td>16</td>
<td>5</td>
<td>5.7</td>
<td>/</td>
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<td>8</td>
<td>31</td>
<td>6.7</td>
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<td>468.3</td>
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Bibliography


"The circle is now complete.

When I left you, I was but the learner,
Now I am the master.

Dark Vador"