Dépôt Institutionnel de l’Université libre de Bruxelles / Université libre de Bruxelles Institutional Repository
Thèse de doctorat/ PhD Thesis

Citation APA:

Disponible à / Available at permalink : https://dipot.ulb.ac.be/dspace/bitstream/2013/211018/4/708c0b50-47a6-40b6-8dee-3647b7a465d4.txt

(English version below)

Cette thèse de doctorat a été numérisée par l’Université libre de Bruxelles. L’auteur qui s’opposerait à sa mise en ligne dans DI-fusion est invité à prendre contact avec l’Université (di-fusion@ulb.ac.be).

Dans le cas où une version électronique native de la thèse existe, l’Université ne peut garantir que la présente version numérisée soit identique à la version électronique native, ni qu’elle soit la version officielle définitive de la thèse.

DI-fusion, le Dépôt Institutionnel de l’Université libre de Bruxelles, recueille la production scientifique de l’Université, mise à disposition en libre accès autant que possible. Les œuvres accessibles dans DI-fusion sont protégées par la législation belge relative aux droits d’auteur et aux droits voisins. Toute personne peut, sans avoir à demander l’autorisation de l’auteur ou de l’ayant-droit, à des fins d’usage privé ou à des fins d’illustration de l’enseignement ou de recherche scientifique, dans la mesure justifiée par le but non lucratif poursuivi, lire, télécharger ou reproduire sur papier ou sur tout autre support, les articles ou des fragments d’autres œuvres, disponibles dans DI-fusion, pour autant que :
- Le nom des auteurs, le titre et la référence bibliographique complète soient cités;
- L’identifiant unique attribué aux métadonnées dans DI-fusion (permalink) soit indiqué;
- Le contenu ne soit pas modifié.

L’œuvre ne peut être stockée dans une autre base de données dans le but d’y donner accès ; l’identifiant unique (permalink) indiqué ci-dessus doit toujours être utilisé pour donner accès à l’œuvre. Toute autre utilisation non mentionnée ci-dessus nécessite l’autorisation de l’auteur de l’œuvre ou de l’ayant droit.

---------------------------------------------------------------------------------------------------------------------------------------

This Ph.D. thesis has been digitized by Université libre de Bruxelles. The author who would disagree on its online availability in DI-fusion is invited to contact the University (di-fusion@ulb.ac.be).

If a native electronic version of the thesis exists, the University can guarantee neither that the present digitized version is identical to the native electronic version, nor that it is the definitive official version of the thesis.

DI-fusion is the Institutional Repository of Université libre de Bruxelles; it collects the research output of the University, available on open access as much as possible. The works included in DI-fusion are protected by the Belgian legislation relating to authors’ rights and neighbouring rights. Any user may, without prior permission from the authors or copyright owners, for private usage or for educational or scientific research purposes, to the extent justified by the non-profit activity, read, download or reproduce on paper or on any other media, the articles or fragments of other works, available in DI-fusion, provided:
- The authors, title and full bibliographic details are credited in any copy;
- The unique identifier (permalink) for the original metadata page in DI-fusion is indicated;
- The content is not changed in any way.

It is not permitted to store the work in another database in order to provide access to it; the unique identifier (permalink) indicated above must always be used to provide access to the work. Any other use not mentioned above requires the authors’ or copyright owners’ permission.
Experimental Investigation of Superheated Liquid Jet Atomization due to Flashing Phenomena

Thesis presented by
Dilek YILDIZ
M.Sc.Civil Engineer

In order to obtain the degree of *Docteur en Sciences Appliquées*,
Université Libre de Bruxelles (ULB), September 2005

Composition of the Jury:

- Prof. Gerard DEGREZ (President of jury, ULB)
- Prof. Jean-Marie BUCHLIN (Director of the thesis, ULB)
- Prof. Jeroen VAN BEECK (Supervisor, VKI)
- Prof. Veronique HALLOIN (Examinator, ULB)
- Prof. Georges BERTHOUD (Examinator, CEA)
- Prof. Oscar HAIDN (Examinator, DLR)
"If you believe certain words, you believe their hidden arguments. When you believe something is right or wrong, true or false, you believe the assumptions in the words which express the arguments. Such assumptions are often full of holes, but remain most precious to the convinced."

The Open-Ended Proof, from the Panoplia Prophetica
Contents

List of Figures x

List of Tables xi

1 Introduction and description of the problem 1
  1.1 Objectives and methodology of the present work 2
  1.2 Chapters overview 3

2 Past studies in the literature and the proposed theories 5
  2.1 Studies on the mechanical cylindrical jet break-up 5
    2.1.1 Mechanical jet break-up regimes 6
  2.2 Liquid Sprays 10
    2.2.1 Spray structure 10
    2.2.2 Liquid and velocity distributions in a drop jet 17
    2.2.3 Droplet size distribution functions 18
  2.3 Nozzle Hydraulics 30
  2.4 Description of Flashing Mechanism 31
    2.4.1 Liquid Vapor Phase Change 37
      2.4.1.1 Classical theory of single bubble growth 38
      2.4.1.2 Explosive Boiling at the limit of superheat 39
    2.4.2 Studies on single exploding (flashing) superheated droplets 41
    2.4.3 Studies on blow-downs 43
  2.5 Studies on the Superheated Jets 47
    2.5.1 Single-component superheated fluid systems 48
      2.5.1.1 Superheated Jet break-up 49
      2.5.1.2 Jet opening angle 57
      2.5.1.3 Bubble growth observations in superheated jets 58
      2.5.1.4 Velocity measurements from superheated jet atomisation 59
      2.5.1.5 Droplet size measurements from superheated jet atomisation 61
    2.5.2 Superheated binary fluid systems 69
    2.6 Droplet size estimations 70

3 Experimental installation and measurement techniques 75
  3.1 Choice of the testing material 75
  3.2 Test facilities 76
<table>
<thead>
<tr>
<th>Section</th>
<th>Title</th>
<th>Page</th>
</tr>
</thead>
<tbody>
<tr>
<td>3.2.1</td>
<td>Experimental apparatus for the preliminary measurements</td>
<td>76</td>
</tr>
<tr>
<td>3.2.2</td>
<td>Final test facility</td>
<td>77</td>
</tr>
<tr>
<td>3.2.2.1</td>
<td>Test nozzles</td>
<td>78</td>
</tr>
<tr>
<td>3.2.2.2</td>
<td>Pressure Transducer</td>
<td>80</td>
</tr>
<tr>
<td>3.2.2.3</td>
<td>Thermocouple measurements inside the discharge tube</td>
<td>81</td>
</tr>
<tr>
<td>3.3</td>
<td>Measurement Techniques and Strategies</td>
<td>81</td>
</tr>
<tr>
<td>3.3.1</td>
<td>High-speed imaging</td>
<td>82</td>
</tr>
<tr>
<td>3.3.2</td>
<td>Non-intrusive Laser-based techniques</td>
<td>82</td>
</tr>
<tr>
<td>3.3.2.1</td>
<td>Phase Doppler Anemometry (PDA)</td>
<td>82</td>
</tr>
<tr>
<td>3.3.2.2</td>
<td>Particle Image Velocimetry (PIV)</td>
<td>84</td>
</tr>
<tr>
<td>3.3.2.3</td>
<td>Measurements performed with the laser-based techniques</td>
<td>86</td>
</tr>
<tr>
<td>3.3.2.4</td>
<td>Measurements performed with the laser-based techniques</td>
<td>89</td>
</tr>
<tr>
<td>3.3.3</td>
<td>Temperature measurements</td>
<td>91</td>
</tr>
<tr>
<td>3.3.3.1</td>
<td>Intrusive temperature measurements</td>
<td>91</td>
</tr>
<tr>
<td>3.3.3.2</td>
<td>Non-intrusive temperature measurements</td>
<td>94</td>
</tr>
<tr>
<td>3.3.4</td>
<td>Discharge coefficient measurements for single phase flow</td>
<td>95</td>
</tr>
<tr>
<td>3.4</td>
<td>Experimental Results</td>
<td>101</td>
</tr>
<tr>
<td>4.1</td>
<td>Exploration of Laser-based Techniques for Characterization of a Flashing Jet</td>
<td>101</td>
</tr>
<tr>
<td>4.1.1</td>
<td>PDA Results</td>
<td>102</td>
</tr>
<tr>
<td>4.1.2</td>
<td>PIV Results</td>
<td>105</td>
</tr>
<tr>
<td>4.1.3</td>
<td>Comparison of PDA and Standard PIV</td>
<td>108</td>
</tr>
<tr>
<td>4.1.4</td>
<td>Assessment of the feasibility of Multi-Intensity Layer PIV</td>
<td>109</td>
</tr>
<tr>
<td>4.1.5</td>
<td>Summary</td>
<td>114</td>
</tr>
<tr>
<td>4.2</td>
<td>Break-up patterns of the superheated liquid jet</td>
<td>115</td>
</tr>
<tr>
<td>4.2.1</td>
<td>Effect of the nozzle diameter</td>
<td>115</td>
</tr>
<tr>
<td>4.2.2</td>
<td>Effect of the temperature</td>
<td>118</td>
</tr>
<tr>
<td>4.2.3</td>
<td>Effect of the backpressure</td>
<td>120</td>
</tr>
<tr>
<td>4.2.4</td>
<td>Effect of the $l/D$ ratio</td>
<td>121</td>
</tr>
<tr>
<td>4.2.5</td>
<td>Break-up distance of the flashing jet</td>
<td>128</td>
</tr>
<tr>
<td>4.2.6</td>
<td>Summary</td>
<td>128</td>
</tr>
<tr>
<td>4.3</td>
<td>Spray characteristics of the atomized superheated liquid jet</td>
<td>130</td>
</tr>
<tr>
<td>4.3.1</td>
<td>Droplet Size Characterizations</td>
<td>135</td>
</tr>
<tr>
<td>4.3.1.1</td>
<td>Evolution of mean drop size profiles in radial and axial directions</td>
<td>135</td>
</tr>
<tr>
<td>4.3.1.2</td>
<td>Effect of the temperature on the drop size</td>
<td>137</td>
</tr>
<tr>
<td>4.3.1.3</td>
<td>Effect of the drive pressure on the mean drop size profiles</td>
<td>140</td>
</tr>
<tr>
<td>4.3.1.4</td>
<td>Orifice diameter effect on mean profiles</td>
<td>141</td>
</tr>
<tr>
<td>4.3.1.5</td>
<td>Influence of the orifice length-to-diameter ($l/D$) ratio on drop mean profiles</td>
<td>143</td>
</tr>
<tr>
<td>4.3.1.6</td>
<td>Summary</td>
<td>146</td>
</tr>
<tr>
<td>4.3.2</td>
<td>Droplet count and mass distributions</td>
<td>147</td>
</tr>
<tr>
<td>4.3.2.1</td>
<td>Effect of the spatial location on count and volume (mass) distributions</td>
<td>147</td>
</tr>
<tr>
<td>Section</td>
<td>Page</td>
<td></td>
</tr>
<tr>
<td>------------------------------------------------------------------------</td>
<td>------</td>
<td></td>
</tr>
<tr>
<td>4.3.2.2 Effect of the temperature on drop size count and volume</td>
<td>150</td>
<td></td>
</tr>
<tr>
<td>(mass) distributions</td>
<td></td>
<td></td>
</tr>
<tr>
<td>4.3.2.3 Pressure effect on drop size count and volume (mass) distributions</td>
<td>152</td>
<td></td>
</tr>
<tr>
<td>4.3.2.4 Effect of orifice size on the drop size count and mass distributions</td>
<td>153</td>
<td></td>
</tr>
<tr>
<td>4.3.2.5 Orifice length/diameter ratio on drop size count and volume distributions</td>
<td>154</td>
<td></td>
</tr>
<tr>
<td>4.3.2.6 Summary</td>
<td>155</td>
<td></td>
</tr>
<tr>
<td>4.3.3 Application of various empirical distribution functions</td>
<td>156</td>
<td></td>
</tr>
<tr>
<td>4.3.4 Velocity Characterizations of a Two-phase Flashing R134A Jet</td>
<td>160</td>
<td></td>
</tr>
<tr>
<td>4.3.4.1 Evolution of the velocity in radial and axial distance</td>
<td>161</td>
<td></td>
</tr>
<tr>
<td>4.3.4.2 Effect of the initial liquid temperature on velocity</td>
<td>161</td>
<td></td>
</tr>
<tr>
<td>4.3.4.3 Effect of the drive pressure on velocity</td>
<td>163</td>
<td></td>
</tr>
<tr>
<td>4.3.4.4 Effect of the orifice diameter on velocity</td>
<td>165</td>
<td></td>
</tr>
<tr>
<td>4.3.4.5 Effect of the orifice length/diameter ratio on velocity</td>
<td>170</td>
<td></td>
</tr>
<tr>
<td>4.3.4.6 Summary</td>
<td>170</td>
<td></td>
</tr>
<tr>
<td>4.4 Temperature measurements in a two-phase flashing jet</td>
<td>173</td>
<td></td>
</tr>
<tr>
<td>4.4.1 Effect of the thermocouple type and repeatability</td>
<td>174</td>
<td></td>
</tr>
<tr>
<td>4.4.2 Temperature evolution measured by thermocouples</td>
<td>175</td>
<td></td>
</tr>
<tr>
<td>4.4.3 Effect of the nozzle diameter on the temperature evolution of the flashing</td>
<td>176</td>
<td></td>
</tr>
<tr>
<td>4.4.3.1 Visual description of the flow</td>
<td>176</td>
<td></td>
</tr>
<tr>
<td>4.4.4 Infrared camera results</td>
<td>179</td>
<td></td>
</tr>
<tr>
<td>4.4.5 Radial temperature profiles</td>
<td>180</td>
<td></td>
</tr>
<tr>
<td>4.4.5.1 Downstream evolution</td>
<td>180</td>
<td></td>
</tr>
<tr>
<td>4.4.5.2 Effect of the superheat</td>
<td>181</td>
<td></td>
</tr>
<tr>
<td>4.4.5.3 Effect of the initial pressure</td>
<td>182</td>
<td></td>
</tr>
<tr>
<td>4.4.5.4 Effect of the nozzle diameter</td>
<td>184</td>
<td></td>
</tr>
<tr>
<td>4.4.5.5 Effect of the nozzle length-to-diameter ratio</td>
<td>185</td>
<td></td>
</tr>
<tr>
<td>4.5 Summary and Discussions</td>
<td>185</td>
<td></td>
</tr>
<tr>
<td>5 Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet</td>
<td>189</td>
<td></td>
</tr>
<tr>
<td>5.1 Introduction</td>
<td>189</td>
<td></td>
</tr>
<tr>
<td>5.2 Mathematical model</td>
<td>189</td>
<td></td>
</tr>
<tr>
<td>5.3 Evaporation model prediction and comparison</td>
<td>192</td>
<td></td>
</tr>
<tr>
<td>5.4 Model sensitivity analysis</td>
<td>195</td>
<td></td>
</tr>
<tr>
<td>5.5 Conclusions</td>
<td>199</td>
<td></td>
</tr>
<tr>
<td>6 Conclusions and perspectives</td>
<td>203</td>
<td></td>
</tr>
<tr>
<td>Bibliography</td>
<td>224</td>
<td></td>
</tr>
</tbody>
</table>
# List of Figures

2.1 Disintegration of a cylindrical jet of liquid caused by (a) axisymmetric waves; (b) asymmetric waves; (c) aerodynamic forces ([15]) ........................................ 7

2.2 Jet breakup regimes. .............................................................................................................. 9

2.3 Different types of pressure atomisers ([70]) ................................................................. 11

2.4 Contraction of a jet in a stationary environment ([15]) .................................................. 12

2.5 Scheme of a jet atomized in a stationary medium; $L_1$, initial segment; $L_2$, main segment ([15]) .......................................................... 17

2.6 Parametric description of the density distribution $q^* = f(r)$([15]) .................. 19

2.7 Effects of the log normal parameters on the count distribution pattern .... 20

2.8 Effect of Upper-limit parameters on the volumetric distribution. ...................... 22

2.9 Effects of the root-normal parameters on the volume distribution pattern .... 23

2.10 Effect of Rosin-Rammler parameters on the volumetric distributions. .... 24

2.11 Effect of modified Rosin-Rammler parameters on the volumetric distributions. .... 26

2.12 Effect of Nukiyama-Tanasawa parameters on the count distributions. 27

2.13 An example of discharge coefficients (displayed as $\mu$) taken from ([15]) ......... 31

2.14 Discharge coefficient evolution with the changes in nozzle geometry and flow conditions. ............................................................ 32

2.15 Types of equilibrium: A schematic behaviour of superheated system in term of pressure gradient ................................................................. 33

2.16 Thermodynamic steps of flashing mechanism. Adapted from Reinke[110] .......... 34

2.17 The thermodynamical steps of flashing in P-T curves ...................................... 35

2.18 A general view of the two-phase flashing jet ......................................................... 37

2.19 The Landau mechanism of surface instability (Prosperetti and Plesset [107]) .............................................................. 41

2.20 Dependence of spray pattern on two-phase flow regime before discharge with the increase of superheating (long transparent nozzle: (a) bubbly flow; (b) slug flow; (c) annular flow. [Park and Lee [101]]) .... 53

2.21 Spray pattern with bubbly flow inside the long and short transparent nozzle for low (a) and high (b) injection temperatures [Park and Lee [101]] .... 54

2.22 Dependence of spray flow pattern on the bubble distribution inside the nozzle: (a) short nozzle or low superheat; (b) long nozzle or high superheat [Park and Lee [101]]. .......... 55

2.23 Variation of shattering temperature with ambient pressure for different nozzle diameters, (Bushnell and Gooderum[24]) ........................................ 57

2.24 Spray angle variation with degree of superheat [Park and Lee [101]] .... 58
2.25 Velocity profiles obtained by Moodie and Ewan[86] ................................. 60
2.26 Radial velocity profile for propane spray from Allen[3]............................... 61
2.27 Line-of-sight drop size distributions (number percent)Reitz[112].................... 62
2.28 SMD variations of the flashing jet in different configuration (for long transparent nozzle). Park and Lee[101] ...................................................... 63
2.29 SMD variation with degree of superheat(single hole nozzle)(Park and Lee[101])........................................................................................................ 65
2.30 Variation of peak droplet sizes in weight distribution for Freon 11 jet spray field. Piper diameter is 4 mm. Triangle: 40mm length at 3 bar source pressure; Circle: 120mm at 3 bar source pressure; Square: 40mm length at 6 bar source pressure; Diamond: 120mm length at 6 bar source pressure.(Moodie and Ewan[86]) 66
2.31 Droplet size evolutions according to Allen[4]............................................... 68

3.1 The experimental facility .......................................................................... 77
3.2 A view of the experimental installation when the PDA, thermocouples and displacement system are installed ...................................................... 78
3.3 Design details of the nozzles .................................................................... 79
3.4 Details of the nozzle with 2 mm diameter and length-to-diameter $l/D$ ratio of 15..................................................................................................... 80
3.5 Calibration curve of the thermocouple used for liquid storage temperature 81
3.6 The synchronization mechanism ................................................................. 85
3.7 The positioning of the Phase Doppler Anemometer ...................................... 87
3.8 Schematic sketch displaying the PIV measurement locations for flashing jet (the rectangles represent three different camera positions in image recording for PIV: a) Region 1: at the nozzle, b) Region 2: the location corresponding to from x=0.05m(x/D=10) to x=0.07m(x/D=14), c) Region 3: the downstream camera position from x=0.07m (x/D=14) to x=0.09m(x/D=18)) 88
3.9 A view of PDPA system and thermocouples ............................................... 90
3.10 Alignment to find the reference '0' point ...................................................... 90
3.11 Laser beam used to align the nozzle with the displacement system ......... 91
3.12 Experimental facility with the rack of thermocouples.............................. 93
3.13 Alignment of the thermocouples with the laser beam ................................ 93
3.14 Determination of the distance of the first thermocouple from the PDA probe volume after the alignment of thermocouples with the laser beams 94
3.15 Photographic investigation of the accurate nozzle diameter determination 96
3.16 Measured discharge coefficients for the 1mm, 2mm and 4mm nozzles. Error bars are given at ±1 percent.Taken from Kubitschek[65] 98
3.17 Geometric dissimilarity between different nozzle sizes as shown by the changing relaxation angles at the nozzle exits. 99
3.18 Nozzle discharge curves.Taken from Kubitschek[65]. 100

4.1 Total volume cumulative distribution for different x/D on the axis ............. 102
4.2 Centreline droplet size and velocity evolution along the jet axis in comparison of $D_{32}$, $D_{50}$, $D_{20}$, $D_{10}$, and mass based mean droplet diameter computed from the cumulative mass distribution 103
4.3 The repeatability test for the velocity and droplet diameter distributions performed at x=18D downstream the nozzle ............................................. 104
4.4 An instantaneous PIV image displaying the sudden perpendicular velocities due to the break-up of superheated ligaments or big droplets .... 105
4.5 The PIV images at Region 1(a), at Region 2(b), at Region 3(c) .......... 106
4.6 The Signal-to-Noise-Ratio distributions at different measurement locations (averaged over 25 images): Region 1(a), at Region 2(b), at Region 3(c) ................................................................. 107
4.7 The velocity vector fields at different measurement locations (averaged over 25 images): Region 1(a), at Region 2(b), at Region 3(c) .... 107
4.8 Example of the histogram deduced from an average of 25 image pairs representing the particle displacement. 108
4.9 Comparison of the axial velocity along the flashing jet axis obtained by PIV and PDA techniques (PIV error bars represents the velocity error corresponding to 0.1 pixel of displacement error) 109
4.10 An original PIV image at the location x=14D-18D downstream the nozzle (Region 3) (Light intensity range: 66-4095) 110
4.11 The comparison of droplet velocity, size distributions measured with PDA at x=14D and x=18D. 111
4.12 Images processed with different light intensities (a-left) Int. lev.: 66-300, (b-middle) Int.lev.:300-900, (c-right) Int.lev.: 900-4095 112
4.13 SNR for the instantaneous plane shown in Fig.4.12 113
4.14 The velocity profile of the flashing jet for different intensity levels and Multi-intensity layer PIV processing 113
4.15 High-speed image sequences for “Experiment 1” for a jet issuing from a 1mm nozzle up to 90 X/D downstream distance, (T_liquid = 14°C, ΔT = 40.4°C, P_liquid = 820kPa) ....................................................... 116
4.16 High-speed image sequences for jets issuing from a 2 and 4mm nozzles 117
4.17 High-speed image sequences for jets issuing from a 1mm nozzles at two different superheat 119
4.18 High-speed image sequences for jets issuing from a 2mm nozzles for two different superheat 120
4.19 Randomly selected high speed images for 1,2,3mm nozzle diameters for two backpressure at low liquid temperature 122
4.20 Randomly selected high speed images for 1,2,3mm nozzle diameters for two backpressure at low liquid temperature 123
4.21 Randomly selected high speed images for 4mm nozzle diameter for two backpressure at low liquid temperature 124
4.22 Randomly selected high speed images for 2mm nozzle with different l/D at low liquid temperature and pressure 126
4.23 Randomly selected high speed images for 2mm nozzle with l/D = 0; 2; 7 for two backpressures (∼850kPa and ∼1250kPa) at low liquid temperature T_liquid ≈ 13°C 127
4.24 Randomly selected high speed images for 2mm nozzle with l/D = 0; 2; 7 for two backpressure (∼850kPa and ∼1250kPa) at high liquid temperature T_liquid ≈ 20°C 129
4.25 The thermodynamical conditions of the PDA test cases. 131
4.26 Schematic representation of the main spray measurements. 132
4.27 Superposition of horizontal-radial and vertical-radial diameter profiles for the test cases with similar initial liquid temperatures and back pressures ........................................... 133
4.28 Spray representation of the horizontal-radial and vertical-radial diameter profiles in one quarter of the spray cross-sectional area and the final-averaged mean profiles (symbols with line) ........................................ 134
4.29 The count percentage distributions of the separate points and the sum of all the data points for \( r/D = 0 \) on Fig. 4.27(a) ........................................... 135
4.30 The total counts of droplets per radial location for different “Test” cases presented in Table 3.4 ................................................................. 136
4.31 Mean droplet sizes profiles for 1mm nozzle at 3 axial locations. ........ 137
4.32 The evolution of global \( D_{10} \) and \( D_{32} \) values obtained from total cross-section with Jakob number \( Ja \). .......................................................................................... 138
4.33 Temperature effect on the \( D_{32} \) and \( D_{10} \) profiles at for two axial locations. 139
4.34 Pressure effect on the drop sizes for different \( Ja \). .......................... 141
4.35 Pressure effect on the profiles for different initial liquid temperatures. 142
4.36 Effect of nozzle diameter on the drop sizes at different \( Ja \) (Results are presented in non-dimensional and dimensional axial distances). 143
4.37 Effect of nozzle diameter on the profiles at different axial locations (Results are presented in non-dimensional and dimensional axial distances). 144
4.38 Effect of nozzle length/diameter ratio on the drop sizes based on total cross-section area ......................................................................................... 145
4.39 Effect of nozzle length/diameter ratio on the drop size profiles at different axial locations. .................................................. 146
4.40 Droplet count distributions at different radial location for different axial locations. .................................................................................................................. 149
4.41 Droplet count distributions at different radial location for different axial locations. .................................................................................................................. 151
4.42 Effect of initial liquid temperature on the global droplet diameter count and mass distributions for the total cross-section at \( x/D = 110 \) ................................................................. 152
4.43 Effect of initial liquid pressure on the global droplet diameter count and mass distributions for the total cross-section at \( x/D = 110 \). (Lines are for low pressure and symbols are for high pressure for different superheats). ......................................................................................... 153
4.44 Effect of orifice diameter on the global droplet diameter count and mass distributions for the total cross-section at \( x/D = 110 \) ................................................................. 154
4.45 Effect of orifice length to diameter ratio on the global droplet diameter count and mass distributions at \( x/D = 110 \). ......................................................................................... 155
4.46 Comparison of the empirical distribution functions for the selected cases. 158
4.47 Nondimensionalized velocity profiles for all the test cases. .......... 161
4.48 Axial and radial evolution of the mean velocity, RMS, turbulence intensity profiles and velocity count distributions for the flashing jet exiting 1mm nozzle. ................................................................. 162
4.49 Effect of initial liquid temperature on the droplet mean velocity, RMS, turbulence intensity radial profiles and velocity count distributions at the axial location of \( x/D = 110 \) ................................................................. 164
4.50 Effect of storage pressure on the mean droplet velocity radial profiles at the axial location of \( x/D = 110 \) ................................................................. 166
4.51 Effect of storage pressure on the mean droplet velocity radial profiles at the axial location of x/D=110. .............................................................. 167
4.52 Effect of the nozzle diameter on the mean droplet velocity, RMS and turbulence intensity profiles. ............................................................... 168
4.53 Effect of nozzle diameter on the velocity count distributions. ...................................................................................................................... 169
4.54 Effect of nozzle length-to-diameter ratio on the mean velocity, rms and turbulent intensity profiles. .......................................................... 171
4.55 Effect of nozzle length-to-diameter ratio on the velocity count distributions. ............................................................................................ 172
4.56 Left: Temperature signal in time for Chromel/Alumel wire of 0.2 mm diameter. Right: Steady temperature at different distances “x” from the nozzle exit. ................................................................................... 174
4.57 Comparison of temperature signals obtained by different thermocouple probes .......................................................................................... 175
4.58 Flow visualization of the first two thermocouples in R-134A jet and the axial temperature profiles ................................................................................. 176
4.59 R134-A jets under 700 KPa at 22°C (Nozzle diameters: 1(top), 2(middle) and 4 mm(bottom)) ........................................................................... 177
4.60 The first two thermocouples in R134-A jet under 700KPa at 22°C (Nozzle diameters: 1, 2 and 4mm from left to right). ........................................ 178
4.61 Temperature profiles for the jets issuing from 1 mm (left) and 4 mm (right) nozzles (P_{liquid} ~700kPa) ........................................................................ 178
4.62 Infrared thermograph (zoomed image) for the flashing two-phase jet under the pressurization of P = 700KPa for the orifice diameter of 1mm. ................................. 179
4.63 The temperature profiles of the sections LO1 compared with two thermocouple results on the centerline of the jet (1mm orifice diameter) .................................................. 180
4.64 Radial mean droplet temperature profiles for 1mm nozzle at 3 axial locations. .................................................................................................... 181
4.65 Temperature effect on the radial mean temperature profiles at for two axial locations. .................................................................................. 182
4.66 Pressure effect on the radial mean temperature profiles at for two different axial locations. .............................................................................. 183
4.67 Effect of nozzle diameter on the radial mean temperature profiles at different axial locations (Results are presented in non-dimensional and dimensional axial distances). .......................................................... 184
4.68 Effect of nozzle length/diameter ratio on the radial mean droplet temperature profiles at different axial locations. .................................................. 185

5.1 Comparison of temperature prediction with measurements of jets exiting a nozzle with a diameter of 1mm ................................................. 193
5.2 History of surface evolution for the representative droplet .......................................................................................................................... 194
5.3 History of the relaxation times associated to the three transfer processes and compared with Stokes ............................................................................. 194
5.4 History of saturation pressure with Mass fraction of R134-A at the surface of the droplet .................................................................................. 195
5.5 Evolution of the dimensionless numbers with distance from the nozzle exit .................................................................................................. 196
5.6 Temperature sensitivity to infinite distance mass fraction ......................................................................................................................... 197
5.7 Temperature sensitivity to initial diameter .................................................................................................................................................. 197
5.8  Temperature sensitivity to correction factor ..................................................198
5.9  Temperature sensitivity to mixing rule coefficient .......................................198
5.10 Temperature sensitivity to temperature dependency of the fluid properties on the evaporation estimation ............................................................199
5.11 Temperature sensitivity to gas velocity .........................................................200
5.12 Temperature sensitivity to correlation .........................................................200
5.13 Comparison of the evolutions of present Nusselt and Sherwood correlations with standard ones ..........................................................201
List of Tables

2.1 Mean diameters and their applications [70] ............................................. 15
2.2 The advantages and disadvantages of the maximum entropy (ME) method and discrete probability function (DPF) according to [8] ...................... 29
2.3 Calculated and measured limits of superheat $\Delta T_{\text{lim}}$ at 1 bar (Calculation and data found in (1): Blander and Katz[18]; (2) Thormaehlen[136]) .... 40
2.4 Drop size equations for plain-orifice atomizers taken from [70] .................. 71
2.5 Drop size estimations for flashing jets ...................................................... 73
3.1 Examples of refrigerant liquids .................................................................. 76
3.2 Test conditions for high-speed imaging ..................................................... 83
3.3 Settings of the “Stanford” and the “Agilent” Signal Generator .................. 85
3.4 Test conditions for droplet size-velocity measurements with Phase Doppler Anemometry (PDA) ................................................................. 92
4.1 The intensity ranges corresponding to different diameter ranges .......... 112
4.2 Velocities obtained from PDA measurements regarding to different diameter ranges at $x=14D\&18D$ far from the nozzle ......................... 114
4.3 The break-up lengths in $X/D$ for different initial conditions .................. 130
4.4 Total count and volume percentages for the first peak (1mm nozzle, $P \sim 800kPa$, $x/D=110$, total cross-sectional data) ......................... 157
4.5 Total count and corresponding volume percentages for the first peak of distributions at different radial distances ............................... 157
4.6 Distribution parameters for empirical functions at different radial distances 159
4.7 Effect of the initial flow conditions on the distribution parameters of the empirical functions ................................................................. 160
Chapter 1

Introduction and description of the problem

The present research is an experimental investigation of the atomization of a superheated pressurized liquid jet that is exposed to the ambient pressure due to a sudden depressurization. This phenomena is called flashing and occurs in several industrial environments.

Liquid flashing phenomena holds an interest in many areas of science and engineering. Typical examples one can mention: a) the accidental release of flammable and toxic pressure-liquefied gases in chemical and nuclear industry; the failure of a vessel or pipe in the form of a small hole results in the formation of a two-phase jet containing a mixture of liquid droplets and vapor, b) atomisation improvement in the fuel injector technology, c) flashing mechanism occurrence in expansion devices of refrigerator cycles etc... The interest in flashing events is especially true in the safety field where any unexpected event is undesirable.

When both violent boiling and aerodynamic liquid fragmentation control the two-phase behaviour, the flow experiences the "flashing phenomenon". This violent evaporation occurs when a liquid finds itself suddenly in a thermodynamic non-equilibrium. The initial stage of this phenomenon, where the system is furthest from thermodynamic equilibrium is not well understood yet. To investigate theoretically such a phenomenon, models need to be validated with accurate and reliable data of size, velocity and temperature of droplet phase as well as vapor formation.

The Commission of European Communities has launched a series of projects under the Fifth Framework programme on the understanding of the source processes with emphasis on flashing release of flammable pressurized liquids. The present thesis study has been fulfilled as a part in that framework in characterizing the two-phase jet after an accidental release and to quantify the effects of initial conditions such as initial
storage pressure, liquid temperature, geometrical effects of the release points on the cloud characteristics. It is expected that the results of the FLIE (Flashing Liquids in Industrial Environments) project will lead to a better understanding of the governing phenomena and thus improve the safety of existing and future industrial plants.

The present work discusses the influence of the initial parameters of the liquid jet on the resulting two-phase jet characteristics downstream the orifice exit in case of a sudden release of pressurized liquefied R-134A, which is the liquid simulating the flashing of pressurized liquefied propane and/or butane.

1.1 Objectives and methodology of the present work

In case of an accident, flammable or toxic gas clouds are anticipated in close regions of the release because of the sudden phase change. Due to the non-equilibrium nature of the flow in these near field regions, conducting accurate data measurements for droplet size and velocity is a challenging task resulting in scarce data in the very close area.

The unresolved issues are the accurate estimation of the flashing event location during the accidental release, the knowledge regarding the liquid fraction that undergoes phase transition or is transported as droplets within the jet, or the liquid fraction that form a liquid pool as rain out. Moreover, mechanisms leading to the breakup of the liquid jet of the liquid ligaments/droplets, accurate characterization of the droplet size and velocity distribution in the jets, and the rate of the phase change of the liquid droplets are also major issues. In the present thesis, a contribution aiming to improve the knowledge of these last points is proposed.

This research has been carried out at the von Karman Institute (VKI) within the 5th framework of European Commission to fulfill the goal of understanding of source processes in flashing liquids in accidental releases. The program is carried out under name of FLIE (Flashing Liquids in Industrial Environments) (Contract no: EVG1-CT-2000-00025) with the collaboration of international industrial partners. The overall goals are to reproduce the flashing release conditions as in realistic industrial environments and to derive model to improve the capabilities for hazard assessments. The development of the models will be achieved by the design and execution of a careful experimental program in laboratory and full-scale tests, by the formulation of a physical model describing flashing, subsequent aerosol dispersion and evaporation. Finally, the resulting models are expected to provide input to CFD simulations.

The specific objectives that are presented in this thesis study are the following:

- to perform an extensive literature survey on the existing studies carried out on flashing phenomena and/or related subjects;
- to perform carefully designed laboratory-scale experiments for the knowledge of flashing break-up patterns, and provide accurate data on the cloud (atomised jet)
1.2 Chapters overview

A comprehensive state of the art of jet break-up patterns, spray characteristics and studies of flashing liquids is presented in Chapter 2.

Chapter 3 presents the experimental facility that is used to study the flashing atomization. The basis of the measurement techniques and experimental campaign are given in detail.

Chapter 4 describes the experimental investigation to characterize the atomization of the superheated liquid jet. To fulfill this goal, laser-based optical techniques like Particle Image Velocimetry (PIV), Phase Doppler Anemometry (PDA) are used to obtain information for particle diameter and velocity evolution in this harsh environment. Moreover, a high-speed video photography presents the possibility to understand the break-up pattern changes of the simulating liquid namely R-134A jet in function of driving pressure, superheat and discharge nozzle characteristics. Global temperature measurements with an intrusive technique such as thermocouples, non-intrusive measurements with Infrared Thermography were performed. Cases for different initial pressures, temperatures and orifice diameters are studied and the break-up pattern, droplet size, velocity distributions and temperature evolutions along the radial and axial directions are presented in function of initial parameters.

A 1-D analytical rapid evaporation model is developed and explained in Chapter 5 in order to explain the strong temperature decrease during the measurements. A sensitivity analysis of this model is provided.

Finally Chapter 6 gives the general conclusions and proposes future work.
Chapter 2

Past studies in the literature and the proposed theories

The problem of flashing involves a wide range of topics such as nozzle hydraulics, thermal non-equilibrium, bubble growth, jet breakup, atomisation characterization, rapid evaporation. To understand an event with such complexity a good understanding of the disintegration of non-superheated liquid jets is necessary. Therefore, before providing literature relevant to flashing jet atomisation, an introductory review of the available relevant studies of stability, breakup, and atomisation of non-superheated liquid jets discharged into air and peripheral topics including nozzle hydraulics and liquid spraying are provided. This is followed by a relatively detailed summary of the existing research performed on flashing phenomena. In order to reach a better understanding of flashing atomisation, effort has been spent to gather information related to various aspects of this phenomenon that are observed but are not the primary goal of this study (i.e. bubble growth in superheated liquids and blow-downs of superheated vessels).

2.1 Studies on the mechanical cylindrical jet breakup

This part consists of the literature investigations involving the studies of breakup pattern and stability associated with high velocity liquid jets discharged into a surrounding gaseous medium (air) that is initially at rest. The aim of giving relevant information for non-superheated cylindrical liquid jets serves the purpose of improving the current understanding of similarities and differences of the breakup mechanisms associated with superheated and non-superheated liquid jets.

The atomisation process is defined by Bayvel and Orzechowski ([15]) as the one in
which bulk liquid is disintegrated into small drops by the acting forces. Lefebvre ([70]), considers atomisation as a disruption of the consolidating influence of surface tension by internal and external forces. In consequence, when mechanical energy is applied on a liquid (e.g. aerodynamic forces or vibration) and its magnitude exceeds that of the surface tension force, breakup occurs and the atomisation process is started. The mechanism of atomisation can be classified as follows:

**Axisymmetric waves:** Incidental internal perturbations cause narrow bands to develop in the jet. The liquid is forced from the narrow to wider bands, leading to the development of drops. In this case the drops develop because of surface tension only. The atomisation process takes place under typical fluid velocities of $1 \text{m/s}$. (Fig. 2.1).

**Asymmetric waves:** When the discharge velocity increases, the jet is subjected to aerodynamic forces that cause asymmetric waves type distortions. Since the air moving along the jet accelerates in the vicinity of the convexities and decelerates in the vicinity of concavities, a negative pressure develops in the convexities and positive pressure develops at the concavities; causing the asymmetric waves to increase their amplitude as they travel away from the nozzle. When the wave amplitude is large enough, tension forces promote the formation of droplets. (Fig. 2.1).

**Aerodynamic forces:** When a liquid jet is subjected to high aerodynamic forces it disintegrates into relatively small droplets. These forces depend mainly on the relative velocity between the jet and the gas at the nozzle discharge, but also on the density of the gas. If the velocity of injection is maintained high enough for aerodynamics forces being the cause of atomisation, higher values in the gas density will promote smaller droplet diameters. This atomisation mechanism is responsible for most of the applications of atomisers whose characteristics will be described briefly in the following sections.

According to Bayvel and Orzechowski ([15]) these forms of disintegration apply to velocities of the order 1, 10 and 100 m/s, respectively. The aerodynamics forces are also responsible for secondary atomisation.

### 2.1.1 Mechanical jet break-up regimes

The mechanical breakup of the liquid jets has received a considerable amount of attention in the scientific and industrial world. As a result of numerous studies, different breakup regimes are defined.

When the liquid is forced through an orifice in the form of a jet, the jet will break up or disintegrate because of jet instabilities Rayleigh, Kelvin-Helmholtz instability etc... In 1936, Ohnesorge proposed jet disintegration criteria, which since then have been commonly used. The criteria indicate three regimes of breakup depending on the value of Reynolds number $Re_L = \frac{\rho_U d_c}{\nu_L}$ and a dimensionless number $Z$ or Ohnesorge number (Oh) which has the expression $Oh = \frac{\mu}{\sqrt{\rho \sigma_d}}$. (reviewed in [70])
2.1.1. Mechanical jet break-up regimes

Figure 2.1: Disintegration of a cylindrical jet of liquid caused by (a) axisymmetric waves; (b) asymmetric waves; (c) aerodynamic forces ([15])
Chapter 2. Past studies in the literature and the proposed theories

In a more recent work, Reitz [111] revisited the Ohnesorge's work [94] and attempted to resolve some of the uncertainties of each Ohnesorge regime. Reitz suggested new regimes of jet disintegration: Rayleigh Regime or Drip Flow Regime, First Wind-Induced Regime, Second Wind-Induced Regime, and Fully Developed Atomization Regime. These regimes are represented in a Oh-Re plot in Fig.2.2(a) (taken from [111]) and sketched in Fig.2.2(b) (taken from [39]).

One regime that is not considered on this last figure is the “drip regime” (tap leakage). It involves slow formation of large drops immediately at the orifice exit, which then falls as a single stream. Flow rates are very small for the smaller orifices whereas orifices of the order of 10mm produce very large droplets in this regime. Lefebvre[70] quotes the size of the spherical drop resulting from this mechanism as $D = \frac{3}{4} \rho_L C_F L \sigma / \pi$.

This expression predicts that a 1mm sharp-edged orifice produces drops sizes of 3.6mm and 2.8mm for water and kerosene, respectively.

Rayleigh regime: Lord Rayleigh considered surface wave instabilities as a mechanism for fragmentation of a long viscous cylindrical liquid jet when the Weber number $We_L = \rho_L U_L d_o / \sigma_L$ is low, i.e. the surface tension forces dominate the aerodynamical ‘stripping’ forces (Fig.2.2(b)). He studied the liquid jet fragmentation by comparing the surface energy of a jet with and without external disturbances ([109]). He predicted that the liquid first comes out from the tube in the form of a cylinder and then breaks into cylinders of smaller lengths. Each short cylinder becomes a drop later on. He also proposed that all the broken drops have uniform size and the gaps between the drops are equal. Rayleigh break-up is dominated by surface tension effects; it occurs many jet diameters from the jet exit point and yields a stream of drops of sizes greater than the orifice diameter. Rayleigh derived the relationship $D = 1.89 d_o$ for this regime.

However, highspeed photographs showed that the jet had a dumb bell shape before breaking up into a drop ([70]). The gravitational force tries to pull the liquid down or to break the liquid cylinder into a drop, but the viscous and surface forces of the liquid prevent that from happening. Moreover, the drops formed coalesce with each other, so the jet consists of consecutive large and small drops. Eventually, each drop becomes spherical to minimize surface energy.

Once the velocity of the liquid jet increases, the friction force between the surface of the liquid cylinder and the air becomes important. In this case, the wavelength of the disturbance is longer and increases the instability of the cylinder. Wind-induced breakup occurs because of instabilities caused by the relative motion of gas and liquid, moderated to some extent by surface tension. Characteristics of this regime is a finite length along which insignificant atomisation occurs, followed by a diverging jet region containing droplets ranging from the size of the orifice diameter to approximately one order of magnitude smaller (Fig.2.2(b)). Two different subregimes are distinguished. The first wind induced regime, the liquid cylinder twists leading to an increase in its instability, break up occurs many jet diameters far away from the orifice and the droplets created in this condition are about the jet diameter. For the Second Wind-induced regime the friction force at the surface of the liquid jet is so great that breakup occurs at the surface several diameters downstream the nozzle exit yielding a wide range of drop sizes. Average drop diameters created in this last regime are much
2.1.1. Mechanical jet break-up regimes

(a) Classification of modes of disintegration[111]

(b) Change in the breakup regime and liquid core length with exit velocity [39]

Figure 2.2: Jet breakup regimes.
smaller than the jet diameter.

The fully developed atomisation regime is considered to be potentially the most hazardous situation. The exit velocity in this regime is faster than in the other regimes. Because of a very high friction force on its interface, the surface of the liquid jet breaks into very fine drops immediately at the nozzle exit, producing a conical jet (cone angle $5^\circ < \alpha < 15^\circ$) of finely atomized spray right at the orifice exit.

It should be emphasized that predictive criteria governing transition from one regime to another still have not been uniquely defined. Reitz([111]) and Faeth([39]) quote a correlation dependent on gas Weber number, and Faeth presents a transition graph indicating where he considers the transition areas lie. The curve governing transition between the atomisation and second-wind-induced regions is given explicitly by Lefebvre([?]) and transforms to give $Ca = 149.20\text{We}^{0.01}$.

### 2.2 Liquid Sprays

Liquid sprays are formed through mechanical break up. The disintegration patterns and resulting spray characteristics such as droplet size and velocity distributions can be adjusted using different atomiser designs. It is possible to find a large number of different types of atomisers: plane-orifice atomisers, simplex atomisers, pressure-swirl atomisers, dual-orifice atomisers, spill-return atomisers, sheet atomisers, etc. ([70], [15]) and the choice of which to use depends on the specific applications(Fig.2.3). However, the mechanism of atomisation are very similar in all of them.

Though it is on the edge of the concerns of the present study, a brief information will be given regarding the sprays that are formed by liquid sheets instead of liquid jets. For this type of sprays, the mechanism of atomisation are rather similar. Fraser and Eisenklam([42]), defined three regimes of sheet disintegration, which are “rim breakup, wave breakup and perforated sheet breakup”. Rim breakup is found where the viscosity and surface tension of the liquid are both high. In this mode, the free edge of a liquid sheet is contracted into a thick rim by forces created by the surface tension. That rim, or ligament, breaks up in a similar manner to a free jet as it continues to travel downstream. Wave breakup disintegration occurs through the generation of wave motion on the sheet by which some areas of the sheet break up behind the front edge. Finally, the perforated sheet breakup is generated by the appearance of holes in the sheet that grow rapidly until forming non-well shaped ligaments that disintegrate into drops of varying size.

#### 2.2.1 Spray structure

In general the spray angle and the range of the jet (i.e. spray penetration that is the length of the spray in axial direction) characterize its external shape, whereas the
2.2.1. Spray structure

internal structure makes reference mostly to the distribution of the liquid in the spray.

According to [15] a spray can be considered as consisting of three regions:

- **The core region:** The core region is a still recognizable liquid jet coming out of the nozzle. This region has not been perturbed enough to be disintegrated into small droplets as it is protected, by the liquid ahead, from the drag forces of the carrier gas.

- **The breakup region:** The breakup region is located at the edge of the core region, where the perturbations are large enough to disintegrate it.

- **The main spray region:** The main spray is further downstream from the breakup region, where the liquid is fully atomized. In this region the spray is constituted only by droplets flowing downstream in an approximately conical shape.

Some parameters that characterize the external shape of a spray are the **injection angle**, the **spray penetration** and the **degree of dispersion**. The **injection angle** is defined as the apex angle of the spray and is often measured by drawing two straight lines about the spray from the discharge orifice to some specified distance \(x\) from the nozzle where the jet narrows under the influence of the surrounding gas entrainment (Fig.2.4).
Chapter 2. Past studies in the literature and the proposed theories

Figure 2.4: Contraction of a jet in a stationary environment ([15])

The spray penetration is defined as the maximum axial length of the spray jet injected in stagnant gas. This length is governed by the relative magnitude of the kinetic energy of the liquid and the aerodynamic resistance of the carrier gas, i.e., if the liquid is injected in an environment of higher density, the penetration of the spray is reduced.

Lefebvre ([70]), considers the degree of dispersion as another external spray characteristic. He defines it as the ratio of the volume of the spray to the volume of the liquid contained within it. A spray is considered to have a good dispersion when the liquid mixes rapidly with the carrier gas and the subsequent rates of vaporization are high. However, this characteristic could also be considered as one of the internal structure of the spray if the scatter of the droplet size distribution is concerned.

Liquid atomisation is the process of the conversion of the bulk liquid into a multitude of individual fragments (drops). Some means of describing the drops and obtaining quantitative information is necessary to evaluate and compare sprays. There are two fundamental physical quantities associated with a given drop: its diameter and velocity. The atomisation process in a high-speed atomiser includes various mechanisms; hence, the resulting droplets present a wide range of diameters and velocities. The set of drops produced by a given spray can be divided into classes where each class consists of a drop whose diameter is within some range of given diameter $D$, i.e., each class consists of drops whose diameters are in the range $[D - \frac{\Delta D}{2}, D + \frac{\Delta D}{2}]$. By counting the number of drops in each class, it is possible to construct a histogram of the frequency occurrence of a given class. The continuous version of the histogram is the probability density function ($PDF$ presented as $f(D)$) of the drop size, or the drop size distribution function. It is also possible to construct volume or area distributions where the variables
2.2.1. Spray structure

are the volume and area, respectively. As for the droplet size distribution, the velocity distribution function can be constructed in a similar manner, as well. To effectively characterize the internal structure of a given spray, the distribution functions of these two physical droplet parameters (diameter and velocity) are sufficient. The basics of spray characterization and the available methods to do it will be discussed in this section, giving special attention to droplet size distributions. This discussion closely follows that of Babinsky and Sojka ([8]), and Bayvel ([14]), keeping in mind that their comparisons are focused on isothermal, non-evaporating sprays, where electrical charge is absent.

Bayvel and Orzechowski ([15]), assert atomisation quality as a general term that has two specific meanings: degree of atomisation and uniformity of atomisation. Degree of atomisation refers to the mean drop size (diameter), with a higher degree of atomisation denoting a smaller mean drop diameter. “Uniformity of atomisation”, defined by the drop size distribution, describes the scatter of the drop diameters, with higher uniformity of atomisation denoting a smaller scatter. A size distribution contains both parameters.

In general, the breakup of bulk fluid results in a spray where the sizes of drops are distributed between some non-zero minimum diameter and a finite maximum diameter. A finite maximum diameter exists because of aerodynamic forces acting on a drop tend to break up a large drop into smaller ones. A non-zero minimum diameter exists because the cohesive surface tension forces increase as the drop size approaches zero, and the available aerodynamic forces cannot overcome the surface tension force.

However, although this is physically impossible, most droplet size distributions assume that the droplet diameters range from zero to infinity. This is very convenient as no minimum and maximum diameters have to be known, and the effects in the accuracy of the distribution are too small to be accounted, i.e. in a properly constructed PDF, the total number of drops below some minimum diameter and above some maximum diameter should be very small (Eqn. 2.1). In this case, for the drop size distribution to be physically valid, it is necessary to accomplish the following conditions:

\[ \lim_{D \to 0} \int_0^D f(D) dD = 0 \]

\[ \lim_{D \to \infty} \int_D^{\infty} f(D) dD = 0 \]

Moreover, a PDF must be positive and normalized (Babinsky and Sojka ([8]), (Eqn. 2.2)

\[ f(D) \geq 0 \]

\[ \int_0^{\infty} f(D) dD = 1 \]
Chapter 2. Past studies in the literature and the proposed theories

In spray application, it is desirable to characterize a particular size distribution in terms of a representative drop diameters [8]. The polydisperse distribution is thus replaced with a monodisperse one, where the diameter of each drop is equal to the appropriate representative drop diameter. Mugele and Evans [89] have generalized the concept of mean diameter. A general expression to calculate mean diameters is given in Eqn.2.3.

\[
(D_{mn}) = \left[ \frac{\int_0^\infty D^m f(D) dD}{\int_0^\infty D^n f(D) dD} \right]^{1/(m-n)}
\]  

(2.3)

where \( m \) and \( n \) are integers. The sum of \( m + n \) is called the order of the mean diameter. The continuous Eqn.2.3 may also be written in a discrete form as following:

\[
D_{mn} = \left[ \frac{\sum N_i D_i^m}{\sum N_i D_i^n} \right]^{1/(m-n)}
\]  

(2.4)

where \( i \) denotes the size range considered, \( N_i \) is the number of drops in size range \( i \), and \( D_i \) is the middle diameter of size range \( i \).

Some of the commonly used diameters are the arithmetic mean diameter \( D_{10} \), the surface mean diameter \( D_{20} \), the volume mean diameter \( D_{30} \), the Sauter mean diameter \( D_{32} \), and the Bruckere mean diameter \( D_{43} \). Table 2.1 lists some of the mean diameters along with their field of applications.

For most engineering purposes the distribution of the drop size in a spray may be represented as a function of one of these representative diameters and the range of the drop sizes. Chin and Lefebvre ([28]) strongly recommended the use of the Sauter mean diameter (SMD) as the best representative diameter to indicate the fineness of the spray. Some researchers such as Walmsley and Yule [138] characterize their experimental data by using a volume mean diameter, \( D_{3.5} \), defined as in Eqn.2.5 where \( v(D) \) is the volumetric PDF.

\[
\int_0^{D_{3.5}} v(D) dD = 0.5
\]  

(2.5)

It is important to realize that a drop size distribution is a statistical entity and hence has a set of commonly accepted statistical moments. Four of the frequently used expressions that involve the statistical moments are the mean, the standard deviation, the coefficient of skewness, and the coefficient of kurtosis ([131]).

There are other possible choices of representative diameter, each of which could play a role in defining the distribution function. A list of some of the representative diameters are as follows [70]:
## Table 2.1: Mean diameters and their applications [70]

<table>
<thead>
<tr>
<th>m+n (order)</th>
<th>Symbol</th>
<th>Name of mean diameter</th>
<th>Expression</th>
<th>Application</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 0 1</td>
<td>$D_{10}$</td>
<td>Length</td>
<td>$\frac{\sum N_i D_i}{\sum N_i}$</td>
<td>Comparisons</td>
</tr>
<tr>
<td>2 0 2</td>
<td>$D_{20}$</td>
<td>Surface area</td>
<td>$\left(\frac{\sum N_i D_i^2}{\sum N_i d_i^2}\right)^{1/2}$</td>
<td>Surface area controlling</td>
</tr>
<tr>
<td>3 0 3</td>
<td>$D_{30}$</td>
<td>Volume</td>
<td>$\left(\frac{\sum N_i D_i^3}{\sum N_i d_i^3}\right)^{1/3}$</td>
<td>Volume controlling</td>
</tr>
<tr>
<td>2 1 3</td>
<td>$D_{21}$</td>
<td>Surface area-length</td>
<td>$\frac{\sum N_i D_i^2}{\sum N_i D_i}$</td>
<td>Absorption</td>
</tr>
<tr>
<td>3 1 4</td>
<td>$D_{31}$</td>
<td>Volume-length</td>
<td>$\left(\frac{\sum N_i D_i^3}{\sum N_i D_i^3}\right)^{1/2}$</td>
<td>Evaporation, molecular diffusion</td>
</tr>
<tr>
<td>3 2 5</td>
<td>$D_{32}$</td>
<td>Sauter (SMD)</td>
<td>$\frac{\sum N_i D_i^3}{\sum N_i D_i^3}$</td>
<td>Mass transfer, reaction</td>
</tr>
<tr>
<td>4 3 7</td>
<td>$D_{43}$</td>
<td>De Brouckere or Herdan</td>
<td>$\frac{\sum N_i D_i^4}{\sum N_i D_i^4}$</td>
<td>Combustion equilibrium</td>
</tr>
</tbody>
</table>

- $D_{0.1}$ = drop diameter such that 10% of total liquid volume is in drops of smaller diameter.
- $D_{0.5}$ = drop diameter such that 50% of total liquid volume is in drops of smaller diameter. This is called as mass median mean (MMD), as well.
- $D_{0.632}$ = drop diameter such that 63.2% of total liquid volume is in drops of smaller diameter. This is called the “Rosin-Rammler mean” diameter.
- $D_{0.9}$ = drop diameter such that 90% of total liquid volume is in drops of smaller diameter.
Chapter 2. Past studies in the literature and the proposed theories

- $D_{0.999}$ = drop diameter such that 99.9% of total liquid volume is in drops of smaller diameter.
- $D_{\text{peak}}$ = value of $D$ corresponding to the peak of drop size frequency distribution curve.

The mass mean diameter (MMD) can be approximated to the volume mean diameter in isothermal sprays. Simmons [126], [125], based on a large number of tests on several types of fuel nozzles, concluded that the ratio of MMD to SMD is always close to 1.2. Zhao et al. [153], [152] disagree with this conclusion, as their experimental data show that the ratio of MMD to SMD is not constant, but a function of the dispersion of the distribution. It is generally recognized that the ratio $\text{MMD}/\text{SMD}$ provides a good indication of drop size dispersion [79], however, a slight change in this ratio could correspond to large changes in dispersion.

The term dispersion is sometimes used as an alternative to distribution to express the range of drop sizes in a spray. The relationship between the diameter $D_{0.999}$ and $D_{0.5}$ is given in the following form (Eqn.2.6) being defined as dispersion boundary factor (DBF) [28]. This factor is a function of the diameter distribution parameter ($q$ is the dispersion factor in Rosin-Rammler distribution, which will explained in detail in the Section2.2.3).

$$DBF = \frac{D_{0.999} - D_{0.5}}{D_{0.5}}$$ (2.6)

$q = 2 \quad D_{0.999} = 3.16D_{0.5}$
$q = 3 \quad D_{0.999} = 2.15D_{0.5}$
$q = 4 \quad D_{0.999} = 1.77D_{0.5}$

Tate [133] defined a droplet uniformity index (DUI) to describe the spread of drop sizes in a spray (Eqn.2.7). DUI indicates the spread of the droplet sizes relative to $D_{0.5}$ by taking into account all the discrete size classes. The dependence of the DUI mainly on $q$ and to a lesser extent on $D_{0.5}$ can be shown [70].

$$\text{DUI (volumetric basis)} = \frac{\sum_i V_i (D_{0.5} - D_i)}{D_{0.5}}$$ (2.7)

where $D_i$ is the midpoint of size class $i$ and $V_i$ is the volume fraction in the size class.

A last parameter, the relative span factor $\Delta$ (Eqn.2.8), provides a direct indication of the range of drop sizes relative to the mass median diameter and as shown by Chin et al. [28] and Lefebvre [70], it is a unique function of $q$ (the Rosin-Rammler distribution function to be defined in Section2.2.3). It has been established that this factor correctly characterizes the combustion process in gas turbines.

$$\Delta = \frac{D_{0.9} - D_{0.1}}{D_{0.5}}$$ (2.8)
2.2.2 Liquid and velocity distributions in a drop jet

Simplified models of drop jet generated by jet atomizers are based on the assumption that such jets can be treated as a free turbulent jet([15]). Such models cannot be solved in a theoretical way without being supplemented by an experiment, which leads to very simple semi-empirical equations. A free turbulent jet and a jet of drops are similar in the following ways: a) The boundary of the jet is of a conical shape; b) the volume of the jet increases significantly because of gas ejection from the environment; c) thorough mixing of drops and gas ensures that the jet has varying density; d) all exchange processes (mass, momentum and heat) proceed due to intensive turbulence.

A scheme of drop jet is shown in Fig.2.5. Two segments can be distinguished in the jet: the initial segment $L_1$, which is characterized by a very high concentration of drops already developed or just developing, and the main segment $L_2$, characterized by the relatively low concentration of the drops moving along with the ejected gas. The length of the initial segment is ([15] referred in [15]) where $W_L = \rho_L w_0^2 d_o / \sigma$, $L_P$ is the Laplace number $\rho_L \sigma d_o / \rho_d^2$, and $M$ is the characteristic number $M = \rho_d / \rho_L$.

$$L_1 = 8.25 d_o W_L e^{0.25} L_P^{-0.4} M^{-0.3}$$ (2.9)

![Figure 2.5: Scheme of a jet atomized in a stationary medium; $L_1$, initial segment; $L_2$, main segment ([15])](image)

For the main segment $L_2$, the starting point is the principle of conservation of momentum of the elementary mass of the jet. The gravity forces and pressure forces are neglected; only the forces arising during the turbulent mass exchange between the jet and ambient gas are considered. Assuming the motion of the jet as stable, the differential equation of motion is obtained. Assuming constant jet density and making transformations, Eqn.2.10 is obtained for the velocity distribution in the jet([15]). The generalized equation of velocity can be expressed in the form of Eqn.2.11.

$$w = \frac{d_o w_0}{2 \sqrt{2 \pi}} \exp \left( - \frac{r^2}{4(a_w x)^2} \right)$$ (2.10)
\[
\begin{align*}
\frac{w}{w_{\text{max}}} &= \frac{d_o w_o}{2 \sqrt{2a_u x}} \\
&\quad \exp \left[ -0.693 \left( \frac{r}{\bar{r}} \right)^2 \right]
\end{align*}
\]  

(2.11)

where \( d_o \) is the diameter of the outlet orifice of the atomizer, \( w_o \) is the initial velocity of the drop jet, \( a_u \) is the (experimentally obtained) free turbulence coefficient and \( \bar{r} = \frac{w_{\text{max}}}{2} \). Eqn. 2.11 has been validated experimentally.

The radial liquid density distribution is by nature non-uniform for all types of atomizers. The characteristic liquid density distributions \( q^* = f(r) \) (where \( q^* = \frac{\Delta Q}{\Delta A} \) (volumetric flow rate)/ (surface perpendicular to atomizer axis)) are displayed for jet atomizers, swirl atomizers, and swirl-jet atomizers (Fig. 2.6). For the sprays generated by jet atomizers, large drops are located in the core of the spray. According to Yule et al. [149], measurements in the core of these jets are difficult, and they indicate that the process of the disintegration is not complete. Contrary to jet atomisers, the density distribution is highly non-uniform, with small \( q^* \)'s in the spray axis and high density at its periphery for a swirl atomiser (at an intermediate cross-section \( II - II \) as shown in Fig. 2.6(c)). In the far field, maximum \( q^* \) occurs on the axis for this atomiser.

### 2.2.3 Droplet size distribution functions

The primary drop size distribution is needed as an input to models that predict the effect of secondary atomisation, but unfortunately, the literature that focuses on modeling these distributions is fairly limited. Babinsky and Sojka ([8]), present a good review of the different methods to produce such distributions. They mention three ways to produce a PDF; the empirical method, the maximum entropy method and the discrete probability function method.

**The empirical method**

The most common method to model drop size distribution is empirical. The procedure is to obtain a wide set of experimental data from different nozzles and fit it into a curve. Some of the most commonly used empirical distributions are the log-normal distribution, the upper-limit distribution, the root-normal distribution, the Rosin-Rammler distribution, the Nukiyama-Tanasawa distribution, the log-hyperbolic distribution and the three-parameter log-hyperbolic distribution ([100]).

From this point forward, each symbol that represents a distribution function will have a subscript that indicates the type of distribution. The subscript 0 represents the number distribution and the subscript 3 is used for volume. A lower case symbol \( f \) will be used for class-wise distribution whereas the upper case \( F \) is used for cumulative distributions.

18
2.2.3. Droplet size distribution functions

(a) (a) jet atomizers; (b) swirl atomizers; (c) jet-swirl atomizers

(b) jet atomizer (1, 2, 3 are in increasing distance $x$

(c) swirl atomizer (I-I, II-II, III-III are in increasing distance $x$

Figure 2.6: Parametric description of the density distribution $q^* = f(r)$([15])
The log-normal distribution is given as following:

\[
\begin{align*}
  f_0(D) &= \frac{1}{\sqrt{2\pi D \ln \sigma_{LN}}} \exp\left\{-\frac{1}{2} \left[ \frac{\ln(D/\bar{D}_{LN})}{\ln \sigma_{LN}} \right]^2 \right\} \\
  F_0(D) &= \frac{1}{\sqrt{2\pi \ln \sigma_{LN}}} \int_{-\infty}^{\ln D} \exp\left\{-\frac{1}{2} \left[ \frac{\ln(D/\bar{D}_{LN})}{\ln \sigma_{LN}} \right]^2 \right\} d(\ln D)
\end{align*}
\]

(2.12)

\[
\begin{align*}
  \bar{D}_{LN} &= \exp \left[ \sum_0^\infty \frac{(dn/dD)}{\sum_0^\infty (dn/dD)} \ln D \right] \\
  \sigma_{LN} &= \exp \left[ \sum_0^\infty \frac{(dn/dD)}{\sum_0^\infty (dn/dD)} \left( \ln D - \ln \bar{D}_{LN} \right)^2 \right]
\end{align*}
\]

where \( \bar{D}_{LN} \) is the logarithmic mean size (or geometric mean size) of the distribution and \( \sigma_{LN} \) is the width of the distribution. Here, \( \ln \sigma_{LN} \) can be called as geometric standard deviation, as well. Fig.2.7 shows the typical shape of the distribution and presents the influence of the distribution parameters as \( \bar{D}_{LN} \) and \( \sigma_{LN} \).

Figure 2.7: Effects of the log-normal parameters on the count distribution pattern

The upper limit distribution is a modification of the log-normal distribution in that a maximum drop size is introduced.

\[
\begin{align*}
  f_3(D) &= \frac{D_{\text{max}}}{\sqrt{2\pi D \ln \sigma_{UL} (D_{\text{max}} - D)}} \exp\left\{-\frac{1}{2(\ln \sigma_{UL})^2} \left[ \ln \left( \frac{D_{\text{max}} D}{D_{\text{UL}} (D_{\text{max}} - D)} \right) \right]^2 \right\}
\end{align*}
\]
2.2.3. Droplet size distribution functions

\[ F_3(D) = \frac{1}{\sqrt{2\pi} \ln \sigma_{UL}} \int_{-\infty}^{\ln D} \exp \left\{ -\frac{1}{2(\ln \sigma_{UL})^2} \ln \left( \frac{D_{\text{max}} D}{D_{UL}(D_{\text{max}} - D)} \right)^2 \right\} d(\ln D) \] (2.13)

\[ \sigma_{UL} = \exp \left[ \sum_{0}^{\infty} \frac{(dn/dD)D^3}{\sum_{0}^{\infty} (dn/dD)D^3} \left( \ln D - \ln D_{UL} \right)^2 \right] \]

\[ D_{UL} = \exp \left[ \sum_{0}^{\infty} \frac{(dn/dD)D^3}{\sum_{0}^{\infty} (dn/dD)D^3} \ln D \right] \]

Here, \( \sigma_{UL} \) represents the distribution width, \( D_{\text{max}} \) is the maximum drop diameter and \( D_{UL} \) is a representative diameter. Fig.2.8(a),2.8(b) shows the typical distribution and the effect of the distribution parameters \( D, D_{\text{max}}, \sigma_{UL} \). The upper-limit function was first introduced by Mugele and Evans [89], who wanted to modify the log-normal distribution by specifying a maximum drop diameter. It is clearly seen (Fig.2.8(b)) that the effect of \( D_{\text{max}} \) diminishes with increasing values and upper-limit distribution approaches the log-normal distribution as the maximum diameter tends to infinity.

The root-normal distribution was proposed by Tate and Marshall ([134]) to express the volume distribution of drops in sprays.

\[ f_3(D) = \frac{1}{2\sqrt{2\pi}D\sigma_{RN}} \exp \left\{ -\frac{1}{2} \left[ \frac{\sqrt{D} - \sqrt{D_{RN}}}{\sigma_{RN}} \right]^2 \right\} \]

\[ F_3(D) = \frac{1}{\sqrt{2\pi}\sigma_{RN}} \int_{-\infty}^{\sqrt{D}} \exp \left\{ -\frac{1}{2} \left[ \frac{\sqrt{D} - \sqrt{D_{RN}}}{\sigma_{RN}} \right]^2 \right\} d(\ln D) \]

\[ F_3(D_{RN}) = 0.5 \] (2.14)

\[ D_{RN} = \left[ \sum_{0}^{\infty} \frac{(dn/dD)D^3}{\sum_{0}^{\infty} (dn/dD)D^3} \sqrt{D} \right]^2 \]

\[ \sigma_{RN} = \sqrt{\left[ \sum_{0}^{\infty} \frac{(dn/dD)D^3}{\sum_{0}^{\infty} (dn/dD)D^3} \left( \sqrt{D} - \sqrt{D_{RN}} \right)^2 \right]} \]

where \( \sigma_{RN} > 0 \). \( \sigma_{RN} \) represents the width of the distribution and \( D_{RN} \) is the representative mean diameter corresponding to \( F_3(D_{RN}) = 0.5 \). Fig.2.9 shows typical distributions and the effect of variation of distribution parameters \( D_{RN} \) and \( \sigma_{RN} \).

The Rosin-Rammler distribution was introduced by Rosin and Rammler ([116]), to describe the cumulative volume distribution of coal particles. Nevertheless, this distribution is widely used in spray studies, mainly because of its mathematical simplicity. The Rosin-Rammler number distribution is defined in Eqn.2.15. Very often, the cu-
Figure 2.8: Effect of Upper-limit parameters on the volumetric distribution.
2.2.3. Droplet size distribution functions

Figure 2.9: Effects of the root-normal parameters on the volume distribution pattern

The cumulative Rosin-Rammler volume distribution (Eqn.2.15) is found in the literature to characterize sprays.

\[
F_3(D) = 1 - \exp\left(-\left(D/\overline{D_{RR}}\right)^q\right)
\]

where \( \overline{D_{RR}} \) represents the mean of the distribution and \( q \), known as the spread parameter, indicates the value of the width of the distribution. This means that if the spread parameter is small, then the distribution will be more dispersed than one with a large value. Fig.2.10 shows the typical distributions and the effect of variation of distribution parameters \( \overline{D_{RR}} \) and \( q \).

From analysis of a considerable set of drop size data obtained with pressure swirl nozzles, Rizk and Lefebvre [114] found that although the Rosin-Rammler expression provides an adequate data fit over most of the drop size range, there is occasionally a significant deviation from the experimental data for the larger drop sizes and the extremities of the distributions. By rewriting the Rosin-Rammler equation in the form as presented in Eqn.2.16, they found out that this formulation gives a better fit than ordinary Rosin-Rammler distribution [113], however, they were aware that many
Chapter 2. Past studies in the literature and the proposed theories

Figure 2.10: Effect of Rosin-Rammler parameters on the volumetric distributions.

(a) cumulative distribution

(b) volumetric distribution per class
more comparative assessments must be performed before the modified version could be claimed superior. As can be seen from Fig.2.11, the percentages on the central peak classes are augmented through the modification compared to Fig.2.10.

\[ f_3(D) = q \ln \frac{D}{D_{RR}}^\xi \ln D^{\eta-1} \exp(-\ln D/ \ln D_{RR})^\xi \]

\[ F_3(D) = 1 - \exp(-\ln D/ \ln D_{RR})^\eta \]  \hspace{1cm} (2.16)

\[ F_3(D_{RR}) = 0.632 \]

The **Nukiyama-Tanasawa distribution** was introduced by Nukiyama and Tanasawa ([93]) to describe the droplet number distribution from a pneumatic atomiser. The Nukiyama-Tanasawa number size distribution is defined as,

\[ f_3(D) = a \bar{D}^p \exp\{b \bar{D}^{q_{NT}}\} \]  \hspace{1cm} (2.17)

where \( b, p \) and \( q_{NT} \) are adjustable parameters and \( a \) is a normalizing constant. Sometimes \( p \) is fixed to 2. The width of the distribution and the location of the mean is controlled by \( b, p \) and \( q_{NT} \). Fig.2.12 shows typical distributions and the effect of variation of distribution parameters. According to Paloposki([100]), the ranges of either \( p > 1 \) and \( q_{NT} > 0 \) or \( p < -4 \) and \( q_{NT} < 0 \) leads to physical results.

Paloposki([100]) analyzed all the distributions mentioned above using a \( \chi^2 \) test. He used data from 22 sets provided by seven experimental studies; some of these data sets are widely used as benchmarks. Both number and volume distributions were considered. The distributions that gave the best fit to the experiments that he was testing were the Nukiyama-Tanasawa along with log-hyperbolic distributions. He found out that the log-normal and upper-limit distributions were reasonably accurate but inferior to Nukiyama-Tanasawa and the log-hyperbolic distributions. The Rosin-Rammler and three parameter log-hyperbolic performed poorly. It is observed that better fit was obtained when the number of adjustable parameters are greater. The higher number of parameters provide a greater degree of shape freedom, however, according to Paloposki's performance on the mathematical stability of parameters of distribution, this characteristic also brings its own problems, as the number of parameters makes the distribution highly unstable and reaching convergence proves to be a difficult task. In this case, Nukiyama-Tanasawa and log-hyperbolic distributions had problems with stability whereas the log-normal distribution was more stable.

Though the empirical methods are widely applied, a big disadvantage of the empirical methods is the difficulty of extrapolating the data to operating regimes outside the experimental range.

**Maximum entropy method**

This is a theoretical approach to predict a droplet size distribution. The maximum
Chapter 2. Past studies in the literature and the proposed theories

Figure 2.11: Effect of modified Rosin-Rammler parameters on the volumetric distributions.
2.2.3. Droplet size distribution functions

(a) effects of parameters $a$ and $q_T$

(b) effect of parameter $b$

Figure 2.12: Effect of Nukiyama-Tanasawa parameters on the count distributions.
entropy method treats the atomisation process as a black box that transforms the liquid into a system of drops with a particular size distribution (Babinsky and Sojka[8]). The method does not model the details of such transformation. Depending on the atomisation conditions the spray size distribution will take one form or another, the possibilities are infinite. The maximum entropy states that the most likely form of droplet size distribution is the one that maximizes the total entropy of the system subject to physical constraints of the atomisation process. The first attempt to develop a particle size distribution based on this concept was done by Griffith[49], to predict the particle size of triturated materials. The application of this formalism in sprays is rather recent; Sellens and Brzustowski[121] appear to be the first to use this method in sprays. Cousin et al.[29], Boyaval and Doumouchel[19] and Malot and Doumouchel[78], conducted recent studies about producing droplet size distributions using the maximum entropy method.

There is a major problem in using this method. According to Babinsky and Sojka[8], the constraints of the problem should be formulated in terms of some representative diameters of the resulting distribution. It appears that at least two representative diameters are required as inputs to the maximum entropy method to produce a realistic number distribution. Nevertheless, it is only possible to predict one representative diameter by the use of the stability analysis. This seriously reduces the utility of the maximum entropy method.

The discrete probability function method

This method, contrary to the maximum entropy method, divides the spray formation process into deterministic and non-deterministic events. The first one describes the breakup of the liquid bulk, and the second one describes the influence of the initial condition's fluctuation on the resulting droplet size distribution ([8]). A fluid mechanic instability analysis is used to describe the breakup process (i.e. from fluid structure into ligaments which eventually turn into drops). This analysis is deterministic, which means that for a given set of initial conditions a particular size of droplets will be predicted (i.e. this part of the method is only capable of predicting monodisperse spray). To produce a polydisperse spray, fluctuations in the initial conditions have to be introduced. These fluctuations can be due to a series of different physical factors: fluctuations in the mass flow rate, turbulence effects on the flow, vibrations in the nozzle, cavitation, etc. Hence, to produce a droplet size distribution with this method, it is necessary to know the probability density functions of the fluctuating parameter(s). This is later coupled with the initial conditions to predict a polydisperse drop size distribution. Sovani et al.[130] was the first to use this method to produce drop size distributions.

The comparison of the maximum entropy method and discrete probability function according to [8] is given in the following Table 2.2.
### Table 2.2: The advantages and disadvantages of the maximum entropy (ME) method and discrete probability function (DPF) according to [8].

<table>
<thead>
<tr>
<th></th>
<th>ME</th>
<th>DPF</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Inputs needed</strong></td>
<td>At least two representative drop diameters</td>
<td>Probability density function of the fluctuating parameter</td>
</tr>
<tr>
<td>(in addition to fluid physical properties and atomizer parameters)</td>
<td>One representative diameter using an instability analysis</td>
<td>No experimental PDF's exist</td>
</tr>
<tr>
<td><strong>Inputs that can be currently computed</strong></td>
<td>Unclear</td>
<td>Advances in CFD can (in principle) provide the required PDF</td>
</tr>
<tr>
<td></td>
<td></td>
<td>No. No experimental PDFs exist. Comparison with experiments is meaningless without a correct input PDF.</td>
</tr>
<tr>
<td><strong>Can the lack of necessary inputs be rectified?</strong></td>
<td>No. Agreement achieved only after an adjustment of source terms or obtaining the representative diameter by direct measurement of the experimental distribution.</td>
<td>Simple if CFD is not involved and 1D fluid breakup model is used. Requires the minimization of a one dimensional function. The complexity of the overall method is directly proportional to the complexity of the CFD code and the complexity of fluid breakup model (which will increase if 2D breakup models are necessary).</td>
</tr>
<tr>
<td></td>
<td>Reasonable. Requires the minimization of a multidimensional potential function. The number of dimensions is proportional to the number of constraints.</td>
<td>Yes.</td>
</tr>
<tr>
<td><strong>Produced a priori predictions that agree with experiments?</strong></td>
<td>Unclear</td>
<td>Unknown. Whether it would be possible to formulate the appropriate constraint.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Unclear</td>
</tr>
<tr>
<td><strong>Computational algorithm complexity</strong></td>
<td>Unavailable for the ME method. Requires the minimization of a multidimensional potential function. The number of dimensions is proportional to the number of constraints.</td>
<td>Unavailable for the DPF method. Requires the minimization of a multidimensional potential function. The number of dimensions is proportional to the number of constraints.</td>
</tr>
<tr>
<td><strong>Can be applied to fluids with complex rheology or different atomization modes?</strong></td>
<td>Unknown. Whether it would be possible to formulate the appropriate constraint.</td>
<td>Yes.</td>
</tr>
</tbody>
</table>
2.3 Nozzle Hydraulics

The behavior of both flashing and non-flashing liquid jets is strongly dependent on nozzle geometry and associated flow conditions. A generalized approach has yet to be included in the literature owing to the large range of nozzle configurations that are possible. In any case, it is important to accurately describe and characterize nozzle geometry for any experimental investigation since it may be necessary to classify jet behavior based on nozzle type.

The behavior of jets issuing from sharp-edged orifices is well known and has been studied extensively. The most important feature of sharp-edged orifice behavior is the discharge coefficient that provides a measure of jet contraction following exit from the nozzle. The contraction effect has important implications for jet geometry and perhaps turbulence characteristics.

Lienhard (IV) and Lienhard (V) \cite{73} conducted a detailed study of velocity coefficients for jets produced by sharp edged orifices. The authors provide theoretical and experimental investigations for assessing and predicting the influence of viscosity for large orifices and the influence of surface tension for small orifices. The authors point out that for ideal flow the contraction coefficient for sharp-edged orifices has been well established as \( C_e = \pi / (\pi + 2) = 0.611 \). Some of the best measurements were conducted by Judd and King \cite{61} who simultaneously measured discharge coefficient \( (C) \), contraction coefficient \( (C_e) \), and velocity coefficient \( (C_v) \) for large orifice diameters, > 5cm. The discharge coefficient is related to velocity and contraction coefficients by \( C = C_e C_v \). The importance of these parameters is in the prediction of jet velocity and diameter and hence orifice discharge as given by the well known orifice equation

\[
U = C_v C_e \sqrt{\frac{2\Delta P}{\rho}}
\]

(2.18)

The lowest discharge coefficient measurements were obtained by Medaugh and Johnson \cite{81} who found \( C \rightarrow 0.595 \) for high head discharges, a value that is generally accepted for high \( Re_d \) cases. However, the authors pointed out that \( C \) is highly sensitive to imperfections. Lienhard (IV) and Lienhard (V) \cite{73} developed a prediction model based on experimental results obtained by Judd and King \cite{61} assuming the velocity coefficient, \( C_v = f(Re, We) \) where in this case \( Re = \rho \sqrt{2gh_d} \mu \) and \( We = \rho (2gh_d) \mu / \sigma \), \( h \) being the total head that is related to the drive pressure. From the mechanical energy balance the authors obtain an expression for \( C_v \) given as

\[
C_v = \sqrt{1 - \frac{0.2427C^{3/2}}{Re^{1/2}}}
\]

(2.19)

The fact that surface tension disappears is due to the net exchange of kinetic energy and surface energy that does not occur until jet breakup. Thus, surface tension only affects
2.4. Description of Flashing Mechanism

droplet formation and not jet contraction or velocity for sufficiently large \( \text{We} > 8/\sqrt{C_e} \). The authors conclude that \( C_v = 1.0 \) within 0.1 percent for all sharp-edged apertures provided that \( Re > 10^4 \).

In the following Fig.2.13 and Fig.2.14, the discharge coefficients for different types of nozzle geometries and flow conditions are presented. However, one has to keep in mind that in case of two-phase flow (cavitation or internal flashing) these values risk to be strongly inaccurate.

![Diagram of discharge coefficients](image)

**Figure 2.13:** An example of discharge coefficients (displayed as \( \mu \)) taken from ([15])

### 2.4 Description of Flashing Mechanism

The flashing phenomenon occurs when a liquid is out of thermodynamic equilibrium and becomes superheated; i.e. the temperature of the liquid is above the saturation temperature for the pressure that surrounds it. Different routes may lead to this non-equilibrium state. In the first one, a liquid in equilibrium is heated to a higher temperature while its pressure is maintained constant. In the second case, the liquid is depressurised rapidly keeping its initial temperature constant, so that, because of thermal inertia, the internal temperature finds itself above the saturation temperature at the new pressure condition. The present study focuses on this second scenario.
Chapter 2. Past studies in the literature and the proposed theories

(a) Various shapes of discharge orifices and corresponding values of the discharge coefficients $C$. (a) $C = 0.625$ (b) $C = 0.87$ for $\beta = 20^\circ$, $C = 0.775$ for $\beta = 60^\circ$; (c) $C = 0.85$; (d) $C = 0.865$ for $\beta = 11^\circ 40'$; (e) $C = 0.79$\[15\]

(b) Reynolds number & $l/D$ effect\[70\]

(c) $l/D$ effect\[70\]

(d) Cavitation effect for squared-edged round orifices\[70\]

Figure 2.14: Discharge coefficient evolution with the changes in nozzle geometry and flow conditions.
2.4. Description of Flashing Mechanism

Under carefully controlled conditions (a pure substance in a perfectly clean and smooth vessel, without any physical disturbance and following a slow and almost reversible process), it is possible for a liquid to be maintained in a meta-stable state without flashing occurrence. However, under most practical circumstances, all the ideal conditions cannot be kept and the meta-stable liquid will return violently to a new equilibrium state thanks to a massive evaporation (i.e. consuming its superheat as latent heat).

Fig. 2.15 can show an example of the equilibrium states. The black circles represent the state and the curve represents energy level. All systems tend to preserve an equilibrium state if they are kept under control and free of any external perturbation. Systems in stable equilibrium will stay at their initial conditions after any perturbation, whereas the unstable systems will achieve a new configuration after any perturbation. Metastable system behaviour will be similar to the stable system if the perturbation is not large enough. Pressure differences between the inside and outside of the vessel are considered a large perturbation, so the system loses its initial superheated conditions and it will reach a new stable state.

The idealized thermodynamic steps involved in depressurizing a saturated liquid and vaporizing the superheated liquid are illustrated in Fig. 2.16. Initially, a liquid is present at a temperature \( T_o \) and the corresponding saturation pressure \( P_o \). Decreasing the pressure in the absence of a phase change (removing the weight from the piston) leads to a quasi-isothermal, isentropic shift of the liquid from a saturated (0) to a superheated State (1). This state, also known as a metastable state, is stable for a certain time until a sufficiently strong disturbance initiates the phase change. Part of the liquid then undergoes an isobaric phase change to reach a stable equilibrium state at a temperature \( T_2 \) corresponding to the new pressure \( P_2 \).

This figure also shows the saturation curve with the critical point (C) separating the liquid from the vapor side. The metastable state of a pure liquid is visualized as lying on an isotherm that extends from the stable single-phase liquid region into the two phase region. The form of an isotherm is given by the equation of state (e.g. Van-der-Waals equation of state) as \( P = f(T, v) \). This equation, together with the criterion of Maxwell ([26]), gives the limit between the stable liquid and metastable states, i.e., the saturation curve. The isotherms below the critical temperature show two extreme
values where \((\partial P/\partial v)_T = 0\). Mechanical stability requires that \((\partial P/\partial v)_T < 0\) ([26]). As can be seen from the Fig.2.16, the isotherm between the shaded regions of metastable states violates this stability criterion, that is, a single phase fluid cannot exist in this region. The extreme point of the isotherm where the slope \((\partial p/\partial v)_T\) changes from negative to positive values is named the limit of intrinsic stability, the superheat limit or the spinodal limit.

Connecting the various minima of all isotherms below the critical temperature forms what is known as the spinodal line which is, in addition to the saturation curve, the second limit of the region of theoretically possible metastable liquids. Similar regions and boundaries can be found for metastable vapor, as plotted in Fig.2.16, but are not discussed further here.

Fig.2.16 also shows the steps of a sudden depressurization of liquid. Initially, the liquid is kept at a slightly subcooled (0a) or saturated (0) state. The isothermal depressurization then shifts the liquid to the superheated condition of point (1). From there, boiling or evaporation forms a two-phase system with the liquid and vapor at states (2). In general, as the liquid enters deeper into the metastable range, the likelihood of a phase change increases. At the spinodal line a phase change is virtually certain to occur, that is, homogeneous nucleation starts. The liquid is thermodynamically unstable in the metastable superheated liquid zone, but the phase change in this case is not quasi-instantaneous; it is in fact controlled by nucleation and heat and mass transfer rates.

Pressure, temperature and specific volume or density relationships for actual fluids are expressed by equations of state. Ideal gas relationships (eq. 2.20) are not applicable
for all practical cases. For real fluids, the Van der Waals equation of state (eq. 2.21) is applied.

\[
P = \frac{RT}{V_m}\]

\[
P = \frac{RT}{(V_m - b)} - \frac{a}{V_m^2}
\]

(2.21)

Projecting the saturation surface and the liquid spinodal line on a \( p - T \) diagram and considering a relevant fluid for this process at typical experimental conditions leads to Fig.2.17. The figure shows the saturation curve and the liquid spinodal line for R-134A. The saturation data is plotted from tabular data, while the spinodal line was calculated using the Van-der-Waals equation of state ([26]). As can be seen from Fig.2.17, the metastable liquid can be characterized by its superheat temperature \( \Delta T \), as the difference between actual temperature and saturation temperature at constant pressure (line(2)-(1)):

\[
\Delta T = (T_{\text{actual}} - T_{\text{sat}})_{p=\text{constant}} = (T(1) - T(2))_{P(1)}
\]

(2.22)

Similarly, the underpressure below the saturation pressure at the same temperature can be taken to describe the metastable liquid (line(0)-(1)):

\[
\Delta pP = (P_{\text{actual}} - P_{\text{sat}})_{T=\text{constant}} = (P(0) - P(1))_{T(1)}
\]

(2.23)
Specifying only one of these two related quantities does not describe the specific state of a metastable liquid, since the same degree of superheat can be attained at different temperatures or pressures.

Another important parameter, that describes the possible vapor mass fraction after phase change of a metastable liquid due to adiabatic depressurization, is the Jakob number $Ja$ which is defined as the ratio between the energy available due to superheat and the heat necessary for the vaporization of saturated liquid:

$$Ja = \frac{h_{L1} - h_{L2}}{h_{G2} - h_{L2}}$$  \hspace{1cm} (2.24)

In this definition, the intrinsic enthalpies are used where $h_L$ and $h_G$ are used for liquid and gas phase, respectively. For the initial (1) and final (2) state at rest, the Jakob number $Ja$ is identical to the isenthalpic flash fraction $x_h$. Similarly, an isentropic flash fraction $x_s$ can be defined which is larger than the isenthalpic flash fraction $x_h$:

$$Ja = x_h = \frac{h_{L1} - h_{L2}}{h_{G2} - h_{L2}} > x_s = \frac{s_{L1} - s_{L2}}{s_{G2} - s_{L2}}$$  \hspace{1cm} (2.25)

This means that less vapor is produced during an isentropic vaporization process than in an isenthalpic process. Assuming a constant heat capacity $C_{pL}$ results in:

$$Ja = \frac{C_{pL} \Delta T_{12}}{h_{L1}G2}$$  \hspace{1cm} (2.26)

Frequently, the Jakob number is multiplied by the liquid/vapor density ratio to yield the modified form:

$$Ja^* = \frac{C_{pL} \Delta T_{12} \rho_{L2}}{h_{L1}G2 \rho_{G2}}$$  \hspace{1cm} (2.27)

In general, the Jakob number of metastable liquids is limited by $0 < Ja < 1$, that is, adiabatic flashing is not sufficient to vaporize all the liquid. However, liquids with a high heat capacity or what are known as retrograde fluids potentially have sufficient latent heat to vaporize all the liquid upon adiabatic expansion. As Thomson and Sullivan [135] calculated, this requires a ratio of the specific heat capacity to the ideal gas constant of $c_o^0/R > 11.2$. Among others, Kurschat [67] proved this phenomenon experimentally by using perfluorohexane ($C_6F_{14}$) with $c_o^0/R = 39.3$ resulting in complete vaporisation, whereas water with $c_o^0/R = 3.5$ could not be completely vaporized by adiabatic phase-change.

Owen and Jalil [96] stated that the latent heat will be released through surface evaporation, if the superheat within the depressurised liquid can be conducted to the interface. If, however, the heat cannot be conducted at a sufficiently high rate, evaporation will occur within the liquid through sudden and explosive bubble growth. The bubble explosion serves together with the aerodynamical break-up as a mechanism to offer a maximum surface exchange for the evaporation. Equilibrium will be reached when the fraction of the liquid converted to vapor has extracted enough energy from the residual
Figure 2.18 shows a general view of a two-phase flashing jet. The liquid exits the orifice at its initial temperature, is superheated at ambient pressure and tends to release its superheat through explosive bubble growth.

2.4.1 Liquid Vapor Phase Change

The preceding section has presented the different thermodynamic states occurring during depressurization of liquid below saturation state and its following vaporization. Information about the probability and duration, however, requires a precise knowledge of the mechanism of phase change. Besides film boiling, the transformation from the liquid to the gaseous state occurs via the two mechanisms of evaporation and nucleate boiling. If the vapor pressure of the liquid is only slightly above the the total pressure in the vapor space, calm evaporation (essentially mass transfer) with vapor transition through a flat free surface will occur. In the case of nucleate boiling, the liquid gets locally superheated sufficiently to allow nucleation, vapor is generated more vigorously at various nucleation sites inside or at the boundary of the liquid volume.

In the classical literature, a distinction is made between film and nucleate boiling. In film boiling, the temperature of the heating surface is so high that the surface is blanked by a continuous vapor film from which bubbles are released into the liquid. Ideally, there is no contact between the liquid and solid surfaces. In nucleate boiling, two mechanisms can be identified: heterogeneous and homogeneous nucleation. Heterogeneous nucleation is defined as the generation of vapor at the interface between the liquid and preexistent gas nuclei, whereas homogeneous nucleation is characterized by vapor generation occurring completely within a pure liquid phase ([26]).

While evaporation of a stagnant liquid can occur even without superheat, nucleation always requires a fluid that is at least locally metastable. Homogeneously nucleated liquids vaporize with explosive violence, whereas heterogeneously nucleated liquids boil at a lower superheat but may nevertheless be producing vapor in a violent manner.

Another particular case of boiling to be considered is that involving surface instabilities (generation of surface waves) propagating into the superheated liquid from an initially flat surface. In this type of boiling, no nucleus is required as in the case of homogeneous or heterogeneous nucleation but disturbances produce local variations of the vapor mass flux from the free surface and thus create pressure differences. These in turn lead to further disturbances of the surface which again amplify the vapor generation (Prosperetti and Plesset[107]). This unstable boiling is also observed on bubbles growing rapidly at higher superheats.
2.4.1.1 Classical theory of single bubble growth

Previous studies on depressurization and thermal explosion phenomena involving transient boiling were based on the prediction of bubble growth, since bubbles offer a large interfacial area for the liquid-vapor transition.

To have nucleation in a superheated liquid, the formation of a vapor nucleus should occur first. Taking into account mechanical stability, assuming thermodynamic equilibrium and using the Clausius-Clapeyron equation, a relation for the minimum or critical radius of a spherical bubble that may grow further is obtained in the following equation (Eqn.2.28, [26]).

\[ r^* = \frac{2 \sigma T_{\text{sat}}}{\rho_{G,\text{sat}} h_{L,G} \Delta T} \]  

(2.28)

Only bubbles with \( r > r^* \) may grow, while bubbles with a radius smaller than the critical one will collapse. The equation shows that low surface tension and high superheat result in a small critical radius; nucleation is made easier.

If supplied with heat, the formation of a bubble embryo is followed by bubble growth. During the initial stage of bubble growth, after formation of stable bubble nuclei, the process is inertia-controlled, i.e. the expansion is controlled by the velocity at which the vapor can push back the surrounding liquid. The second stage of bubble growth is controlled by heat transfer. At this later stage, the liquid superheat near the interface has been significantly depleted so that heat for vapor production has to be transported from the surrounding liquid to the liquid-vapor interface. The temperature gradient in a thin thermal boundary layer surrounding the bubble determines the rate of vaporization (Prosperetti and Plesset[106]). This thermally-controlled heat transfer typically occurs at scales of millimeters and milliseconds. Carey [26] gives an expression for the bubble size corresponding approximately to the transition between the inertia-controlled and heat-transfer-controlled growth. For superheated propane at 1 bar and 0°C, the transition occurs approximately at a bubble diameter of \( d_{\text{trans}} = 0.06 \text{mm} \). For the spherically symmetric period of phase growth in an unconfined infinite medium, Plesset and Zwick [108] derived the following relation for the increase in bubble radius with time:

\[ r = \sqrt{\frac{12}{\pi} \sqrt{\frac{\rho_{G} \lambda_{L}}{\rho_{G}(h_{G} - h_{L})}} \Delta T \sqrt{t}} \]  

(2.29)

where \( \lambda_{L} \) is the thermal conductivity of the liquid. Eqn.2.29 can be modified using equation 2.27 to obtain:

\[ r = \sqrt{\frac{12}{\pi} \sqrt{\alpha Ja^* \sqrt{t}}} \]  

(2.30)

Equation 2.30 shows that, in addition to the liquid thermal diffusivity \( \alpha = \lambda_{L}/(\rho_{L} C_{p,L}) \), the Jakob number \( Ja^* \) is the key parameter in describing the growth of the bubble. It also implies that the radius of the bubble \( r \) increases with the square root of time.
2.4.1. Liquid Vapor Phase Change

which in turn indicates a slowdown in the velocity of bubble growth with time. Equations 2.29 or 2.30, which will be referred to as the classical theory of the bubble growth, were found to be in good agreement with experimental results.

More exact theoretical solutions of the differential equations for heat transfer and a further refinement of the bubble growth process are given by Scriven[120] and Prosperetti and Plesset[106]. Mikic et al.[82] describe bubble growth, including the transition from the inertial-controlled to the thermally controlled expansion period. Besides these, there are quite a few bubble growth models which consider temperature and pressure environments of various complexity at different stages of bubble growth. In technical systems with heated solid surfaces, the classical theory of the bubble growth mentioned above must be modified to account for the lack of spherical symmetry and the nonuniformity of the temperature field in the surrounding liquid.

By using the classical growth equation (2.29 or 2.30), the specific mass flux of vapor across the moving bubble interface can be calculated as follows:

\[ \dot{m}_{\text{interface}} = \frac{d(\rho L \lambda_{L})}{dt} \]

This expression shows that the specific vapor flux decreases with time, as the radius increases. For a certain radius size \( r \) of bubble, the mass flux can be expressed by:

\[ \dot{m}_{\text{interface}} = \frac{6 \rho L \lambda_{L}}{\pi \rho G} \left( \frac{\Delta T}{h_{G} - h_{L}} \right)^{2} \]

Equation 2.32 tells us that high mass fluxes can be observed with particular ease for small bubbles and the vaporization rate increases for a given bubble size with the square of the superheat.

2.4.1.2 Explosive Boiling at the limit of superheat

Stable growth of a vapor bubble requires achievement of a critical radius, as determined by equation 2.28. In most technical and natural boiling processes, this minimum volume of gas is provided by pre-existing vapor or inert gas inclusions in small cavities on solid surfaces. These natural or machine-formed pits, scratches, or other irregularities allow heterogeneous nucleation, that is, bubbles will grow at low superheat from these sites.

If heterogeneous nucleation is suppressed, however, a liquid may be heated to temperatures far above its normal boiling point because the critical radius has to be reached by statistical fluctuations of the molecular density within the liquid phase. In this case, the bubble growth begins spontaneously from vapor-like clusters of molecules. This onset of homogeneous nucleation has been theoretically and experimentally investigated.
Chapter 2. Past studies in the literature and the proposed theories

Theoretical prediction of homogeneous nucleation is possible by considering the thermal fluctuations in metastable liquids. These provide a small but finite probability that clusters of molecules with vapor-like energies come together to form a vapor embryo ([26]). According to the Boltzmann equation for the distribution of molecular clusters, the probability of vapor formation increases exponentially with superheat. At a certain threshold, the size of such a cluster is extremely sensitive to the superheat of the liquid, so that a small increase in the superheat leads to an increase in the probability of vapor nuclei of critical size \( r^* \) by some orders of magnitude. The maximum possible superheat is then reached. This is known as the kinetic limit of the superheat and offers a second way of predicting homogeneous nucleation, in addition to the thermodynamic limit based on the equation of state discussed in the previous section.

The kinetic limit of superheat or the onset of the homogeneous nucleation is derived in the classical work of Volmer[137]. This publication gives an historical overview of related phenomena and addresses the calculation of nucleation rate of superheated liquids. A more recent book by Skripov[129] allows a general insight into the thermodynamics of metastable liquids. It presents, for example, thermophysical properties of the superheated state and experimental techniques for investigating homogeneous nucleation. Experiments with various highly superheated liquids were compared with this classical theory of nucleation (reviewed by Skripov [129]). As an example, in Table 2.3 the calculated kinetic limit of superheat and the experimentally observed onset of homogeneous nucleation are compared for a number of fluids.

<table>
<thead>
<tr>
<th></th>
<th>n-butane (1)</th>
<th>propane (1)</th>
<th>water(2)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Theoretical superheat limit ( \Delta T_{\text{thm}}[^\circ C] )</td>
<td>105.1</td>
<td>55.2</td>
<td>307.6</td>
</tr>
<tr>
<td>Experimental superheat limit ( \Delta T_{\text{thm}}[^\circ C] )</td>
<td>104.9</td>
<td>52.9</td>
<td>302.1</td>
</tr>
</tbody>
</table>

Table 2.3: Calculated and measured limits of superheat \( \Delta T_{\text{thm}} \) at 1 bar (Calculation and data found in (1): Blander and Katz[18]; (2) Thormaehlen[136])

The superheat limit for propane given here is derived from the kinetic limit of superheat. It differs from the one which is based on the thermodynamic limit of superheat considering the Van-der-Waals equation of state. In some of the above experiments, the superheat for nucleation was considerably less than that predicted by the homogeneous nucleation theories. According to the experimental work of Yang and Ma[148], the presence of microbubbles is a possible reason for this lowering of the superheat limit. Differences between the measured and calculated results were in particular observed for water, whereas for many light hydrocarbons the superheat limit was correctly predicted by the kinetic theory. As a rule of thumb, the limit of superheat \( \Delta T_{\text{thm}} \) at 1 bar of the latter substances was below the critical temperature \( T_{\text{crit}} \) by a factor of 0.9. In the neighborhood of 1 bar, the limit of superheat varied only slightly with pressure (McCann et al.[80]).
2.4.2 Studies on single exploding (flashing) superheated droplets

In the presence of any trapped vapor, heterogeneous nucleation at interfaces is more likely to take place than homogeneous nucleation in the bulk of the fluid. To overcome this difficulty, experiments for the value of the limiting superheat were conducted with liquid drops embedded in another fluid.

As in the early work of Moore [87], in most of the subsequent experiments, single liquid droplets were allowed to slowly rise in an immiscible “host” liquid where a vertical temperature gradient was present. These became superheated during their rise and explosive vaporization occurred close to the theoretically predicted homogeneous nucleation temperature (e.g., Blander and Katz [18], Shepherd and Sturtevant [122], Frost and Sturtevant [44], Nguyen et al. [91], Frost [43], McCann et al. [80]).

The studies with superheated drops provided also details of the explosive nature of vaporization under these conditions. Shepherd and Sturtevant [122] observed that instabilities distorted and roughened the liquid-vapor interface on a very small scale. The observed rate of vaporization was two orders of magnitude larger than the one predicted by the classical conduction-controlled bubble growth theory not considering the effects of surface instabilities. A high-velocity two-phase flow stream moving away from the interface was produced. It was suggested that the Landau mechanism of instability (see Landau and Lifshitz [69], Problem 2) was playing a dominant role in the initial fragmentation process (Shepherd and Sturtevant [122]). Due to the distortion of the interface, conduction of heat and evaporation of the liquid are stronger in the troughs of the waves and destabilize the surface, as illustrated in Fig. 2.19 (Prosperetti and Plesset [107]). In the studies on the explosive boiling of droplets performed by Frost and Sturtevant [44] and Frost [43], depending on the ambient pressure, a different dynamic behaviour of the bubbles growing inside drops was observed; increasing the ambient pressure moderates the explosion of superheated droplets. Nguyen et al. [91] developed a model to predict pressure field around an exploding liquid particle, while McCann et al. [80] performed measurements to verify this aspect of vaporization.

![Figure 2.19: The Landau mechanism of surface instability (Prosperetti and Plesset [107]).](image)

Gyarmathy [50] calculated the cooling rate of small water droplets suspended in the
expanding flow of a wet steam turbine. He assumed that the drops did not flash until the centre of the drop became superheated by 5°C or more.

Avedisian [7] investigated experimentally the bubble growth within n-octane drops suspended in glycerine under pressure. It was shown how, at high pressures, the excess pressure within the bubble due to vapor generation is reduced and the growth rate of the bubble is controlled by the rate of heat transfer from the liquid. The bubble growth rate, and therefore flashing, is reduced.

Owen and Jalil [96] investigated the vaporization of superheated water through an experimental study with water drops of diameters 1—3mm suspended on a thermocouple junction in a saturated steam environment in a vessel at pressures up to 900kPa in absolute values. Whereas the above experiments investigated boiling at the highest possible superheat, Owen and Jalil focused on determining the lowest superheat required to observe flashing or violent boiling of single drops. The vessel is depressurised at different rates so that drop became superheated. They observed up to AT_{100kPa} ~ 5°C superheat quiescent surface evaporation, between 5 ≤ AT_{100kPa} ≤ −18°C internal boiling without disintegration, above AT_{100kPa} ~ 18°C flashing explosively. The liquid experienced heterogeneous nucleation because of its contact with thermocouple.

Compared to the investigation of the onset of homogeneous nucleation, where stable water drops were observed even at superheats of AT_{100kPa} = 200°C ([136]), these experiments showed that the explosive character of vaporization could already be observed at fairly low superheat at conditions typical for heterogeneous nucleation.

Similar findings were obtained by Maa and Tung [77] who established that, with increasing size, the drops became unstable at decreasing superheat. Sufficiently large drops asymptotically reached the constant value of superheat required for flashing due to heterogeneous nucleation. In order to predict the incipience of flashing, Maa and Tung suggested that this asymptotic value for heterogeneous nucleation should be used rather than that obtained from the theory for homogeneous nucleation. Besides the effect of droplet size, it can be assumed that the flashing of the liquid will be further promoted in a turbulent environment.

Khabeev [62] performed an analytical and numerical analysis of single drops of liquefied gas in water. The evolution of non-steady shock waves, the effects of initial conditions, shock strength, and volume fraction were studied. Analysis of the evaporation process shows that the characteristic time of the drop radius change due to evaporation is much greater than the characteristic time of heat processes because the density of the gas is much smaller than the droplet. The calculations have showed that the process has two stages. The first stage is the “dynamic stage”. It is characterized by intensive pressure oscillations in the gas. At this stage the pressure in the bubble can be considerably greater than initial atmospheric pressure. The second or ‘thermal stage’ is characterized by the monotonic growth of the vapor bubble; pressure in the vapor is settled and equal to the external pressure; the temperature profile is approaching to the quasi-stationary temperature distribution. The initial value of the vapor layer thickness has significant effect on the process at the dynamic stage. The decrease of the initial vapor layer size
leads also to a sharp increase of the evaporation rate and velocity of bubble surface. This is connected with the growth of temperature gradient in the vapor phase due to the decrease of vapor layer thickness.

It can be concluded that explosive bubble growth inside superheated liquid can occur irrespective of whether it was initiated by homogeneous or heterogeneous nucleation. It is characterized by much higher mass-transfer rates than those predicted by the classical bubble growth theory, which presumes a smooth, undisturbed bubble surface. In explosive boiling, liquid may also be contained inside the bubbles.

Depressurization or transfer of heat are two ways of superheating a liquid experimentally. Examples of using heat transfer are the investigation of homogeneous nucleation presented here, where either a host liquid ([87]) or pulse heating of a wire ([129]) served as the energy source for the small droplets. Uniform superheating of a larger volume of fluid can be achieved by depressurization.

### 2.4.3 Studies on blow-downs

A liquid might become superheated either during its flow through a variable pressure field or by suddenly depressurizing a stagnant volume of liquid. Either quasi continuous or instantaneous releases (blow-down phenomena) can occur. The transients related to the depressurization of stagnant liquid will depend on the ratio between the initial volume (reservoir or pipe volume) and the cross-sectional outflow area. Vaporization can range from flashing with extreme changes in temperature and pressure, such as a sudden rupture /opening of pipes, to quasi-continuous outflow such as a blow-down from large tanks through small pipes.

Several scientists have studied blow-downs from complex networks of pipes and vessels associated with nuclear power. The non-equilibrium effects of blow down have been observed experimentally by a number of researchers.

The accelerational and frictional pressure drop causing flashing if liquid flowing continuously in a pipe was investigated by several authors (e.g. [41]). Miller[83] visually recorded that bubbles were fewer in number by more than an order of magnitude and grew faster than expected from various theoretical treatments, for instance, using conduction-controlled bubble growth theory. Nucleation was limited to the walls and was not seen in the bulk.

Releases from initially stagnant liquid were performed by Hooper[57],[56],[58], Edwards and O'Brien[35], for example. They measured pressure transients in pipes evolving at rates of depressurization of up to 162000 kPa/sec. Significant departures from phase equilibrium were observed, with the pressure decreasing from its initial level to a value substantially below the saturation pressure corresponding to the initial liquid temperature at each measurement location. The resulting metastable liquids were even found to reach negative pressures. Thus, fluids were subjected to tension for several millisecond-
Chapter 2. Past studies in the literature and the proposed theories

onds in release experiments where a considerable "pressure undershoot" was observed. Studying the vapor bubble generation due to rapid depressurisation of hot water, Bartak [13] addressed the expansion wave propagating in an abruptly opened pipe. He found that this initial pressure undershoot was stopped by explosion-like vapor generation leading to a period of quasi-static pressure during vaporization. He developed a method for calculating the pressure undershoot below the saturation pressure.

Between the above extremes of continuous flashing in liquid flow and instantaneous vaporization of stagnant superheated liquid, various studies were carried out with releases of different path lengths. Depressurization times ranged from milliseconds with highly transient flow phenomena to days characterized by quasi-steady outflow. Some studies focused on cases such as interacting flashing in a pressure vessel and in a depressurization pipe where the fluid becomes depressurized (e.g. Hervieu and Veneau [53]).

Since flashing occurs in a variety of situations, no general correlation or model is available to predict the exact flow behaviour, for example. Numerous models for flashing flow have, therefore, been proposed. Yan et al. [147], Watanabe et al. [139] and Riznic and Ishii [115] proposed models for the vapor generation in flashing liquids during blowdown. Deligiannis and Cleaver [32] showed that nucleation could be correlated by introducing a factor accounting for heterogeneities in the liquid. Shin and Jones proposed a model for nucleation and the flashing mechanism in nozzles. Despite these various attempts to model flashing flow, it is still difficult to predict the results of depressurization experiments a priori, because of various non-quantifiable effects (e.g. influence of surface conditions at the walls, presence of dissolved gases or impurities). This means that even recent flashing models (e.g. Giot [48], Kolev [64]) still depend on major experimental input to determine the density of nucleation sites, for example.

Phase equilibrium models are not able to predict the experimentally observed pressure undershoots below the initial saturation level. Nevertheless, Garner [45], Lyczkowski et al. [75] and Moore et al. [88] attempted to fit phase equilibrium models to the data of Edwards and O'Brien [35] by adjusting the initial liquid temperature at each pressure transducer location, based on the assumption that the pressure measured immediately following the rupture were saturation pressures corresponding to the "real" initial liquid temperatures. This adjustment amounted to as much as 16.7°C below the measured initial temperature of 515°C. The justification for this adjustment was the uncertainty in the initial temperature that resulted from non-uniform heating of the pipe.

To resolve questions resulting from the temperature uncertainty in the Edwards and O'Brien [35] measurements, Winters and Merte [143] conducted independent pipe blow down experiments utilizing \( R - 12 \) in which the initial temperature in the test section was maintained uniform to within ±0.6K. Measurements of decompression transients at four axial locations along the pipe were compared to the predictions of a one-dimensional transient phase equilibrium model. At each transducer location, the measured pressure transients were observed to fall well below those predicted by the phase equilibrium model.

Jones and Saha [60] presented a review of non-equilibrium phase change models ap-
2.4.3. Studies on blow-downs

Winters [144] conducted experiments and non-equilibrium analysis of pipe blow-down. It appeared that the degree of departure from thermodynamic equilibrium depended on a parameter that includes the system volume-to-discharge-area ratio, since that parameter most reflects the rate of depressurisation and fluid expansion. For small ratios, the rate of depressurisation and fluid expansion was relatively high and there was not sufficient time for the mixing and heat transfer between the phases required to maintain thermodynamic equilibrium. The surface roughness effect also was investigated in this experiment. Even with a change by a factor of 3 in surface roughness, very little difference in the decompression transient was detected. The high-speed motion pictures taken from the viewing window at the closed end indicates that all nucleation began at the duct walls and at the thermocouple junction due to heterogeneous nucleation.

Yan [147] proposed a model of flashing during adiabatic release through pipes or in the vessels considering the heterogeneous factors responsible for nucleation. More attention was put to micro cavities than to the micro bubbles, since all containers and pipes have wall surfaces in the contact with the liquid. The model was based on heterogeneous nucleation for adiabatic vessels and pipes combined with theory for the effects of turbulence to predict the flashing inception. His results were compared with Reocreux's data.

Elias [36] studied theoretically the flashing inception in water during rapid decompression. Homogeneous nucleation with an empirically defined heterogeneity factor has been utilised. At high decompression rates, the heterogeneity effect was reduced and the results obtained from the model approached those obtained for the homogeneous nucleation case. The model has been validated against experimental data covering a wide range of initial temperatures and depressurisation rates.

Dagan [30] developed a one-dimensional model in critical flows in pipes of non-diverging cross-sectional area. The results were compared with experimental data in small-large scale for wide range of pressures, pipe diameters and lengths. The number and rate of growth of vapor bubbles in a variable temperature field, the pressure drop along the channel were studied.

Feburie [40] investigated a model for choked flow through cracks with inlet subcooling to predict the mass flow rate and compared with available experimental data. The tube wall crack and pressure distribution along the crack depth for several thermohydraulic conditions, variation of cross-section area, wall friction and wall heat flux have been studied.

Domnick [33] performed measurements of bubble size, velocity and concentration in flashing flow behind a sudden constriction in vertical upflow in a pipe. Freon 12 was used as the flashing material. Flow visualization, phase-doppler anemometry have
been used to measure local bubble and fluid velocities, local bubble sizes and void fractions. Measurements were done in the mid-plane of a two-dimensional channel with a 2 : 1 stepwise constriction. Bubble nucleation took place in the recirculation zone immediately behind the constriction, which was the location of the lowest static pressure. These bubbles were transported downstream by the mean flow field, while undergoing further growth. No additional nucleation was observed downstream of the recirculation zone. A periodic, bubble cloud formation appeared. The interaction with the bubble cloud and mean flow field resulted in strong disturbances of the mean flow field, which showed up in 10 steps heights behind the constriction as an increase of the fluctuating bubble velocity by a factor of 3 compared to single phase. The measured arithmetic mean bubble diameters rose from approximately 50$\mu$m in the recirculation region to about 70–80$\mu$m 50 steps heights downstream. Maximum local bubble number density and density were found to be 16000/cm$^3$ and 0.8%, respectively.

Kolev [64] developed a mechanistic model of nucleation and flashing phenomena. This is a theoretical work based on heterogeneous nucleation at walls during flashing and boiling. The computation of bubble departure diameter, nucleation site density, bubble generation on heated surface, film flashing bubble generation in adiabatic pipe flow was obtained theoretically and results were compared with experimental data.

Downar et al. [34] developed a non-equilibrium relaxation model for one-dimensional flashing liquid flow. This model can be considered as a homogeneous relaxation model taking into account the non-equilibrium evaporation leading to metastable conditions. It is able to predict the mass flow rates, and the pressure distributions for one-dimensional flashing water. The author found it better than homogeneous equilibrium model.

Ling et al. [74] made an investigation on sub cooled flashing nozzle flow with vaporization enhancement. He used a three region combined CTD (converging, throat, diverging) model. The model proposed a critical flow behaviour that started in the throat region with violent vaporization and initial formation of bubbles and continued as dispersed flow in the diverging part being described with a two-fluid formulation. The study was performed with flat, converging, converging-diverging nozzles of different shapes, diverging angles, throat diameters. The droplet diameters were measured in dispersed phase ($\sim< 100\mu$m). The critical conditions were studied. A relation between the throat length to diameter ratio, the critical pressure ratio and the mass flow rate were found. Throat shape has a strong influence on vapor fraction and velocity at the exit flow.

Skorek and Papadimitriou [128] worked on a simple model for critical flashing flows in nozzles. The computations were conducted for water and hydrogen with varying inlet conditions in the vessel upstream of the nozzle including thermodynamically supercritical conditions for hydrogen and for converging-diverging nozzles of different scales and geometry. After comparing the results with experimental data good agreement was found in the critical flow rate and the pressure distribution along the nozzle.

Gebbeken and Eggers [46] conducted an experimental investigation on top-vented blow
Studies on the Superheated Jets
down of initially supercritical and subcooled carbon dioxide. Significant dependency of the void profile on the thermodynamic conditions were observed during the blow down. Experimental error in terms of void fraction increased when thermodynamics conditions approaches to the critical state.

Attou et al. [6] performed an experimental study of critical flashing through a relief line finding evidence of double choked phenomena. The behaviour of a steady-state critical steam water flowing through a horizontal relief line was observed in case of the an abrupt cross-sectional change such as sudden enlargement and the usage of a circular orifice. In both cases experiments were carried out using various backpressures. Mass flux vs. superheat and critical mass velocity vs superheat was investigated.

In another work, Elias and Chambre [37] investigated the bubble transport in flashing flows. He studied theoretically the net vapor generation rate along the tube, model prediction in terms of flow rates, void fraction distributions compared with measured data of Reocreux.

Ivashnyov et al. [59] conducted experiments on slow waves of boiling under hot water depressurisation. They developed a model and compared the predictions of it with experiments related to slow boiling.

Kumzerova and Schmidt [66] worked on numerical simulations of nucleation and bubble dynamics in a depressurised liquid. The time history and spatial distribution of liquid phase pressure, bubble concentration and sizes, vapor temperature and pressure were studied.

2.5 Studies on the Superheated Jets

The flashing phenomena in a superheated jet has received attention due to its importance in atomizers and also as a model for the loss of coolant accident (LOCA) scenario related to water-cooled nuclear reactors. Much of the work has been concentrated on the critical flow conditions (e.g. Henry [52]; Schrock et al. [119]; Lackme [68]), but others have considered the behaviour of the superheated jet as it emerges from a sharp orifice or various nozzle geometries.

Many researchers have conducted studies on the flashing process of a liquid jet to understand the break-up patterns and the droplet size, velocity and temperature evolution of atomized jet. Many of earlier drop size measurements reported, involved the use of photography (Brown and York [21]; Reitz [112]) or light scattering techniques such as the diffraction methodology in the form of Malvern Particle Analyzer (Park and Lee [101]; Allen [4]) or Phase Doppler Anemometry (Balachandar et al. [11]; Hervieu and Veneau [53]). The measurements along the area where the liquid disintegrates to gain thermodynamic equilibrium have been reported to be optically very challenging.

Most of the measurements have been performed on single-component superheated liq-
uid jets (Brown and York [21], Lienhard and Stephenson [72], Lienhard and Day [71], Wildgen and Straub [141]; Miyatake et al. [84, 85], Ewan and Moodie [38], Peter et al. [102], Park and Lee [101], Reitz [112]). However, binary superheated fluid systems also attracted some interest due to the rising use of multicomponent fuel systems or utilization of the flashing phenomena for better atomization performance in medical spray applications (Sher and Elata [123], Zeigerson-Katz and Sher [124, 150, [151], Gemci et al. [47]).

2.5.1 Single-component superheated fluid systems

The characterization of flashing atomization of single-component superheated liquid jets took a large part of the superheated jet studies.

Brown and York [21], Lienhard and Stephenson [72], and Lienhard and Day [71] investigated the flashing influence on the break up of jets continuously expanding in a low-pressure environment and found a minimum superheat necessary for flashing to occur. Kitamura et al. [63] revealed that this critical requisite superheat decreased with increasing liquid velocity or nozzle diameter. Using short nozzles, Wildgen and Straub [141] observed boiling at the free surface of the jet. Kurschat [67] released jets of retrograde, highly superheated fluid and produced its complete evaporation. In the case of retrograde fluids with a Jakob number $Ja > 1$, the heat was transported only over distances of molecular order of magnitude leading to no heat-transfer limitation. Bharathan and Penney [16] investigated the vaporization of superheated liquid jets for application to flash evaporation processes.

Miyatake et al. [84, 85] performed an analytical study and emphasized the axial temperature distribution of flashing liquid jets at a relatively high pressure. They performed also measurements on a superheated water jet issuing from a circular tube nozzle into a low-pressure environment investigating the effects of superheat, flow rate and nozzle diameter on spray flashing. Balitsky and Shurchckova [12] conducted similar experiments at very low absolute pressures.

Ewan and Moodie [38] took measurements on a two-phase flashing Freon 11 jet, released from various nozzles, using Malvern-particle-sizer studying the evolution of drop size, velocity and temperature in the flashing jet. Allen [3, 4, 5] performed several studies on velocity and drop size measurements of flashing propane jets. He interpreted the double peak in the velocity profiles and the different behaviour of the large and small droplet classes. Hervieu and Veneau [53] undertook measurements on an example of large-scale blow down of a LPG release addressing the flashing problem. They provided pressure, temperature evolutions in the vessel during blow down and droplet size-velocity evolutions of the two-phase jet with effects of nozzle diameter and initial pressure. Balachandar et al. [11] performed an experimental study on velocity and particle size measurement in a two-phase flashing jet generated using various nozzle geometries and reservoir test conditions.
2.5.1. Single-component superheated fluid systems

Peter et al. [102] studied the different shattering patterns of superheated water released in a low pressure region and tried to provide temperature and droplet size distributions with the influence of superheat, pressure and nozzle type. In a later study, they (Peter et al. [103]) made an experimental investigation of the mode of phase dissociation in superheated liquid jets. Pure water and gas-associated solutions at low absolute pressures have been used and the droplet size distribution and volumetric flow rate distribution are studied.

Park and Lee [101] focused their experimental study on the flow patterns before and after the discharge orifice. Depending on the injection conditions, bubbly, slug or annular flow was observed before the orifice. They found that with a thicker orifice the spray droplets are smaller and in general more uniform. Using two different illumination techniques, Reitz [112] was able to observe that the jet consists of two regions: a core region and a surrounding fine spray region. He further speculated that the main jet had several sub jets whose break-up constituted the smaller particles in the outer edges while longer wave length break up (hence larger particles) contributed to the core region.

In the following sections, observations related to the bubble growth, jet break-up, droplet and velocity evolution during flashing atomisation of superheated liquid jets will be summarized.

2.5.1.1 Superheated Jet break-up

Studies on the jet break-up in superheated liquid jets focused mainly on two concepts: the disintegration pattern that could be observed during flashing and the superheat necessary to shatter the liquid jet.

Jet disintegration patterns due to flashing

Spray behaviour depends primarily on the atomisation mechanism. Sher and Elata [123] assumed that the atomisation occurs (i.e. bubble forms) uniformly along the cross-section of the liquid jet just after the discharge. However, from the viewpoint of the phase-changing two-phase flow, it is extremely difficult to have uniform nucleation. Oza [97] and Oza and Sinnamon [98] postulated that the two-phase flow consisting of vapor and drops forms inside the pintle nozzle, in a diesel spray, however, they did not provide any verification for this statement. Reitz [112] studied the phenomena of the external flashing mode by examining spark flash photographs of sprays and found that drops were expelled from an unbroken liquid jet starting right at the nozzle exit. He concluded that the jet may actually be broken up outside the nozzle at the internal flashing condition of Oza [97] and Oza and Sinnamon [98]. Wildgen and Straub [141] subdivided the external flashing mode into two kinds, "surface boiling" and "particle boiling".

Ostrowski [95] measured the break-up length by changing the storage temperature and
pressure. When the storage temperature increased, the break-up length decreased. For the same pressure and temperature, when orifice diameter increased, break-up length decreased. For the same temperature and same nozzle diameter, break-up length decreased with the increase of the storage pressure and this effect displayed itself stronger for small nozzles than large nozzles.

The works of Oza[97] and Oza and Sinnamon[98] proposed a mechanism to explain why the jet diverged immediately at the nozzle exit in the régime of high superheat. They reasoned that boiling and rapid bubble growth occurred within the injector orifice producing a two-phase, already atomized flow at the nozzle exit. In support of their conclusion that the jet is already atomized at the nozzle exit they observed that the spray expanded rapidly immediately upon leaving the orifice “in the manner of an underexpanded, choked, compressible flow”. To emphasize the implications of this theory on the breakup of flash boiling jets, they distinguished between the low and high superheat régimes, referring to the régimes as “external flashing” (breakup occurs outside the nozzle) and “internal flashing” (breakup occurs within the nozzle), respectively. They[98] developed a model in which the bubble growth starts at the exit of the orifice, where there is a pressure fluctuation. The intact jet length was computed as the distance between the orifice and the vapor bubble. Except for small superheats, their experimental results fit with the theory. They have also observed that when the storage temperature, thus superheat, increased the break-up length decreased.

Peter et al. [102] studied the different shattering patterns of superheated water released in a low pressure région and tried to provide temperature and droplet size distributions with the influence of superheat, pressure and nozzle type. They observed four different breakup pattern of the superheated pure liquids with increasing superheat: non-shattering liquid jet, partially shattering liquid jet retaining the inner core, stage-wise shattering liquid jet and the flare flashing liquid jet. The nonshattering liquid jet is the one which retains its column structure for extended distances after being released from the nozzle exit accompanied by a sporadic spew of undispersed ligament falling parallel to the main liquid column. This happens at low superheat degrees in a very low pressure region. The partially shattering liquid jet is the one whose column shatters only in its outer layer and retains its inner layer or column downstream. This type of shattering occurs when the superheat is slightly higher than the preceding one where all the other parameters are kept the same. Further increase in the superheat degree results in a stagewise shattering where the complete shattering of the jet is achieved at a certain distance from the nozzle exit downstream. When the degree of superheat is extremely high, flare shattering is experienced. Liquid shatters erratically just at the nozzle exit and usually the spray angle is bigger than the other cases. The flow pattern changed from partially shattering to stagewise shattering associated with a maximum difference of 10°C in the superheat.

When the stagewise shattering onset conditions are shown for the nozzles of the same length but different diameters, it is observed that the small-diameter nozzles required relatively higher superheat degree of inlet water in order to initiate liquid shattering compared to larger ones. Peter et al. [102] explained this in the logic of faster cooling of a small diameter jet than a larger diameter column jet and therefore the smaller
2.5.1. Single-component superheated fluid systems

According to their measurements, the superheat necessary to shatter the jet was lower for long nozzles than shorter ones, mostly because of the residence time of the bubbles before reaching the nozzle and the number of nucleation sites. Longer nozzles exhibited longer residence times for nucleated bubbles compared to short ones, therefore, the mean size of the bubbles released at the exit was larger.

In a later study, Peter et al.[103], performed tests on the shattering of superheated liquids involving dissolved gas. This time, they observed only two types of liquid jet configurations for the carbon dioxide-water solution: the nonshattering liquid jet and the fiare flashing liquid jet. When the degree of liquid superheat was increased at constant inlet liquid pressure and flashing chamber pressure, the liquid jet directly transformed from the fully coherent state to the totally disintegrated liquid jet state just near the nozzle exit. This typical behaviour emphasized the occurrence of the sudden increase in the rate of bubble nucleation on the wall surface and the bulk of the liquid governed by the dissolved gas dissociation.

According to Park and Lee [101], basically two-flashing modes have been reported based on the spray configuration outside the nozzle: the complete flashing mode (the occurrence of the phase change of the superheated liquid jet outside the nozzle) and the two-phase effluent flashing mode (two-phase flow is already formed inside the nozzle, mostly with long-hole nozzles). In the work of Oza and Sinnamon[98], on the other hand, this two modes were defined as "external flashing mode" and "internal flashing mode", respectively. The complete flashing mode is usually unstable and difficult to control. On the other hand, the two-phase effluent mode is more likely to occur and is easier to control and most applications of flash atomisation should be based on this mode according to Park and Lee [101]. That's why they based their studies on this mode.

However, Park and Lee were convinced that the flash atomisation mechanism could only be understood through simultaneous examination of the internal and external flow behaviour. For this purpose, they used transparent nozzles (rectangular in cross-section, with their two sides made of glass for flow visualization and photographing, the other two sides are made of stainless steel), both the internal and external behaviors were examined simultaneously. They performed measurements with conventional circular single hole nozzles (made of brass, to examine the spray behaviour) to confirm the interpretations of the experiments with transparent nozzles and compare with the qualitative atomization behaviour reported by other researchers. The test material was water at maximum 400kPa assuming that only the thermodynamic breakup mode (flash) initiated the breakup and hydrodynamic breakup mode played a secondary role. They used a non-dimensionalised superheat definition defined as in Eqn. 2.33 where \( \Delta T^* = 1 \) implies boiling in the stagnation chamber.

\[
\Delta T^* = \frac{T_{inj} - T_{sat}(P_{ambient})}{T_{sat}(P_{inj}) - T_{sat}(P_{ambient})}
\]  

(2.33)
Investigating the effect of superheat (Fig. 2.20), Park and Lee observed for the long transparent nozzle that the internal flow pattern changed from bubbly flow first to slug flow and then to annular flow with the superheat increments. As for the atomization pattern outside the nozzle (Fig. 2.20), the bubbles were crowding at the nozzle wall with the bubbly flow case and when this bubbly mixture is ejected from the nozzle, what was observed was the intact liquid core full of bubbles which burst into fine drops at the surface along the core. Park and Lee claimed that this is the same flow pattern that has been found out in the photographic study of Reitz [112]. With the increase of the superheat, nucleation and growth of bubbles became more active; thus bubbles collided with each other and coalesced inside the nozzle to form large slug bubbles. When the slug flow was discharged from the nozzle, the slug bubbles burst into ligaments and disintegrated into small drops, but the large liquid blobs originated from liquid slugs were still observed. The large liquid blobs finally break into drops, which are usually larger than the ones from the ligaments. The intact core became much shorter compared to the bubbly flow case. At a higher superheat, a liquid film formed at the nozzle wall and the vapor flowed at much higher velocity along the core region. When the annular flow was discharged from the nozzle, the liquid film disintegrates into drops finer than the previous cases.

Fig. 2.21 shows the effect of the nozzle diameter length at high and low superheats for the same injection pressure. With higher superheats, the long nozzle (left) provided a more active bubble formation/growth with a nucleation starting more upstream, a shorter intact core, larger spray angle and better dispersed spray drops compared to short nozzle. At low injection temperature, bubble formation/growth was much less active with the short nozzle due to the insufficient number of nucleation sites and also to the short residence time for bubble growth within the nozzle. Therefore, the liquid was discharged from the nozzle in a superheated state and then partly flashes but mostly evaporated.

Park and Lee emphasized that slug flow or the annular flow occurred when the injection pressure was low and the injection temperature was relatively high or when a long nozzle was used. Keeping a lower injection pressure implied a lower flow rate, and thus a longer residence time for the fluid inside the nozzle, as similar as a longer nozzle involved a longer residence time for the fluid. Thus, bubbles had more time to grow resulting in a fluid with a high-void-fraction at the nozzle exit and the flow patterns appeared to be slug or annular flow type. When the injection temperature was high, void formation was more active and the flow also tended to be in a slug or annular flow regime.

Park and Lee concluded their observations for nozzle length and superheat effect in Fig. 2.22. In the use of a short nozzle and/or low superheat, the liquid was discharged through the nozzle before the bubbly layer grew to the centerline. Moreover, a long intact (liquid jet) core occurred. Small drops around the liquid jet core were formed from the bubbly layer by bubble bursting and the large drops at the downstream center were produced from the liquid jet core. On the other hand, with the long nozzle and/or with a high degree of superheat, the nozzle hole was saturated with the bubbles, which burst into drops almost right after the discharge; this made the spray drops smaller.
2.5.1. Single-component superheated fluid systems

Figure 2.20: Dependence of spray pattern on two-phase flow regime before discharge with the increase of superheat (long transparent nozzle: (a) bubbly flow; (b) slug flow; (c) annular flow. [Park and Lee [101]]
Chapter 2. Past studies in the literature and the proposed theories

Figure 2.21: Spray pattern with bubbly flow inside the long and short transparent nozzle for low (a) and high (b) injections temperatures [Park and Lee [101]]

and more uniform.

Nagai et al. [90] performed measurements with water stored at a temperature of 90°C, during which they have seen a cylindrical jet in a stable form with the existence of some bubble of vapor which developed inside the core. With the increase of the temperature to 140°C, the liquid core length decreased and the number of bubbles increased with premature explosions. They postulated a bubble formation inside the nozzle by wall nucleation and reported the aspect ratio \( l/D = 7 \) as the criterion between the external and internal flashing modes. Generally, more active bubble formation is expected with the longer nozzle holes, which may be due to the larger number of wall nucleation sites. It is evidenced by the promotion of atomization when a wire net is installed inside the nozzle.

Reitz [112] applied high resolution photography to reveal details about the structure of flash boiling jets whose spray divergence starts at the nozzle exit. The results showed that these flashing jets actually comprised an inner intact core which was normally obscured by the surrounding diverging spray. For the measurements at room temperature (no superheat), the onset of long-wavelength Rayleigh-type breakup which is due to a surface tension instability was clearly seen. The jet was broken up by disturbances whose wavelengths were larger than the jet diameter. When the liquid was superheated, the short-wavelength disturbances were superimposed on a longer wavelength instability. The long wavelength breakup produced relatively large drops, seen far downstream the nozzle. Smaller droplets and ligaments accompanied the short
wavelength breakup closer to the nozzle. Moreover, numerous small diameter jets were seen emerging from the main jet, especially near the nozzle. When the superheated was increased more, these sub-jets near the nozzle became numerous. The breakup could, in this case, be divided roughly into a core region (where longer wavelength breakup was still evident some distance downstream of the nozzle) and a surrounding fine spray (formed from breakup of sub-jets). With a further increase of the superheat (8°C below the reservoir boiling temperature), the jet stayed still intact in the vicinity of the nozzle but the core breaks up sooner. The spray cone increased with increasing temperature, as well.

**Superheat necessary to shatter the liquid jet**

Brown and York [21] conducted a study of the behaviour of flashing liquid jets (water and Freon 11 jets) at atmospheric conditions, employing different forms of nozzles. They made measurements of the level of superheat required to produce an effective spray and the jets were analyzed for drop sizes, drop velocities and spray patterns. The temperatures below which no disintegration effects were observed on the jet and above which the jet was shattered by flashing were in a narrow range of about ~ 2.5°C for each flow rate in each nozzle, although the limiting temperatures shifted in absolute value for each change in variable.

Hewitt [54] suggests that superheat temperatures found in practice for water are about 10°C and Lienhard and Stephenson [72], in their experiments with superheated water jets issuing from sharp-edged orifices, found the water could not withstand superheat temperatures higher than about 27°C at atmospheric pressure. Some fluids such as diethyl ether, pentane, isopentane, butane and n-octane, on the other hand, can be heated to temperatures close to their superheat limit from where they will nucleate

---

**Figure 2.22:** Dependence of spray flow pattern on the bubble distribution inside the nozzle: (a) short nozzle or low superheat; (b) long nozzle or high superheat [Park and Lee [101]].
Ostrowski [95] found the temperature difference between a coherent and an essentially completely shattered water jet to be of the order of \( \sim 11.1^\circ C \).

Bushnell and Gooderum [24] investigated the atomization of the superheated water jets at low ambient pressures and low \( W_e \) numbers. In their experiments the shattering temperature decreased with ambient pressure. For smaller ambient pressures the jet shattered at smaller temperatures. Fig. 2.23 shows that the correlation curve of the “ambient pressure vs shattering temperature” is parallel to the saturation line. Therefore, it may be concluded from their results that the superheat leading to shattering of the jet is the same even if the ambient pressure or liquid temperature varies. The relation of the curve was found as \((T_{sh} - T_{sat})/T_{sh} = 0.10\) with the temperature in \(^\circ\)F units) and represented the data for \( W_e < 12 \) well up to \( T_i = 190^\circ F (\sim 87.8^\circ C) \) since below this value the liquid core was still observable. Above this temperature, the two-phase flow was present in the nozzle. (Attention should be paid to the fact that this correlation is unit dependent and can not be applied as a general correlation to other temperature scales. Conversion should be made, for instance it should be used as \((T_{sh} - T_{sat})/(T_{sh} - 32) = 0.10\) in \(^\circ\)C units.) The data had a scatter band of \( \pm 10^\circ F (\sim 5.6^\circ C) \) without any systematic effect of either orifice diameter or liquid velocity. This means that the aerodynamic forces were secondary during the atomization process. Comparing both figures, it can be concluded that for the single phase flow the data could fit to the correlation \((T_{sh} - T_{sat})/T_{sh} = 0.10\) in \(^\circ\)F units), whereas with the existence of two phase flow in the nozzle the data fits in to correlation \((T_{sh} - T_{sat})/T_{sh} = 0.07\) in \(^\circ\)F units). A remark should be made to the fact that for \( d_o = 0.011m \) and \( 0.02in \) the single phase flow was seen for \( T_i = 190^\circ F (\sim 87.8^\circ C) \) whereas for \( d_o = 0.03 \) it drops to \( T_i = 140^\circ F (\sim 60.0^\circ C) \).

Lienhard and Stephenson [72] deduced a simple expression for the delay time \( t_d \) defined as the time between a liquid becoming superheated and the onset of boiling required before homogeneous nucleation would occur in the jet. They also found in a similar study that the critical superheat increased from \( 23^\circ C \) to \( 30^\circ C \) as the jet diameter increased from \( 2.5mm \) to \( 5.0mm \).

In a later paper, Lienhard and Day [71] made comparisons of the predicted delay time with experimental measurements. By considering their jet as an infinitely long cylinder of diameter \( d \) with an initial uniform temperature which experiences a step change in the surface temperature from \( T_s(P_1) \) to \( T_s(P_2) \), as it emerges from the orifice, they were able to calculate its cooling rate by conduction theory. It was reasoned by Lienhard and Day [71] that since the superheat is proportional to \( t_d^{-2/7} \) (where \( t_d \) is the delay time between a liquid becoming superheated and the onset of boiling), if the jet cooled at a rate faster than this, it would escape flashing. Their observations showed that superheat temperatures of up to \( 95^\circ C \) might be possible for some liquids but they were certainly not sustainable in water.

As already explained before, the necessary superheat difference to change the flow pattern of a water jet from partially shattering to stagewise shattering is found out
2.5.1. Single-component superheated fluid systems

(a) \( d_0 = 0.010\text{in}(0.254\text{mm}) \) and \( 0.020\text{in}(0.508\text{mm}) \)  
(b) \( d_0 = 0.030\text{in}(0.762\text{mm}) \) and \( 0.040\text{in}(1.016\text{mm}) \)

Figure 2.23: Variation of shattering temperature with ambient pressure for different nozzle diameters, (Bushnell and Gooderum[24])

\( 10^\circ C \) as maximum in the work of Peter et al. [102].

2.5.1.2 Jet opening angle

Oza[97] used propane, methanol, and indoline to understand the influence of the storage temperature and ambient pressure on the jet-opening angle. He concluded that only pressure played a role on the jet angle and claimed that storage temperature had no influence on it. However, the measurements were done keeping the injection temperature and pressure constant while the ratio of the injection pressure to the reservoir pressure was varied. In this case, no attention was given to the fact that the superheat of the liquid was changing with this ratio change, as well. In this point of view, his conclusions are questionable.

Reitz [112] has found that storage temperature plays an important role on the jet-opening angle. When the storage temperature increased, the jet opening increased, as well.

The jet opening was studied in detail by Nagai et al. [90]. The jet angle was determined from the distance between the exit and 30\( mm \) downstream. The storage pressure did not play a role on the opening angle. The angle reached its maximum value at the dimensionless superheat of \( \Delta T_S^* = 0.55 \) and then decreased when \( \Delta T_S^* \) increased. The definition of the dimensionless superheat \( \Delta T_S^* \) is given in Eqn.2.34. According to their observations, the augmentation in the storage temperature increased the entrainment
of the small droplets towards the jet axis.

\[ \Delta T_i^* = \frac{T_{inj} - T_{sat}(P_{ambient})}{T_{sat}(P_{inj}) - 100} \] 

Park and Lee [101] compared their results to those of Nagai et al. [90] in Fig. 2.24(a) and Fig. 2.24(b) for two different nozzles. Differently, they [101] measured their jet-opening angle at 20 mm downstream distance from the exit and used an average of 5 images. No influence of the storage pressure was observed on the spray angle. For Park and Lee, the superheat values for the maximum spray angle are found to be around 0.85 whereas this value is 0.45 in the work of Nagai et al.

![Figure 2.24: Spray angle variation with degree of superheat [Park and Lee [101]]](image)

2.5.1.3 Bubble growth observations in superheated jets

Observing the results of the effect of changing thermodynamic conditions of the system on the bubble nucleation and departure has shown to Peter et al. [103] that depending on the liquid velocity inside the tube, bubbles can be heterogeneously nucleated on the wall even under the local liquid subcooled conditions. Sporadic release of vapor bubbles from the wall surface is attainable even at the extremely high subcooled conditions, while steady departure of the bubbles is achieved at near equilibrium condition.

According to Scriven [120], the degree of pressure perturbation at a location appears to be independent of the tube or channel diameter at larger diameters. Peter et al. [103] had observed the effect of bubble growth inside the nozzle on the jet break-up and they
2.5.1. Single-component superheated fluid systems

claimed that the effect of the pressure perturbations on the bubble growth might be stronger for smaller tube diameters. Accordingly, they pointed out that the degree of perturbation may be positively decreased with the increase of the respective diameter of the channel or the tube. They observed that at lower liquid temperatures, the conditions for bubble nucleation are more easily obtained for a carbon dioxide-water solution compared to pure water and less superheat sufficed to trigger the nucleation. However, the degree of superheat necessary to initialize the bubble nucleation for the solution increases with the increase in water temperature and approaches that of pure water at temperatures close to or above 60°C. At lower temperatures of the solution, higher degrees of subcooled conditions for triggering nucleation are governed by the dominance of dissociated bubble nucleation due to high molar concentrations of the dissolved gas. At higher temperatures, dissociated bubble nucleation process becomes weaker and the evaporation becomes more active.

Suzuki et al. [132] undertook measurements of the vapor bubble growth rate, idle time of nuclei bubble, and the number of nuclei were measured from the photographs. The bubble growth rate constant was less than that calculated from the theoretical equation presented by Forster and Zuber. Large scatters in the growth rate constant, the idle time and the number of the bubble nuclei were observed.

2.5.1.4 Velocity measurements from superheated jet atomisation

In the study of Brown and York [21], the large droplet classes display higher velocities than smaller ones. The change is almost linear at the axis and at very close distances (1cm) from the axis. Whatever is the droplet size class, the ones on the axis have higher velocities than the ones on the periphery. The explanation of Brown and York [21] relies on the increase of the friction on the edges of the spray than on its axis.

Moodie and Ewan [86] studied the influence of the pressure on the velocity of the superheated liquid Freon 11 jets, where the liquid is discharged from a long tube of 120mm length and a diameter of 4mm. Obviously, it is expected that the jet experiences internal flashing. The velocities increase with increasing pressure and decreased going downstream the exit. An interesting initial observation concerned the velocity close to the exit. For 700kPa, the predicted liquid exit velocity is around 24m/s and as can be seen in Fig.2.25(a), the centerline velocity at 100mm from the exit reaches 61m/s. The predicted exit velocity for vapor is much greater, at 118m/s, and the results suggest that a strong interaction between vapor and liquid may be tending to equalize the velocities. As would be expected, velocity decay rates are significantly slower than for gas jets. At 100 diameters downstream, the jet still preserves 50% of their initial velocity. In Fig.2.25(b) the radial variation of axial velocity also indicates that a Gaussian profile was only achieved at ~ 60 – 70 diameters, which is three to four times longer than for gases. Far downstream the nozzle, the profiles are flatter and the velocities became uniform in the cross-section.

Allen [3] performed laser-based velocity measurements in two-phase flashing propane jets; centerline and lateral profiles of axial velocity are presented and compared with a
Chapter 2. Past studies in the literature and the proposed theories

(a) Axial velocity on centerline for Freon 11 spray field from release pipe 120mm long and 4mm diameter at various source pressures: triangle for 700kPa, square for 500kPa, circle for 400kPa, diamond for 300kPa.

(b) Axial velocity field for Freon 11 spray for single case of release length 120mm, diameter 4mm, source pressure 500kPa.

Figure 2.25: Velocity profiles obtained by Moodie and Ewan[86]
simple jet model. In the first part of the jet which is close to the nozzle exit, the velocity appears to increase slightly from the nozzle exit velocity, then is followed by an expected decrease in axial velocity with increasing downstream distance. Lateral profiles show Gaussian behaviour in their shape and self-similarity in a non-dimensionalized form (Fig.2.26).

![Figure 2.26: Radial velocity profile for propane spray from Allen[3].](image)

**2.5.1.5 Droplet size measurements from superheated jet atomisation**

Brown and York [21] investigated the superheat conditions required to produce an effective spray and the flashing of liquid jets (water and Freon 11 jets) were analyzed in terms of drop sizes, drop velocities and spray patterns at atmospheric conditions, employing different forms of nozzles. The bubble growth rate and the mean drop size were correlated with Weber number. An empirical correlation was found between the size of generated droplets and the rate of the bubble growth. The averaged drop size $D_{10}$ was found inversely proportional to the growth rate coefficient of the bubbles and decreases with increasing Weber number. They emphasized the importance of the homogeneous nucleation and the subsequent rate of bubble growth on drops sizes and spray angles. The related correlation will be given in the following section where the droplet size estimations will be discussed.

As explained in the previous section in detail, Reitz[112] observed that the core (center-line) breakup produces relatively large drops for the superheated jet. Nearer the spray edge the peak drop size is smaller confirming that the spray plume surrounding the core contained smaller droplets (Fig.2.27(a)). The measurements on the axis indicates that the core drop size has been reduces with downstream distance and with increasing
superheat (Fig. 2.27(b)). The radial evolution at a given axial distance from the nozzle shows smaller drops on the edge of the spray than those at the core (Fig. 2.27(c)).

![Figure 2.27: Line-of-sight drop size distributions (number percent) Reitz[112].](image1)

Park and Lee[101] measured the Sauter mean diameter of the droplets using a Malvern particle sizer. Their measurements confirm that the droplets are small at the edge and large in the centerline region but the difference becomes smaller at further downstream (Fig. 2.28(a)). This behaviour is commented as a further flashing of the superheated liquid in the core region outside the nozzle, but also as the lateral entrainment of small drops from the edge to the core region as the spray develops downstream. In other words, the Sauter Mean Diameter $SMD$ appears to become large at the edge and small at the centerline portion as the spray flows downstream because the small drops are pulled inward by the entrained air from the sides. Moreover, the drop sizes approach to be uniform and small when the injection temperature increases. Higher quantities of small drops are detected at the outer edge of the spray for both short and long nozzles at a fixed injection condition. With the increase of injection temperatures, the peak number-percent at the centerline shifts to the smaller drop sizes and resulted in smaller $SMD$ values (Fig. 2.28(c)).

The tests that Park and Lee[101] performed using a single hole nozzle to have a spray behaviour closer to the one produced by commercial nozzles showed that $SMD$ decreases with increased dimensionless superheat $\Delta T^*$ and with increased injection pressure for both short and long single hole nozzles (Fig. 2.29(a), 2.29(b)). The $SMD$ decrease is sharp with injection temperature at the low values of superheat until a certain value where a transition point is reached. Above that limit temperature, the decrease in $SMD$ is less sharp with further increase of superheat. Similar results have been reported by Nagai et al.[90], who explained the reason to be fully developed bubble generation at this transition point. This transition point may represent a condition for the flow regime transition from bubbly flow to slug or annular flow if the internal nozzle flow observations of Park and Lee[101] are referred. Another interesting point is that the pronounced effect of the injection pressure on $SMD$ at the low degree of superheat such that for smaller injections pressures larger $SMD$ are produced. But this effect...
2.5.1. Single-component superheated fluid systems

(a) Spatial distribution of SMD for long transparent nozzles

(b) Injection temperature effect on SMD distributions along the spray cross-section

(c) Injection temperature effect on mean SMD variations

Figure 2.28: SMD variations of the flashing jet in different configuration (for long transparent nozzle). Park and Lee[101]
lessens with the increase of the superheat. It is explained with the gain of importance of the aerodynamic breakup mode once the disintegration was initiated by other means (flashing) at the low superheat condition. The radial distributions for both long and short single hole nozzles (Fig.2.29(c),2.29(d)) confirm the observations obtained with the transparent nozzles such as the existence of small drops at the edges and large drops at the center region of the spray.

For Nagai et al.[90] the SMD variation with the dimensionless superheat appears as a single curve regardless of the injection pressure and the results are smaller than the one of Park and Lee.[101]. However, the results of Nagai et al. were averaged values through the cross-section and at a downstream distance 5 times greater than the one of Park and Lee.[101].

According to Moodie and Ewan[86], two regimes of evaporation behaviour are important for the superheated liquid releases. They involve the region immediately beyond the exit, followed by the development and the entrainment region. In the immediate region, there exists the newly emerged liquid, which can be highly superheated with respect to the local pressure. Liquid break-up due to aerodynamic forces and Rayleigh instabilities have been well documented. For high degrees of superheat, the liquid phase undergoes shattering till the associated evaporative temperature drop has sufficiently reduced the liquid temperature. Fig.2.30 shows the droplet size measurements for a number of Freon 11 releases. In these tests, two pipe lengths and two storage pressures are used. The figure displays the peak sizes $D_{peak}$ in the weight distributions versus the distance from the jet exit. The weight distributions are measured across the spray cross-sectional area for the corresponding axial distance. Smaller peak drop sizes $D_{peak}$ are found for higher storage pressures and hence liquid superheat at the exit, and this is consistent with more vigorous shattering. Moreover, an increase in peak drop size is observed when shorter pipes are used for the same pressure. The reason is obviously the different void fraction for the shorter lengths and thus different atomization behaviour. The size results and trends are consistent with reported literature values for water and in particular for Freon 11 (Brown and York[21]) at temperatures of 51°C and 67°C, where measured sizes were , respectively 55μm and 36μm. The change in measured peak sizes $D_{peak}$ with distance from the exit is most likely an evaporation event experienced faster by the smaller droplets, particularly around the jet edges.

Balachandar et al. [11] performed an experimental study on velocity and particle size measurement in a horizontal two-phase flashing jet generated using various nozzle geometries and reservoir test conditions. During the measurements, Phase Doppler Anemometry was used. The nozzle had 0.61mm diameter and a throat length of 6.1mm ($L/D = 10$). The pressure 4000kPa and temperature 230°C were the initial conditions. The vessel was pressurized with $N_2$ and connected with a pipe and nozzle. The flow was observed after the nozzle. He found that at a section closer to the nozzle the droplets are larger along the middle portions of the jet. However larger droplets are found to occur below the axis of the jet and the size distributions are clearly asymmetric. Visual observations indicate that the flashing jet is characterized by the formation of a nearly hemispherical expansion followed by linear spreading. The radial distributions of velocity and droplet size were measured at two axial distance $Z/D = 800$ and
2.5.1. Single-component superheated fluid systems

(a) SMD variation with degree of superheat (long single hole nozzle)
(b) SMD variation with degree of superheat (short single hole nozzle)

(c) on radial SMD distribution (long single hole nozzle)
(d) on radial SMD distribution (short single hole nozzle)

Figure 2.29: SMD variation with degree of superheat (single hole nozzle) (Park and Lee[101])

65
Figure 2.30: Variation of peak droplet sizes in weight distribution for Freon 11 jet spray field. Piper diameter is 4 mm. Triangle: 40mm length at 3 bar source pressure; Circle: 120mm at 3 bar source pressure; Square: 40mm length at 6 bar source pressure; Diamond: 120mm length at 6 bar source pressure. (Moodie and Ewan[86])

$Z/D = 2400$. The velocity distributions denote an increase in the spreading of the jet as the distance from the nozzle increases. The rate of the growth resembles that obtained in a nonsuperheated two-phase flow jet. The probability density distributions appear to closely follow a Gaussian curve. The flatness factor of velocity fluctuations is similar to the one’s in single phase flows. Deviations of the probability density distributions from a Gaussian curve are considerable towards the outer edges of the jet and these deviations are fairly similar at the two axial stations considered. As expected, the velocities decrease with increasing distance from the nozzle. Close to the nozzle the droplets are larger along the middle portions of the jet. He found also that at any axial station the mean diameter were generally larger below the axis and this could be considered because of the gravity. The value of the mean diameter decreases with increasing distance from the nozzle. He suggested that the usage of one mean diameter does not reflect the nature in reality due to the polydisperse nature of the flow. Count median diameter and volume median diameter is also important. As example, the sizes of the drops change from 1$\mu$m to 60$\mu$m. 50% of them are below 6.5 microns while 90% are below 20 microns.

Hervieu and Veneau[53] undertook measurements on an example of large-scale blow down of a LPG release addressing the flashing problem. They provided pressure, temperature evolutions in the vessel during blow down and droplet size-velocity evolutions of the two-phase jet with effects of nozzle diameter and initial pressure. The fluid utilized is of high purity (99.5%) liquid propane, released into atmospheric conditions. The release conditions such as the exit orifice diameter (2, 5, and 8mm) and the initial storage pressure ($\sim 500$, $\sim 1100$, $\sim 1700$ kPa) have been varied. The propane is stored under saturated conditions, so superheat would have varied as a function of storage
2.5.1. Single-component superheated fluid systems

pressure. Measurements were undertaken at 3 downstream axial locations, utilizing a PDA system as discussed earlier. It was found that whilst a decrease in droplet size was noted along the axis of the jet (due to evaporation) even closest to the nozzle, no droplets greater than 80μm were recorded. The effects of evaporation were also clearly noticeable in one set of radial droplet size measurements, as mean sizes decreased towards the edge of the jet. For ~500kPa releases at 60mm downstream, the measured SMD varied between 39 – 49μm for the 2mm and 5mm cases, respectively. At ~1100kPa at 60mm downstream, the droplet sizes reduced to 30 – 31μm for the two orifice sizes. These measurements indicate the very small influence of orifice size at the higher pressure, and the more significant dependence of droplet sizes on release pressure and superheat (these two effects cannot be decoupled from this series). At ~1700kPa, the SMD was below 30μm at all locations for the 2mm and 5mm orifices.

The drop size distributions in flashing propane jets are studied by Allen [4], as well. He used again non-intrusive optical measurements of droplet size distributions by use of a diffraction-based model technique; centerline and lateral profiles of droplet size are presented. He used a commercial Malvern particle sizer employing a line measurement technique based on diffraction theory. The results are highly repeatable for any position within the jet. Unusual shape of droplet size distribution is obtained as depicted in Fig.2.31. In the discussion of the drop size distribution, the data is divided into two size bands: 0 – 21.4μm and 21.4 – 41.2μm. These two bands exhibit opposite trends (Fig.2.31). The decrease in relative volume in one size band is explained by a real decrease in droplet number in that band, or maybe by an increase in the droplet number in the other band. A relative increase in volume in one band will also generate a decrease in the other. Data are compared with the data reported by Ewan and Moodie [38]. During their measurements on a two-phase flashing Freon-11 jet released from various nozzles, Ewan and Moodie observed that the $D_{peak}$ increases in an approximately linear manner until 700mm from the release point, then it remains constant until the end of the measurement range, 900mm. The propane droplet trends behave in a similar way. After 900mm the propane peak weight size would decrease. The reasoning of Ewan and Moodie is similar to Allen [4]: a large amount of small droplets is initially formed. They subsequently boil off. They disappear leading to an increase in the droplet size at which the weight distribution peak occurs. After a distance of 900mm, the large droplets form smaller droplets and then the peak weight size will then shift back towards the smaller sizes. The most important common feature is the change in droplet size distribution behaviour at 700mm as observed in Fig.2.31.

Peter et al. [102] conducted droplet size measurements by trapping droplets using silicone oil. All measurements were taken at a distance of about 150mm below the nozzle outlet at the central axis of the jet. They studied the effect of nozzle type, inlet water temperature and surrounding pressure. The percentage number of the small droplets along the central axis increases with the increase in superheat at constant pressure or inlet water temperature. Mean droplet sizes change with the change in the surrounding pressure at constant superheat and decreases with the increase in the superheat degree at constant surrounding pressure.
Chapter 2. Past studies in the literature and the proposed theories

Figure 2.31: Droplet size evolutions according to Allen[4]
2.5.2 Superheated binary fluid systems

There have also been studies performed on superheated binary fluids systems. The main concern of the present thesis study is to understand the flashing atomization of the single component liquid jets, therefore, only a brief summary will be given about the studies on superheated binary fluid atomization.

Sher and Elata [123] developed a physically based correlation between the droplet median diameter and the thermodynamic properties of the binary fluid system. Their model correlated the energy stored by the droplets as surface energy and the energy of the bubbles just before bursting and suggested the average drop size is linearly proportional to the liquid surface tension and inversely proportional to the superheat degree raised by power of 4.

Later on, Zeigerson-Katz and Sher [124, 150] presented an attempt to correlate the Sauter mean diameter with the thermodynamic properties of a binary mixture at initial conditions, by means of the process efficiency interpretation and the “available energy” theorem. Two limiting cases were considered, an isothermal process and an adiabatic process. The results of the adiabatic model were found to be more realistic and better fit the experimental observations. In this model, ideal mixture behaviour has been considered. It was found that the Sauter mean diameter decreases when the mole fraction of the propellant and initial temperature of the binary liquid increase. The final temperature depends also on the mass fraction of the propellant and initial temperature of the binary mixture. It is assumed that during the flashing process, a fraction of the propellant evaporates and cools down the liquid phase (the droplets). At final conditions, the liquid temperature approaches the saturation temperature of the propellant, which corresponds to its partial pressure. It is expected that a higher propellant content within the reservoir would therefore result in lower final temperature. Keeping the molar fraction of the propellant constant, changing initial temperature of the mixture, it was observed that the higher was the initial temperature of the mixture the higher was the final temperature of the droplets. It was seen numerically as well as experimentally that the Sauter mean diameter increases as the superheat degree increases. The superheat degree was defined as the difference between the initial temperature, \( T_i \), and the saturated temperature of the propellant at the initial pressure, \( T_{\text{sat}}(P_i) \). The explanation may be as follows. Decreasing the propellant initial mass fraction (which is associated with a larger Sauter mean diameter) results in decreasing both the initial pressure, \( P_i \), and the corresponding saturation temperature, \( T_{\text{sat}}(P_i) \). The temperature difference, \( [T_i - T_{\text{sat}}(P_i)] \), represented by the degree of superheat is therefore increased.

In a later study, Zeigerson-Katz and Sher [151] investigated experimentally the effect of the injection system design on the spray characteristics. They found that the important stage of nucleation occurs at the metering orifice and apparently the main duct does not play any significant role in the bubbles nucleation. Moreover, neither the expansion chamber diameter nor its length has any significant effect on the droplet Sauter mean diameter. They found that an orifice's diameter ratio (metering to discharge) between 0.6 and 0.9 results in a minimum Sauter mean diameter. In this study, to include
the effect of the initial concentration of the propellant, the Sauter mean diameter was correlated with a modified Jakob number of the form of

\[ J^*_n = \chi_0 (\sigma_0 \Delta T / h_f) \]

(2.35)

where \( \chi_0 \) is the molar fraction of the propellant (\( \beta \)) in the pressurized container, \( \Delta T \) is the superheat degree, which is defined as the difference between the mixture temperature and the propellant saturation temperature, at initial conditions and it was observed that the isotherms coincided to a single curve.

Recently, Gemci et al. [47] conducted a study on the flash atomization of a hydrocarbon solution containing n-hexadecane and n-butane. Nitrogen was used as propellant gas. They studied the break up patterns and spray characteristics with the effect of superheat, butane concentration and nitrogen flow rate.

### 2.6 Droplet size estimations

In liquid spraying primary breakup is thought to occur first along the free surface of the jet owing to the interaction with the surrounding gas (i.e. aerodynamic effects). Secondary breakup is further exhibited following primary breakup and may be described as further breakup of fluid ligaments or individual droplets into smaller droplets experiencing aerodynamic interaction with the surrounding gas. Although the breakup evolution is argued to be difficult to treat analytically, some semi-empirical success has apparently been achieved.

The following correlation has been reported in the literature review of Witlox and Bowen [145] and is given by

\[ \frac{D_{32}}{d_o} = 82.23 C^{0.64} W e^{-0.07} R e^{-0.5} \]

(2.36)

where \( D_{32} \) is the Sauter mean diameter, \( d_o \) is the orifice diameter, \( C \) is the discharge coefficient, \( W e \) is the Weber number based on orifice velocity and diameter, \( R e \) is the Reynolds number based on orifice velocity and diameter, and 82.23 is an experimentally determined coefficient. At first glance it would appear that all of the relevant parameters are included, however this is not the case, since neither the effect of surrounding gas nor spray angle are included. It is not entirely clear what effect spray angle imparts and whether it is due solely to nozzle hydraulics for certain conditions. It would intuitively seem that spray angle should be dependent largely on nozzle type and hence should be associated with nozzle hydraulics.

Considerable work has been done at the VKI with regard to liquid sprays. Buchlin [22, 20] gives an overview of liquid spray dynamics involving the nature of momentum
2.6. Droplet size estimations

transfer between the spray and surrounding gas and parameters that are relevant. The results suggest a correlation that agrees well with experimental data given as

\[
\frac{D_{32}}{d_o} = C[We \sin\left(\frac{\theta}{2}\right)]^{-1/3}\left(\frac{\rho_l}{\rho_g}\right)^{1/6}
\]

(2.37)

where \(We\) is again based on orifice velocity and diameter. It would appear in this case that most of the relevant parameters have been included, in particular this correlation, includes effects associated with surrounding gas density and nozzle hydraulics represented as experimentally determined \(C\) and spray angle, \(\theta\). However, this correlation does not allow for effects associated with radially non-uniform droplet size distributions nor does it include the effect of distance from the nozzle. In this case it would seem that it is applicable only for spray regions of spatially uniform size distributions.

Plain-orifices lead to the atomization which is similar to the mechanical break up of a cylindrical jet. Table 2.4 gives equations for plain-orifice atomizers which apply strictly to the injection of liquids into quiescent air [70].

<table>
<thead>
<tr>
<th>Investigation</th>
<th>Equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Merrington &amp; Richardson</td>
<td>(SMD = \frac{560d_0^{1.2}U_{ul}^{0.2}}{U_L})</td>
</tr>
<tr>
<td>Panasenko</td>
<td>(MMD = 6d_oRe_L^{-0.15})</td>
</tr>
<tr>
<td>Harmon</td>
<td>(SMD = 3330d_0^{0.3}\mu_L^{0.07}\rho_L^{-0.648}\sigma^{-0.15}U_{ul}^{-0.15}\mu_G^{0.78}\rho_G^{-0.052})</td>
</tr>
<tr>
<td>Miesse</td>
<td>(D_{0.999} = d_oWe_L^{-0.333}(23.5 + 0.000395Re_L))</td>
</tr>
<tr>
<td>Tanasawa and Toyoda</td>
<td>(SMD = 47d_0U_{ul}^{-1}\left(\frac{\sigma}{\rho_G}\right)^{0.25}\left[1 + 331\frac{\rho_l}{(\rho_Ld_0)^{0.03}}\right])</td>
</tr>
<tr>
<td>Hiroyasu and Katoda</td>
<td>(SMD = 2330\rho_L^{1.21}Q^{0.131}\Delta P^{-0.135})</td>
</tr>
<tr>
<td>Elkobt</td>
<td>(SMD = 3.08\rho_L^{0.385}(\sigma \rho_L)^{0.737}\rho_A^{0.06}\Delta P_L^{-0.54})</td>
</tr>
</tbody>
</table>

Table 2.4: Drop size equations for plain-orifice atomizers taken from [70].

However, as explained clearly in the previous sections of this Chapter, flashing jets are subjected to dense nucleation, bubble growths which results in an explosive shattering. In this sense, similarities could be found between the flashing jets and the sprays created by effervescent atomizers. Air-to-liquid mass ratio is the dominant variable governing effervescent spray characteristics, which may play an analogous role to the void fraction at the orifice exit for flashing jets.

For the flashing releases, droplet size estimations have been an important concern for
the numerical simulations and risk assessment studies. Several empirical correlations have been found. They are listed in Tab 2.5.
### Table 2.5: Drop size estimations for flashing jets.

<table>
<thead>
<tr>
<th>Investigation</th>
<th>Equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Brown &amp; York</td>
<td>( D_{10} = \frac{[1840 - 5.18T(\text{°F})]}{We} )</td>
</tr>
<tr>
<td>Tilton &amp; Farley</td>
<td>( D_{\text{init}} \approx 5 \times 10^{-4} \left( \frac{\rho_0}{\rho_w} \right) )</td>
</tr>
<tr>
<td>Koestel et al.</td>
<td>for heterogeneous nucleation:</td>
</tr>
<tr>
<td></td>
<td>( D_{\text{drop}} = \left( 1 - \frac{C_L(T_e - T)^2}{\Delta h} \right) \left( \frac{g}{\pi N} \right) \left( \frac{1345}{\sqrt{T} C} \right)^{2/3} )</td>
</tr>
<tr>
<td></td>
<td>( T: ) fragmentation temperature</td>
</tr>
<tr>
<td></td>
<td>( C = 0.1 - 0.003We ) for ( We &lt; 12.5 )</td>
</tr>
<tr>
<td></td>
<td>( C = 0.058 - 0.0021We ) for ( We &gt; 12.5 )</td>
</tr>
<tr>
<td></td>
<td>for homogeneous nucleation:</td>
</tr>
<tr>
<td></td>
<td>( D_{\text{drop}} = \left( 1 - \frac{C_L(T_e - T)^2}{\Delta h} \right) \left( \frac{g}{\pi N} \right) \left( \frac{\sqrt{\rho_0 L}}{C} \right)^{2/3} )</td>
</tr>
<tr>
<td>Woodward &amp; Papadourakis</td>
<td>( D_{\text{Drop}} = 722 - 57.33 \ln E_p ) (( \mu m ))</td>
</tr>
<tr>
<td></td>
<td>where ( E_p = -\Delta h \bigg</td>
</tr>
<tr>
<td>Nagai et al.</td>
<td>( SMD = 36.8 (\Delta T_{sh}^*)^{-2.58} ) ( \mu m )</td>
</tr>
<tr>
<td></td>
<td>for ( l/D &lt; 7 ) and ( 0.55 &lt; \Delta T_{sh}^* &lt; 1.0 )</td>
</tr>
<tr>
<td></td>
<td>( SMD = 70.4 \left[ -1 + 0.14 \left( \frac{L}{d_o} \right) \right]^{-0.22} \left( \frac{d_o}{0.72} \right)^{0.38} ) ( \mu m ),</td>
</tr>
<tr>
<td></td>
<td>for ( l/D &gt; 7.8 ) and ( 0 &lt; \Delta T_{sh}^* &lt; 0.55 )</td>
</tr>
<tr>
<td></td>
<td>( SMD = 39.1 \left[ -1 + 0.14 \left( \frac{L}{d_o} \right) \right]^{-0.22} \left( \frac{d_o}{0.72} \right)^{0.38} ) ( \mu m ),</td>
</tr>
<tr>
<td></td>
<td>for ( l/D &gt; 7.8 ) and ( 0.55 &lt; \Delta T_{sh}^* &lt; 1.0 )</td>
</tr>
</tbody>
</table>
Chapter 3

Experimental installation and measurement techniques

In this chapter, the main characteristics of the experimental installations that are used throughout the study are described. Details of the measurement techniques that are utilized to investigate the flashing atomization of a superheated liquid jet are given. Finally, the test cases that describe the different initial flow conditions are explained for different measurement techniques.

3.1 Choice of the testing material

As it was explained in Section 2.4, flashing phenomena occurs when a liquid finds itself suddenly in a condition where its temperature is higher than its saturation temperature at the final surrounding pressure. Therefore, a material that can be easily superheated without advanced technology and energy requirements while exposed to the ambient pressure has been looked for. The first choice could have been propane or butane since these materials have been heavily used under high pressure and liquified conditions and are highly prone to flashing risk in case of an accident. However, another concern in the choice of the material has been about the safety of this material in terms of flammability and human health. This concept left the usage of propane and butane out of question. Materials with similar thermodynamical behaviours and molecular type were sought in the family of refrigeration liquids.

The refrigeration fluids involve a large range of products and are divided into three sections such as chlorofluorocarbons (CFC)'s, hydrochlorofluorocarbons (HCFC)'s and hydrofluorocarbons (HFC)'s. Table 3.1 gives a short list of the existing refrigeration liquids. It has to be kept in mind that the usage of the CFC's has been prohibited by
Chapter 3. Experimental installation and measurement techniques

laws due to its effects on the ozone layer and their toxicity. Most of the HFCF’s show high toxicity and flammability, as well. Therefore, it is more preferable to utilize the substitution materials HFC’s. As it can be seen from the Table 3.1, some of the substitutes are mixtures of several other refrigerant liquids with different thermodynamical characteristics. Not to complicate the interpretation of the thermodynamics behaviour, a single component refrigerant liquid is sought for the flashing atomization and this material has ended up to be R-134A (1,1,1,2 – Tetrafluoroethane: $CF_3 — CH_2 F$). R-134A has a saturation temperature of $-26.4^\circ C$ at atmospheric conditions. Being stored in liquid phase within commercial bottles at the saturation conditions at $25^\circ C$ with a pressure of $663kPa$, this material can provide easily a superheat of $51.4^\circ C$. The detailed thermodynamical and physical characteristics of R-134A are reported in the Appendix.

Table 3.1: Examples of refrigerant liquids

<table>
<thead>
<tr>
<th>CFC</th>
<th>HCFC (Transition)</th>
<th>HFC (Substitution)</th>
</tr>
</thead>
<tbody>
<tr>
<td>R-11</td>
<td>R-123/R-141B</td>
<td>***</td>
</tr>
<tr>
<td>R-12</td>
<td>R-22/FX56(R-409A)</td>
<td>R-134A</td>
</tr>
<tr>
<td>(R-500)</td>
<td>R-22/FX57</td>
<td>R-134A</td>
</tr>
<tr>
<td></td>
<td>R-22</td>
<td>R-407C/R-134A/R-410A</td>
</tr>
<tr>
<td></td>
<td>R-22</td>
<td>R-134A</td>
</tr>
<tr>
<td>R-13(R503)</td>
<td>R-23</td>
<td>R-23/R-508B</td>
</tr>
<tr>
<td>R-113</td>
<td>R-123/R-141B</td>
<td>***</td>
</tr>
<tr>
<td>R-114</td>
<td>R-124</td>
<td>***</td>
</tr>
<tr>
<td>R-115(R502)</td>
<td>FX10 (R-408A)</td>
<td>R-404A/R-507</td>
</tr>
<tr>
<td>R-12B1</td>
<td></td>
<td></td>
</tr>
<tr>
<td>R-13B1</td>
<td>R-23/FX80</td>
<td></td>
</tr>
</tbody>
</table>

3.2 Test facilities

Two types of test facilities have been used: a very simple installation for the preliminary measurements and a final test apparatus for the investigation of the flow. This section deals with the related equipment presentation.

3.2.1 Experimental apparatus for the preliminary measurements

The preliminary experimental investigation aims the feasibility assessment of the non-intrusive laser based techniques applications such as Phase Doppler Anemometry (PDA) and Particle Image Velocimetry (PIV) on a horizontal flashing two-phase R-134A jet. The flow is initiated from a nozzle with a diameter of $D=5mm$ connected to a standard commercial R-134A bottle (20 liters) To compare flows from bottle to bottle with identical initial conditions, the pressure was tracked through a pressure transducer implemented on the nozzle.
3.2.2 Final test facility

The preliminary measurements pointed out the need to control rigorously the initial conditions of the tests such as temperature and pressure. The new experimental installation is designed in such a way that the control over the initial temperature and pressure is assessed and that any evolution could be tracked. To understand better the influence of the storage conditions on the flashing behaviour, it is preferred to design the set-up so that the depressurized material (R-134A) exits the orifice in liquid phase and flashes downstream.

Fig. 3.1 sketches the experimental installation. It allows the pressurization of liquid R-134A under different pressure values. The pressurized liquefied R-134A is stored at a pressure above its vapor pressure at ambient temperature (i.e. 663kPa at 25°C). Nitrogen gas is introduced into the R-134A tank to adjust the driving pressure. A pressure transducer monitors the pressure history in the tube connecting the R-134A and N₂ tanks. At the connection with the R-134A tank, a two-entrance valve allows pressurization with N₂(in) and flow of R-134A (out) simultaneously. The liquid R-134A flows from the tank through a horizontal tube. At the end of this tube, a pneumatic ball valve system is installed and it is operated using compressed air. A thermocouple is introduced in the tube to measure the temperature of the liquid R134-A before the exposure to the ambient. Different nozzle geometries can be mounted on the pneumatic valve.

![Figure 3.1: The experimental facility](image-url)
Fig. 3.2 shows general view of the experimental installation where droplet size, velocity and temperature measurements are performed simultaneously.

![Figure 3.2: A view of the experimental installation when the PDA, thermocouples and displacement system are installed](image)

3.2.2.1 Test nozzles

For the breakup pattern observations, droplet size and velocity measurements 7 different nozzles are tested. Four of them are sharp edged diaphragm type orifices with very little thickness to limit the solid surface contact during depressurization but with different diameter as $D_{\text{nozzle}} = 1, 2, 3$ and $4\text{mm}$. The other three nozzles have the same diameter ($D_{\text{nozzle}} = 2\text{mm}$) but different lengths ($l_{\text{nozzle}} = 4, 14, 30\text{mm}$) to observe the effect of the length-to-diameter ($l/D$) ratios.

Figures 3.3(a), 3.3(b), 3.3(c) and 3.3(d) represent the technical details for each of the sharp edged orifices. These nozzles have an insignificant length-to-diameter ratio, which was represented as $l/D = 0$ for simplicity. The most important observation is that these nozzles are not strictly geometrically similar due the fact that the surface curvature upstream of the nozzle exit is fixed for all orifice diameters. Thus, the exit angle is relaxed with increasing orifice diameter. This lack of geometric similarity should be considered in comparing the hydrodynamic behavior between nozzles. In particular it may be expected that contraction is slightly relaxed for the $4\text{mm}$ nozzle as compared with the $1\text{mm}$ nozzle. Such relaxation may significantly affect issuing jet diameter and hence jet velocity. The technical details of the nozzles of $2\text{mm}$ in diameter and with $l/D = 2, 7$ and $l/D = 15$ are given in Fig.3.3(e),3.3(f) and Fig.3.4, respectively.

78
3.2.2. Final test facility

Figure 3.3: Design details of the nozzles
3.2.2.2 Pressure Transducer

The drive pressure is measured using a 0 - 2 MPa differential pressure transducer and 0 - 10 V digital voltmeter with a resolution of 0.01 V. The span of the demodulator is scaled to provide 200 kPa/volt. The transducer is calibrated over the full range of 0 - 2 MPa prior to the experiments using a DIMED Digital Pressure Indicator (PDI-601) with a resolution of 0.1 kPa.

The uncertainty associated with all of the static pressure measurements that represent the drive pressure is estimated from the calibration results assuming no bias in the Digital Pressure Indicator, which is used as the standard. The DPI has a pressure resolution of 0.001 MPa and the pressure transducer voltage indicator has a resolution of 0.01 V. Then the uncertainty in the pressure measurements may be obtained from the calibration coefficients given here as

\[ P_d = aE + b; a = 2.02, b = 0.065 \] (3.1)

where \( E \) is the measured voltage with \( \delta E = 0.01 V \), \( P_d \) is the measured pressure with \( \delta P_d = 0.001 MPa \), and \( a \) and \( b \) are the regression coefficients. The uncertainty in the regression coefficients may be estimated from the uncertainty in voltage and pressure calibration measurements as \( \delta a = 0.0005 MPa/V \) and \( \delta b = 0.0021 MPa \). Then the uncertainty of the drive pressure measurements is given by

\[ \delta P_d = \sqrt{(\delta a)^2 + (\delta bE)^2 + (b\delta E)^2} \] (3.2)
Thus, the uncertainty for the maximum pressure measurements \( P_d = 1200 kPa \) is obtained as \( \delta P_d = 0.02 kPa \).

### 3.2.2.3 Thermocouple measurements inside the discharge tube

A Chromel/Alumel thermocouple is placed inside the discharge tube. The calibration curve is given in Fig. 3.5. The calibration is performed by placing the thermocouple in an isolated liquid bath where the liquid temperature can be adjusted. The liquid bath temperature indicator has a temperature resolution of 0.1°C. The home-made thermocouple amplification instrument has resolution of 0.1°C, as well. If the uncertainty analysis of the thermocouple measurements is done as for the pressure transducer, the maximum uncertainty in temperature measurements is 0.1°C.

![Figure 3.5: Calibration curve of the thermocouple used for liquid storage temperature](image)

### 3.3 Measurement Techniques and Strategies

In this section, the various measurement techniques that have been involved in the atomization investigation of the superheated R-134A liquid jets are discussed. They are high-speed imaging to observe the breakup pattern, laser based techniques such as Phase Doppler Anemometry (PDA) and Particle Image Velocimetry (PIV) to measure the droplet sizes and velocities of the disintegrated jet, non-intrusive temperature measurements using Infrared Thermography to measure the surface temperature evolution of the non-shattered liquid jet and intrusive measurements for the point-wise
temperature evolution of the evaporating spray.

### 3.3.1 High-speed imaging

A high speed camera system namely "Phantom v7" from Photosonics is used for the imaging of the jet behaviour once it is discharged in the ambient atmosphere. The Phantom v7 camera systems can record up to 4800 pictures per second using the full 800x600 pixel SR-CMOS imaging sensor array. The operator may also specify other aspect ratios to increase recording speeds or extend recording times. For example, using a resolution of 512x128 pixels can give a frame rate of 29600 frames/seconds. The exposure time is variable and independent of the frame rates. It can be reduced to 2\( \mu \)s.

The high speed imaging campaign helps to understand the break up process of the two-phase flashing jet. The change in the flow patterns is observed modifying parameters such as nozzle diameter, superheat and backpressure. The test conditions are presented in Table 3.2. The backpressure or driving pressure is the storage pressure of the liquid R-134A in the vessel and the superheat is defined as the difference between the initial liquid temperature and the saturation temperature at the final pressure (atmospheric pressure).

### 3.3.2 Non-intrusive Laser-based techniques

#### 3.3.2.1 Phase Doppler Anemometry (PDA)

The aim of this section is not to present the state of art of PDA technique, but rather a short description of the working principles. Very detailed reviews could be found in Albrecht et al. [2] and Bachalo [9, 10].

This technique allows the simultaneous measurements of the 1-component velocity and the size of the particles, in the present case, of the droplets. The operational principle of this non-intrusive technique is measuring the scattered light by a particle. The system is composed of a transmitter and a receiver. The transmitter is a standard system of a 10mW HeNe laser light source followed by a combination of beam expander and diffraction grating to split the beam. The initial beam is splitted in two laser beams and their crossing point can be called as the measurement point or probe volume. The diffraction grating is rotated at precisely controlled rates to provide frequency shifting for measurements in complex sprays. The receiver consists of a 108mm diameter f/5 lens located at 30° to the plane of the transmitted beams to collect the light scattered from the drops moving through the probe volume. Three detectors are used in the receiver to eliminate measurement ambiguity, add redundant measurements to increase reliability, and provide high sensitivity over a high size range. Their juxtaposition is such that Doppler burst signals are produced by each detector but with a relative...
Table 3.2: Test conditions for high-speed imaging

<table>
<thead>
<tr>
<th>Exp no</th>
<th>Nozzle diameter (mm)</th>
<th>$l/D$</th>
<th>$T_{\text{liquid}}$ (°C)</th>
<th>Superheat (°C)</th>
<th>Back pressure (KPa)</th>
<th>Frame rate (Images/sec)</th>
<th>Image size (px²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>0</td>
<td>14</td>
<td>40.4</td>
<td>820</td>
<td>32700</td>
<td>480x120</td>
</tr>
<tr>
<td>2</td>
<td>2</td>
<td>0</td>
<td>14</td>
<td>40.4</td>
<td>820</td>
<td>38400</td>
<td>608x80</td>
</tr>
<tr>
<td>3</td>
<td>4</td>
<td>0</td>
<td>14.1</td>
<td>40.5</td>
<td>820</td>
<td>33300</td>
<td>256x200</td>
</tr>
<tr>
<td>4</td>
<td>1</td>
<td>0</td>
<td>18.5</td>
<td>44.9</td>
<td>942</td>
<td>40000</td>
<td>512x64</td>
</tr>
<tr>
<td>5</td>
<td>1</td>
<td>0</td>
<td>20.4</td>
<td>46.8</td>
<td>886</td>
<td>40000</td>
<td>512x64</td>
</tr>
<tr>
<td>6</td>
<td>2</td>
<td>0</td>
<td>18.2</td>
<td>44.6</td>
<td>886</td>
<td>40000</td>
<td>512x64</td>
</tr>
<tr>
<td>7</td>
<td>2</td>
<td>0</td>
<td>20.2</td>
<td>46.6</td>
<td>886</td>
<td>40000</td>
<td>512x64</td>
</tr>
<tr>
<td>8</td>
<td>1</td>
<td>0</td>
<td>9.5</td>
<td>35.9</td>
<td>608</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>9</td>
<td>2</td>
<td>0</td>
<td>9.5</td>
<td>35.9</td>
<td>598</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>10</td>
<td>3</td>
<td>0</td>
<td>10.8</td>
<td>37.2</td>
<td>592</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>11</td>
<td>4</td>
<td>0</td>
<td>11.7</td>
<td>38.1</td>
<td>560</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>12</td>
<td>2</td>
<td>2</td>
<td>12.5</td>
<td>38.9</td>
<td>580</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>13</td>
<td>2</td>
<td>7</td>
<td>12.5</td>
<td>38.9</td>
<td>586</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>14</td>
<td>1</td>
<td>0</td>
<td>13.5</td>
<td>39.9</td>
<td>864</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>15</td>
<td>2</td>
<td>0</td>
<td>12.8</td>
<td>39.3</td>
<td>864</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>16</td>
<td>3</td>
<td>0</td>
<td>13.4</td>
<td>39.8</td>
<td>862</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>17</td>
<td>4</td>
<td>0</td>
<td>13</td>
<td>39.4</td>
<td>842</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>18</td>
<td>2</td>
<td>2</td>
<td>12.8</td>
<td>39.2</td>
<td>864</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>19</td>
<td>2</td>
<td>7</td>
<td>12.5</td>
<td>38.9</td>
<td>862</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>20</td>
<td>1</td>
<td>0</td>
<td>12.6</td>
<td>39.0</td>
<td>1260</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>21</td>
<td>2</td>
<td>0</td>
<td>12.8</td>
<td>39.2</td>
<td>1226</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>22</td>
<td>3</td>
<td>0</td>
<td>12.2</td>
<td>38.6</td>
<td>1232</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>23</td>
<td>4</td>
<td>0</td>
<td>12.8</td>
<td>39.2</td>
<td>1266</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>24</td>
<td>2</td>
<td>2</td>
<td>12.6</td>
<td>39.0</td>
<td>1252</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>25</td>
<td>2</td>
<td>7</td>
<td>12.5</td>
<td>38.9</td>
<td>1272</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>26</td>
<td>1</td>
<td>0</td>
<td>20.1</td>
<td>46.5</td>
<td>828</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>27</td>
<td>2</td>
<td>0</td>
<td>20.5</td>
<td>46.9</td>
<td>846</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>28</td>
<td>3</td>
<td>0</td>
<td>19.4</td>
<td>45.8</td>
<td>824</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>29</td>
<td>2</td>
<td>2</td>
<td>20.6</td>
<td>47</td>
<td>846</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>30</td>
<td>2</td>
<td>7</td>
<td>20.5</td>
<td>46.9</td>
<td>824</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>31</td>
<td>1</td>
<td>0</td>
<td>19.5</td>
<td>45.9</td>
<td>1224</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>32</td>
<td>2</td>
<td>2</td>
<td>20.4</td>
<td>46.8</td>
<td>1228</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>33</td>
<td>3</td>
<td>0</td>
<td>20</td>
<td>46.4</td>
<td>1228</td>
<td>43000</td>
<td>512x80</td>
</tr>
<tr>
<td>34</td>
<td>2</td>
<td>2</td>
<td>20.2</td>
<td>46.6</td>
<td>1236</td>
<td>49000</td>
<td>512x80</td>
</tr>
<tr>
<td>35</td>
<td>2</td>
<td>7</td>
<td>20.5</td>
<td>46.9</td>
<td>1232</td>
<td>43000</td>
<td>512x80</td>
</tr>
</tbody>
</table>

Phase shift. This phase shift is linearly related to the drop size. Signals from the photodetectors are amplified and transferred to the signal processor. Spherical particles ranging in diameter from 0.5 μm to 3000 μm can be measured over a dynamic size range of 100 with a single optical setting. Simultaneously, particle velocities up to 100 m/s or more can be measured depending on the particle size range and optical set-up.
Before taking the axial profile, the optimum values of the three PDA parameters (diameter range, velocity range and photo-multiplier high voltage) have to be selected. This choice influences strongly the measurement efficiency and the quality of the results. As a matter of fact, when processing the data, the PDA software may validate or reject events based on some criteria as droplet asphericity, range limits exceeding or signal saturation. To determine the optimal combination of these parameters, a parametric investigation has been undertaken. For this purpose, preliminary runs were performed aiming to cover the diameter and velocity ranges of the whole droplet population with the highest acquisitions (events/s) and validation rates. For transparent particles, light scatter at 30° in the forward direction is dominated by refraction.

3.3.2.2 Particle Image Velocimetry (PIV)

Particle Image Velocimetry (PIV) is based on the measurement of the velocity of tracer particles carried by the fluid. However, rather than concentrating light in a small probe volume as in Laser Doppler Velocimetry (LDV) or Phase Doppler Anemometry (PDA), a complete plane of the flow under investigation is illuminated. This is performed by creating a narrow light sheet which is spread over the region of interest. Tracer particles are therefore made visible and images of the illuminated particles are recorded. These recordings contain either successive images of the single tracers in time or successive frames of instantaneous images of the whole flow field. The displacement of the tracer is then determined through the analysis of these records. The instantaneous velocity of the fluid is then deduced from the determination of the displacements of the tracer particles illuminated by the sheet of the light.

The PIV measurement chain is composed of:

1. A double pulse YAG laser (wavelength 532nm and pulse duration of 5ns) firing up to 200mJ per pulse at a frequency \( f_{YAG} = 8.2\,Hz \) for our experiments; the laser sheet of about 0.27mm thickness illuminates a meridian plane of the flow
2. A signal generator “Agilent” which was used as a ‘master’ to generate signals,
3. A multi-channel delay generator of Stanford type which was triggered externally,
4. A PCO fan-cooled 12-bit (4096 grey levels) digital camera, having a full field resolution of \((1280\times1024px^2)\) acquiring images from an angle of 90° with the laser
5. A PC with frame grabber and acquisition program Sensicam allowing to acquire series of 25 pairs of images at a frequency equal to the half of the frequency of the YAG laser: \( f_{acc} = 4.1Hz \)

No seeding is needed for the measurements performed on the flashing jet since the velocity of the R134-A droplets of the jet is measured. Water droplets are also created by the cooling effect of the jet on the co-flowing ambient air.
3.3.2. Non-intrusive Laser-based techniques

The minimum $\Delta t$, between two frames of the PCO camera is 290ns. The integration time of frame 1 is 155$\mu$s (time for the charging of the pockel cells of the laser), while the integration time for the frame 2 is fixed. With these camera characteristics, using only a Stanford type delay generator allows a minimum pulse separation of 2$\mu$s. To go below the pulse separation that the Stanford type delay generator can produce, a signal generator of "Agilent" type is used. This equipment is employed as master to produce a signal that is sent to both the camera and the "Stanford". This configuration allows adjusting a minimum pulse separation of 0.5$\mu$s. To check the conditions in which both equipment should work, the output signals coming from the signal generator and the "Stanford" are firstly connected to an oscilloscope. Fig. 3.6 shows the sketch of the synchronization system and Table 3.3 gives the settings of the signal generator and Stanford box.

<table>
<thead>
<tr>
<th>&quot;Agilent&quot; Signal Generator</th>
<th>&quot;Stanford&quot;</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carrier wave : squared</td>
<td>Trigger: external</td>
</tr>
<tr>
<td>Carrier frequency : 3.00kHz</td>
<td>$A = T + 0.0000181$ sec</td>
</tr>
<tr>
<td>Amplitude: 2 Volts (peak to peak)</td>
<td>($T$ is the signal received from the signal generator)</td>
</tr>
<tr>
<td>Offset : 1 Volt</td>
<td>$B = A + 0.000155$ sec</td>
</tr>
<tr>
<td>Burst rate: 8.2 Hz</td>
<td>$C = A + \Delta t$</td>
</tr>
<tr>
<td>Burst count: 1 cycle</td>
<td>$D = C + 0.000155$ sec</td>
</tr>
<tr>
<td>Burst phase: 0</td>
<td></td>
</tr>
</tbody>
</table>

Table 3.3: Settings of the "Stanford" and the "Agilent" Signal Generator

Figure 3.6: The synchronization mechanism
Chapter 3. Experimental installation and measurement techniques

The PCO camera is working in double short mode, with a frequency of $4.12 \text{Hz}$. Full resolution of 1280 pixels in the horizontal direction and 1024 pixels in the vertical direction is chosen. A 20 mm ring and 35 mm objective are added to the camera. Special attention is given to the optical systems (lenses and cameras) to avoid distortions and aberrations. The camera is placed at $90^\circ$ orthogonal to the laser sheet that cuts the jet along the centerline. Good care is taken to minimize variations in the image intensity distribution between the PIV image pairs to obtain trustable correlation in Multi-intensity treatment of the image pairs. The image number is kept as 25 per case for the assumption of having the same initial conditions at a time (i.e. about 6 seconds of measurements after a transient time before starting the acquisition of about 1-2 seconds compared to ~ 5 minutes to empty the bottle). The spatial calibration gives a length of $1.6 \times 10^{-6} \text{m/px}$ for all regions.

The digital PIV images are processed using the cross-correlation algorithm WIDIM (Window Displacement Iterative Multigrid) developed at VKI by Scarano et al. [117]. This algorithm is based on an iterative multigrid predictor-corrector approach, where the displacement field obtained on a coarse grid at the step $i$ is used as a predictor for the computation of the corrected displacement field on a finer grid at the step $i+1$. This allows to partially get rid of the coupling resolution/accuracy that would otherwise limit the spatial resolution of the measurement. Furthermore, the sub-pixel displacement of the interrogation window allows minimizing the peak-locking phenomena. Finally, the interrogation windows are deformed to account for the velocity gradient.

The number of refinement steps and therefore, the size of final window and the number of vectors determined, depend on the tracer particle (here ligaments and droplets) density. In the processing of the images, an initial window size of 128 x 128 pixels is selected for all the measurement locations.

3.3.2.3 Measurements performed with the laser-based techniques on the preliminary test facility

Since a simple commercial bottle is used as liquid source in the preliminary measurements and since the depressurization is initiated by simply opening the valve, the flow does not retain its initial conditions in the bottle such as pressure and temperature for a long while leading to difficulties when comparison of the different techniques is concerned. It is the reason why the number of samples and the associated measurement duration are kept limited presuming almost no change in the initial conditions. It is comprehended that the limitation in testing time leads to an insufficient number of samples to give a good interpretation concerning the physics of the flow (mean velocity, mean droplet diameter etc). In spite of that, the test provide sufficient information to explore the measurement problems and reproduce patterns of flashing flows.

To determine the optimal combination of the PDA settings, a parametric investigation has been carried out. For this purpose, preliminary runs are performed aiming to cover the diameter and velocity ranges of the whole droplet population with the highest acquisitions (events/s) and validation rates. During the measurements a wide
3.3.2. Non-Intrusive Laser-based techniques

droplet range spreading from 21.5\mu m to 753.3\mu m is observed. Considering that the large droplets form an important part of the mass-based mean values though they are considerably less in number, it was preferred to keep the 753\mu m as maximum limit and assumed that droplets under 20\mu m will have less impact on the flow. For the velocities, the measured value is never less than 15m/s and never exceeds 50m/s. Therefore, the measurement ranges [21.5\mu m — 753.3\mu m] and [11.55m/s — 56.82m/s] are found to be reasonable.

PDA measurements are performed on the centerline along the jet axis at \( \phi = 30^\circ \) forward scattering mode placing the emitter and receiver optics as shown in Fig. 3.7. The receiver off-axis angle may change of \(+/- 2^\circ\) introducing an error in the droplet size estimation up to 3\% (Albrecht et al. \[2\]). Considering the bias due to flow characteristics (very dense flow, high non-sphericity, liquid ligaments etc.) this error may be negligible.

![Figure 3.7: The positioning of the Phase Doppler Anemometer](image)

PIV measurements are also taken along the jet axis. The droplets of the two-phase jet are illuminated by a double cavity pulsed Nd: Yag laser, the laser sheet being aligned with the centerline. The flow is investigated at three positions covering the area close to the nozzle where the information related to the source processes of flashing phenomena is more crucial: a) “Region 1” at the nozzle \( (x \in [0.01 m; 0.02 m] \) or \( x/D \in [2; 4] \) , b) “Region 2” at a position where the axial position covers \( (x \in [0.05 m; 0.07 m] \) or \( x/D \in [10; 14] \) downstream the nozzle, and c) “Region 3” at an axial position where the camera sees the field such as \( (x \in [0.07 m; 0.09 m] \) or \( x/D \in [14; 18] \) from the nozzle. Fig. 3.8 shows the camera positions at different axial distances.

It was mentioned in Section 3.3.2.2 that an initial windows size of 128x128 pixels is
Figure 3.8: Schematic sketch displaying the PIV measurement locations for flashing jet (the rectangles represent three different camera positions in image recording for PIV: a) Region 1: at the nozzle, b) Region 2: the location corresponding to from \( x=0.05m (x/D=10) \) to \( x=0.07m (x/D=14) \), c) Region 3: the downstream camera position from \( x=0.07m (x/D=14) \) to \( x=0.09m (x/D=18) \).
3.3.2 Non-intrusive Laser-based techniques

selected for all the measurement locations for the processing of the images. For the Region 1 and 2, only 1 refinement and 64x64 pixels final window size give better results. Starting from Region 3, single droplets are clearly resolved providing the possibility of using higher refinement steps and smaller final window sizes. However, to keep uniformity in the three regions, all the initial parameters applied to the previous two regions are also applied to the latter region.

3.3.2.4 Measurements performed with the laser-based techniques on the final test facility

PDA is chosen as the main measurement technique due to its ability of providing velocity and droplet sizes, simultaneously.

Preliminary runs dedicated to find the best instrument settings led to the following measurements ranges [10.33μm - 377.3μm] and [0.27m/s - 28.41m/s].

Fig. 3.9 shows the arrangements PDA, thermocouples and nozzle. Fig. 3.10 and Fig. 3.11 show the alignment of the nozzle with the displacement system where the PDA and thermocouples are mounted in order to find a reliable reference “0” point for correct comparisons between different cases and bottles.

The reference “0” point is determined as following:

- A point laser is mounted on the nozzle at the orifice location; this laser beam defines the path of the flow (Fig. 3.11),
- The next step is to align the displacement system of the PDA in such a way that it is perpendicular to the nozzle to avoid any axial distortions while displacing in the x, y, z directions.
- For this purpose, two vertical identical Plexiglas plates painted into black with holes to let the laser beam pass through are placed in a line to a horizontal plate (Fig. 3.10). The height of the holes are fixed in such a way that the laser beam representing the path of the jet passes from the probe volume of the PDA. The displacement system is adjusted till the laser beam passes through the holes in both parallel vertical plates. This position is accepted as Point “0” (Reference/Starting point) for the profiles.

The displacement in the vertical-radial (Z), horizontal-radial (Y) and axial (X) directions are evaluated by means of optical counters with an accuracy of 0.1mm in radial (Y), 0.01mm in vertical (Z) and 1mm in axial (X) directions. The directions can be consulted in Fig.3.1.

Spray characteristics are obtained by means of Phase Doppler Anemometry (PDA) at relatively far fields in axial dimensionless distance compared to the locations investigated with high-speed imaging which is up to x/D = 90. The high rejection rates of
Figure 3.9: A view of PDPA system and thermocouples

Figure 3.10: Alignment to find the reference '0' point
3.3.3 Temperature measurements

3.3.3.1 Intrusive temperature measurements

The thermodynamical non-equilibrium nature of the flow creates difficulties to obtain accurate data measurements (rapid change in diameter of droplet associated with rapid change in temperature). In the present study, both intrusive and non-intrusive techniques are used to investigate the thermal behaviour of the flashing jet.

Fig. 3.12 shows the sketch of the experimental installation with the thermocouples. The temperature of the liquid R-134A before the exposure to the ambient is measured...
Chapter 3. Experimental installation and measurement techniques

<table>
<thead>
<tr>
<th>Test no</th>
<th>Nozzle diameter mm</th>
<th>L/D</th>
<th>Liquid temperature °C</th>
<th>Liquid superheat °C</th>
<th>Back pressure kPa</th>
<th>Axial position x/D</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 (ref.case)</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>20.2</td>
<td>46.6</td>
<td>800</td>
<td>110</td>
</tr>
<tr>
<td>2 (ref.case)</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>20.6</td>
<td>47</td>
<td>790</td>
<td>220</td>
</tr>
<tr>
<td>3 (ref.case)</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>20</td>
<td>46.4</td>
<td>830</td>
<td>440</td>
</tr>
<tr>
<td>4</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>23.3</td>
<td>49.7</td>
<td>790</td>
<td>110</td>
</tr>
<tr>
<td>5</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>23.3</td>
<td>49.7</td>
<td>780</td>
<td>220</td>
</tr>
<tr>
<td>6</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>24.6</td>
<td>51</td>
<td>840</td>
<td>110</td>
</tr>
<tr>
<td>7</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>25</td>
<td>51.4</td>
<td>760</td>
<td>220</td>
</tr>
<tr>
<td>8</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>27</td>
<td>53.4</td>
<td>820</td>
<td>110</td>
</tr>
<tr>
<td>9</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>26.4</td>
<td>52.8</td>
<td>810</td>
<td>220</td>
</tr>
<tr>
<td>10</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>28.3</td>
<td>54.7</td>
<td>800</td>
<td>110</td>
</tr>
<tr>
<td>11</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>20.5</td>
<td>46.9</td>
<td>1210</td>
<td>110</td>
</tr>
<tr>
<td>12</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>20</td>
<td>46.4</td>
<td>1200</td>
<td>220</td>
</tr>
<tr>
<td>13</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>21.6</td>
<td>48</td>
<td>1180</td>
<td>110</td>
</tr>
<tr>
<td>14</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>24</td>
<td>50.4</td>
<td>1200</td>
<td>110</td>
</tr>
<tr>
<td>15</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>24.2</td>
<td>50.6</td>
<td>1390</td>
<td>110</td>
</tr>
<tr>
<td>16</td>
<td>1.00</td>
<td>&quot;0&quot;</td>
<td>26.5</td>
<td>52.0</td>
<td>1350</td>
<td>110</td>
</tr>
<tr>
<td>17 (ref.case)</td>
<td>2.00</td>
<td>&quot;0&quot;</td>
<td>20.2</td>
<td>46.6</td>
<td>780</td>
<td>110</td>
</tr>
<tr>
<td>18 (ref.case)</td>
<td>2.00</td>
<td>&quot;0&quot;</td>
<td>20.1</td>
<td>46.5</td>
<td>800</td>
<td>220</td>
</tr>
<tr>
<td>19</td>
<td>2.00</td>
<td>&quot;0&quot;</td>
<td>21.3</td>
<td>47.7</td>
<td>790</td>
<td>110</td>
</tr>
<tr>
<td>20</td>
<td>2.00</td>
<td>&quot;0&quot;</td>
<td>24.7</td>
<td>51.1</td>
<td>860</td>
<td>110</td>
</tr>
<tr>
<td>21</td>
<td>2.00</td>
<td>2</td>
<td>20.8</td>
<td>47.2</td>
<td>800</td>
<td>110</td>
</tr>
<tr>
<td>22</td>
<td>2.00</td>
<td>2</td>
<td>20.6</td>
<td>47</td>
<td>810</td>
<td>220</td>
</tr>
<tr>
<td>23</td>
<td>2.00</td>
<td>7</td>
<td>20</td>
<td>46.4</td>
<td>790</td>
<td>110</td>
</tr>
<tr>
<td>24</td>
<td>2.00</td>
<td>7</td>
<td>20.4</td>
<td>46.8</td>
<td>790</td>
<td>220</td>
</tr>
</tbody>
</table>

Table 3.4: Test conditions for droplet size-velocity measurements with Phase Doppler Anemometry (PDA)

by a thermocouple that is introduced in the discharge tube. A rack of 10 thermocouples with a distance of 0.05 m between each other was mounted on the displacement system (Fig. 3.2, and Fig. 3.9) in the downstream direction of the pneumatic valve in order to observe the axial temperature change at 10 different points simultaneously in time using a DAS16 acquisition system. The thermocouples give point measurements in the disintegrated part of the jet.

Once the displacements system is aligned and the position of the reference point is adjusted, the rack of thermocouples is placed on the displacement table in the axis of the flow. The laser beam is used one more time to align the rack with the axis of the flow as seen in Fig. 3.13.

Care is taken to place the rack of thermocouples at the same downstream location from the probe volume of PDA (Fig. 3.14). The distance between the first thermocouple and the PDA probe volume is 10 mm +/-1mm. This distance is necessary to prevent a probable blockage of the PDA probe volume due to the ice formation on the
3.3.3. Temperature measurements

Figure 3.12: Experimental facility with the rack of thermocouples

Figure 3.13: Alignment of the thermocouples with the laser beam
Chapter 3. Experimental installation and measurement techniques

3.3.3.2 Non-intrusive temperature measurements

The thermocouples give point measurements in the disintegrated part of the jet. However, information related to the temperature evolution of the unbroken part of the flow (if it exists) between the nozzle orifice and the first thermocouple is not obtained with this technique. To obtain thermal information on the upstream portion of the liquid jet, non-intrusive Infrared Camera Thermography measurements are performed using a FLIR Systems' ThermaCAM™ SC3000 camera. It consists of a rugged IR-camera (IP:54 housing) with a built-in 20° lens, a remote control, cables and connectors and a range of optical hardware and software accessories. Due to the difficulties of calibrating the camera system with R134-A and difficulty of determining the emissivity of an evaporating mixture of liquid core and aerosols, the obtained thermographs give
3.3.4 Discharge coefficient measurements for single phase flow

From a stability perspective, it is important if not necessary to establish the characteristics of any nozzle that produces a liquid jet or spray simply because the characteristics of the jet or spray are strongly dependent on nozzle geometry and discharge characteristics. The traditional approach for characterizing nozzle hydraulics involves the measurement of the discharge coefficient that quantifies hydraulic performance from the standpoint of discharge capacity or flow rate which depends on issuing jet contraction. Measurements using water as test material are carried out for discharge coefficient determination of the test nozzles in case of single phase flow by Kubitschek[65] in the frame work of the present study. In addition to providing an improved understanding of nozzle effects on jet hydrodynamics, the measurement of discharge coefficients also provides a means of predicting and thereby measuring discharge and hence nozzle exit velocity for R-134A testing as long as the flow through the nozzle is single phase.

The discharge coefficients for each of the 1, 2, and 4mm nozzles are determined by measuring the flow rate for a range of back pressures. The volumetric flow rate measurements are achieved using a catch tank and a digital weight scale capable of measuring mass to a resolution of 0.001kg. The scale is assumed to have been factory calibrated and free of systematic error. The catch tank filling time, \( t_f \) was then measured using a digital stop watch with a resolution of 0.01s. The mass flow rate is then obtained as \( \dot{m} = \frac{m}{t_f} \) that may then be converted to volumetric flow rate using the known density, \( \rho \) written as \( Q = \frac{\dot{m}}{\rho} \). Having the volumetric flow rate, the discharge coefficients, \( C \) may then be solved for from Eq. 3.3 and computed using the known orifice and approach pipe diameters. The orifice discharge equation used here is

\[
Q = CA_o \sqrt{\frac{2P_d}{\rho(1 - C^2(d_o/A_p)^2)}}
\]  

(3.3)

where \( Q \) is the volumetric flow rate, \( C \) is the experimentally measured discharge coefficient, \( P_d \) is the nozzle drive pressure, \( \rho \) is the fluid density, \( A_o = \pi d_o^2/4 \) is the nozzle exit or orifice area computed from the orifice diameter \( d_o \), and \( A_p = \pi D_p^2/4 \) is the approach pipe area computed from the internal pipe diameter \( D_p \).

To obtain the most accurate measure of discharge coefficients, each of the nozzles are photographed under a microscope. The images are digitized at high resolution to obtain orifice diameter measurements. Figures 3.15(a) and 3.15(b) represents the digitized images and corresponding calibration scale images. The images are calibrated using a 5-mm-strip with 100 graduations to obtain a 0.05mm precision. The added advantage of photographing the nozzles is the evidence that small imperfections exist due to limitations in machining precision and material hardness. This is particularly evident for the 2mm brass nozzle in which rather significant defects are observable. As will
be shown, not only the discharge coefficient is sensitive to such imperfections, but the jet characteristics are significantly modified as well. As a result another 2\text{mm} nozzle has been fabricated using stainless steel and the discharge coefficients are similarly measured.

(a) Photographs of nozzles under the microscope for 1\text{mm} at 50X (a), 2\text{mm} defective at 25X (b), 2\text{mm} Stainless at 25X (c), and 4\text{mm} at 16X (d).

(b) Photograph of the scale used to calibrate the images for 1\text{mm} at 50X. Similar calibration images were also acquired for the 2\text{mm} and 4\text{mm} at 25X and 16X, respectively.

Figure 3.15: Photographic investigation of the accurate nozzle diameter determination

The uncertainty in orifice diameter measurements are given by the resolution of the calibration scale image used. The uncertainty in each of the calibration images is estimated as \pm 6\text{pixels}, \pm 4\text{pixels}, \pm 6\text{pixels} for the 1, 2, and 4\text{mm} scale images. The number of pixels for the diameters of each of the nozzles is obtained from the nozzle images by averaging 5 diametrical measurements are 968\text{p/mm}, 496\text{p/mm}, and 312\text{p/mm}.
3.3.4. Discharge coefficient measurements for single phase flow

The associated uncertainty calculated from standard error propagation techniques is 
\( \delta o = 0.006, 0.01, \) and \( 0.02mm \) for the 1, 2, and 4mm nozzles, respectively.

3.3.4.1 Discharge Coefficient Measurements

Having uncertainty estimates for the pressure measurements and the nozzle diameters allows for determination of the uncertainty associated with calculated discharge coefficients, \( C \) from the estimated fractional uncertainties in discharge measurements obtained from twice the standard deviations of the means as \( \delta Q/Q = 0.006 \). Then from the orifice discharge equation (Eqn. 3.3) solving for \( C \) and differentiating accordingly gives an uncertainty estimate

\[
\delta C = \sqrt{\left( \frac{\partial C}{\partial A_o} \delta A_o \right)^2 + \left( \frac{\partial C}{\partial A_p} \delta A_p \right)^2 + \left( \frac{\partial C}{\partial Q} \delta Q \right)^2 + \left( \frac{\partial C}{\partial P_d} \delta P_d \right)^2}
\] (3.4)

where \( \delta P_d/P = 0.003 \) as previously obtained, \( \delta A_p/A_p = 0.005 \), and \( \delta A_o/A_o = 0.01 \). Thus, the fractional uncertainties for discharge coefficient measurements are estimated to be \( \delta C/C = 0.012 \). It should be noted that the primary contribution to the uncertainty is from the nozzle orifice diameter measurements that propagates to the orifice area, \( A_o \). Thus, it is necessary to have accurate nozzle diameter measurements to have accurate discharge coefficient measurements.

It has been shown that the discharge coefficients is measured to within ±1.2 percent accuracy and hence with high accuracy pressure measurements, the discharge or volumetric flow rate for R-134A testing should be measurable, conservatively to within ±2 percent.

The discharge coefficients, \( C \) for each of the nozzles provide an indication of hydraulic performance that may significantly effect the hydrodynamics and hence stability of the issuing jet. Nozzle hydraulic characteristics are also known to effect droplet size distribution in the resulting two-phase dispersed flow following jet breakup. Thus, for any correlation to be developed adequately, the nozzle hydraulics must be taken into account. However, other important parameters such as nozzle spray angle, radial variation in size distribution, liquid-gas momentum ratio, and distance from the nozzle exit are not included. Each of the nozzles used during these experiments are nearly representative of sharp-edged orifices. However, as was previously shown, they are not entirely sharp edged orifices owing to the curvature of the upstream surface (Figures 3.3(a), 3.3(b), and 3.3(d)). The work of Lienhard and Lienhard [73], suggests that surface tension has a negligible effect on the velocity coefficient, \( C_v \) provided \( We > 8/\sqrt{C_v} \approx 10 \) which is certainly much lower than the range of these investigations. Furthermore, \( C_v \) and hence \( C \) is essentially constant to within 1% and independent of \( Re \) for values of \( Re > 10000 \). Thus, it seems only necessary to consider \( C \) versus \( Re \) for an adequate representation of the discharge coefficient in parameter space. Fig. 3.16 gives the measured values of \( C \) for each of the nozzles over the full range of
Chapter 3. Experimental installation and measurement techniques

Re investigated.

Figure 3.16: Measured discharge coefficients for the 1mm, 2mm and 4mm nozzles. Error bars are given at ±1 percent. Taken from Kubitschek [65]

The most obvious feature of these results is that in all cases $C$ is fairly constant over the range of $Re_d$ tested with perhaps a slight increase with increased $Re_d$. Furthermore, each of the nozzles compare reasonably well with each other except for the 2mm brass nozzle that was shown to have significant defects. As a result much lower discharge coefficients are measured in that case as compared with the other nozzles that were shown photographically to be relatively free of imperfections. These lower values of $C$ reflect an inefficiency in nozzle hydraulics that is most likely a result of the modified flow patterns near the nozzle exit.

The problems associated with nozzle imperfections may be alleviated simply by choosing a different material of construction. The original nozzles were fabricated using a Cu-Zn alloy commonly known as brass. However, this material is relatively soft compared with other materials (e.g. stainless steel). Having discovered these imperfections it was suggested that future nozzles be constructed of stainless steel to allow for improved hardness and achieve precise machining with reduced potential for damage during future testing. This was done for the replacement 2mm stainless steel nozzle and the discharge coefficient results shown represent a significant improvement in hydraulic performance, but more importantly improved consistency as compared with the other nozzles tested.

Further comparison of the measured discharge coefficients shows the 4mm nozzle appears to have the highest values of $C$. This is most likely attributable to the relaxation of the jet at the nozzle exit for which a larger relief angle exists in the case of the 4mm nozzle. This relaxation consists in the tangent to the curved surface at the edge of the
orifice. Fig. 3.17 shows the relief angles for each nozzle diameter that results from the change in orifice diameter owing to the fixed upstream surface radius of curvature. The effect is a slight relaxation of the jet exiting the nozzle and hence improved hydraulic efficiency through reduced jet contraction.

Figure 3.17: Geometric dissimilarity between different nozzle sizes as shown by the changing relaxation angles at the nozzle exits.

The discharge coefficients measured during these experiments may also be compared with the data published by Brater and King [20]. It is interesting to note that for the result reported by Brater and King [20], $C$ values vary significantly with both $Re_d$ as well as the orifice to pipe diameter ratio, $d_o/D_p$ only below a value of $Re_d \approx 10^5$. Above this value the discharge coefficient appears to be independent of $Re_d$ and only weakly dependent on $d_o/D_p$. The effect of the orifice to pipe diameter ratio is to decrease $C$ with decreasing $d_o/D_p$ to the point where $d_o/D_p \approx 0.25$, below which $C$ is essentially independent of $d_o/D_p$. For the experiments of this thesis study, the 4mm nozzle represents the largest $d_o/D_p$ and is equivalent to 0.25. However, as can be seen from Fig.3.16 the measured $C$ values, although they are larger by more than 5 percent than those reported by Brater and King [20], exhibit a slightly increasing trend with $Re_d$. It is difficult to explain this trend however, it may be a result of relaxed contraction due to the concave surface geometry of the nozzle. Thus, it would seem that the nozzle geometry is sufficiently different from the traditional sharp-edged orifice case resulting in a difference in results given by Brater and King. In any case a constant $C$ can be assumed for the ranges of $Re_d$ tested with an uncertainty of ±2 percent.

Based on the measured $C$ values, discharge curves have been developed in Fig. 3.18 taking $C = constant = 0.6310, 0.6420$, and 0.6485 for the 1, 2, and 4mm nozzles. The results are given for R-134A as a function of drive pressure to provide a measure of flow rate. The most important limitation of applicability to R-134A testing is that single-phase flow is required at the nozzle exit. This may not always be the case due to vaporization that potentially occurs upstream of the nozzle exit under certain release conditions (i.e. high levels of superheat).

The next chapter will present the results of the different measurement campaigns that
Nozzle Discharge Curves
for R-134a

Figure 3.18: Nozzle discharge curves. Taken from Kubitschek[65].

are introduced hereby.
Chapter 4

Experimental Results

This Chapter deals with the experimental results. The presentation is twofold. The first part involves the experimental difficulties observed during the preliminary studies in the feasibility of the non-intrusive laser based techniques. In addition to that, a short interpretation of the flow behaviour will also be given. The second part focuses on the results obtained with the final test facility. The break-up patterns, spray characterization of the flow such as drop size, velocity and temperature evolution in relation with the change of storage and release parameters are interpreted.

4.1 Exploration of Laser-based Techniques for Characterization of a Flashing Jet

Due to the dense and non-equilibrium nature of the near field regions of flashing jets, accurate measurement of flow characteristics is very arduous and hardly possible with intrusive techniques. Laser-based optical techniques (such as Particle Image Velocimetry (PIV), Particle Tracking Velocimetry and Sizing (PTVS), Phase Doppler Anemometry (PDA) etc.) offer a powerful means to obtain information about droplet diameter and velocity evolution.

Still, even with the above mentioned techniques, the measurements have been reported as optically very challenging, especially in the region where the liquid disintegrates to reach thermodynamic equilibrium. (Brown and York [21]; Reitz [112]; Park and Lee [101]; Allen [4]; Balachandar et al. [11]; Hervieu and Veneau [53])

The main objective of the present chapter is to explore the potential of non-intrusive laser based techniques such as PDA and PIV on the characterization of a two-phase R-134A flashing jet. The encountered problems are explained. An attempt is made to
measure the velocity of different droplets classes using Multi-intensity-layer PIV.

4.1.1 PDA Results

A high amount of ligaments and non-spherical droplets are observed close to the nozzle at dimensionless distances \( x/D < 30 \). Such a flow pattern creates very difficult conditions for PDA technique. PDA software rejects velocities corresponding to events based on non-spherical droplets and ligaments, range limits exceeding or signal saturation. As a general observation, the validation rates of the droplets increased with downstream distance mainly due to the occurrence of the small, spherical droplets.

Analysis of PDA measurements displays a wide range of droplet size distribution. Fig. 4.1 shows the total cumulative volume distribution with respect to diameter classes. It is apparent that more droplets in the higher size classes are measured closer to the nozzle and that the flow is poly-dispersed. Moving downstream the nozzle, the measured droplet size ranges become narrower showing a trend to a more "mono-dispersed" flow. For example, the volume of the droplets larger than 200 \( \mu m \) constitutes 90 % of the total droplets at \( x/D = 6 \) whereas this percentage is 40 % at \( x/D = 46 \).

![Figure 4.1: Total volume cumulative distribution for different x/D on the axis](image)

Fig. 4.2 shows the evolution of the arithmetic mean velocity and different mean droplet sizes such as \( D_{32} \) (average droplet diameter computed by the ratio of the total volume...
4.1.1. PDA Results

to total surface area of all the droplets at that measurement point), $D_{20}$ (volumetric based mean value of the droplet diameters), $D_{20}$ (surface based mean value of the droplet diameters), $D_{10}$ (the arithmetic mean value of the droplet diameters). Depending on the number of large droplets detected in the probe volume, the mean droplet diameter values may exhibit fluctuations since the big droplets have a stronger impact on the total cumulative volume than the smaller ones. An increase of about 10% in centreline velocity is observable up to $x/D = 30$ from the nozzle. After that location, the velocity decreases with the downstream distance, as in an ordinary spray, due to air entrainment and evaporation of the small droplets. When the evolution of the mean diameters is observed, the values of $D_{32}$ and $D_{10}$ differ from each other close to the nozzle representing a poly-dispersed character. Moving downstream, these two curves approach to each other indicating that the spray becomes more mono-dispersed. From the nozzle up to 30D downstream, the change in $D_{32}$ is very steep whereas after 30D it adopts a mild evolution. It is reasonable to consider that the change in mean velocity

![Figure 4.2: Centreline droplet size and velocity evolution along the jet axis in comparison of $D_{32}$, $D_{30}$, $D_{20}$, $D_{10}$, and mass based mean droplet diameter computed from the cumulative mass distribution](image)

and droplet size pattern before and after 30D is due to the existence of the superheated ligaments and large droplets near the nozzle. These ligaments and droplets continue to shatter into smaller droplets with acceleration and create an expansion region up to 30D downstream the nozzle resulting in an increase of velocity and rapid decrease of $D_{32}$. The effect of this break up process diminishes after 30D and evaporation gains importance so that a mild evolution of $D_{32}$ and a decrease in velocity are observed.

Tests have also been conducted to evaluate the confidence intervals on the experimental results. The repeatability for velocity and droplet diameters is displayed in Fig. 4.3(a) and Fig. 4.3(b) through probability density functions of two PDA data sets taken at
18D downstream the nozzle, respectively. It is found that the dispersion did not exceed ± 3\% in the mean drop velocity and ±15\% on the mean diameters (7\% in arithmetic mean diameters ($D_{10}$), 3\% in surface mean diameters ($D_{20}$), 4\% in volumetric mean diameters ($D_{30}$) and 15\% in Sauter mean diameters ($D_{32}$)). Fig. 4.3(c) represents the droplet size-velocity relation of these two repetitive tests. The small droplet classes show high similarity whereas larger discrepancy is observed for the coarse droplets due to the insufficient data sampling (i.e. 1 or 2 droplets). Experiments with higher number of samples are expected to attenuate this effect. Even if the experiments are not fully controlled in terms of preservation of initial conditions, good repeatability for short measurement time is obtained from Test 1 and Test 2, which are performed at different times using different bottles.

Figure 4.3: The repeatability test for the velocity and droplet diameter distributions performed at $x=18D$ downstream the nozzle
4.1.2 PIV Results

As stated by several researchers (Hervieu et al. [53], Allen [3, 4]), two-phase flashing jets present an optically harsh environment. The high density of the droplets generated from explosively flashing jet, and the existence of very steep density, velocity and temperature gradients inside the jet pose many problems such as high beam attenuation, strong ambient backscatter and potential beam path distortion. Even for the most sophisticated optical technique this can lead to inaccurate measurement or, at the extreme, an inability to acquire any data at all in certain regions.

In principle, PIV measures the displacement of all objects in the flow, either ligaments or droplets. However, it is very well known that the quality of PIV measurements relies very much on the concentration and space distribution of tracer particles. Velocity vectors are often biased to some extent by particle entering and exiting the sampled region during the time between pulses. These phenomena are associated with excessive velocity gradients (in-plane drop-out and under-matched particle pairs). Moreover, the out-of-plane velocities of the particles can determine the presence in the correlation plane of multiple velocity peaks. Additionally, errors occur from insufficient data caused by lack of seeding (non-uniform particle distribution, low number of particle in one interrogation area) or from particles overlapping (high seeding density, speckle-phenomenon) (Hart [51]). These problems are dramatically observed in the present study of flashing jet, where both different sizes of ligaments and droplets appear in the same frame and in the same image regions with high and low particle density. Moreover, these superheated droplets and ligaments explode into smaller droplets to reach thermal equilibrium and create a local perpendicular (radial) flow as can be seen from Fig. 4.4 resulting in out-of-plane motions.

![An instantaneous PIV image displaying the sudden perpendicular velocities due to the break-up of superheated ligaments or big droplets](image)

Figure 4.4: An instantaneous PIV image displaying the sudden perpendicular velocities due to the break-up of superheated ligaments or big droplets

To minimize this effect, the laser beam can be thickened or the pulse separation can be decreased. Nevertheless, increasing the laser beam thickness illuminates too many ligaments and droplets worsening the cross-correlations. Therefore, it is strongly reckoned that choosing a small enough pulse separation would be a better solution for minimizing the out of plane motions, droplets coalescence effect and/or out of plane loss of the droplets. Better signal cross-correlation results have been achieved with a
final pulse separation of 0.5µs. Though decreasing the pulse separation reduces also
the accuracy in velocity detection, the measurements provide the necessary patterns
regarding to the flashing jet, within this accuracy.

Fig. 4.5 shows instantaneous PIV images that are taken using a pulse separation of
0.5µs at “Region 1”, “Region 2”, and “Region 3”. In “Region 1”, a high amount of
liquid ligaments, large droplets and speckle patterns are present. It is difficult to elim-
ninate saturation in the images due to the high amount of liquid. As clearly displayed
in Fig. 4.5a, the droplet concentration is very dense with respect to the tracer par-
ticle concentration required for a standard PIV application. In “Region 2” (Fig. 4.5b),
there are smaller droplets and the speckle pattern is less obvious. Moving to “Region
3” (Fig. 4.5c) provides an optically easier environment, where the concentration of the
droplets is moderate, the droplets can be easily observed and speckle behaviour almost
disappears.

![Figure 4.5: The PIV images at Region 1(a), at Region 2(b), at Region 3(c)](image)

Large windows sizes should be used in the processing due to the density of the flow
that cannot be optically separated from each other. Close to the nozzle (Region 1),
applying window-sizes smaller than 128 by 128 pixels lead to low signal correlation
(SNR < 1.2) and high amount of “bad vectors”. The effects are visible especially
when the vector-field is depicted. By using a 128x128pxds initial window size and 1
refinement, PIV measures the velocity of ensembles of droplets and gives higher signal
correlation results.

Fig. 4.6 shows the averaged Signal-to-Noise-Ratio (SNR) (i.e. the ratio of the highest
peak to the second high peak in the cross-correlation) distributions on these three
positions. The average vector velocity field is superposed on the SNR map, as well.
Regions where SNR is less than 1.2 are excluded. “Region 1” has the lowest SNR
values (majority between 1.2 to 1.80). Though the out-of-plane motions of the seeding
particles are minimized by fixing Δt = 0.5µs, the seeding non-homogeneity and the
correlation of smaller/bigger particles in high/low density regions together are here
the reasons of the low SNR values. At “Region 2”, the majority of the SNR value
increases to a range 1.80-2.2 and the averaged Signal-to-Noise-Ratio is higher than 2.2
in overall at “Region 3”, mainly because of the moderate concentration of the droplets
in the downstream.
4.1.2. PIV Results

Figure 4.6: The Signal-to-Noise-Ratio distributions at different measurement locations (averaged over 25 images): Region 1(a), at Region 2(b); at Region 3(c)

Fig. 4.7 gives the averaged velocity fields at the three observation regions. The vector field shows expected flow patterns, going further downstream less scattered average vector field is obtained even with few images, due to the nature of the flow.

Figure 4.7: The velocity vector fields at different measurement locations (averaged over 25 images): Region 1(a), at Region 2(b); at Region 3(c)

In such measurement techniques, the velocity uncertainty is mainly associated with the uncertainty in determining the particle image displacement. For displacements of around 10 pixels, PIV is assumed to give an error of one tenth of a pixel (1%) [Willert and Gharib [142]]. This 0.1 pixel of error corresponds to an error of ± 3.2 m/s in velocity for the present measurement campaign due to the very small pulse separation. The histogram in Fig.4.8 illustrates the mean stream displacement distribution for 25 measures of velocity fields. Looking at the displacement of droplets, Fig.4.8 clearly indicates that this error value in the velocity is overestimated since displacements much less than 0.1 pixel were detected.

Peak locking generally appears as a spurious concentration of measurements corre-
Chapter 4. Experimental Results

Corresponding to an integer number of pixel displacements. The existence and magnitude of peak locking in the results can be easily ascertained inspecting the velocity histograms. In Fig. 4.8, scattering of data is obviously much less than 0.1 pixel for the random error and the systematic bias error peak locking effect is hardly detectable. In the literature (Westerweel [140], Nogueira et al. [92]), several sources of peak-locking effect have been identified and described. A very important reason for pixel locking is considered as the sensor geometry and resolution. It has been shown that when particles have an image diameter lower than 2 pixels in PIV applications, the signal is under sampled and this can reduce the accuracy of the estimated velocity field. Below this limit, peak locking starts to grow and become relevant. In the present case, a majority of the tracer's image diameters is bigger than 3 pixels for clearly followed droplets by a visual check. Fig. 4.8 shows no tendency towards integer values (i.e. no detectable Pixel-locking effect). A second systematic error leading to pixel locking is the choice of the sub-pixel peak-fitting algorithms in image processing. The details of the utilized sub-pixel peak-fitting algorithms to avoid peak locking is given in Scarano et al. ([117, 118]) in detail.

![Figure 4.8: Example of the histogram deduced from an average of 25 image pairs representing the particle displacement.](image)

4.1.3 Comparison of PDA and Standard PIV

Fig. 4.9 gives the axial evolution of the mean centerline velocity profiles obtained with PIV and PDA in Region 2 and Region 3. The velocities obtained by both techniques fall in same ranges taking the errors into account. PIV gives slightly higher mean values than PDA measurements. Fluctuations along the centerline are observed from
4.1.4 Assessment of the feasibility of Multi-Intensity Layer PIV

It has been explained in previous sections that PDA measurements reveals a wide range of droplet size distribution and these droplets receive different light intensities. This observation leads us to attempt a Multi-Intensity-Layer PIV (MIL-PIV) approach, which was initiated by Yamada et al. [146] and used by Palero et al. [99]. In this technique, the original source image is split into different layers of images made of different ranges of intensity. It is anticipated that these different ranges of intensities
correspond to different ranges of droplet sizes.

It was stated in the previous sections that close to the nozzle (Region 1), PIV gives a low Signal-to-Noise-Ratio (majority between 1.4 and 1.6) value with only 1 window refinement. It was possible to measure only the velocity of ensembles of droplets that optically could not be separated from each other, due to the speckle-like-pattern. This explains why in Region 1 the multi-layer approach is not likely to give any additional information. But further downstream, more window refinement could be applied because single droplets can be identified. Therefore, the MIL-PIV is attempted on the images taken in Region 3. Fig. 4.10 presents an original non-processed 12-bits PIV image (intensity levels range of 0-4095), where the intensity range is from 66 to 4095 with intensity from 0 to 65 corresponding to background noise.

![Figure 4.10: An original PIV image at the location \(x=14D-18D\) downstream the nozzle (Region 3) (Light intensity range: 66-4095)](image)

PDA data taken in Region 3 shows that droplet diameters are in the range of \(21.5\mu m\) to \(753\mu m\) for \(x/D=14\) and \(21.5\mu m\) to \(640\mu m\) for \(x/D=18\). Indeed, these two measurement points show high similarities for velocity and droplet size distributions as depicted in Fig. 4.11(a) and Fig. 4.11(b), respectively. The local discrepancies are mainly due to different count of large droplets. This explains the differences in volume based cumulative distributions shown in Fig. 4.11(c), which are highly sensitive to the number of large droplets. Fig. 4.11(d) displays the size classified velocity distribution for these two measurement points. This graph makes clear that the small droplet classes follow the same pattern while the large droplets differ in velocities.

Three main droplet classes for multi-intensity treatment are determined by the slope (pattern) changes in the Rosin-Rammler distributions. Since the minimum droplet size measured with PDA is \(21.5\mu m\), this droplet size is assumed to have minimum light
4.1.4. Assessment of the feasibility of Multi-Intensity Layer PIV

Figure 4.11: The comparison of droplet velocity, size distributions measured with PDA at x=14D and x=18D.
intensity of the image after the background intensity is subtracted, whereas the largest observed droplets (753\(\mu\)m) correspond to intensity level 4095. Three different layers are defined with intensity ranges 66-300, 301-900 and 901-4095 (Fig. 4.12). Table 4.1 shows the corresponding diameter ranges determined from Fig. 4.12 for three intensity ranges. The square values of the droplets sizes are used due to \(\text{Intensity} \propto \text{Diameter}^2\).

<table>
<thead>
<tr>
<th>Layer #</th>
<th>Diameter(^2) range ((\mu)m)</th>
<th>Intensity range</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>21.50(^2)-230(^2) ((\mu)m)(^2)</td>
<td>66-300</td>
</tr>
<tr>
<td>2</td>
<td>230(^2)-400(^2) ((\mu)m)(^2)</td>
<td>301-900</td>
</tr>
<tr>
<td>3</td>
<td>400(^2)-753(^2) ((\mu)m)(^2)</td>
<td>901-4095</td>
</tr>
</tbody>
</table>

Table 4.1: The intensity ranges corresponding to different diameter ranges

Figure 4.12: Images processed with different light intensities (a-left) Int. lev.: 66-300, (b-middle) Int.lev.:300-900, (c-right) Int.lev.: 900-4095

Fig. 4.12 confirms that large droplets tend to be near the jet axis and the droplets occupy more than 1 pixel to eliminate peak-locking, which is different from the measurements of Yamada et al. [146] and Palero et al. [99]. When the particle's diffraction peak extends over more than 1 pixel, the same particles can contribute to various intensity levels. Moreover, the Gaussian intensity distribution of the laser beam, which is also a known problem even when the droplets occupy less than a pixel, explains why Fig. 4.12a, Fig. 4.12b and Fig. 4.12c provide often information belonging to the same droplets. In such conditions, processing the original image with different light intensity layers does not guarantee that different light intensities will correspond to different droplet sizes, especially for the lower range of intensity levels.

It was also observed that Multi-intensity-layer PIV approach has not improved the Signal-to-Noise Ratio as seen from Fig. 4.13, which shows the SNR for the image pairs displayed in Fig. 4.10 and Fig. 4.12. The image with 66-4095 intensity level gives better SNR overall than the “Layers”.

Fig. 4.14 presents the axial evolution of mean velocity determined from 25 image pairs for the different light intensity ranges. The results belonging to “Layer 1” lead to similar values to those of the complete 66-4095 light intensity layer, all over the measurement section, whereas the other two cases show high discrepancies.

As can be seen from Table 4.2, the PDA measurements do show slight changes in
4.1.4. Assessment of the feasibility of Multi-Intensity Layer PIV

Figure 4.13: SNR for the instantaneous plane shown in Fig.4.12

Figure 4.14: The velocity profile of the flashing jet for different intensity levels and Multi-intensity layer PIV processing
Chapter 4. Experimental Results

velocities for different diameter ranges, whereas the changes are more apparent in Multi-intensity-layer. The treatment per intensity layer did not lead to a higher SNR comparing to Standard PIV and was, therefore, not really beneficial for the present case than just indicating that the bigger droplets were encountered near the jet's centerline.

<table>
<thead>
<tr>
<th>Diameter (d) ranges</th>
<th>Velocity at x=14D</th>
<th>Velocity at x=18D</th>
</tr>
</thead>
<tbody>
<tr>
<td>d &lt; 230\mu m</td>
<td>32.7 m/s</td>
<td>33.2 m/s</td>
</tr>
<tr>
<td>230\mu m ≤ d &lt; 400\mu m</td>
<td>31.8 m/s</td>
<td>32.5 m/s</td>
</tr>
<tr>
<td>400\mu m ≤ d &lt; 753\mu m</td>
<td>30.0 m/s</td>
<td>34.0 m/s</td>
</tr>
</tbody>
</table>

Table 4.2: Velocities obtained from PDA measurements regarding to different diameter ranges at x=14D&18D far from the nozzle

4.1.5 Summary

A study is performed with a simple set-up for the assessment of the laser-based techniques on a two-phase flashing R134A jet using PDA and PIV. An attempt is also made to measure the velocity of different droplet classes using a multi-intensity-layer treatment of PIV images.

The preliminary measurements are taken under the limitation of testing times. However, they are sufficient to explore the measurement problems and to produce patterns that are observed within flashing jets. The flow exhibits difficult optical conditions for both PDA and PIV. The existence of ligaments and non-spherical droplets close to the nozzle lead to high rejection rate of data in PDA. The same flow pattern close to the nozzle creates speckle-type image patterns resulting in low SNR in PIV. Going downstream the nozzle, these superheated big ligaments break up into smaller droplets to form a more mono-dispersed spray. This behaviour increases the validation rates in PDA and resulted in better SNR for PIV.

Different tests show that a very small pulse separation minimized the effect of local perpendiculax flows due to the rapid explosive break up of superheated large ligaments and gave high signal correlation in PIV measurements. Moreover, using large window sizes during processing increases also the SNR due to the speckle pattern. PDA and PIV measurements exhibit similar trends in centerline velocity evolution with slight differences due to testing time limitations and insufficient data samples. Longer testing times in an environment with well-controlled initial conditions are expected to minimize these differences.

For both techniques, an increase of centerline velocity is observed further away from the nozzle due to the flashing break-up of the superheated ligaments and large droplets. The droplet size evolution indicates that the flow owns a poly-dispersed character close to the nozzle and tends to become mono-dispersed downstream. PDA measurements illustrate that different diameter ranges have only slight velocity differences in the centerline. Multi-intensity-layer PIV treatment does not improve the SNR compared to Standard PIV. For the present study, multi-intensity-layer PIV is found ambiguous.
4.2 Break-up patterns of the superheated liquid jet

The main objective of the present section is to assess the influence of initial flow conditions such as liquid storage pressure, nozzle diameter and superheat on the break-up patterns of a superheated jet. A non-intrusive technique like high-speed imaging is used to observe the break-up patterns.

The break-up patterns of the superheated R-134A jet will be discussed for the effects of the nozzle diameter, temperature, backpressure and nozzle \( l/D \) ratio. Flows issuing from diaphragm type nozzles of 1mm, 2mm and 4mm diameter for one superheat degree are presented in Fig.4.15(a),4.15(b), Fig.4.16(a) and Fig.4.16(b), respectively. To understand the effect of the superheat, Fig.4.17(a),4.17(b),4.18(a), 4.18(b) are added. Effect of pressure increase is displayed in Fig.4.19,4.20 for flows exiting nozzles of 1, 2, 3mm in diameter and in Fig.4.21 for 4mm-diameter nozzle at both low and high liquid temperatures. Finally, Fig.4.22, 4.23 and 4.24 present the breakup patterns due to the \( l/D \) increase. The images are selected in such a way that repetitive patterns are displayed.

4.2.1 Effect of the nozzle diameter

Brown and York [21] studied the effect of nozzle geometry and superheat on break-up patterns of superheated water jets at atmospheric conditions. For the same superheat and driving pressure, they observed that disintegration due to flashing occurs as sudden explosions for the jets exiting from larger sharp edged nozzles at distances differentiating from 2.5 to 12.5\( X/D \) downstream whereas for the smaller nozzles the disintegration is further downstream and in a manner that cuts the jet into distinct continuous cylindrical liquid fragments sections with a slower disintegration. Peter et al.[102] found out that a superheated water jet exposed to a vacuum chamber through a cylindrical nozzle with larger inner diameters experiences flashing phenomena more easily than the ones with smaller inner diameters.

The high-speed measurements, which are subject to the present work, support these observations. Based on Fig.4.15(a), it is observed that for the jet exiting 1 mm nozzle with an initial liquid temperature of \( T_{\text{liquid}} = 14^\circ\text{C} \) (i.e.\( \Delta T = 40.4^\circ\text{C} \)) and backpressure of \( P_{\text{liquid}} = 820kPa \) a slow expansion downstream the nozzle is noticeable. As seen in Fig.4.15(b), a group of bubbles growing very rapidly shatters the jet violently and locally, from time to time.

Fig.4.16(a) exhibits that exiting from 2mm nozzle presents a more violent break-up
Figure 4.15: High-speed image sequences for “Experiment 1" for a jet issuing from a 1mm nozzle up to 90 X/D downstream distance, (T_{liquid} = 14°C, ΔT = 40.4°C, P_{liquid} = 820 kPa)
4.2.1. Effect of the nozzle diameter

than the one of 1mm nozzle for the same backpressure and superheat. Additionally, the disintegration of a 2mm jet appears at a distance of few diameters from the nozzle (~6 X/D), whereas for 1 mm jet this non-dimensional value is one order higher (i.e. X/D > 10).

For 4mm nozzle,(Fig.4.16(b)) it is easy to see that the disintegration is the most hostile compared to the others and on the order of 3 X/D from the nozzle. On the intact part of the liquid core, more bubbles are visible.

As a conclusion, for the same backpressure and superheat (T_{liquid} = 14°C, ΔT = 40.4°C, P_{liquid} = 820kPa), violent break-up is observed for larger nozzles. Moreover, there is higher bubble production for the large nozzles and the break-up is closer to the nozzle exit.

(a) “Experiment 2”: a view of the jet exiting from a 2mm nozzle up to 45 X/D downstream distance, (T_{liquid} = 14°C, ΔT = 40.4°C, P_{liquid} = 820kPa)

(b) “Experiment 3”: a view of the jet exiting from a 4mm nozzle up to 6 X/D downstream distance, (T_{liquid} = 14°C, ΔT = 40.5°C, P_{liquid} = 820kPa)

Figure 4.16: High-speed image sequences for jets issuing from a 2 and 4mm nozzles
4.2.2 Effect of the temperature

Previous studies have shown that even very small changes in the superheat may change the break-up pattern drastically when the superheat effect on the jet break-up is concerned (Brown and York[21], Peter et al.[102], Miyatake et al.[84],[85], Park and Lee[101], Gooderum and Bushnell[24]).

Brown and York[21] observed that an increase of superheat with a narrow range of 5°F (~2.8°C) may provoke shattering of the jet due to flashing from an initial state where there is no effect of temperature. Miyatake et al.[84],[85] noticed that with the increase of superheat flashing process becomes more violent and the column-wise liquid core near the nozzle exit becomes shorter. In the study of Peter et al.[102], the disintegration of the jet is defined in four different pattern as “non-shattering” (liquid column preserved for extended distances and undispersed ligaments falling parallel to the liquid core), “partially shattering” (liquid column retained in the centre and droplets shatters only from the sides), “stage wise shattering” (complete shattering after a distance downstream) and “flare flashing” (complete disintegration of the jet at the nozzle exit). They observed that an increase of ~10°C in superheat transforms the non-shattering jet into stage-wise shattering jet and the transition from partially shattering to stage-wise shattering pattern may happen in a band of ~7°C. Gemci et al.[47] also reported that the primary break-up length and ligaments get shorter upon increasing the superheat.

For Balitsky and Shurchkova[12], the shape of the superheated jet flashing under vacuum is a strong function of the superheat. When the the liquid superheat is \( \Delta T = 6 - 7°C \), the liquid jet is cylindrical. With a \( \Delta T = 15°C \) drops break away all over the cross section. The superheat of \( \Delta T = 16 - 17°C \) leads to flare-like flashing with an initial opening angle of 60°. This angle increases up to 180° with further superheat increase. At this regime, liquid flows out in the form of mist.

The high speed images presented in this study show the behaviour of the initially unbroken jet with superheat increment up to 6.4 °C. Observing the jet exiting from the nozzle of 1 mm diameter(L/D=0) with the lowest liquid temperature (i.e. \( T_{\text{liquid}} = 14°C \)) (Fig.4.15), a slow expansion downstream the nozzle is noticeable. From time to time, a group of bubbles growing very rapidly shatters the jet violently and locally. Applying a superheat increment of 4.5°C (i.e. \( T_{\text{liquid}} = 18.5°C \)) generates an increase in the numbers of bubbles appearing all over the jet and breaking the jet gradually downstream with an apparent and intact liquid core at distinct sections (Fig.4.17(a)). A further increment of 1.9°C (i.e. \( T_{\text{liquid}} = 20.4°C \)) leads to complete shattering of the jet and a spray-like behaviour (Fig.4.17(b)).

For a larger nozzle, the shift of the break-up distance towards the nozzle exit is also apparent. Fig.4.16(a), Fig.4.18(a) and Fig.4.18(b) give the disintegration patterns of the jet exiting a 2 mm diaphragm type orifice where L/D is assumed 0 for \( T_{\text{liquid}} = 14°C, 18.2°C \) and 20.2°C, respectively.

For the lowest superheat, (Fig.4.16(a)), the disintegration is close to the nozzle, how-
4.2.2. Effect of the temperature

(a) "Experiment 4": a view of the jet exiting from a 1mm nozzle up to 76 X/D downstream distance, \([T_{\text{liquid}} = 18.5^\circ C, \Delta T = 44.9^\circ C, P_{\text{liquid}} = 942kPa}\)

(b) "Experiment 5": a view of the jet exiting from a 1mm nozzle up to 74 X/D downstream distance, \([T_{\text{liquid}} = 20.4^\circ C, \Delta T = 46.8^\circ C, P_{\text{liquid}} = 886kPa}\)

Figure 4.17: High-speed image sequences for jets issuing from a 1mm nozzles at two different superheat
ever, the liquid body is visible till the end. With the increment of 4.2°C (Fig.4.18(a)), the disintegration pattern changes character, after 25 X/D there is only the very dense cloud apparent. Starting from the nozzle exit and up to X/D=25 there is a transition period for the bubbles to grow. Surface deformation due to the bubble growths are very apparent.

Additional increment of 2.0°C, shifts this very dense cloud made of fine droplets closer to the nozzle (approximatively X/D=15). More surface bubbles are observed. (Fig.4.18(b))

Figure 4.18: High-speed image sequences for jets issuing from a 2mm nozzles for two different superheat

4.2.3 Effect of the backpressure

As explained in Section 2.1, the breakup phenomena of the nonsuperheated liquid jets are controlled by both the internal turbulence in the nozzle and the interfacial force between the jet and the surrounding medium. With the increase in the pressure, i.e. in the velocity, the interfacial forces such as shear forces and pressure perturbations around the jet become important factors in the motion of the interface. The turbulence in the internal flow increases with increase in the velocity. It is emphasized by
Hiroyasu[55] that the increased velocity finally results in cavitation fixed at the entrance of the nozzle generating strong turbulence in the internal flow of a nonsuperheated jet, and this strong turbulence is amplified by the inertial forces mentioned. Under these conditions, the jet is expected to disintegrate quickly into a spray.

In a superheated jet disintegration the mechanical effects are coupled with the thermal effects. The increase in pressure leads to an increase in internal turbulence in liquid resulting in more bubble activation, however, since the injection rate is higher the bubble residence time is shorter. Fig. 4.19 and Fig. 4.20 present the pressure increase effect for nozzles of 1, 2 and 3 mm in diameter at low and high liquid temperatures, respectively. In both figures, the left-hand-sides refers to a pressure of ~850 kPa whereas the right-hand-side is dedicated for the backpressure of ~1250 kPa.

The comparison is not straightforward. For 1 mm nozzle at low liquid temperature \(T_{\text{liquid}} \approx 13^\circ C\), an increase of bubble numbers is observed with pressure increase in Fig. 4.19(a) and Fig. 4.19(b). Moreover, comparisons of Fig. 4.23(a), 4.23(b), 4.19(c) and 4.19(f) representing the visualizations of 2 and 3 mm nozzles show that pressure increase creates more surface disturbances. However, no obvious effect on the breakup length can be defined globally. For high liquid temperature \(T_{\text{liquid}} \approx 20^\circ C\), the high superheat activates strongly the bubble production and shatters the jet at the nozzle exit and all along the liquid surface. In this case, the pressure increase influence cannot be decoupled from thermal effects.

However, a very clear and interesting effect of pressure can be seen in Fig. 4.21 when the flow at low temperature exiting from a 4 mm nozzle is observed. Fig. 4.21(a) shows the behaviour when a backpressure of 824 \(kPa\) is applied whereas Fig. 4.21(b) displays the flow pattern at 1226 \(kPa\). The liquid temperature is low such that the thermal effect does not dominate the breakup though bubble growth is still existent. For the low pressure case, the liquid core of the jet is not stable and bursts of droplets occur often at the nozzle or very close to the nozzle. The breakup and cloud formation distance from the nozzle is stabilized increasing the backpressure to 1226 \(kPa\). The pressure increase limits the residence time of bubble inside the nozzle such that they burst at \(\sim 3D\) from the nozzle exit.

**4.2.4 Effect of the \(l/D\) ratio**

For a nonsuperheated liquid jet, increasing the \(l/D\) in the nozzle design is expected to change the internal turbulence in the nozzle leading to a decrease in the stability of the jet. In this case the nozzle shape, especially in the form of a sharp edged orifice, can lead to cavitation resulting in stronger internal turbulence (Hiroyasu[55]). In the case of a superheated liquid jet, the contact with the nozzle walls will enhance bubble creation and growth activating the existing nucleus on the nozzle walls. The bigger is the \(l/D\), the longer is the residence time of the bubbles inside the nozzle and the higher is the amount of initial nucleus.
Figure 4.19: Randomly selected high speed images for 1,2,3mm nozzle diameters for two backpressure at low liquid temperature
4.2.4 Effect of the $l/D$ ratio

(a) 1mm nozzle $T_{\text{liquid}} = 20.1^°C$ $P_{\text{liquid}} = 320kPa$

(b) 1mm nozzle $T_{\text{liquid}} = 19.5^°C$ $P_{\text{liquid}} = 1224kPa$

(c) 2mm nozzle $T_{\text{liquid}} = 20.5^°C$ $P_{\text{liquid}} = 846kPa$

(d) 2mm nozzle $T_{\text{liquid}} = 20.4^°C$ $P_{\text{liquid}} = 1228kPa$

(e) 3mm nozzle $T_{\text{liquid}} = 19.4^°C$ $P_{\text{liquid}} = 824kPa$

(f) 3mm nozzle $T_{\text{liquid}} = 20^°C$ $P_{\text{liquid}} = 1228kPa$

Figure 4.20: Randomly selected high speed images for 1, 2, 3mm nozzle diameters for two backpressures at low liquid temperature
Chapter 4. Experimental Results

(a) 4mm nozzle $T_{\text{liquid}} = 13^\circ C$ $P_{\text{liquid}} = 842kPa$

(b) 4mm nozzle $T_{\text{liquid}} = 12.8^\circ C$ $P_{\text{liquid}} = 1265kPa$

Figure 4.21: Randomly selected high speed images for 4mm nozzle diameter for two backpressure at low liquid temperature

124
4.2.4. Effect of the l/D ratio

High speed visualization displays the influence of the l/D ratio on the breakup pattern of the superheated jet. Fig.4.22(a) and 4.22(b) display the liquid jet exiting the nozzle of 2mm diameter with l/D = 2 and l/D = 7, respectively. The liquid temperature is kept very low to provide the existence of the liquid core outside the nozzle and to reduce the thermal effect in order to observe the mechanical disintegration pattern. The jet formed passing through the sharp edge orifice (l/D = 0) at the same initial conditions preserves its intact and smooth cylindrical form during all the observed flow field. From time to time, the creation and growth of bubbles leads to disturbance and burst in the liquid surface. For the similar initial condition, Fig.4.22(a) displays a highly disturbed jet for the nozzle with l/D = 2. Number of bubbles is considerably augmented and surface shattering takes place.

Increasing the l/D to 7 displays a very interesting flow pattern (Fig.4.22(b)). The liquid may exit the nozzle in the form of cloud for a time period, then the width of the cloud becomes narrow till the unbroken liquid core is found back. At this stage, the liquid core starts to burst in a perfectly periodic regime at the nozzle exit. This behavior can be explained as periodic bubble formation inside the nozzle. Similar periodic and cloudwise bubble formation is cited in the study Domnick[33]. This breakup pattern can also be linked to the vapor slug formation inside the nozzle, as mentioned in the study of Park and Lee[101].

In the following paragraphs, two sets of images will be presented to discuss the influence of l/D on the breakup pattern in connection with initial liquid temperature and pressure. The first set will present the pressure increase effect on the flow formed through nozzles with l/D = 0; 2; 7 at a low liquid temperature (Fig.4.23). The second set is obtained at the high liquid temperature at two different pressures (Fig.4.24).

For the first case, a low liquid temperature (T_liquid \approx 13^\circ C) similar to the one described above is used but with different backpressures. The left hand side of Fig.4.23 presents the backpressure of \approx 850kPa whereas the right-hand-side displays the one of \approx 1250kPa. The aim is to detect a possible change in the atomisation pattern due to pressure increase. It should be reminded that a significant effect of pressure and nozzle length on the atomisation pattern has been already observed by Park and Lee[101]. They strongly emphasize that with short nozzle or low superheat bubble formation/growth is much less active due to insufficient number of the nucleation sites and short residence time of bubbles inside the nozzle. In addition, high injection rates involve shorter residence time of the bubbles and bubbles grow less. In these conditions, longer intact core length are observed. Fig.4.23(c), 4.23(d), 4.23(e) and 4.23(f) confirm their observations for the nozzles with l/D = 2 and l/D = 7. The liquid core is is preserved for longer time for the nozzle l/D = 2 and the periodic bursts diminish significantly for the flow exiting the nozzle with l/D = 7. No macroscopic effect can be observed for the sharp edge orifice flow in Fig.4.23(a) and Fig.4.23(b) with pressure increase.

Fig.4.24 shows the change in the flow patterns for these three nozzle designs when the pressure increase is applied to liquids at a higher temperature T_liquid \approx 20^\circ C. The effect of superheat is explained in the previous subsections. As expected, higher
Chapter 4. Experimental Results

Figure 4.22: Randomly selected high speed images for 2mm nozzle with different $l/D$ at low liquid temperature and pressure

(a) 2mm nozzle with $l/D = 2$; $T_{\text{liquid}} = 12.5^\circ C$; $P_{\text{liquid}} = 580kPa$

(b) 2mm nozzle with $l/D = 7$; $T_{\text{liquid}} = 12.5^\circ C$; $P_{\text{liquid}} = 586kPa$

Figure 4.22: Randomly selected high speed images for 2mm nozzle with different $l/D$ at low liquid temperature and pressure
4.2.4. Effect of the $l/D$ ratio

(a) 2mm nozzle with $l/D = 0$; $T_{\text{liquid}} = 12.3^\circ\text{C}$; $P_{\text{liquid}} = 864\text{kPa}$

(b) 2mm nozzle with $l/D = 0$; $T_{\text{liquid}} = 12.8^\circ\text{C}$; $P_{\text{liquid}} = 1226\text{kPa}$

(c) 2mm nozzle with $l/D = 2$; $T_{\text{liquid}} = 12.5^\circ\text{C}$; $P_{\text{liquid}} = 864\text{kPa}$

(d) 2mm nozzle with $l/D = 2$; $T_{\text{liquid}} = 12.6^\circ\text{C}$; $P_{\text{liquid}} = 1252\text{kPa}$

(e) 2mm nozzle with $l/D = 7$; $T_{\text{liquid}} = 12.5^\circ\text{C}$; $P_{\text{liquid}} = 862\text{kPa}$

(f) 2mm nozzle with $l/D = 7$; $T_{\text{liquid}} = 12.5^\circ\text{C}$; $P_{\text{liquid}} = 1272\text{kPa}$

Figure 4.23: Randomly selected high speed images for 2mm nozzle with $l/D = 0; 2; 7$ for two backpressures ($\approx 850\text{kPa} \text{ and } \approx 1250\text{kPa}$) at low liquid temperature $T_{\text{liquid}} \approx 13^\circ\text{C}$
superheat results in stronger atomization for all three nozzles presented. Though no straightforward influence of the pressure on the global behaviour can be visually defined for nozzles with $I/D = 0$ and $I/D = 2$ in Fig. 4.24(a), 4.24(b), 4.24(c) and Fig. 4.24(d), a strong effect is found for the nozzle with $I/D = 7$ in Fig. 4.24(e) and Fig. 4.24(f). The pressure increase leads to a strong and complete cloudwise atomization. The periodic burst disappears completely.

4.2.5 Break-up distance of the flashing jet

As is has been stated in the literature and experimentally observed, the break-up length depends very much on the initial parameters such as superheat level, nozzle diameter and geometry, driving pressure. The highspeed visualization has shown that giving a definite breakup length is quasi impossible due to the various disintegration patterns that can exist in one single case.

Patterns of the atomization has been presented in Fig. 2.2(b) by Faeth[39] as a simplified model to illustrate the phenomena. Such pattern is nearly impossible to observe in the present case.

For the determination of length of the liquid core, development of a rigorous and trustable method is obligatory with a prerequisite of an exact definition of the intact liquid core length (i.e. free of bubble and/or diameter unchanged and/or straight and aligned with the axis etc...). It has to be underlined that the strict definition of the liquid core and adequate post-processing method to define a breakup length was not the goal of this present work. It would probably require a research program specially dedicated to this problem.

As a matter of fact, some theoretical models assume that the jet will break when the bubble reach the jet diameter (Lienhard and Day[71]; Wildgen and Straub[141]; Suzuki et al.[132]). The present high speed images show the events that the jet does not break even with a bubble larger than the jet diameter in Fig. 4.19(a), 4.19(b), 4.23(a) for low liquid-temperatures. On the other hand, there are cases that the liquid core breaks even if the bubbles are significantly smaller than the jet diameter. Therefore, no straightforward conclusion regarding the breakup length can be driven.

Despite the ambiguities, an approximate lengths along which the liquid core preserves its initial diameter for each test case are listed in Table 4.3.

4.2.6 Summary

The effects of initial pressure, temperature, orifice diameter and $I/D$ on the break-up pattern are studied by mean of high-speed imaging. For the same backpressure and superheat, the high-speed camera images display that the jet exiting from larger nozzle
4.2.6 Summary

(a) 2mm nozzle with $l/D = 0; T_{\text{liquid}} = 20.5^\circ C; P_{\text{liquid}} = 846kPa$

(b) 2mm nozzle with $l/D = 0; T_{\text{liquid}} = 20.4^\circ C; P_{\text{liquid}} = 1225kPa$

(c) 2mm nozzle with $l/D = 2; T_{\text{liquid}} = 20.5^\circ C; P_{\text{liquid}} = 846kPa$

(d) 2mm nozzle with $l/D = 2; T_{\text{liquid}} = 20.2^\circ C; P_{\text{liquid}} = 1236kPa$

(e) 2mm nozzle with $l/D = 7; T_{\text{liquid}} = 20.5^\circ C; P_{\text{liquid}} = 824kPa$

(f) 2mm nozzle with $l/D = 7; T_{\text{liquid}} = 20.5^\circ C; P_{\text{liquid}} = 1232kPa$

Figure 4.24: Randomly selected high speed images for 2mm nozzle with $l/D = 0; 2; 7$ for two backpressure ($\approx 850kPa$ and $\approx 1250kPa$) at high liquid temperature $T_{\text{liquid}} \approx 20^\circ C$
Chapter 4. Experimental Results

<table>
<thead>
<tr>
<th>$P_{\text{liquid}}$</th>
<th>$T_{\text{liquid}}$</th>
<th>$\sim 13^\circ C$</th>
<th>$\sim 18^\circ C$</th>
<th>$\sim 20^\circ C$</th>
<th>$\sim 13^\circ C$</th>
<th>$\sim 20^\circ C$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_{\text{nozzle}}$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1mm</td>
<td>&gt; 25</td>
<td>&gt; 10</td>
<td>&gt; 5</td>
<td>&gt; 25</td>
<td>&gt; 5</td>
<td>&gt; 3</td>
</tr>
<tr>
<td>2mm</td>
<td>10-20</td>
<td>&gt; 10</td>
<td>&gt; 5</td>
<td>&gt; 10</td>
<td>&gt; 5</td>
<td>&gt; 3</td>
</tr>
<tr>
<td>3mm</td>
<td>&gt; 5</td>
<td>&gt; 3</td>
<td>&gt; 3</td>
<td>&gt; 5</td>
<td>&gt; 3</td>
<td>&gt; 3</td>
</tr>
<tr>
<td>4mm</td>
<td>0-3</td>
<td>0</td>
<td>0</td>
<td>&gt; 3</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>2mm $i/D = 2$</td>
<td>&gt; 10</td>
<td></td>
<td>&gt; 5</td>
<td>&gt; 10</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2mm $i/D = 7$</td>
<td>0-10</td>
<td>0-3</td>
<td>0-10</td>
<td>0</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 4.3: The break-up lengths in $X/D$ for different initial conditions

presents a more violent break-up than the smaller one. Additionally, the disintegration of the larger jet is closer to the nozzle exit. The high-speed images presented in this study show also that the behaviour of the jet with superheat increment up to 6.4 °C changes from slow expansion to a spray-like behaviour. The results obtained with high speed imaging are in agreement with previously published data.

4.3 Spray characteristics of the atomized superheated liquid jet

The main objective of the present section is to characterize the atomization of the superheated liquid jet once it disintegrates into droplets. The evolution of the size, velocity and temperature of the formed droplets is provided. The influence of initial flow conditions such as liquid storage pressure (called “backpressure” or “driving pressure” hereafter), nozzle geometry (i.e. diameter and length) and superheat on the resulting spray characteristics is also investigated.

Spray characteristics are obtained in the final test facility by means of Phase Doppler Anemometry (PDA) at relatively far field dimensionless axial distances $X/D = 110; 220; 440$ compared to the locations investigated with high-speed imaging which is up to $X/D = 90$. The high rejection rates of data due to the existence of fragments of liquid core, ligaments and non-spherical droplets result in a choice of measurement location where the liquid core is assumed to be completely disintegrated into droplets. These distances are significantly downstream than the measurement locations of the preliminary measurements. The reason is the different flow conditions such that the liquid exits the nozzle as two-phase flow in the preliminary measurements due to a long discharge tube placed inside the commercial nozzle. This has made measurements close to the nozzle possible. However, at the final test facility, the jet exits as liquid core and breaks up rather downstream distances.

Fig.4.25 displays the thermodynamical conditions of the investigated test cases listed in Table.3.4 compared to saturation and spinodal curves. The data range that is investigated is clearly short due to the limitations in the measurement technique. Liquid temperatures smaller than 20°C lead to low validation rates due to an incomplete
4.3. Spray characteristics of the atomized superheated liquid jet

atomization, non-spherical droplets and ligaments. On the other hand, high liquid temperatures as well as large diameter nozzles create very dense sprays, which obscure signals.

![Saturation curve of R-134A](image)

Figure 4.25: The thermodynamical conditions of the PDA test cases.

For most of the cases, the measurements were taken at the following two axial locations: $x/D = 110$ and $x/D = 220$. However, for the reference case, the axial position $x/D = 440$ is added. For each measurement campaign, two radial profiles (horizontally and vertically) are taken to detect a possible effect of gravity leading to rain out. For most of the cases, the experiments are repeated at least once within more or less similar conditions in terms of initial liquid temperature and pressure to qualify the repeatability level. The achievement of strictly equal initial conditions was nearly impossible in the laboratory.

For the main analysis for each test case, the horizontal and vertical profiles are superposed and averaged around the spray X-axis for a $r/D$ representation for one single radius as illustrated in Fig. 4.26. Fig. 4.27(a) and Fig. 4.27(b) show the superposition of both Y-axis radial and Z-axis radial profiles of diameter and velocity for the jet exiting a 1mm-nozzle at around $20^\circ C$ liquid temperature and with a backpressure of around 800kPa. These figures display different test cases at similar conditions. An axisymmetry is observed in spite of some discrepancies in values due to the non-equilibrium nature of the flow, and due to gravity effect. Nevertheless, the gravity effect is not taken into account and an average of the four directional radius is taken to represent the spray cross-section through a single radial direction.
This almost axisymmetric behavior and the desire of having more populated statistical representation of the droplet diameter and velocities in terms of sampling numbers, the superheated atomized jet is represented in one quarter of its cross-sectional surface area. Originally, the PDA collects 20 000 droplet sizes at each measurement point $r/D$. The superposition of the measurement events at different times but similar conditions, superposition of the vertical and horizontal radial profiles results in an increase of the collected droplet numbers from 20 000 samples to a minimum of 80 000 samples. For some cases, this total number per $r/D$ reaches up to 400 000 samples due to several repetitions. This increase in the sampling number is thought to give a more correct idea of the droplet distribution in such a thermodynamically unstable flow where repeatability of the measurements in terms of initial conditions and spray characteristics is really challenging. Fig. 4.28(a) and Fig. 4.28(b) compare the separate data points presented in Fig. 4.27(a) and Fig. 4.27(b) superposed on the same $r/D$'s in one quarter of the cross-section area and the final-averaged values from the collection of the separate points per $r/D$. The initial conditions are also represented in the averaged form. The condition associated to “Test 1” is given in Table.3.4. From now on, the comparisons and comments will be given on the final-averaged profiles.

Fig. 4.29 gives the count percentage distributions for the separate measurements points and the sum of all the separate points at $r/D = 0$ for the “Test” cases displayed in Fig. 4.27(a). The distribution of the sum of all the three separate points gives good description of the spray behaviour.

The total droplet counts per radial position $r/D$ for all the “Test” cases that are listed...
4.3. Spray characteristics of the atomized superheated liquid jet

Figure 4.27: Superposition of horizontal-radial and vertical-radial diameter profiles for the test cases with similar initial liquid temperatures and back pressures.
Figure 4.28: Spray representation of the horizontal-radial and vertical-radial diameter profiles in one quarter of the spray cross-sectional area and the final-averaged mean profiles (symbols with line).
4.3.1 Droplet Size Characterizations

In this section, the mean droplet size evolution of the superheated flashing jet is investigated in the radial and axial directions. The influence of the superheat level, back pressure, nozzle diameter and nozzle length-to-diameter ratio on the mean droplet size profiles are emphasized.

4.3.1.1 Evolution of mean drop size profiles in radial and axial directions

For a jet issuing from a 1 mm nozzle with a driving pressure of \( \sim 800\,\text{kPa} \) (790-830kPa) and a superheat of 46.4-47.0°C (liquid temperature of 20 - 20.6°C) (the reference case in Table 3.4 "Test's 1;2;3"), the radial profiles of the arithmetic mean \( (D_{10}) \) and Sauter mean \( (D_{32}) \) diameters are plotted in Fig. 4.31 for three axial stations \( (x/D = 110, 220 \) and 440). As it can be seen in Fig. 4.31, at all axial distances, both the arithmetic mean
Chapter 4. Experimental Results

Figure 4.30: The total counts of droplets per radial location for different "Test" cases presented in Table 3.4.

$(D_{10})$ and Sauter mean $(D_{32})$ diameters are larger in the centre (axis) and they decrease in the radial direction. Towards the periphery the $D_{32}$ approaches $D_{10}$ becoming more mono dispersed.

One interesting point is the evolution of the profiles in axial direction. Going downstream the nozzle, the $D_{10}$ value shows a systematic increase (almost parallel radial profiles). For the $D_{32}$ values, this is valid for $2 \leq r/D \leq 12$. On the jet axis $(r/D < 2)$, $D_{32}$'s for both axial distances $x/D = 220$ and $x/D = 440$ are similar to each other. However, they are significantly smaller compared to the one at $x/D = 110$. This result can be explained by the evaporation process. Evaporation leads to quicker disappearance of the small droplet contents of the distribution (i.e. the count % associated to small diameters decreases). This automatically leads to an increase of the arithmetic mean $D_{10}$. On the other hand, this disappearance of small droplet classes let the total volume almost unaffected whereas the total surface decreases leading to an increase of $D_{32}$.

As explained in the bibliographic survey chapter, Park and Lee [101] and Reitz [112] found also large droplets in the centerline and smaller in the periphery. Balachandar et al. [11] found a similar trend in the arithmetical mean droplet diameter evolutions along the radius. They also indicated that the Sauter mean diameters are increasing moving further from the nozzle. This observation is fitting with the results presented in Fig. 4.31 for the profiles at $x/D = 220$ and $x/D = 440$. The profile at $x/D = 110$ follows this trend except in the very core region. A suitable explanation for the different
4.3.1. Droplet Size Characterizations

behaviour in the core region is the incompleteness of the jet break-up process. On the other hand, one has to keep in mind that the compared measurement points for the case of Balachandar et al. [11] were $x/D = 800$ and $x/D = 2400$ which are far downstream from the measurement points presented in Fig. 4.31. Disappearance of a large percentage of small particles by evaporation may also explain the increase of the Sauter mean diameter.

One may also observe that $D_{10}$ and $D_{32}$ get roughly closer on the axis when the axial distance from the nozzle increase. This is a sign that the diameter distribution slowly converges towards a more monodispersed distribution.

4.3.1.2 Effect of the temperature on the drop size

High-speed camera visualizations have illustrated that even a narrow superheat increment such as ~ 2°C may lead to significant changes in the break-up patterns, as also observed in literature by Park and Lee [101]. Therefore, a significant effect of superheat on the spray characteristics is expected to be found.

To have a global view of the effect of the liquid temperature, the $D_{10}$ and $D_{32}$ values are computed on the total cross-section at an axial location $x/D$. Their evolution is plotted in Fig. 4.32 with respect to Jacob number $Ja$ which is a strong function of liquid superheat ($Ja = (C_{pl} \Delta T_{l2})/h_{LG}$). A clear decrease in droplet diameters is observed with the increase of $Ja$, whatever is the pressure, nozzle diameter or axial location.
With the superheat effect, the $D_{32}$ and $D_{10}$ values approach each other significantly. In other words, with very high superheat the distribution converges to a mono disperse one. It can be conjectured that at a superheat corresponding to $0.38 \leq Ja \leq 0.40$ the global distribution over the radius at $x/D = 110$ will be mono disperse.

![Figure 4.32](image)

Figure 4.32: The evolution of global $D_{10}$ and $D_{32}$ values obtained from total cross-section with Jakob number $Ja$.

Fig. 4.33 shows the effect of liquid temperature on the $D_{32}$ and $D_{10}$ radial profiles at $x/D = 110$ and $x/D = 220$. Looking at the Sauter mean $D_{32}$ diameter evolution at the close field ($x/D = 110$), one can observe that the lowest liquid temperature ($T_{liq} = 20.2^\circ C$) gives very high value at $r/D = 0$ and this value decreases significantly with an increment of ~ $3^\circ C$ of superheat ($T_{liq} = 23.3^\circ C$). A further increase of ~ $1.3^\circ C$ ($T_{liq} = 24.6^\circ C$) does not show any strong effect comparing to the previous one. The increase in the liquid temperatures up to $27^\circ C$ and $28.3^\circ C$ leads to a systematic decrease of $D_{32}$-values all over the radial profile. For the close field ($x/D = 110$), the liquid temperatures $23.3^\circ C$ and $24.6^\circ C$ give slightly higher diameters than for the temperature $20.2^\circ C$ for $r/D \geq 5$. The higher temperatures $27^\circ C$ and $28.3^\circ C$ provide smaller diameters all over the radial profile.

In the far field ($x/D = 220$), the effect of the liquid temperature is more clear. With the increment of temperature, smaller droplet diameters are observed all over the profile compared to the lowest temperature. Moreover, the droplet profiles become flatter and droplet mean sizes more uniform with increasing superheat, i.e., the distributions become almost mono disperse and identical over the radius especially for the experiment of $T_{\text{liquid}} = 26.4^\circ C$.

Only the experiment done at $T_{\text{liquid}} = 23.3^\circ C$ shows a discrepancy along the radius. For
4.3.1. Droplet Size Characterizations

$r/D \leq 6$ it has smaller droplet diameters than the experiment done at $T_{\text{liquid}} = 20.6^\circ\text{C}$, afterwards ($r/D \geq 6$) the droplets are larger. In this case, smaller superheat leads to a radial distribution having a higher arithmetic mean diameter ($D_{10}$) at $r/D = 0$ than the one of the higher superheat. At this axial position, the envelope of the spray expands radially as superheat increases. This expansion may result in a shift of some big droplets towards the edge (opening of the spray) and may explain why the arithmetic mean diameter ($D_{10}$) and Sauter mean diameter ($D_{32}$) show an opposite behaviour in the periphery than the centre for increasing superheat. As it will be explained in Section 4.3.4, the velocity distribution does not show a significant change, therefore, the difference in diameter distribution may be explained only by an atomisation process due to a higher production of bubbles than due to aerodynamic break-up effect.

These observations are consistent with those of Brown and York [21], Peter et al. [102], Park and Lee[101], Gooderum and Bushnell[24] and Gemci et al.[47]. They also observed a decrease of the mean diameter size with increasing superheat at constant back
Chapter 4. Experimental Results

pressure. Moreover, the droplet size measurements of Nagai et al. [90] at 250mm downstream the nozzle \((x/D = 500)\), showed a decrease of \(D_{32}\) with the increase of the dimensionless superheat, as well.

4.3.1.3 Effect of the drive pressure on the mean drop size profiles

Literature work regarding the mechanical break up of the cylindrical jets highlights the importance of the exit velocity (i.e. back pressure) on the disintegration mechanism and thus final drop sizes. Therefore, the effect of the backpressure is investigated in this subsection.

To separate the effect of the backpressure from the one of the superheat, measurements have been performed by pressurizing with \(N_2\) while keeping the superheat constant. Indeed, measurements have shown that the increase of the storage pressure results in different flow patterns with different liquid temperatures. Fig. 4.34 and 4.35 shows the evolution of the averaged droplet diameters \((D_{10} \text{ and } D_{32})\) with increasing pressure using three different liquid temperatures.

In Fig. 4.34(a) and Fig. 4.34(b) the backpressure effect is shown as changes in \(D_{32}\) and \(D_{10}\) based on the total cross-sectional area. According to these figures, high liquid pressure provides a less sharp decrease in mean \(D\)-values with increasing \(Ja\) compared to low pressure conditions (Fig. 4.34(a)) at same axial location. Again for the same axial location, pressure increase results in a decrease in \(D_{32}\) for low \(Ja\). A slight increase in \(Ja\) (i.e. superheat) shows no change in \(D_{32}\) with pressure increase, a further more increase in \(Ja\) leads to an increase in \(D_{32}\) when pressure is increased (Fig. 4.34(b)). One can conclude that at low \(Ja\), pressure effect is more pronounced and this can be interpreted as mechanical breakup domination. At high \(Ja\), however, pressure effect is small and thermal effects dominate the atomization. Another important observation is the approach of \(D_{32}\) and \(D_{10}\) values at the very high \(Ja\). It means that the sprays tends to become monodispersed, being more pronounced for low liquid pressure.

To display the effect of pressure increase at a low liquid temperature \((Ja \approx 0.32)\), radial profiles for the jet initiated from a 1 mm nozzle have been measured for two axial locations \(x/D = 110\) and \(x/D = 220\). Fig. 4.35(a) and Fig. 4.35(b) display them. For this case, the pressures used are 790 - 800kPa, and 1200 - 1210kPa. The \(D_{32}\) and \(D_{10}\) decrease significantly with increasing pressure at the centerline when the measurement at \(x/D=110\) is concerned (Fig. 4.35(a)). Towards the periphery, higher pressure gives higher \(D_{32}\) but no change is observed on the \(D_{10}\). At \(x/D = 220\), the changes on the \(D_{32}\) and \(D_{10}\) are insignificant all over the profile (Fig. 4.35(b)).

The test pressures for the second temperature \((T_{\text{liquid}} \sim 24^\circ C \text{ and } Ja \approx 0.34)\) are 790kPa, 1200kPa and 1390kPa (Fig. 4.35(c)). For this case, the pressure increment does not affect the \(D_{10}\) and \(D_{32}\) values clearly all over the radial profile.

For the last liquid temperature \((T_{\text{liquid}} \sim 27^\circ C \text{ and } Ja \approx 0.36)\), the pressure effects of
4.3.1. Droplet Size Characterizations

4.3.1.1 Pressure effect with changing Ja

(a) Pressure effect with changing Ja
(b) Pressure vs. $D_{32}$ for $D_{nozzle} = 1\text{mm}$ at $x/D = 110$

Figure 4.34: Pressure effect on the drop sizes for different Ja.

820kPa and 1350kPa are compared in Fig. 4.35(d). $D_{10}$ and $D_{32}$ are higher for the higher pressure all over the radial profile.

In the literature, the decrease of droplet diameter with pressure increase was also observed by Brown and York[21] and Hervieu and Veneau[53]. However, it has to be stated that the measurements in the work of Hervieu and Veneau[53] have been taken at saturation conditions, that is, when the injection pressure is high the superheat is high, as well. Obviously the superheat has a stronger influence on atomisation than the injection pressure, therefore, it is expected to have systematically smaller droplets with increasing pressure, in this case.

4.3.1.4 Orifice diameter effect on mean profiles

According to Brown and York [21] smaller jet diameters seem to give larger droplets diameter when the superheat and driving pressure are kept constant. On the contrary, Hervieu and Veneau[53] observed larger droplet diameters for larger nozzles if the absolute distance from the nozzle, driving pressure and superheat are kept constant. Moreover, Nagai et al.[90] concluded that the droplet diameters are independent of orifice diameters for short nozzles (i.e. $l/D < 7$).

Gooderum and Bushnell[24] performed tests on water jets with very weak ambient pressures ($2.5 < P_{ambient} < 130\text{mbar}$) to neglect the effect of aerodynamics forces on the break-up. They found that droplet diameters are proportional to the orifice size ($D_{droplet}/D_o = f(T_{storage})$), when the Weber number is small.

To clarify these ambiguities, experiments are performed using 1mm and 2mm nozzles.
Figure 4.35: Pressure effect on the profiles for different initial liquid temperatures.
4.3.1. Droplet Size Characterizations

Attempts to perform measurements with 3mm and 4mm nozzles have failed due to high droplet densities in the path of the laser beams obscuring the signal.

It is hard to evaluate the influence of the nozzle diameter on the $D_{32}$ and $D_{10}$. Fig.4.36(a) shows that 2mm nozzle produces slightly smaller $D_{32}$ than 1mm nozzle at the same $Ja$ number and $x/D$. However, the $D_{10}$ values are higher than the one of 1mm nozzle and their difference in $D_{10}$ diminishes with increasing $Ja$. If the comparison is done in $x$ distances (i.e. the experiment of 1mm nozzle at $x/D = 220$ is compared with the experiment of 2mm nozzle at $x/D = 110$), the difference between the two nozzles are negligible (Fig.4.36(b)).

![Figure 4.36: Effect of nozzle diameter on the drop sizes at different $Ja$ (Results are presented in non-dimensional and dimensional axial distances).](image)

A better understanding can be drawn when looking at the radial mean drop size profiles. Fig. 4.37 represents the evolution of $D_{32}$ and $D_{10}$ radial profiles with two different nozzle diameters (1mm and 2mm). Both measurements are taken at similar initial conditions. Fig. 4.37(a) and Fig. 4.37(b) show the comparison of experiments taken at non-dimensionalized axial distances whereas Fig. 4.37(c) and Fig. 4.37(d) compare the experiments taken at dimensionalized axial distances. Both representations illustrate that bigger nozzle diameters lead to larger $D_{32}$ and $D_{10}$ values all over the profiles, however, the difference is more pronounced when the comparisons are done in non-dimensional distances (Fig. 4.37(a) and Fig. 4.37(b)).

4.3.1.5 Influence of the orifice length-to-diameter ($l/D$) ratio on drop mean profiles

To investigate the effect of the nozzle length-to-diameter ($l/D$) ratio, three designs are compared. The nozzle diameter is 2mm with length-to-diameter ratios of $l/D = 0; 2; 7$. The mean values $D_{10}$ and $D_{32}$ representing the cross-sectional area are compared at two axial locations $x/D = 110$ and $x/D = 220$ in Fig.4.38. At $x/D = 110$, the
Figure 4.37: Effect of nozzle diameter on the profiles at different axial locations (Results are presented in non-dimensional and dimensional axial distances).
4.3.1. Droplet Size Characterizations

nozzles $l/D \simeq 0$ and $l/D = 2$ produce the same $D_{10}$ while the nozzle with $l/D = 7$ leads to larger $D_{10}$. $D_{32}$ of the sharp orifice is higher than the other two, and the $D_{32}$ of the $l/D = 2$ is the smallest. In the far field at $x/D = 220$, the increase in $l/D$ results in smaller $D_{32}$ and $D_{10}$. The differences in the mean diameters for the experiments of $l/D = 2$ and $l/D = 7$ are, however, small.

The high speed visualizations in Fig.4.24 can provide a possible explanation for this behaviour. The nozzle with $l/D = 7$ preserves higher amounts of the liquid core due to periodic burst compared to the smoother atomization that the nozzle with $l/D = 2$ provides. Therefore, the incomplete atomization created by the nozzle with $l/D = 7$ leads to larger droplets at $x/D = 110$. Evaporation of the already very small droplets formed by the nozzle with $l/D = 2$ and further atomization of the large droplets created through the nozzle with $l/D = 7$ approach the mean values to each other at $x/D = 220$.

This behaviour can be more clearly analyzed looking at the radial profiles. Having compared the $D_{32}$ profiles, one can easily see that the jet exiting the sharp edge orifice ($l/D = 0$) has largest droplet diameters in the centerline $r/D = 0$ (Fig. 4.39(a)). The droplets formed from a jet exiting the nozzle with $l/D = 2$ are smaller than the sharp edge orifice all over the profile. Using a nozzle with $l/D = 7$ gives results larger than the one of $l/D = 2$ everywhere in the radius. At the centerline, its $D_{32}$ is lower than the one of $l/D = 0$, however, it becomes larger at the periphery. Moreover, the jet expansion formed from the $l/D = 7$ is larger than the other two nozzles. The $D_{10}$ profiles show that the higher values are obtained for the nozzle with $l/D = 7$. The other two nozzles give similar results.

For the far field (Fig. 4.39(b)), the $D_{32}$ and $D_{10}$ values for the nozzle with $l/D = 0$ is higher all over the measured part of the radius. The other two nozzles with $l/D = 2$ and $l/D = 7$ provide atomisation with similar (almost identical) $D_{32}$ and $D_{10}$ at this
Chapter 4. Experimental Results

axial location. This observation is consistent with the observation of Nagai et al.\cite{90} since their measurements are taken at rather downstream axial locations.

Nagai et al.\cite{90} found out that the droplet diameter is independent of \( l \) in case of \( l/D \leq 7 \), that means that the real orifice behaves as an ideal orifice (\( l \sim 0 \)). They believed that it was due to the detachment of the flow from the wall of the orifice so that there was no nucleation in the wall. They concluded that in case of external flashing the droplet diameters did not depend on the \( l \) as long as \( l/D \leq 13 \). Above this value, they decrease as \( l/D \) increases due to internal flashing.

\begin{figure}[h]
\centering
\includegraphics[width=\textwidth]{figure4_39.png}
\caption{Effect of nozzle length/diameter ratio on the drop size profiles at different axial locations.}
\end{figure}

4.3.1.6 Summary

The effect of different initial pressures, temperatures and orifice diameters on the droplet mean size profiles of a flashing R134A jet along the radial and axial directions are studied by means of Phase Doppler Anemometry (PDA).

Radial evolutions shows that the droplet sizes are larger in the centre of the jet and they decrease towards the edges of the jet. When axial evolution is concerned, the droplet sizes decrease going further from the nozzle due to evaporation and further break-up of large drops. Moreover, a spreading in the width of jet is also clearly observed.

In the measured range of \( J_a \), the liquid superheat plays the most dominant role on the droplet diameters no matter what is the drive pressure, nozzle diameter or axial location. A clear and sharp decrease in droplet sizes is observed with the increase of superheat. The \( D_{32} \) and \( D_{10} \) approach each other significantly representing a more monodisperse distribution. The envelope of the spray is wider. Moreover, the mean drop size radial profiles becomes flat and the mean drop sizes are more uniform with
4.3.2. Droplet count and mass distributions

The increase in the storage pressure results in different flow patterns with different flow temperatures. When the measurements are done with a low superheat \((Ja \approx 0.32)\) but different pressures, it is observed that the droplet sizes decrease with increasing pressure with a more important effect on the centerline than towards the periphery. With a higher superheat \((Ja \approx 0.34)\), no change of mean drop sizes is observed with increasing pressure. Further increase in superheat \((Ja \approx 0.36)\) results in higher mean drop sizes with pressure increase. However on a global point of view, high pressure liquids provide a less sharp decrease in mean drop size with increasing superheat than low pressure liquids as if the mechanical and thermal effects counteract.

It is not straightforward to evaluate the effect of the nozzle diameter on the drop sizes. The mean drop size radial profiles shows that the larger nozzle leads to slightly larger mean diameters of \((D_{10} \text{ and } D_{32})\) than the smaller one. On a more global view, the change of drop sizes with the nozzle diameter is insignificant compared to the influence of the superheat.

Except the sharp edge orifice with \(l/D \sim 0\), nozzles with \(l/D = 2\&7\) are also tested. Increasing the length-to-diameter ratio's of the nozzle diameter results in smaller diameters compared to the sharp edge orifice. In the close field, the \(l/D = 7\) provides larger drop sizes than \(l/D = 2\) most probably due to incomplete atomization created by the periodic existence of bursts and liquid core together.. In the far field, the differences in drop sizes for both nozzles disappear as observed in the literature by Nagai et al.[90].

4.3.2 Droplet count and mass distributions

In this section, the effects of the initial parameters on the droplet count and volume (mass) distributions are discussed. Together with the axial and radial evolution of the distributions, the macro behaviour of the atomized jet is investigated using all droplets from a given radial cross-section to give a global view instead of a point-wise analysis.

4.3.2.1 Effect of the spatial location on count and volume (mass) distributions

Fig. 4.40(a) and Fig. 4.40(b) displays the droplet size count distributions for different radial positions in the jet and at the two axial locations. At both \(x/D = 110\) and \(x/D = 440\), the distribution shows higher percentage of large droplets in the centerline \((r/D = 0)\) than in the periphery \((r/D \geq 4)\). The radial evolution shows that on the spray axis (i.e. \(r/d = 0\) the number of the large droplets are high and going from the spray axis towards the board (periphery) of the spray radius, this percentage for the large droplets decreases. In this sense, it can be said that the distribution on the edge of the spray have a pattern closer to a mono dispersed distribution than a poly-dispersed one. Nevertheless, this remark is only true at the position \(x/D = 110\). At this
Chapter 4. Experimental Results

location, the primary fragmentation is still active. The axial droplets are the results of the liquid jet breakup and are rather large. The satellite droplets are the results of both the mechanical instabilities and the bursts of bubbles, and so they are rather small. Going further downstream along the axis (at \( x/D = 440 \)), the breakup of the rather large droplets from the axis at \( x/D = 110 \) spread away towards the edge of the spray. This opening of the spray leads to a distribution offering a more poly-dispersed pattern from the axis of the spray to the edge radius \( r/D = 9 \).

The mass percentage distributions displayed in Fig. 4.40(c) and Fig. 4.40(d) confirm the predominance of large droplets on the centerline of the spray, when the edge of the spray are occupied by a larger mass fraction of small droplets.

From Fig. 4.40(a) an oscillation (i.e. several peaks) of both the count and mass distribution on the axis is noticed. A similar observation has been reported in the study of Touille[?]. This oscillation with a wavelength of the order of 100\( \mu \text{m} \) is thought to be a technical artifact. After several repetitions of the measurements with careful verifications no technical drawback could be identified. However, as explained before, one has to keep in mind that the PDA technique can face difficulties in measuring the large and thus nonspherical drop sizes. It has to be emphasized here that when the triple peaks appear in the core of the jet \((r/D = 0; 1)\) for all the experiments where the liquid temperature is low \((T_{\text{liquid}} \sim 20^\circ \text{C})\), the validation for this measurements are also low due to the high rejection rates of non-spherical droplets.

It is noticeable that this oscillations do not appear on the edge of the spray. One possible physical interpretation could be the slug of liquid jet observed by Park and Lee.[101] on the center of the flashing jet with their instantaneous images. But this conjecture could not be verified here. However, the high speed images obtained for this flow condition do present high presence of liquid volume on the centerline. The incomplete atomisation of the large droplets due to the weak superheat and the existence of the large nonspherical droplets in core region of the jet can create this oscillation. This reasoning is enhanced when the triple peaks disappear with increasing \( r/D \) distance towards the periphery.

Another important fact has to be kept in mind. On the count distribution, one can see that the extreme diameters are roughly 5\( \mu \text{m} \) and 375\( \mu \text{m} \). Let’s imagine an experiment where the probability to find a droplet of 375\( \mu \text{m} \) is reduced to 1 event over 100 000 events constituted of droplets of 5\( \mu \text{m} \). This large droplet would have on the \% count diagram a value of about \( 10^{-3} \). Nevertheless, on the mass percentage this unprobable event would be equivalent to more than four times the total mass associated to the 100 000 small droplets.

It is the reason why, on the different graphs proposed all over the thesis, the unprobable events \( \leq 10^{-2} \) in \% percentage are not ‘filtered out’ but are displayed to keep in mind the importance and the polydispersed distribution characteristics in a flashing jet.

Fig. 4.41(a) and Fig. 4.41(b) display the axial evolution of the droplet size count percentage distributions. For a clear understanding, two radial points are chosen (i.e. the
4.3.2. Droplet count and mass distributions

Figure 4.40: Droplet count distributions at different radial location for different axial locations.
Chapter 4. Experimental Results

axis \((r/D = 0)\) and a point at the periphery \((r/D = 8)\) for three axial positions.

As can be seen in Fig. 4.41(a), at the axis \((r/D = 0)\) the distribution has a hilly pattern having peaks at around \(20\mu m; 200\mu m; 300\mu m\) and nearly \(400\mu m\) at the close field \((x/D = 110)\). For the other axial positions \((x/D = 220\) and \(x/D = 440)\), this hilly pattern persists, however, with a decrease in percentage count for the large droplets. The highest peak at the very small size classes shifts towards larger size classes mostly due to evaporation.

There is a tendency to a “uniform distribution” going downstream. The very large droplet classes around \(300\mu m\) keep on having an important effect for mass percentage distribution. Going downstream the nozzle, this effect decreases very slightly. The big change is seen mostly on the very little classes and middle classes. From \(x/D = 110\) to \(x/D = 220\) a very clear increase and uniformization is observed in mass percentage of the droplet classes ranging between \(20 - 200\mu m\). The distribution becomes even more uniform in the furthest downstream axial distance \(x/D = 440\).

Fig. 4.41(b) shows that at the periphery of the atomized jet the count percentage of the large droplets increase going further downstream the nozzle whereas the count percentage of the very small droplets decrease drastically due to the evaporation. A probable reason of the increase of the percent count distribution further downstream is also the migration of the big droplets towards the periphery due the dispersion and expansion of the jet in the further field.

The evaporation effect is also clear in mass percentage distributions displayed in Fig. 4.41(d). The peak that the small droplet classes form decreases in percentage and its location shifts towards larger droplet sizes.

4.3.2.2 Effect of the temperature on drop size count and volume (mass) distributions

The evolution of the global droplet count and volume percentage distributions for the total cross-section at at the axial location \(x/D = 110\) under the effect of liquid superheat is displayed in Fig. 4.42(a) and Fig. 4.42(b), respectively.

For the lowest liquid temperature \((T_{\text{liquid}} = 20.2^\circ C)\), higher count percentage is observed for larger droplets. Increasing the liquid temperature leads to an atomization where the large droplet counts decrease significantly together with the oscillating pattern associated to a possible ‘slug’ regime found at low superheat by Park and Lee[101]. It is interesting to see that three distributions obtained from three different liquid temperatures \((T_{\text{liquid}} = 23.3^\circ C, 24.6^\circ C\) and \(27^\circ C)\) show similar patterns. However, even if they seem to be self similar the count and mass percentages of small droplets increase with increasing initial liquid temperature. The augmentation of small droplets and lack of large droplets are most significant for the highest liquid temperature \(T_{\text{liquid}} = 28.3^\circ C\).
Figure 4.41: Droplet count distributions at different radial location for different axial locations.
Chapter 4. Experimental Results

In the far field \((x/D = 220)\), the effect of temperature on the distributions is consistent with the close field but are not presented hereafter. Increase of liquid temperature leads into a finer atomisation diminishing the large droplets and shifting the peak values of count and mass percentages towards the small droplet classes.

\[
\begin{align*}
T_{\text{liquid}} = 20.2°C, P = 800kPa \\
T_{\text{liquid}} = 23.3°C, P = 790kPa \\
T_{\text{liquid}} = 24.6°C, P = 840kPa \\
T_{\text{liquid}} = 27.0°C, P = 820kPa \\
T_{\text{liquid}} = 28.3°C, P = 800kPa
\end{align*}
\]

Figure 4.42: Effect of initial liquid temperature on the global droplet diameter count and mass distributions for the total cross-section at \(x/D = 110\).

4.3.2.3 Pressure effect on drop size count and volume(mass) distributions

In Fig. 4.43(a) and Fig. 4.43(b), the influence of pressure increase on the count and volume distributions for \(T_{\text{liquid}} \approx 20°C, \approx 24°C\) and \(\approx 27°C\) at \(x/D = 110\) is presented. In the figures, lines represent the low pressures and symbols the high pressures. This presentation allows to demonstrate the competition between the mechanical breakup and thermal induced breakup. Three different situations are encountered.

For the lowest liquid temperature, pressure increment diminishes the number of droplets in large size classes resulting in an increase of mass and count percentage of small droplets. In this regime, superheat is low enough to have still a marked pressure effect on the fragmentation process.

For \(T_{\text{liquid}} \approx 24°C\), all the distributions superpose more or less when the pressure increases. However, we can still observe that the higher pressure leads to a slightly weaker evaporation by pushing the spray faster and leaving less time for droplets to evaporate till the measurement point. Therefore, the large droplets gain to some degree higher importance for the mass percentage than for the small pressure. Still, the difference is rather small. In this regime, superheat and pressure effect are equally responsible on the fragmentation.

For \(T_{\text{liquid}} \approx 27°C\), the count and mass percentage distributions show slightly higher
4.3.2. Droplet count and mass distributions

Figure 4.43: Effect of initial liquid pressure on the global droplet diameter count and mass distributions for the total cross-section at \( x/D = 110 \). (Lines are for low pressure and symbols are for high pressure for different superheats).

Theoretical interpretation for this last régime is difficult to interpret. The increase in superheat enhances the fragmentation because higher amount of nuclei is activated along the discharge tube walls till the nozzle exit. If the pressure effect pushes the highly superheated liquid faster, one may find a situation where the residence time to develop a bubble from a nuclei is affected. At the moment, this interpretation has to be considered with care because it can not be demonstrated further.

Theoretically, if the superheat is further increased, the fragmentation should be through homogeneous nucleation and pressure effect would not be able to perturb the fragmentation because breakup will not depend on residence time anymore.

4.3.2.4 Effect of orifice size on the drop size count and mass distributions

Fig. 4.44(a) and Fig. 4.44(b) present here the global classwise volumetric and count percentage distributions for the total cross-sections at \( x/D = 110 \) and \( x = 220 \text{mm} \). There are higher number of large droplets at the measured dimensionless axial locations for a liquid jet exiting a larger nozzle.

If this comparison is done in dimensional distances such as \( x = 220 \text{mm} \) (where the test cases \( D = 1\text{mm} \times/D = 220 \) and \( D = 2\text{mm} \times/D = 110 \) are compared), this observations is much more weaker but valid. In this case, the mass and count percentages more or less superpose with each other for droplets inferior to 150\( \mu \text{m} \) displaying at large droplet classes slightly higher values for the larger nozzle diameter.
Chapter 4. Experimental Results

4.3.2.5 Orifice length/diameter ratio on drop size count and volume distributions

Fig. 4.45(a) and Fig. 4.45(b) show the global count and mass percentage distributions for sprays produced by nozzles with different "1/D" ratios for the total cross-section at $x/D = 110$. First, one can observe the existence of the triple peaks whatever is the $l/D$ of the nozzle. The highest percentage of large droplets are observed for sharp edged orifice ($l/D = 0$). For the other $l/D$ ratios, this percentage of large classes decrease and the percentage of the small classes increase. The nozzle with $l/D = 7$ has more large drops and less percentage of small drops compared to the nozzle with $l/D = 2$.

For the axial position $x/D = 220$, the sharp edge nozzle ($l/D = 0$) has again higher percentage of large droplets. For the other two nozzles, it has been noticed that the small droplet classes percentage increases. The nozzle $l/D=2$ and $l/D=7$ give very identical distributions. These results are displayed hereafter.

The a priori prediction of the effect of the $l/D$ on the distributions is rather difficult. With a very high $l/D$, the liquid may flow through the nozzle restriction and develop in to a cylindrical flow before the exit into the atmosphere. Of course, the liquid in the nozzle additional tube is protected from mechanical aerodynamically induced instabilities but is more exposed to nuclei sites and therefore to bubble formation. For the experiment described here, the short additional length $l/D = 2$ is not long enough to obtain the development of a liquid jet but still offers supplementary sites of nuclei for the bubble formation leading to a less percentage of droplets over 200$\mu$m. The medium additional length of $l/D = 7$ seems to be long enough to start the development of a liquid jet but of course offer also more nuclei sites than the shorter tube, leading to a periodic bursts and liquid core pattern at the exit. Unfortunately, in this small range of $l/D$, the effect on the distributions is unclear.
4.3.2.6 Summary

Triple peaks in the distributions can be mentioned as a very important feature. These peaks occur mainly in case of the lowest liquid temperature \(T_{\text{liquid}} \sim 20^\circ\text{C}\) or in the core radial position (i.e. \(r/D = 0; 1\)). The two peaks in large drop classes diminish going towards the periphery and with the increase of the liquid temperature. Physically, this oscillation in the distribution may be attributed to the slug regime observed in the work of Park and Lee[101].

This observation can be interpreted twofold: The first reason is the incomplete atomization of the large droplets due to the weak superheat and the existence of the large nonspherical droplets in core region of the jet. This reasoning is validated when the triple peaks disappear with increasing \(r/D\) distance towards the periphery and with increasing liquid temperature, i.e. superheat, which has a dominant role in producing small droplets. Another possible reason is the insufficiency of the PDA technique in measuring the large and thus nonspherical drop sizes. It has to be emphasized here that when the triple peaks appear in the core of the jet (\(r/D = 0; 1\)) for all the experiments where the liquid temperature is low\(T_{\text{liquid}} \sim 20^\circ\text{C}\), the validation for this measurements are also low due to the high rejection rates of non-spherical droplets.

For low liquid temperatures, a smaller distribution is found for high pressure. This behaviour is identical to the observations for the mechanical breakup occurred in non-superheated liquid jets. For a moderate superheat, the mechanical breakup effects loses its dominance and no change in distribution is observed with increasing liquid pressures. With a further increase in superheat, thermal disintegration gain importance compared the mechanical disintegration and the higher is the pressure the larger are the droplets in the distribution. A possible explanation can be that the increase in superheat enhances the fragmentation because higher amount of nuclei is activated.
along the discharge tube walls till the nozzle exit. If the pressure effect pushes the highly superheated liquid faster, one may found a situation where the residence time to develop a bubble from a nuclei is affected.

Larger nozzle produced larger droplets. This behaviour is observed in sprays produced through mechanical breakup. However, if comparisons are done in dimensional distance the difference in the distributions for large and small nozzle is smaller. Increasing the $l/D$ ratio in a nozzle to small extent (i.e. $\leq 7$) has an unclear effect. The distributions seem to have less large droplets than sharp edge orifices. At $x/D = 110$, the atomization of the jet exiting from the nozzle with $l/D = 7$ produce rather larger droplets compared to the nozzle with $l/D = 2$. At $x/D = 220$, both nozzles produce identical distributions that are smaller than the distribution produced by the sharp edge orifice.

4.3.3 Application of various empirical distribution functions

The previous subsection points out that for some measurement cases several peaks exist in the count and volume percentage distributions. The various empirical distribution functions such as Rosin-Rammler, modified Rosin-Rammler, log-normal, upper limit, root normal and Nukiyama-Tanasawa distributions are usually applied on single peaks and are not expected to have a good representation of the distribution in case of anomalies. Therefore, direct application of these empirical functions on the count distributions associated with low superheat and presented in the previous subsection will not be fruitful.

Although the measured distributions are not adapted for the use of the traditional functions, the small droplets will be taken into account for comparison hereafter, as a first attempt, keeping only the first peak corresponding generally to $\sim 97 - 99\%$ of the total count distribution and ignoring the large droplets fewer in number. The various empirical distribution functions are applied on these selected first peaks. Here, two flow cases will be presented. These cases are the distributions of the jets exiting 1mm nozzle with a storage pressure of $\sim 800\text{kPa}$ for $T_{\text{liquid}} = 20.2^\circ C$ and $T_{\text{liquid}} = 28.3^\circ C$. The global section distributions obtained from all the droplets at $x/D = 110$ are compared. The comparison of the global section distribution weakens the effect of triple peaks, as well as providing sufficient information of the total cross-section.

The reason of selecting these cases is their different distribution patterns. For $T_{\text{liquid}} = 20.2^\circ C$, triple peaks exist in the distribution and the first peak correspond to rather limited portion of the total volume of the total droplet size classes. For $T_{\text{liquid}} = 28.3^\circ C$, however, the first peak has already the majority of the total volume. Table 4.4 shows the corresponding total count and volume percentages of the first peak of distributions for the two selected flow. One should be aware that limitation of the distribution to the first peak can not be representative with the highest fidelity for the real flow behaviour for $T_{\text{liquid}} = 20.2^\circ C$ case because it does not take into account a very considerable amount of volume though the excluded part is little in the count percentage. However, comparisons will proceed using the first peak only for the sake of understanding the
4.3.3. Application of various empirical distribution functions

relative evolution of distribution parameters in the empirical functions.

Table 4.4: Total count and volume percentages for the first peak (1mm nozzle, $P \sim 800kPa$, $x/D=110$, total cross-sectional data)

<table>
<thead>
<tr>
<th>$T_{\text{liquid}}$</th>
<th>Total count %</th>
<th>Total volume %</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T_{\text{liquid}} = 20.2^\circ C$</td>
<td>97.9% out of 1050752 drops</td>
<td>28.5%</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 28.3^\circ C$</td>
<td>99.6% out of 160352 drops</td>
<td>76.4%</td>
</tr>
</tbody>
</table>

Fig.4.46 shows the comparisons of the empirical functions to the measurements. There are five curves representing the volume distributions computed from Rosin-Rammler (RR), modified Rosin-Rammler (modified-RR), upper limit (UL) logarithmic distribution, log-normal (LN) distribution and root-normal (RN) distribution. The upper-limit distribution is computed using a maximum drop diameter equal to the nozzle diameter (i.e. $D_{\text{max}} = 1000 \mu m$). The effect of two different representative diameters such as Rosin-Rammler diameter ($D_r$) and geometric mean diameter ($D_{gm}$) are checked. For the modified Rosin-Rammler distribution, a modification to the equation is made. The original equation is given in Eqn 2.16, however, one should keep in mind that the $Q_{\text{modified-RR}} \neq Q_{\text{RR}}$.

For the small temperature (Fig.4.46(a)), Rosin-Rammler function gives better fitting whereas for the high liquid temperature (Fig.4.46(b)) (i.e. better atomization) it is the log-normal volumetric distribution, which provides better representation. Considering the count distribution comparisons for the latter temperature (Fig.4.46(d)), it is again the log-normal count distribution function which gives a more satisfactory fit, whereas for the $T_{\text{liquid}} = 20.2^\circ C$ it is the Nukiyama-Tanasawa distribution.

After this global section distribution analysis, a similar procedure is applied for the size distributions at different radial distances. This time, the total counts at each $r/D$ value are used. Table 4.5 gives the relation of the total count and subsequent total volume percentages for the selected first peak. It is interesting to see that moving towards periphery increases the volume percentage occupied by the first peak due to fewer large droplets and narrower distribution widths (i.e. the problem of the triple peak or oscillations in the distribution disappears moving towards the periphery).

Table 4.5: Total count and corresponding volume percentages for the first peak of distributions at different radial distances

<table>
<thead>
<tr>
<th>$T_{\text{liquid}}$</th>
<th>Total count %</th>
<th>Total volume %</th>
</tr>
</thead>
<tbody>
<tr>
<td>$T_{\text{liquid}} = 20.2^\circ C$</td>
<td>$r/D=0$</td>
<td>93.58% out of 111575 drops</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 20.2^\circ C$</td>
<td>$r/D=4$</td>
<td>99.09% out of 152332 drops</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 20.2^\circ C$</td>
<td>$r/D=12$</td>
<td>99.53% out of 24700 drops</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 28.3^\circ C$</td>
<td>$r/D=0$</td>
<td>98.47% out of 18944 drops</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 28.3^\circ C$</td>
<td>$r/D=6$</td>
<td>99.39% out of 34130 drops</td>
</tr>
<tr>
<td>$T_{\text{liquid}} = 28.3^\circ C$</td>
<td>$r/D=12$</td>
<td>99.67% out of 24202 drops</td>
</tr>
</tbody>
</table>

Table 4.6 gives the changes in the distribution parameters of the different empirical functions for the measurements presented in Table 4.5. As demonstrated in
Chapter 4. Experimental Results

(a) volume $T_{\text{liquid}} = 20.2°C$ and $x/D=110$

(b) volume $T_{\text{liquid}} = 28.3°C$ and $x/D=110$

(c) count $T_{\text{liquid}} = 20.2°C$ and $x/D=110$

(d) count $T_{\text{liquid}} = 28.3°C$ and $x/D=110$

Figure 4.46: Comparison of the empirical distribution functions for the selected cases.
4.3.3. Application of various empirical distribution functions

Fig.'s 2.7, 2.8(a), 2.9 higher $\sigma$ values represent lower peak and large width of distribution for log-normal, upper-limit and root normal distributions. However, for Rosin-Rammler and Nukiyama-Tanasawa distributions, large width of distributions are obtained with a decrease in $q$ (Fig. 2.10(b), 2.12(a)). In this case, Table 4.6 demonstrates that the width of the distribution decreases when moving from the axis towards the periphery for both liquid temperatures, and that the representative diameters shift towards the smaller droplet classes. It means that the sprays tends to become monodispersed with a peak diameter at small droplet classes.

Table 4.6: Distribution parameters for empirical functions at different radial distances

<table>
<thead>
<tr>
<th>$T_{\text{liquid}}$</th>
<th>Empirical functions</th>
<th>$r/D=0$</th>
<th>$r/D=4$</th>
<th>$r/D=12$</th>
</tr>
</thead>
<tbody>
<tr>
<td>20.2°C</td>
<td>RR</td>
<td>$q_{RR}$</td>
<td>2.70</td>
<td>2.60</td>
</tr>
<tr>
<td></td>
<td>$D_{RR}(\mu m)$</td>
<td>94.76</td>
<td>57.79</td>
<td>36.53</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{LN}$</td>
<td>2.51</td>
<td>2.00</td>
<td>1.97</td>
</tr>
<tr>
<td></td>
<td>$D_{LN}(\mu m)$</td>
<td>20.41</td>
<td>21.75</td>
<td>14.93</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{UL}$</td>
<td>1.52</td>
<td>1.51</td>
<td>1.51</td>
</tr>
<tr>
<td></td>
<td>$D_{UL}(\mu m)$</td>
<td>94.76</td>
<td>55.77</td>
<td>32.75</td>
</tr>
<tr>
<td></td>
<td>$D_{max}(\mu m)$</td>
<td>1000</td>
<td>1000</td>
<td>1000</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{RN}$</td>
<td>1.70</td>
<td>1.40</td>
<td>1.18</td>
</tr>
<tr>
<td></td>
<td>$D_{RN}(\mu m)$</td>
<td>83.06</td>
<td>52.17</td>
<td>35.71</td>
</tr>
<tr>
<td></td>
<td>Nuk-Tan</td>
<td>a</td>
<td>0.05199</td>
<td>0.00292</td>
</tr>
<tr>
<td></td>
<td>p</td>
<td>2</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>b</td>
<td>2.0642</td>
<td>0.4582</td>
<td>0.3893</td>
</tr>
<tr>
<td></td>
<td>$q_{NT}$</td>
<td>0.4</td>
<td>0.7</td>
<td>0.8</td>
</tr>
<tr>
<td>28.3°C</td>
<td>RR</td>
<td>$q_{RR}$</td>
<td>2.95</td>
<td>3.15</td>
</tr>
<tr>
<td></td>
<td>$D_{RR}(\mu m)$</td>
<td>38.80</td>
<td>29.68</td>
<td>26.17</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{LN}$</td>
<td>1.62</td>
<td>1.55</td>
<td>1.58</td>
</tr>
<tr>
<td></td>
<td>$D_{LN}(\mu m)$</td>
<td>21.75</td>
<td>17.85</td>
<td>15.78</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{UL}$</td>
<td>1.43</td>
<td>1.40</td>
<td>1.39</td>
</tr>
<tr>
<td></td>
<td>$D_{UL}(\mu m)$</td>
<td>38.80</td>
<td>29.68</td>
<td>26.17</td>
</tr>
<tr>
<td></td>
<td>$D_{max}(\mu m)$</td>
<td>1000</td>
<td>1000</td>
<td>1000</td>
</tr>
<tr>
<td></td>
<td>$\sigma_{RN}$</td>
<td>1.04</td>
<td>0.86</td>
<td>0.78</td>
</tr>
<tr>
<td></td>
<td>$D_{RN}(\mu m)$</td>
<td>36.43</td>
<td>28.20</td>
<td>24.95</td>
</tr>
<tr>
<td></td>
<td>Nuk-Tan</td>
<td>a</td>
<td>0.000392</td>
<td>0.000447</td>
</tr>
<tr>
<td></td>
<td>p</td>
<td>2</td>
<td>2</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>b</td>
<td>0.0236</td>
<td>0.0081</td>
<td>0.0084</td>
</tr>
<tr>
<td></td>
<td>$q_{NT}$</td>
<td>1.4</td>
<td>1.75</td>
<td>1.8</td>
</tr>
</tbody>
</table>

The effects of the initial flow conditions on the distribution parameters are given in Table 4.7. The total droplets from the whole cross-sectional area are used for this comparison. Temperature increase results in a decrease of the distribution width (i.e. increase in $q$ and decrease in $\sigma$) and a shift towards the smaller droplet classes (i.e. smaller representative droplets). Though it is still slightly observable that the $\sigma$ values and
representative diameters decrease, it can be concluded that the pressure increase has a weak effect on the distribution parameters.

Compared to the pressure, the nozzle diameter has a much stronger effect on the width of distribution (decrease) and representative diameters (increase). Increasing the length-to-diameter \((l/D)\) ratio, leads also into a decrease in \(\sigma\) (increase in \(q\)). The representative diameter change is seen unclear with the increasing \(l/D\).

Table 4.7: Effect of the initial flow conditions on the distribution parameters of the empirical functions

<table>
<thead>
<tr>
<th>Distribution parameter</th>
<th>(~20^\circ C)</th>
<th>(~28^\circ C)</th>
<th>(~20^\circ C)</th>
<th>(~20^\circ C)</th>
<th>(~20^\circ C)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>(~800kPa)</td>
<td>(~800kPa)</td>
<td>(~1200kPa)</td>
<td>(~800kPa)</td>
<td>(~800kPa)</td>
</tr>
<tr>
<td>(\bar{q}_{RR})</td>
<td>2.3</td>
<td>3.15</td>
<td>2.3</td>
<td>2.7</td>
<td>2.8</td>
</tr>
<tr>
<td>(D_{RR}(\mu m))</td>
<td>90.42</td>
<td>30.42</td>
<td>85.6</td>
<td>101.67</td>
<td>100.84</td>
</tr>
<tr>
<td>(\sigma_{LN})</td>
<td>2.26</td>
<td>1.59</td>
<td>2.25</td>
<td>2.12</td>
<td>2.06</td>
</tr>
<tr>
<td>(D_{LN}(\mu m))</td>
<td>21.54</td>
<td>17.54</td>
<td>20.37</td>
<td>34.85</td>
<td>35.72</td>
</tr>
<tr>
<td>(\sigma_{UL})</td>
<td>1.64</td>
<td>1.41</td>
<td>1.64</td>
<td>1.53</td>
<td>1.51</td>
</tr>
<tr>
<td>(D_{UL}(\mu m))</td>
<td>90.42</td>
<td>30.42</td>
<td>85.60</td>
<td>101.67</td>
<td>100.84</td>
</tr>
<tr>
<td>(D_{max}(\mu m))</td>
<td>1000</td>
<td>1000</td>
<td>1000</td>
<td>2000</td>
<td>2000</td>
</tr>
<tr>
<td>(\sigma_{RN})</td>
<td>2.04</td>
<td>0.90</td>
<td>1.98</td>
<td>1.90</td>
<td>1.84</td>
</tr>
<tr>
<td>(D_{RN}(\mu m))</td>
<td>79.13</td>
<td>28.79</td>
<td>74.9</td>
<td>90.93</td>
<td>90.56</td>
</tr>
<tr>
<td>(b)</td>
<td>0.7132</td>
<td>0.0083</td>
<td>0.4282</td>
<td>0.1163</td>
<td>0.0678</td>
</tr>
<tr>
<td>(q)</td>
<td>0.6</td>
<td>1.75</td>
<td>0.7</td>
<td>0.9</td>
<td>1</td>
</tr>
</tbody>
</table>

4.3.4 Velocity Characterizations of a Two-phase Flashing R134A Jet

In this subsection, the evolution of the mean velocity profiles and velocity distributions will be given in function of the initial flow conditions.

Fig.4.47 provides the non-dimensionalized velocity profiles obtained from all the experiments listed in Table 3.4. In Fig.4.47(a), the velocities of all the droplet classes are taken into consideration, whereas in Fig.4.47(b) only the smallest droplet class \((D_{\text{drop}} < 10\mu m)\) is taken with the assumption that they will represent the gas flow. As seen clearly in Fig.4.47(a), the non-dimensional profiles show self-similar patterns. Comparing the data obtained from the velocities of the total droplet classes with the correlations that have been used for single phase turbulent jets and two-phase jets (for example, Eqn.2.11) (Hetkorn, 1971, Pope 2000, Bayvel and Orzechowski [15]) shows that the non-dimensional velocity data follow the correlation but a slight shift towards velocity inferior to the computed values is noticed. For the gas case (i.e. velocities of the smallest droplet classes), the data is more dispersed, which may be the result of insufficient data points.
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

(a) For two-phase flow case (all droplets)
(b) For gas phase (smallest class of the droplets)

Figure 4.47: Nondimensionalized velocity profiles for all the test cases.

4.3.4.1 Evolution of the velocity in radial and axial distance

The radial velocity profiles are plotted in Fig. 4.48 for the reference tests case ($D_{nozzle} = 1\text{mm}$, $P \sim 800kPa$, $T_{liquid} \sim 20^\circ C$). The three axial distances, $x/D = 110, 220$ and $440$ are considered. At all $x/D$ presented hereby, the velocities are larger on the axis and they decrease in the radial direction, as in a single phase jet. Similarly, the velocity mean value decreases while the RMS values together with the turbulence intensity increase going radially from the axis.

The radial evolution of the velocity count distribution shows that at the centerline the distribution is narrow and it becomes wider on the periphery for both axial locations $x/D = 110$ (Fig. 4.48(c)) and $x/D = 440$ (Fig. 4.48(d)), as in a single phase jet. The droplets in the centerline move faster than the ones on the spray edges where the interaction with the entrained air is stronger.

Contrary to the count distribution, there is no multiple peak associated with the axial distribution of the velocity for the low superheat cases.

4.3.4.2 Effect of the initial liquid temperature on velocity

The effect of liquid temperature on the mean velocity, RMS and turbulence intensity profiles at the axial location $x/D = 110$ is displayed in Fig. 4.49. The profiles may be split in two groups: In the first group associated with $T_{liquid} = 20.2^\circ C$, $23.3^\circ C$ and
Figure 4.48: Axial and radial evolution of the mean velocity, RMS, turbulence intensity profiles and velocity count distributions for the flashing jet exiting 1mm nozzle.
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

24.6°C, the velocity, RMS and turbulence intensity on the jet axis shown in Fig. 4.49(a) and Fig. 4.49(b) are rather similar albeit having different droplet size count distributions (Fig. 4.42(a)). In the second group associated with $T_{\text{liquid}} = 27°C$ and 28.3°C, the drop size count distributions in Fig. 4.42(a) show finer droplets that are projected in Fig. 4.49(a) into lower velocities. On the contrary, for the same group, the droplet turbulence intensity is increased in Fig. 4.49(b).

A precision has to be given here; in single phase flow, the turbulence intensity is measured by the ratio of the RMS velocity to the mean velocity. When measuring the gas velocity around the spray or the liquid velocity in the tube, this definition is adequate, but when measuring droplets, particles of a liquid jet in a fragmentation process, the ratio does neither give the turbulence intensity of the liquid flow nor of the gas flow. Therefore, it will be called hereafter as droplet turbulence intensity.

At the edge of the spray, the droplet turbulence intensity values increase strongly when the temperature increase. This effect is related to a droplet size decrease on the edges of the spray together with strong interaction with the ambient environment.

Fig. 4.49(c), 4.49(d) give the velocity count percentage distribution for different liquid temperatures and for the axial location $x/D = 110$ at the centerline and at the periphery, respectively. At $r/D = 0$, the velocity count percentage distribution show a self-similar character for $T_{\text{liquid}} = 20.2°C$, 23.3°C and 24.6°C. Above this value, When the temperature increases the distribution gets wider and skews towards the smaller velocities. However, the peak value of the distribution does not shift and stays on the high velocity classes. For the same temperatures, one has to keep in mind that higher percentage of small droplet counts are observed (Fig. 4.42(a)).

An explanation may be proposed such that a distribution with high number of large droplets keep longer the initial momentum of the liquid jet (i.e. narrow velocity distribution on the axis) whereas a distribution with a majority of medium to small droplets in count and volume exchanges its initial momentum with the air faster presenting a wide velocity distribution.

Towards the periphery ($r/D = 12$, Fig. 4.49(d)), all the profiles show wider distributions. For $T = 20.2°C$, the profile has its peak on high velocity classes but it is skewed towards the smaller velocities. For all the other three temperatures the peaks move towards smaller velocities and they are widely dispersed.

4.3.4.3 Effect of the drive pressure on velocity

The pressure effect is investigated for various liquid temperatures. As expected, the velocity shows a shift towards higher values with pressure increase, whatever is the liquid temperatures (Fig. 4.50(a), Fig. 4.50(c) and Fig. 4.50(e)). On the contrary, the RMS values do not show a significant change with the increase of pressure.

163
Figure 4.49: Effect of initial liquid temperature on the droplet mean velocity, RMS, turbulence intensity radial profiles and velocity count distributions at the axial location of x/D=110.
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

At $T_{\text{liquid}} \approx 20^\circ\text{C}$ the droplet turbulence intensity does not change up to $r/D = 4$ at $x/D = 110$ and afterwards it decreases for higher pressure. This may be the simple fact the jet core is better preserved for high pressure as it may be seen in Fig. 4.20. For the liquid temperature $T_{\text{liquid}} \approx 24^\circ\text{C}$, all pressures result in similar droplet turbulence intensities in most part of the cross-section. Discrepancies may appear mostly in the spray borders where the interaction with ambient air is stronger. For the highest temperature $T_{\text{liquid}} \approx 27^\circ\text{C}$ higher pressure gives lower droplet turbulence intensities. This is simply due to fact that the RMS remains unchanged whereas the mean velocity significantly increases.

Fig. 4.51 shows that the velocity count percentage distributions on the spray centerline display an obvious shift of the peak towards higher velocity classes when the pressure increases, without changing significantly the distribution pattern for all the tested cases. One may notice that the peaks are around the velocity of 16m/s for $P_{\text{liquid}} \approx 800\text{kPa}$ and 20m/s for $P_{\text{liquid}} = 1210\text{kPa}$. If a hypothesis is done in such a way that the coefficient of head losses is the same for these two cases, then one may try to predict the second peak after having measured the first one with the simple relation

$$U_{2} \approx U_{1} \sqrt{\frac{P_{\text{liquid}}}{800\text{kPa}}}.$$ 

This would give an estimated value at 19.68m/s, which is rather close the measured 20m/s. So, the increase in backpressure is converted in an increase of kinetic energy for the droplets. The distributions on the periphery exhibit also a shift of the peak towards high velocities with high pressure with more velocity classes.

For all temperatures and pressures, the velocity distributions at the periphery are wider than the distributions at the centerline due the interaction with the ambient air. A more populated low velocity part of the distribution is associated to the presence of 'isolated' small droplets.

4.3.4.4 Effect of the orifice diameter on velocity

The effect of the nozzle diameter on the velocity profiles and count percentage distributions are displayed in Fig. 4.52(a), 4.52(b). The comparisons are performed for dimensional and non-dimensional axial locations. The velocity profile shows a small decrease when the nozzle diameter varies from 1mm to 2mm. No changes are noticed for the droplet turbulence intensity. (Fig. 4.52(c), 4.52(d)).

The comparison for the velocity count percentage distributions at non-dimensional axial distance $x/D = 110$ at the centerline presented in Fig. 4.53(a) displays a slight shift of the velocity distribution towards the smaller velocity classes for the large nozzle.

At the dimensional distance $x = 220\text{mm}$ (Fig.4.53(b)), the two nozzles provide distributions with the same peak and percentage values for the high velocity classes. Differences are visible for the small velocity classes. There is more spreading for the jet exiting the small nozzle diameter due to the presence of more tiny droplets that slow down experiencing evaporation.
Chapter 4. Experimental Results

Figure 4.50: Effect of storage pressure on the mean droplet velocity radial profiles at the axial location of $x/D=110$. 

(a) Mean velocity and RMS for $T_{liquid} \sim 20^\circ C$ $x/D=110$

(b) Droplet turbulence intensity for $T_{liquid} \sim 20^\circ C$ $x/D=110$

(c) Mean velocity and RMS for $T_{liquid} \sim 24^\circ C$ $x/D=110$

(d) Droplet turbulence intensity for $T_{liquid} \sim 24^\circ C$ $x/D=110$

(e) Mean velocity and RMS for $T_{liquid} \sim 27^\circ C$ $x/D=110$

(f) Droplet turbulence intensity for $T_{liquid} \sim 27^\circ C$ $x/D=110$
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

Figure 4.51: Effect of storage pressure on the mean droplet velocity radial profiles at the axial location of x/D=110.
Chapter 4. Experimental Results

Figure 4.52: Effect of the nozzle diameter on the mean droplet velocity, RMS and turbulence intensity profiles.
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

Figure 4.53: Effect of nozzle diameter on the velocity count distributions.
Chapter 4. Experimental Results

At the periphery of the spray, Fig. 4.53(c) and 4.53(d) indicate that the peak of the distribution moves slightly towards the smaller velocity classes and that the percentage value increase for small velocities when the large nozzle is used.

4.3.4.5 Effect of the orifice length/diameter ratio on velocity

As explained in Section 4.3.1.5, the effect of the nozzle length-to-diameter ratio on the spray characteristics are investigated using nozzle designs with three \( l/D \) ratios. Fig. 4.54 presents the influence of the \( l/D \) ratio on the mean velocity, RMS and droplet turbulence intensity radial profiles for two axial locations \( x/D = 110 \) and \( x/D = 220 \). At \( x/D = 110 \), the sprays formed by the nozzles with \( l/D = 0 \) and \( l/D = 2 \) are similar in mean velocities, RMS and turbulence intensity. The jet exiting the nozzle with \( l/D = 7 \) gives the same mean velocity, RMS and droplet turbulence intensity at the centerline as the other two nozzles, but it expands more and has high mean velocity and low RMS giving low turbulence intensity at \( 3 \leq r/D \leq 12 \). At \( x/D = 220 \), the nozzles with smaller \( l/D \) leads to higher mean velocity, smaller RMS and droplet turbulence intensity at the centerline. Towards the periphery they all give more or less similar values.

At \( x/D = 110 \) and \( r/D = 0 \) all the nozzles give the same velocity count percentage distribution (Fig. 4.55(a)). Moving more downstream, the peak of the velocity distribution at the centerline decreases both in percentage and corresponding velocity class. Moreover, there is more dispersion when the \( l/D \) increases. At the periphery for both axial distances Fig. 4.55(c) and Fig.4.55(d) show that the three distributions are self-similar.

4.3.4.6 Summary

The nondimensional velocity profiles for the droplets and gas phase show self-similar pattern and can be represented with the correlations in the literature for single phase turbulent jets and two-phase jets. As in a single phase turbulent jet, the velocity is high in the centerline and decreases towards the periphery with the interaction to the ambient, whereas the RMS and droplet turbulence intensity is small in the center and increases at the periphery as long as low superheat is concerned. At higher superheat, high droplet turbulent intensity in the center is also observed.

Across a section, the distributions are narrow in the centerline and broad on the borders of the spray with more velocity classes. Moving downstream, the nozzle leads to the widening of the distributions due to the slow-down of the droplets in the aerodynamical interaction with the surrounding and the diameter decrease due to evaporation.

As explained in the previous section, increasing superheat results in a production of smaller droplets. With low or moderate superheats, no significant change is exhibited in the profiles, whereas high superheat enhances the fragmentation of the droplets thus
4.3.4. Velocity Characterizations of a Two-phase Flashing R134A Jet

Figure 4.54: Effect of nozzle length-to-diameter ratio on the mean velocity, rms and turbulent intensity profiles.
Figure 4.55: Effect of nozzle length-to-diameter ratio on the velocity count distributions.
accelerates the momentum exchange of the droplets with the air leading to a general slow down in velocity. Moreover, flatter, higher RMS and droplet turbulence intensity profiles are obtained with high superheats.

Pressure increase results in an increase in mean velocity values all over the cross-section without changing the RMS values. The droplet turbulence intensity is lower for the higher pressure due to preservation of the jet core for longer distances. Velocity count distributions exhibit a shift of the peak value towards the higher values with pressure increase both at the centerline and periphery. The velocity distributions at the centerline is more narrow whereas the ones at the periphery are outspread due to the interaction with the ambient air. For high pressures, more velocity classes are observed in the distribution at the periphery than for lower pressures. At the centerline, however, the width of the distribution is preserved with pressure increase.

Changing the nozzle diameter from 1mm to 2mm only changes slightly the mean velocity, but not the RMS and droplet turbulence intensity profiles. Distributions compared at dimensional distance show identical peak value and percentages for both nozzles at the centerline with a slight increase of percentage in small velocity classes when the large nozzle is used. Nevertheless, a higher distance from the nozzle, the small nozzle produce a better interaction with the ambient (i.e. the velocity distribution is wider).

Using three nozzle designs with different \( \frac{l}{D} \), shows that sprays formed from nozzles with \( \frac{l}{D} = 0 \) and \( \frac{l}{D} = 2 \) produce identical mean velocity, RMS and turbulence intensity profiles at \( x/D = 110 \). Profiles for \( \frac{l}{D} = 7 \) are different at the periphery than the ones obtained from the other two nozzles. At \( x/D = 220 \), nozzles with higher \( \frac{l}{D} \) produce lower mean velocity together with higher RMS and turbulence intensity at the centerline. At the periphery, all profiles are identical. The distributions obtained from three designs are identical at the centerline at \( x/D = 110 \) and shifts towards lower velocity classes for larger \( \frac{l}{D} \) at \( x/D = 220 \). At the periphery, the differences are slight such that the distributions can be called identical.

4.4 Temperature measurements in a two-phase flashing jet

The present section deals with the temperature evolution of the two-phase flashing R-134A jet, using intrusive and non-intrusive techniques. Temperature measurements are done with intrusive techniques like thermocouples to assess the thermal behaviour of the two-phase flashing R-134A jet originated from a diaphragm type orifice. The temperature measurements show that the flow is stationary at the point wise measurements and is repeatable. The effect of the thermocouple type on the final temperature value is also discussed. The study includes the effect of different orifice diameters on the axial temperature evolution of the flashing jet.
4.4.1 Effect of the thermocouple type and repeatability

In order to understand the effect of the thermocouple probes on the measurements, three different thermocouples such as Chromel/Alumel wire of 0.2mm diameter, Chromel/Alumel wire of 0.8mm diameter, and Copper/Constantan wire of 0.2mm diameter are tested. The junction of the thermocouple is placed at the centerline along the jet axis. Fig. 4.56(a) shows a typical thermocouple signal of time series acquisition taken with DAS20 system. As clearly seen, the acquisition starts with the ambient temperature at ~ 25°C. Once the acquisition has started, the depressurisation was initiated by opening the ball valve. The sudden decrease happens when the liquid touches the thermocouple. The peaks on the ambient temperature part are due to the electronic parasites caused by the opening of ball valve. The signal in time shows that the flow reaches a steady temperature. Fig. 4.56(b) shows the mean steady temperature values that are deduced from the steady part of each signal measured at different axial distance from the nozzle. Even when the thermocouple is placed right at the nozzle exit it would measure a maximum liquid temperature which corresponds to the boiling temperature of R-134A at atmospheric conditions since the thermocouple creates intrusiveness resulting in immediate flashing. Measurements performed on the R-134A jet at the same initial conditions during different experiments proved to be highly repeatable as shown in Fig. 4.56(a), which gives the signals of the chromel/alumel thermocouple wires of 0.2mm diameter in the R-134A jet issuing from a 2mm nozzle with a driving pressure of 920KPa and a liquid temperature of 24.3°C. The probe is placed at a distance of \( x = 42 \text{mm} \) from the nozzle.

One question is the effect of the thermocouple wire diameter size on the measurements. Fig. 4.57(a) displays the temperature signals in time obtained by both chromel/alumel wires of 0.2mm and 0.8mm diameters at different axial distances \( x \) from the nozzle.
4.4.2 Temperature evolution measured by thermocouples

The measurements show that there is a difference of ~ 2°C in the steady temperature values obtained by the two thermocouples. Theoretical analysis reports that for the above mentioned diameters there should be only a difference of ~ 1°C in contact point temperature for a liquid R-134A jet of 0.8mm in diameter. Therefore, for a flashing R-134A jet consisting of vapor, droplets and entrained air, a discrepancy of ~ 2°C is found to be reasonable. Furthermore it is noticed in Fig. 4.57(a) that the temperature of the chromel/alumel 0.8mm wire is higher than that of the chromel/alumel 0.2mm wire. It is expected that a larger radius results in higher heat transfer from ambient due to conduction and therefore it may measure a temperature that is influenced more by ambient temperature (entrained warm air).

Another question is the effect of the thermocouple type on the final temperature values. Fig. 4.57(b) compares the axial mean steady temperature evolution obtained with the "chromel/alumel probe with a wire of 0.2mm in diameter", "chromel/alumel probe with a wire of 0.8mm in diameter" and "copper/constantan probe with a wire of 0.2mm in diameter". The copper/constantan thermocouple probe of 0.2mm gives values of ~ 2°C lower than the one of chromel/alumel probe of 0.2mm.

Figure 4.57: Comparison of temperature signals obtained by different thermocouple probes

4.4.2 Temperature evolution measured by thermocouples

The behaviour of the flow under a driving pressure of 700 KPa with 1mm nozzle diameter is visualized in Fig. 4.58(a). The supports of the two first thermocouples appear as vertical white objects and their impact on the pattern of the flow is obviously intrusive.
provoking flashing. A representation of the temperature evolution along the jet axis is given in Fig. 4.58(b) for jets exiting from 1mm (left) nozzle. It can be noticed that the first measured temperature goes below the boiling temperature \( T_{\text{boiling}} = -26.7^\circ C \) at 1atm and that the axial evolution of the temperature reaches a plateau at about \(-55^\circ C\) far from the nozzle. This evolution is due to the high evaporation rate as will be confirmed by the theoretical model in the following chapter. It can also be pointed out that the comparison between the temperatures obtained with a rack of 10 thermocouples and the one given by a single thermocouple wire is demonstrating low intrusiveness induced by the complete rack.

\[
\begin{align*}
D_{\text{nozzle}} &= 1 \text{ mm} \\
\end{align*}
\]

(a) The first two thermocouples in R134-A jet under 700 KPa at 22°C for 1mm nozzle diameter

(b) Axial evolution of temperature profiles for jets issuing from 1 mm nozzle

Figure 4.58: Flow visualization of the first two thermocouples in R-134A jet and the axial temperature profiles

### 4.4.3 Effect of the nozzle diameter on the temperature evolution of the flashing

#### 4.4.3.1 Visual description of the flow

Fig. 4.59 yields visualizations of the flashing jet for a driving pressure of 700KPa with different nozzle diameters. As it can be concluded from the images, the jet passing through 1 and 2mm nozzles does experience an unstable behaviour since its disintegration occurs downstream the nozzle in an unpredictable flashing point whereas for the 4mm nozzle, the droplet formation occurs right at the nozzle.

The behaviour of the flow for a driving pressure of 700KPa when the rack of thermocouples is installed is shown in Fig. 4.60 for 3 nozzle diameters. The thermocouples appear as vertical white objects. The jet passing through 1mm and 2mm orifices exits
4.4.3. Effect of the nozzle diameter on the temperature evolution of the flashing

Figure 4.59: R134-A jets under 700 KPa at 22°C; (Nozzle diameters: 1(top), 2(middle) and 4 mm(bottom))
the nozzle as liquid core and the first thermocouple probe triggers the break-up and droplet formation. However, for the 4mm nozzle, the droplet formation occurs right at the nozzle upstream the probe. In this image, the rack of thermocouples is kept at the same axial distance from the nozzle in all the cases.

![Figure 4.60: The first two thermocouples in R134-A jet under 700 KPa at 22°C (Nozzle diameters: 1, 2 and 4mm from left to right).](image)

A representation of the temperature evolution along the jet axis is given in Fig. 4.61 for 1mm and 4mm nozzles. It was already said that the first measured temperature goes below the boiling temperature \( T_{\text{boiling}} = -26.7°C \) at 1atm) and the temperature reaches a plateau at about \(-55°C\) far from the nozzle, for both of them. However, Fig. 4.61(b) reports a very rapid decrease in the temperature for 4mm nozzle compared to the 1mm nozzle. The main reason for this behaviour may be due to the atomisation of the jet right at the nozzle exit leading a higher air entrainment forcing high evaporation fluxes.

![Figure 4.61: Temperature profiles for the jets issuing from 1 mm (left) and 4 mm (right) nozzles (\(P_{\text{liquid}} \sim 700\text{KPa}\)).](image)

Though the present temperature data do not permit to conclude definitively about the superheat effect on the final temperature profile, it is worth emphasizing that
Fig. 4.61(b) does not reveal any effect for a change of ~ 3°C in $T_{\text{liquid}}$ when using the 4mm nozzle. One should also keep in mind that the superheat for 4mm tests are rather low.

### 4.4.4 Infrared camera results

Fig. 4.62 displays a typical thermograph where the liquid core and the first thermocouple placed on the centerline of the jet can be seen. The thermograph values obtained from this figure are scaled to match the temperature value measured by the first thermocouple of the rack. Fig. 4.63 displays the scaled infrared measurement points extracted from Fig. 4.62 along the axial line labeled LO1. It can be observed that the temperature measured inside the tube upstream the nozzle equals the one given by the thermograph near the nozzle exit. The liquid core does experience a rapid temperature decrease to the boiling point at ambient pressure before reaching the first thermocouple probe (vertical shadow on the thermograph). No significant presence of droplets of shattering can be observed between the nozzle and the first thermocouple probe to claim the occurrence of flashing burst.

![Infrared thermograph](image.png)

Figure 4.62: Infrared thermograph (zoomed image) for the flashing two-phase jet under the pressurization of $P = 700\, \text{kPa}$ for the orifice diameter of 1mm.

Despite the difficulties in calibration, the thermograph provide qualitative information related to the surface temperature change of the intact liquid core. In this region, the usage of thermocouple will be impossible due to its intrusiveness. Once the flashing commences, thermography will give a global temperature of the atomized liquid jet with high amount of uncertainty. Therefore, the application region should be limited only to location where liquid core still exists. Once the atomization starts, thermocouple proves to be the appropriate instrument to investigate the temperature evolution of...
Chapter 4. Experimental Results

4.4.5 Radial temperature profiles

Apart from the centerline temperature evolution, the radial mean temperature evolution has been investigated as well. The experimental conditions are the same as the ones described in Table 3.4. For these experiments, the first thermocouple probe is placed at 11 mm downstream from the PDA-laser probe. The evaporation along this difference of 11 mm is considered negligible and the temperature profiles are regarded as representative of the cases displayed and commented in Section 4.3.1. For the radial temperature profiles, the same procedure of graphical representation as performed for the droplet sizes and velocities is applied.

4.4.5.1 Downstream evolution

The radial temperature evolution for the reference case at three axial locations ('Test' cases 1, 2, &3) is displayed in Fig. 4.64. The radial profiles state clearly a decrease in temperature all along the radius while going towards the downstream distances. For each axial position (x/D), the jet temperature is higher in the center and decreases towards the jet periphery since there is more air entrainment on the side than on the centerline together with a high population of small droplets and this increases evaporation. Going more further away in the radial distance, temperature starts to
increase again due to the heating of the ambient through entrainment of air.

Figure 4.64: Radial mean droplet temperature profiles for 1mm nozzle at 3 axial locations.

4.4.5.2 Effect of the superheat

The effect of the initial liquid temperature on the spray temperature evolution is displayed in Fig. 4.65 for the axial distance of $x/D = 110$ and $x/D = 220$. For both axial distances, the increase in liquid temperature leads to a decrease of spray temperature all over the profile, having an effect stronger on the centerline than on the sides. The higher is the liquid temperature, the more uniform and low is the spray temperatures along the radius.

As explained in the previous sections, high superheat results in shorter breakup lengths, stronger atomisation, provide smaller and slower droplets. Therefore, when the superheat increases, the distance between the breakup point and measurements location of the first thermocouple probe will augment. Since the velocity is smaller as well, droplets will pass more time to reach the measurement point experiencing more evaporation, thus having lower temperatures uniformly across the section.
Chapter 4. Experimental Results

4.4.5.3 Effect of the initial pressure

The influence of the liquid pressure on the radial temperature profiles of a liquid jet at \( \sim 20^\circ C \) exiting a 1mm nozzle is presented in Fig. 4.66 for two axial locations. For the close field (i.e., \( x/D = 110 \)), the higher pressure leads to higher spray temperature except the core region close to the axis. However, looking at the profile at the downstream axial location, no significant effect of the pressure increase is observed. This observation is consistent with the droplet size profiles that are explained in previous sections. At \( x/D = 110 \), the high pressure leads to smaller droplet sizes in the core and larger sizes around the periphery compared to low pressure. As it will be demonstrated in the following chapter describing the evaporation of the droplets, large droplets keep their temperature for longer time and evaporate, thus cool down, slower. At \( x/D = 220 \), the droplet size profiles are identical for these experiments, as their temperatures.

Fig.4.66(c) displays the pressure increase effect on the temperature profiles for a range of \( T_{\text{liquid}} = 23.3 - 24.6^\circ C \). Two profiles of low pressure with \( T_{\text{liquid}} = 23.3^\circ C \) and \( 24.6^\circ C \) are the upper and lower envelopes. The temperature profiles of the high pressure flow fall in between these upper and lower envelopes. For these experiments, the droplet size profiles are similar, as well.

For the \( T_{\text{liquid}} = 27^\circ C \), the temperature profiles have higher values for higher pressure as shown in Fig.4.66(d). The droplet sizes for high pressure at this liquid temperature are also larger than the low pressure experiment, and they move faster so that they have less time to evaporate till the measurement point.
4.4.5. Radial temperature profiles

Figure 4.66: Pressure effect on the radial mean temperature profiles at two different axial locations.
4.4.5.4 Effect of the nozzle diameter

The temperature profiles comparing the effect of the nozzle diameter are presented both in non-dimensional (Fig. 4.67(a), 4.67(b)) and dimensional (Fig. 4.67(c), 4.67(d)) axial distances. Temperature profiles show that the change of the nozzle diameter does not have an effect if compared in non-dimensional distances. However, the representation in dimensional form gives higher temperature values for the larger nozzle mostly in the core close to the centerline. This may be explained by the slight change in the other distributions such as drop size and velocity. Small-diameter nozzle creates a jet that is slightly slower, thus droplets form from the jet exiting the small nozzle have more time to evaporate till the measurement point.

![Temperature profiles](image)

Figure 4.67: Effect of nozzle diameter on the radial mean temperature profiles at different axial locations (Results are presented in non-dimensional and dimensional axial distances).
4.5.5 Effect of the nozzle length-to-diameter ratio

Fig. 4.68(a) shows that increasing the $l/D$ ratio, decreases the temperature values strongly in the centerline but not in the periphery of the spray compared the case $l/D \approx 0$ at the axial location ($x/D = 110$). However, the nozzles of $l/D = 2$ and $l/D = 7$ provide nearly identical temperature profiles at $x/D = 110$. This trend changes moving downstream from the nozzle exit. In the far field ($x/D = 220$), the increase in length-to-diameter ratio results in a decrease of spray temperature all over the radial profile.

The explanation is as follows: Fig. 4.45(b) gives higher volume fraction for droplets ≤ 200μm when the $l/D$ increases resulting in a higher evaporation.

![Graphs showing temperature profiles](image)

Figure 4.68: Effect of nozzle length/diameter ratio on the radial mean droplet temperature profiles at different axial locations.

4.5 Summary and Discussions

The effect of different initial pressures, temperatures and orifice diameters on the droplet size and velocity distributions of a flashing R-134A jet along the radial and axial directions are studied by means of high speed imaging and Phase Doppler Anemometry (PDA). Moreover, temperature measurements are performed using Infrared thermography and thermocouples.

Analysis of radial profiles points out that the droplet sizes and velocities are larger in the centre of the jet and they decrease towards the edges of the jet. When axial evolution is concerned, the droplet sizes and velocity mean values decrease going further from the nozzle due to evaporation and further break-up of large drops. Moreover, a spreading
of the jet width is also clearly observed. The non-dimensional velocity profiles are self similar as in single phase jets.

The liquid superheat plays the most dominant role on the droplet diameters no matter what is the driving pressure, nozzle diameter or axial location. As superheat increases, droplet sizes decrease sharply; the envelope of the spray is wider; $D_{32}$ approach $D_{10}$ indicating monodisperse nature; radial profiles of mean drop sizes become flatter and uniform over the cross-section. Moreover, the mean velocities decrease, RMS and droplet turbulence intensity values increase.

The increase of the storage pressure results in different flow patterns with different flow temperatures. When the measurements are done with the same superheat but different pressures, it is observed that the droplet sizes decrease with increasing pressure with a more important effect on the centerline than towards the periphery. With a higher superheat, no change of mean drop sizes is observed with increasing pressure. Further increase in superheat results in higher mean drop sizes with pressure increase due to a lower residence time to allow the nuclei transforming into bubbles. On a global point of view, high liquid pressure provides a less sharp decrease in mean drop size with increasing superheat than low liquid pressure. As expected, the velocity of the jet increases with the increase of the storage pressure.

It is not straightforward to evaluate the effect of the nozzle diameter on the drop sizes. For low superheats, the mean drop size radial profiles shows that the larger nozzle leads to slightly larger mean diameters of ($D_{10}$ and $D_{32}$) than the smaller one without influencing the velocities. When superheat increases, nozzle diameter does not have an effect on the drop sizes. On a more global view, it can be concluded that nozzle diameter has almost no effect on the drop sizes, mean velocity, RMS and droplet turbulence intensity values compared to superheat effect. These conclusions are valid for the nozzle diameters investigated.

Increasing the length-to-diameter ratio's of the nozzle results in smaller droplet diameters compared to the sharp edge orifice. In the close field, the nozzle with $l/D = 7$ provides larger drop sizes than $l/D = 2$ most probably due to incomplete detachment from the nozzle walls for $l/D = 7$. In the far field, the differences in drop sizes for both nozzles disappear.

The drop size distributions shows different patterns depending on the flow conditions, for example, high superheat leads to narrow width of count and volume distribution with a peak value on the small droplet classes whereas small superheat provides a large width of count and volume distributions with peak values on the larger drop classes. Triple peaks in count and volume distribution are observed with small superheats with the existence of large droplets and incomplete droplet breakup on the axis and could be associated to slug flow described in the literature of pulsative flow observed in highspeed images. These secondary peaks disappear with the increasing superheat or in the periphery of the jet radius.

Various empirical distribution functions are tested on the drop size count and volume
distributions. The empirical functions cannot provide good fitting if the complete range of droplet diameters (that is, triple peaks) are taken into account. If only the first peak in the distributions which represent ~97% of total droplet count population is considered, the evolution of the empirical distribution function parameters with the initial conditions can be investigated. With this approach, the comparisons show that for the small superheats, Rosin-Rammler volumetric gives good fitting whereas log-normal volumetric distribution is found better for the high superheats. When the number count distributions are compared, log-normal number distribution gives better representation of the data for high superheat whereas it is Nukiyama-Tanasawa distribution for low superheat. However, one has to keep in mind that selecting only the first peak does not provide a realistic representation of the spray distribution since the large droplets constitute the majority of the volume though they are considerably less in number. Alternative representation has to be searched for.

The temperature evolution in a flashing atomized jet is studied through thermocouple measurements. A high repeatability has been demonstrated during the measurement campaign. A rack of ten thermocouples is used to measure temperature at different distances from the nozzle. Comparisons with measurements with a single thermocouple assess a low intrusiveness of the rack. The choice of thermocouple type and wire diameter gave slight differences in the measured steady mean temperature. If the breakup of the jet is in the proximity of the first thermocouple probe, it measures mainly a temperature close to the boiling point $-26.4^\circ C$ at atmospheric pressure. The temperature continues to drop and reaches a plateau of $-55^\circ C$ downstream the nozzle whatever is the superheat, pressure, nozzle diameter or $l/D$.

When the flashing breakup occurs at the nozzle exit, the evaporation of the droplets before they reach the thermocouple probe is higher and a stronger temperature drop is observed. This is valid for the experiments with very large nozzle diameters such as 4mm or very high superheat.

The increase in superheat leads to a decrease of spray temperature all over the profile, having an effect stronger on the centerline than on the sides. The higher is the liquid temperature, the more uniform and low is the spray temperatures along the radius. To recall, high superheat results in shorter breakup lengths, stronger atomisation, provide smaller and slower droplets. Therefore, when the superheat increases, the distance between the breakup point and measurements location of the first thermocouple probe will augment. Since the velocity is smaller as well, droplets will pass more time to reach the measurement point experiencing more evaporation, thus having lower temperatures uniformly across the section.

Higher pressure generally leads to higher spray temperature on the regions where it produces also larger droplets. Large droplets keep their temperature for longer time and evaporate, thus cool down, slower. Moreover, with high pressure they move faster so that they have less time to evaporate till the measurement point.
Chapter 5

Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

5.1 Introduction

In the evaporation approach a relatively simple (1D) formulation is used to predict the temperature evolution of a representative mean diameter of the droplet distribution. This model takes into account the momentum, energy and mass equations of a droplet with its surroundings. This chapter aims to clarify the characteristics of this model with the necessity of the correction factors that are adopted, with a sensitivity analysis. The aim is not to do a full review of models used for evaporation, reader interested in model derivation will consult Bird et al. [17] or Sirignano [127].

5.2 Mathematical model

Hereafter, the temperature variation inside of the droplet together with the temperature evolution around the droplet will not be taken into account. The droplet and the surrounding gas are both characterized by a single temperature $T_d$ and $T_g$, respectively. Following the derivation of Pretrel [105], the set of first order differential equations may be presented in a reduced form as:
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

\[
\begin{align*}
\frac{du_{dr}}{dt} &= \frac{u_g - u_{dr}}{\tau} \\
\frac{dT_{dr}}{dt} &= \frac{T_g - T_{dr}}{\tau_t} - \frac{L_{hd}}{C_{p_{dr}}} \frac{1}{\tau_m} \\
\frac{dd_{dr}}{dt} &= \frac{-d_{dr}}{3\tau_m} \\
\frac{dx_{dr}}{dt} &= u_{dr}
\end{align*}
\]  

(5.1)

The left hand side of Eq.5.1 is formed by the time derivatives of the four main variables associated with the droplet \(u_{dr}, d_{dr}\) and \(x_{dr}\) are respectively the velocity, the diameter and the distance from the initial point).

The right hand side contains the driving terms for momentum, energy and mass balances together with the kinetic of the droplet.

The reduced form of the driving terms associate a relaxation time for each mechanism of transfer (i.e. \(\tau, \tau_t, \tau_m\) are respectively the relaxation time associated with dynamical, thermal and mass transfer for the droplet). Two relaxation times, one for the convection \((\tau_t)\) and one for the evaporation \((\tau_m)\), control the droplet equation of energy. Please note that \(L_{hd}\) and \(C_{p_{dr}}\) stand for the droplet latent heat and the droplet heat capacity, respectively, and that throughout this section, indices like \(g\) and \(dr\) are used for gas or droplet variables (or properties).

The definitions of the relaxation times are given in Eq.5.2.

\[
\begin{align*}
\tau &= \frac{4}{3} \frac{\rho_{dr}}{\mu_g} \frac{d_{dr}^2}{C_{p_{dr}} R e_{dr}} \\
\tau_t &= \frac{1}{6} \frac{\rho_{dr}}{k_g} \frac{C_{p_{dr}} d_{dr}^2}{N u_{dr}} \\
\tau_m &= \frac{1}{6} \frac{\rho_{dr}}{D_{dr-g}} \frac{1}{(y_{dr,g} - y_{dr,\infty}) S h_{dr}} \frac{d_{dr}^2}{S_h_{dr}}
\end{align*}
\]

(5.2)

where \(\rho\) is the volumetric mass, \(\mu\) is the dynamical viscosity, \(k\) is the conductivity, \(D_{dr-g}\) is the mass diffusivity of R134A in air, \(y_{dr,g}\) is the mass fraction of R134-A in gas close to the particle and \(y_{dr,\infty}\) is the mass fraction of R134-A in gas at an infinite distance from the particle.

Following Abramzon and Sirignano [1] the definition of the mass fraction \((y_{dr,g})\) is given in Eq. 5.3. Here, it is interesting to comment this formulation whenever flashing is concerned. During the experiment, a saturated liquid (R134-A) at ambient temperature and in a thermodynamic equilibrium is released at atmospheric pressure. Due to the
thermal inertia, the liquid “finds itself” with the initial temperature at atmospheric pressure. This temporary state is called a metastable equilibrium. In this configuration and without a proper limitation, the use of a pure equilibrium law such as the one giving the saturation pressure versus the temperature would lead to inconsistencies i.e. a mass fraction exceeding the unity (the saturation pressure at ambient temperature is more than five times higher than the atmospheric pressure). Therefore the mass fraction of Eq. 5.3 is limited by an arbitrary value of 0.99 that cannot be exceeded. When the liquid is out of its thermodynamic equilibrium, this limitation prevents the saturation pressure to be higher than the ambient pressure. A limit on the mass fraction of 0.99 corresponds to a limit of the saturation pressure of about 97% of the atmospheric pressure. An alternative approach would be the implementation of a non-equilibrium saturation pressure. Indeed, the pressure of a metastable liquid may be much lower than the equilibrium phase change pressure. As explained in Section 2.4, the minimum of pressure for the liquid is known to be located on the spinodal curve computed from an equation of state (Peng-Robinson equation in de Sá et al. [31]). Experimentally, this under pressure of vaporization had been also observed for R134-A in an adiabatic capillary tube of 0.6 mm diameter when the heat transfer is not high enough (Chen and Lin [27]). Because an equivalent of the $P_{sat}$ law for metastable liquid is not known Eq 5.3 with a proper limitation will be used hereafter.

$$y_{dr,3} = \frac{M_{R123A}P_{sat}(T_{dr})}{M_{R134A}P_{sat}(T_{dr}) + M_{Air}P_g}$$
$$with P_g = (P_{atm} - P_{sat}(T_{dr}))$$  (5.3)

In the set of equations defining the relaxation time (Eq. 5.2), four dimensionless numbers appear (i.e. $Re_{dr}$ for the droplet Reynolds number, $C_D$ for the droplet drag coefficient, $Nu_{dr}$ for the droplet Nusselt number and $Sh_{dr}$ for the droplet Sherwood number). The three last dimensionless numbers involve in their definition the first one i.e.; the droplet Reynolds number defined in Eq 5.4.

$$Re_{dr} = \frac{|u_g - u_{dr}|\rho_g d_{dr}}{\mu_g}$$  (5.4)

The drag coefficient correlation used hereafter is also valid out of the Stokes regime (i.e. when $Re_{dr} > 1$)(Eq. 5.5).

$$C_D = 24(1 + 0.2 Re_{dr}^{0.55})/Re_{dr}$$  (5.5)

The Nusselt and Sherwood correlations are the ones used by Abramzon and Sirignano [1] written in a form valid out of the Stokes regime (Eq. 5.6). Inside of the Stokes regime, $Re_{dr}^{0.077}$ is replaced by “1” in these two last correlations.

191
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

\begin{align*}
N_{udr} &= 2 + \left( 1 + (1 + Pr_{dr} Re_{dr})^{1/3} Re_{dr}^{0.077} - 2 \right) / FT \\
Sh_{dr} &= 2 + \left( 1 + (1 + Sc_{dr} Re_{dr})^{1/3} Re_{dr}^{0.077} - 2 \right) / FM
\end{align*}

(5.6)

Following the advice of Bussmann and Renksizbulut [25], the correction factor, which is generally used in combustion of solid particles, and known as the "Renksizbulut-Yuen correlation", is not applied here (in this last reference the use of this correction is not advised in the case of a highly evaporative particle). \( Pr_{dr} \) and \( Sc_{dr} \) are the Prandtl and Schmidt number of the particle (determined in the numerical program from the film properties, see comments hereafter).

\( FT \) and \( FM \) defined in Eq. ?? are the correction factors taking into account the increased fluxes (compared to Fick’s diffusion) due to the blowing effect (Stefan’s flux). These correction numbers are expressed in terms of the Spalding factors \( (BT, BM) \) defined as in Abramzon and Sirignano [1].

\begin{align*}
FT &= \ln(1 + BT)(1 + BT)^{0.7} / BT \\
FM &= \ln(1 + BM)(1 + BM)^{0.7} / BM \\
BT &= C_p_{dr}|T_{dr} - T_g| / L h_{dr} \\
BM &= (y_{dr,g} - y_{dr,goo}) / (1 - y_{dr,g})
\end{align*}

(5.7)

(5.8)

Because of the low CPU time sought for with the present approach (only one representative diameter is examined) the properties of both R134-A vapor and of the air entering in the Schmidt, Prandtl and Reynolds numbers are considered temperature dependent and established with a mass averaged formulation at a given mixing temperature (i.e. 1/3 rule for the film temperature of Yuen and Chen in Abramzon and Sirignano [1]).

All other vapor or gas properties are temperature dependent together with the latent heat of the refrigerant. For the thermodynamical properties of R134-A, data are extracted from Poling et al. [104] and/or the NIST web database. The present approach does not consider the internal flow inside of the droplet. The set of pde’s .1 is solved numerically with a commercial Runge-Kutta 4 solver using a constant time step (named \textit{rkfixed} in MathCAD 2001 Professional Software (©1986-2000 MathSoft, Inc.)).

5.3 Evaporation model prediction and comparison

In Fig. 5.1, a comparison of the measurements (symbols) obtained with 1 mm nozzle and the evaporation model (bold curve) prediction is displayed. The prediction given
by the evaporation model is obtained for a set of initial parameters such as the initial droplet velocity 25 m/s, the initial diameter 200 μm and the initial droplet temperature -20°C. The ambient gas temperature is 25°C and is stagnant. The mass fraction at infinity is chosen as 0.2. The general trend associated with the temperature decrease is in close agreement with the measurements. Nevertheless, this result is dependent on the set of initial values. Such sensitivity is analyzed in the following paragraphs.

![Figure 5.1: Comparison of temperature prediction with measurements of jets exiting a nozzle with a diameter of 1mm](image)

In Fig. 5.2, a prediction of the droplet surface variation in time is displayed. It appears that there is no linear relation between the droplet surface decrease and the time when the droplet is not in a quasi-steady regime. It confirms that a quasi-steady hypothesis is required when the "D² law" is used.

In Fig. 5.3, the history of the different relaxation times is presented and compared with the Stokes relaxation time. Based on this figure, it is found that the transfer of momentum is the fastest. The time relaxation associated with the transfer of mass is the slowest. The Stokes prediction and the relaxation time associated with momentum transfer coincide when the droplet enters in the Stokes regime (i.e. \(Re_{dr} < 1\)). This happens after 0.2 sec or at a distance of about 0.39 m from the nozzle.

Fig. 5.4 displays the limited mass fraction of R134A at the droplet surface. The mass fraction limitation is active only during the very first millisecond even when a time step as small as 10⁻⁴ seconds is used (when an initial temperature of -20°C was considered). It is confirmed that the use of saturation pressure curve under pure equilibrium leads to unphysical mass fraction when used in Eq. 5.3 (i.e. at 20°C using the equilibrium saturation pressure yields a mass fraction of 1.31). Therefore it is found that a limitation on mass fraction is necessary (limited to 0.99 hereafter). Any limitation (p%) on the mass fraction is equivalent to a limitation to the saturation pressure according to the
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

Figure 5.2: History of surface evolution for the representative droplet

Figure 5.3: History of the relaxation times associated to the three transfer processes and compared with Stokes
relation:

$$P_{red} \leq \frac{M_{AIRD%}}{M_{R134A}(1-p\%) + M_{AIRD%}}P_{Atm}$$

In the present case, the limitation of 99% on the mass fraction induces a limitation on the saturation pressure to be no more than 96.57% of the atmospheric pressure.

In Fig. 5.5, the different dimensionless numbers of transport phenomena are displayed. (Please note that the number of symbols on the curves has been reduced for clarity). On the line with square symbols representing the spatial evolution of the droplet Reynolds number, it is seen that the major part of the droplet path takes place out of the Stokes regime ($Re_{dr} > 1$). In the same figure, one has to notice that Stefan's flow induces an increase of the film temperature thickness of about 3 to 6% (i.e. $1.03 < FT < 1.06$) when the film mass thickness experiences a growth of about 5 to 30% (i.e. $1.05 < FM < 1.3$). The low but increasing values of the film Schmidt and film Prandtl numbers are noticeable in the same Fig. 5.5. The effect of mass fraction limitation is clearly seen on the Spalding mass factor with a plateau value of about 80.

5.4 Model sensitivity analysis

In the use of the evaporation model it is found that the lowest value of the droplet temperature is very sensitive to the mass fraction at an infinite distance of the droplet.
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

Figure 5.5: Evolution of the dimensionless numbers with distance from the nozzle exit

($y_{d,\infty}$). A droplet in a pure fluid experiences a higher evaporation than a droplet in a surrounding fluid already containing traces of R-134A as seen in Fig. 5.6. The spray influence was better taken into account with a value of the mass fraction $y_{d,\infty}$ of 0.2. This parameter proves to be crucial to follow the measured temperatures.

In Fig. 5.7 the droplet thermal inertia is displayed. Obviously, the model confirms that the bigger droplets keep their temperature on a longer distance than smaller droplets. Therefore, the initial droplet diameter is also a major parameter to get a correct slope of the temperature decrease.

Literature of evaporation models proposes a rather large choice of correlations. The correlation used by Abramzon and Sirignano (1989) involves a Reynolds dependency as $Re_{dr}^{0.077}$. As it can be seen in Fig. 5.8, this term is amplifying the decrease of temperature at high droplet Reynolds numbers but does not affect the value of the temperature plateau.

In Fig. 5.9, the sensitivity of the model to the film temperature defined by the mixing rule coefficient is displayed. A minor effect is noted between the case with and without the mixing temperature based on a weighting factor of 1/3.

Finally, a noticeable difference shows up as the choice of temperature dependent film properties is concerned. When compared with the film properties fixed at the initial temperature (-20°C) all along the droplet path, Fig. 5.10 shows that this difference is higher than the experimental uncertainty and therefore should be taken into account when high temperature variations are expected.
5.4. Model sensitivity analysis

Figure 5.6: Temperature sensitivity to infinite distance mass fraction

Figure 5.7: Temperature sensitivity to initial diameter
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

Figure 5.8: Temperature sensitivity to correction factor

Figure 5.9: Temperature sensitivity to mixing rule coefficient
5.5 Conclusions

In Fig. 5.11, it is shown that the effect of air speed entrained by the droplet slow down the evaporation process mainly because of the lower resulting values of the droplet Reynolds number. As the air and the droplet are moving in the same direction, the higher is the entrained air velocity, the lower is the relative velocity and therefore the droplet Reynolds number. This parameter has a rather weak influence when considering the measurement uncertainty.

Replacing the full correlation (Eq. 5) with a more standard one ($2 + 0.55 Re^{1/3} (Sc, Pr)^{1/3}$) seems to have a reasonably weak influence on the droplet temperature decrease (Fig. 5.12) keeping in mind the measurement uncertainty.

Nevertheless, the two sets of correlation exhibit different values of droplet Sherwood and droplet Nusselt numbers where the lowest values are associated with the correlation adopted in the present model (see Fig. 5.13).

5.5 Conclusions

A one-dimensional model taking into account the Stefan flow and the dependence of the properties on temperature has been developed. It captures the overall trend of the thermodynamical behaviour of the flashing jet. The plateau value of temperature is found to be highly sensible to the vapor mass fraction in the gas phase ($\gamma_{\text{d,co}}$). Despite the metastable state of the liquid during the atomisation, the use of the saturation pressure obtained at equilibrium gives a satisfactory trend if a limitation is introduced.
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet

Figure 5.11: Temperature sensitivity to gas velocity

Figure 5.12: Temperature sensitivity to correlation
5.5. Conclusions

Figure 5.13: Comparison of the evolutions of present Nusselt and Sherwood correlations with standard ones
to avoid a mass fraction exceeding the unity. On the numerical point of view of the model, sensitivity to parameters such as droplet diameter, vapor mass fraction in the gas phase and temperature dependence of the film properties is found to be important.
Chapter 5. Theoretical modeling of the rapid evaporation in an atomized two-phase flashing jet
Chapter 6

Conclusions and perspectives

A survey of past experimental and analytical work revealed that only a limited understanding exists on the boiling, break-up and atomization mechanisms involved in a sudden release of superheated liquid. This is particularly true for the accidental spillage of liquefied gas following the complete failure of a vessel.

Despite the valuable efforts spent on the characterization of the spray characteristics of a superheated liquid jet in order to understand the source processes of flashing, the limited number of measurements due to the very harsh flow environments led to major uncertainties predicting the consequences of severe accidents. To reduce this gap in knowledge, the aim here was to quantify and obtain a better understanding of the atomization process of a suddenly depressurized (and thus) superheated liquid jet. Moreover, special attention is paid on the influence of the initial flow conditions such as liquid storage temperature, which plays a role on the superheat level, initial storage pressure, which plays a role in the exit velocity of the liquid and nozzle geometries that play a role in the nucleation and bubble growth while the liquid exit the orifice.

Preliminary measurements

Due to the non-equilibrium nature of the near field regions of flashing jets, accurate measurement of flow characteristics has been very arduous and hardly possible with intrusive techniques. Laser-based optical techniques (such as Particle Image Velocimetry (PIV), Particle Tracking Velocimetry and Sizing (PTVS), Phase Doppler Anemometry (PDA) etc.) present the only possibility to obtain information for particle diameter and velocity evolution. Still, even with these techniques, the measurements have been reported as optically very challenging, especially along the area where the liquid disintegrates to reach thermodynamic equilibrium.

A study is performed with a simple set-up for the assessment of the laser-based techniques on a two-phase flashing R134A jet using PDA and PIV. An attempt is also
made to measure the velocity of different droplet classes using a multi-intensity-layer treatment of PIV images.

The preliminary measurements are taken under the limitation of testing times. However, they are sufficient to explore the measurement problems and to produce patterns that are observed within flashing jets. The flow exhibits difficult optical conditions for both PDA and PIV. The existence of ligaments and non-spherical droplets close to the nozzle lead to high rejection rate of data in PDA. The same flow pattern close to the nozzle creates speckle-type image patterns resulting in low SNR in PIV. Going downstream the nozzle, these superheated big ligaments break up into smaller droplets to form a more mono-dispersed spray. This behaviour increases the validation rates in PDA and resulted in better SNR for PIV.

Different tests show that a very small pulse separation minimized the effect of local perpendicular flows due to the rapid explosive break up of superheated large ligaments and gave high signal correlation in PIV measurements. Moreover, using large window sizes during processing increases also the SNR due to the speckle pattern. PDA and PIV measurements exhibit similar trends in centerline velocity evolution with slight differences due to testing time limitations and insufficient data samples. Longer testing times in an environment with well-controlled initial conditions are expected to minimize these differences.

For both techniques, an increase of centerline velocity is observed further away from the nozzle due to the flashing break-up of the superheated ligaments and large droplets. The droplet size evolution indicates that the flow owns a poly-dispersed character close to the nozzle and tends to become mono-dispersed downstream. PDA measurements illustrate that different diameter ranges have only slight velocity differences in the centerline. Multi-intensity-layer PIV treatment does not improve the SNR compared to Standard PIV. For the present study, multi-intensity-layer PIV is found ambiguous due to the following phenomena: the speckle-type scattering of highly dense regions, the Gaussian intensity profile of the laser beam and the extension of the particle diffraction pattern over more than one pixel.

The preliminary measurements pointed out the need to control rigorously the initial conditions of the tests. A new experimental installation is designed in such a way that the control over the initial temperature and pressure is assessed and that any evolution could be tracked. To understand better the influence of the storage conditions on the flashing behaviour, it is preferred to design the setup so that the depressurized material (R-134A) exits the orifice in liquid phase and flashes downstream. The non-intrusive techniques such as high speed video imaging, Phase Doppler Anemometry (PDA), Infrared Thermography and intrusive techniques such as thermocouples are employed for the investigations on break-up and spray characteristics.

Break up patterns of the superheated liquid jet

The main objective of the observation regarding the break up patterns is to assess the influence of initial flow conditions such as liquid storage pressure, nozzle diameter
and superheat on the fragmentation patterns of a superheated jet. A non-intrusive technique like high-speed imaging is used to observe the break up patterns.

For the same backpressure and superheat, the high-speed camera images display that the jet exiting from larger nozzle presents a more violent break-up than the smaller one. Additionally, the disintegration of the larger jet is closer to the nozzle exit.

The high-speed images presented in this study show also that the behaviour of the jet with superheat increment up to 6.4 °C changes from slow expansion to a spray-like behaviour. The results obtained with high speed imaging are in agreement with previously published data.

As explained in the literature, the breakup phenomena of the nonsuperheated liquid jets are controlled by both the internal turbulence in the nozzle and the interfacial force between the jet and the surrounding medium. With the increase in the pressure, i.e. in the velocity, the interfacial forces such as shear forces and pressure perturbations around the jet become important factors in the motion of the interface. The turbulence in the internal flow increases with increase in the velocity. In a superheated jet disintegration the mechanical effects are coupled with the thermal effects. The increase in pressure leads to increased internal turbulence in liquid resulting in more bubble activation, however, since the injection rate is higher the bubble residence time is shorter.

For low liquid temperature the effect of pressure is obvious. When the liquid temperature is low, the thermal effect does not dominate the breakup albeit the existent bubble growth. For example, for the low pressure case, the liquid core of the jet is not stable and bursts of droplets occur often at the nozzle or very close to the nozzle when a large nozzle diameter is used. The break up and cloud formation distance from the nozzle is stabilized increasing the backpressure. The pressure increase limits the residence time of bubble inside the nozzle such that they burst at ~ 3D from the nozzle exit.

For relatively high liquid temperatures, the high superheat activates strongly the bubble production and shatters the jet at the nozzle exit and all along the liquid surface. In this case, the pressure increase influence cannot be visually decoupled from thermal effects in a straightforward way.

For a nonsuperheated liquid jet, increasing the l/D in the nozzle design is expected to change the internal turbulence in the nozzle leading to a decrease in the stability of the jet. In this case, it is explained in the literature that the nozzle shape, especially in the form of a sharp edged orifice, can lead to cavitation resulting in stronger internal turbulence. In the case of a superheated liquid jet, the contact with the nozzle walls will enhance bubble creation and growth activating the existing nucleus on the nozzle walls. The bigger is the l/D, the longer is the residence time of the bubbles inside the nozzle and the higher is the amount of initial nucleus. The visualizations of the flow created by a moderate l/D increase (i.e. l/D = 2) displays a highly disturbed jet. The number of bubbles is considerably augmented and surface shattering takes place.
Increasing the $l/D$ to 7 displays a very interesting flow pattern. The liquid may exit the nozzle in the form of cloud for a time period, then the width of the cloud becomes narrow till the unbroken liquid core is found back. At this stage, the liquid core starts to burst in a perfectly periodic regime at the nozzle exit. This behavior can be explained as periodic bubble formation inside the nozzle. Similar periodic and cloudwise bubble formation is cited in the study Domnick[33]. This breakup pattern can also be linked to the vapor slug formation inside the nozzle, as mentioned in the study of Park and Lee[101].

The highspeed visualization has shown that giving a definite breakup length is quasi impossible due to the various disintegration patterns that can exist in one single case.

One has to keep in mind that patterns of the atomization has been presented in the literature as a simplified model to illustrate the phenomena. Such pattern is nearly impossible to observe in the present case. As a matter of fact, development of a rigorous and trustable method is obligatory for the determination of length of the liquid core, with a prerequisite of an exact definition of the intact liquid core length (i.e. free of bubble and/or diameter unchanged and/or straight and aligned with the axis etc...). It has to be underlined that the strict definition of the liquid core and adequate post-processing method to define a breakup length was not the goal of this present work. It would probably require a research program specially dedicated to this problem.

An interesting observation remark should be done regarding some theoretical models that assume the jet breakup when a bubble reach the jet diameter(Lienhard and Day[71]; Wildgen and Straub[141]; Suzuki et al.[132]). The present high speed images show the events that the jet does not break even with a bubble larger than the jet diameter for low liquid temperatures. On the other hand, there are cases that the liquid core breaks even if the bubbles are significantly smaller than the jet diameter. Therefore, no straightforward conclusion regarding the breakup length can be driven.

The main objective of the present thesis is to characterize the atomization of the superheated liquid jet once it disintegrates into droplets. In this context, the evolution of the size, velocity and temperature of the formed droplets is provided. The influence of initial flow conditions such as liquid storage pressure (called “backpressure” or “driving pressure”), nozzle geometry (i.e. diameter and length) and superheat on the resulting spray characteristics is also investigated.

Evolution of the droplet size in the atomized superheated jet

The effect of different initial pressures, temperatures and orifice diameters on the droplet size and velocity distributions of a flashing R-134A jet along the radial and axial directions are studied by means of Phase Doppler Anemometry (PDA) due to its ability of providing droplet size and velocity, simultaneously. A highly sufficient number of droplets are collected for each investigated case to obtain highly populated statistical averages.

Spray characteristics are obtained by means of Phase Doppler Anemometry (PDA) in
the final test facility where the initial conditions can be preserved during measurement durations. The measurements are performed at relatively far field dimensionless axial distances \( X/D = 110; 220; 440 \). The high rejection rates of data due to the existence of fragments of liquid core, ligaments and non-spherical droplets result in a choice of measurement location where the liquid core is assumed to be completely disintegrated into droplets. These distances are significantly downstream than the measurement locations of the preliminary measurements. The reason is the different flow conditions such that the liquid exits the nozzle as two-phase flow in the preliminary measurements due to a long discharge tube placed inside the commercial nozzle. This has made measurements close to the nozzle possible. However, at the final test facility, the jet exits as liquid core and breaks up rather downstream distances.

Moreover, initial experimental conditions are chosen keeping in mind the technical limitations of the PDA technique, and this results in an investigation of limited conditions considering the superheat levels, but still provides very valuable information for flashing atomisation characterization.

Radial evolutions shows that the droplet sizes and velocities are larger in the centre of the jet and they decrease towards the edges of the jet. When axial evolution is concerned, the droplet sizes and velocity mean values decrease going further from the nozzle due to evaporation and further break-up of large drops. Moreover, a spreading in the width of jet is also clearly observed. The non-dimensional velocity profiles are self similar and follow the analytical radial evolution for the single phase jets.

One interesting point is the evolution of the profiles in axial direction. Going downstream the nozzle, the \( D_{10} \) value shows a systematic increase (almost parallel radial profiles). For the \( D_{32} \) values, this is valid for \( 2 \leq \tau/D \leq 12 \). On the jet axis \( (\tau/D < 2) \), \( D_{32} \)'s for both axial distances \( \tau/D = 220 \) and \( \tau/D = 440 \) are similar to each other. However, they are significantly smaller compared to the one at \( \tau/D = 110 \). This result can be explained by the evaporation process. Evaporation leads to quicker disappearance of the small droplet contents of the distribution (i.e. the count % associated to small diameters decreases). This automatically leads to an increase of the arithmetic mean \( D_{10} \). On the other hand, this disappearance of small droplet classes let the total volume almost unaffected whereas the total surface decreases leading to an increase of \( D_{32} \).

One may also observe that \( D_{10} \) and \( D_{32} \) get roughly closer on the axis when the axial distance from the nozzle increase. This is a sign that the diameter distribution slowly converges towards a more monodispersed distribution.

The liquid superheat plays the most dominant role on the droplet diameters no matter what is the drive pressure, nozzle diameter or axial location. A clear and sharp decrease in droplet sizes is observed with the increase of superheat. The \( D_{32} \) and \( D_{10} \) approach each other significantly. The envelope of the spray is wider. Moreover, the mean drop size radial profiles becomes flat and the mean drop sizes are more uniform with increasing superheat. With increasing superheat, the mean velocities decrease, RMS and turbulence intensity values increase.
When the evolution of $D_{10}$ and $D_{32}$ is plotted with respect to Jacob number $Ja$ which is a strong function of liquid superheat ($Ja = (C_{pl} \Delta T_{ij})/h_{LG}$), a clear decrease in droplet diameters is observed with the increase of $Ja$, whatever is the pressure, nozzle diameter or axial location. With the superheat effect, the $D_{32}$ and $D_{10}$ values approach each other significantly. In other words, with very high superheat the distribution converges to a mono disperse one. It can be conjectured that at a superheat corresponding to $0.38 < Ja < 0.40$ the global distribution over the radius at $x/D = 110$ will be mono disperse. This fact may be explained only by an atomisation process due to a higher production of bubbles than due to aerodynamic break-up effect.

To separate the effect of the backpressure from the one of the superheat, measurements have been performed by pressurizing with $N_2$ while keeping the superheat constant. Indeed, measurements have shown that the increase of the storage pressure results in different flow patterns with different liquid temperatures. At low $Ja$, pressure effect is more pronounced and this can be interpreted as mechanical breakup domination. At high $Ja$, however, pressure effect is small and thermal effects dominate the atomization.

At low $Ja$ (i.e. low superheat), it is observed that the droplet sizes decrease with increasing pressure with a more important effect on the centerline than towards the periphery. With a moderately higher superheat, no change of mean drop sizes is observed with increasing pressure. Further increase in superheat results in higher mean drop sizes with pressure increase. However on a global point of view, high pressure liquids provide a less sharp decrease in mean drop size with increasing superheat than low pressure liquids. The velocity of the jet increase with the increase of the storage velocity, as expected.

It is not straightforward to evaluate the effect of the nozzle diameter on the drop sizes. For low superheats, the mean drop size radial profiles shows that the larger nozzle leads to slightly larger mean diameters of ($D_{10}$ and $D_{32}$) than the smaller one without influencing the velocities. When superheat increases, nozzle diameter does not have an effect on the drop sizes. On a more global view, it can be concluded that nozzle diameter has almost no effect on the drop sizes, mean velocity, RMS and droplet turbulence intensity values compared to superheat effect. These conclusions are valid for the nozzle diameters investigated.

To investigate the effect of the nozzle length-to-diameter ($l/D$) ratio, three designs are compared. The nozzle diameter is 2mm with length-to-diameter ratios of $l/D = 0; 2; 7$. Increasing the length-to-diameter ratio's of the nozzle diameter results in smaller diameters compared to the sharp edge orifice. In the close field, the $l/D = 7$ provides larger drop sizes than $l/D = 2$. In the far field, the differences in drop sizes for both nozzles disappear. The high speed visualizations can provide a possible explanation for this behaviour. The nozzle with $l/D = 7$ preserves higher amounts of the liquid core due to periodic burst compared to the smoother atomization that the nozzle with $l/D = 2$ provides. Therefore, the incomplete atomization created by the nozzle with $l/D = 7$ leads to larger droplets at $x/D = 110$.
are also discussed. Together with the axial and radial evolution of the distributions, the macro behaviour of the atomized jet is investigated using all droplets from a given radial cross-section to give a global view instead of a point-wise analysis.

The radial evolution of the count drop size distributions shows that the number of the large droplets are high on the spray axis and this percentage for the large droplets decreases going from the spray axis towards the board (periphery) of the spray radius. In this sense, it can be said that the distribution on the edge of the spray have a pattern closer to a mono dispersed distribution than a poly-dispersed one. Nevertheless, this remark is only true at the position $x/D = 110$. At this location, the primary fragmentation is still active. The axial droplets are the results of the liquid jet breakup and are rather large. The satellite droplets are the results of both the mechanical instabilities and the bursts of bubbles, and so they are rather small. Going further downstream along the axis (at $x/D = 440$), the breakup of the rather large droplets from the axis at $x/D = 110$ spread away towards the edge of the spray. This opening of the spray leads to a distribution offering a more poly-dispersed pattern from the axis of the spray to the edge radius.

An oscillation (i.e. several peaks) of both the count and mass distribution on the axis is noticed. This oscillation with a wavelength of the order of 100$\mu$m is thought to be a technical artifact. After several repetitions of the measurements with careful verifications no technical drawback could be identified. However, as explained before, one has to keep in mind that the PDA technique can face difficulties in measuring the large and thus nonspherical drop sizes. It has to be emphasized here that when the triple peaks appear in the core of the jet ($r/D = 0.1$) for all the experiments where the liquid temperature is low, the validation for this measurements are also low due to the high rejection rates of non-spherical droplets.

It is noticeable that this oscillations do not appear on the edge of the spray. One possible physical interpretation could be the slug of liquid jet observed by Park and Lee.[101] on the center of the flashing jet with their instantaneous images. But this conjecture could not be verified here. However, the high speed images obtained for this flow condition do present high presence of liquid volume on the centerline. The incomplete atomisation of the large droplets due to the weak superheat and the existence of the large nonspherical droplets in core region of the jet can create this oscillation. This reasoning is enhanced when the triple peaks disappear with increasing $r/D$ distance towards the periphery.

There is a tendency to a "uniform distribution" going downstream. The very large droplet classes around 300$\mu$m keep on having an important effect for mass percentage distribution. Going downstream the nozzle, this effect decreases very slightly. The big change is seen mostly on the very little classes and middle classes. From $x/D = 110$ to $x/D = 220$ a very clear increase and uniformization is observed in mass percentage of the droplet classes ranging between 20 – 200$\mu$m. The distribution becomes even more uniform in the furthest downstream axial distance $x/D = 440$.

For the lowest liquid temperature, higher count percentage is observed for larger
droplets. Increasing the liquid temperature leads to an atomisation where the large droplet counts decrease significantly together with the oscillating pattern associated to a possible 'slug' régime found at low superheat by Park and Lee[101]. The count and mass percentages of small droplets increase with increasing initial liquid temperature. The augmentation of small droplets and lack of large droplets are most significant for the highest measured liquid temperature. Increase of liquid temperature leads into a finer atomisation diminishing the large droplets and shifting the peak values of count and mass percentages towards the small droplet classes.

A competition between the mechanical breakup and thermal induced breakup is demonstrated in the drop size distributions. For the lowest liquid temperature, pressure increment diminishes the number of droplets in large size classes resulting in an increase of mass and count percentage of small droplets. In this régime, superheat is low enough to have still a marked pressure effect on the fragmentation process. For moderate superheat, pressure effect is considerably weak, but still, the higher pressure leads to a slightly weaker evaporation by pushing the jet faster, so that droplets have less time to evaporate till the measurement point. In this régime, superheat and pressure effect are equally responsible on the fragmentation. For relatively high superheat, the count and mass percentage distributions show slightly higher count percentages for all the droplet classes for the higher pressure. The increase in superheat enhances the fragmentation because higher amount of nuclei is activated along the discharge tube walls till the nozzle exit. If the pressure effect pushes the highly superheated liquid faster, one may find a situation where the residence time to develop a bubble from a nuclei is affected. At the moment, this interpretation has to be considered with care because it can not be demonstrated further. Theoretically, if the superheat is further increased, the fragmentation should be through homogeneous nucleation and pressure effect would not be able to perturb the fragmentation because breakup will not depend on residence time anymore.

Nozzle diameter increase shows that there are higher number of large droplets at the measured dimensionless axial locations for a liquid jet exiting a larger nozzle. If this comparison is done in dimensional axial distances, this observation is much more weaker but still valid. In this case, the mass and count percentages more or less superpose with each other for droplets inferior to 150μm displaying at large droplet classes slightly higher values for the larger nozzle diameter.

The a priori prediction of the effect of the \( l/D \) on the drop size distributions is rather difficult. With a very high \( l/D \), the liquid may flow through the nozzle restriction and develop in to a cylindrical flow before the exit into the atmosphere. Of course, the liquid in the nozzle additional tube is protected from mechanical aerodynamically induced instabilities but is more exposed to nuclei sites and therefore to bubble formation. For the experiment with the short additional length \( l/D = 2 \), the nozzle tube length is not long enough to obtain the development of a liquid jet but still offers supplementary sites of nuclei for the bubble formation leading to a less percentage of large droplets. The medium additional length of \( l/D = 7 \) seems to be long enough to start the development of a liquid jet but of course offer also more nuclei sites than the shorter tube, leading to a periodic bursts and liquid core pattern at the exit, therefore it produces higher...
amount of large droplets compared to the nozzle with \( l/D = 2 \) but lower than the one of \( l/D = 0 \).

Various empirical distribution functions are tested on the drop size count and volume distributions. Only the first peaks in the distributions are taken into consideration. For the small superheats, Rosin-Rammler volumetric gives good fitting whereas log-normal volumetric distribution is found better for the high superheats. When the number count distributions are compared, log-normal number distribution gives better representation of the data for high superheat whereas it is Nukiyama-Tanasawa distribution for low superheat.

Although the measured distributions are not adapted for the use of the traditional functions, the small droplets will be taken into account for comparison hereafter, as a first attempt, keeping only the first peak corresponding generally to \( \sim 97 - 99\% \) of the total count distribution and ignoring the large droplets fewer in number. The various empirical distribution functions are applied on these selected first peaks. Here, two flow cases will be presented. These cases are the distributions of the jets exiting 1mm nozzle with a storage pressure of \( \sim 800kPa \) for \( T_{\text{liquid}} = 20.2^\circ C \) and \( T_{\text{liquid}} = 28.3^\circ C \). The global section distributions obtained from all the droplets at \( x/D = 110 \) are compared. The comparison of the global section distribution weakens the effect of triple peaks, as well as providing sufficient information of the total cross-section.

For the small temperature (Fig.4.46(a)), Rosin-Rammler function gives better fitting whereas for the high liquid temperature (Fig.4.46(b)) (i.e. better atomization) it is the log-normal volumetric distribution, which provides better representation. Considering the count distribution comparisons for the latter temperature (Fig.4.46(d)), it is again the log-normal count distribution function which gives a more satisfactory fit, whereas for the \( T_{\text{liquid}} = 20.2^\circ C \) it is the Nukiyama-Tanasawa distribution.

**Evolution of the velocity in the atomized superheated jet**

The nondimensional velocity profiles for the droplets and gas phase show self-similar pattern and can be represented with the correlations in the literature for single phase turbulent jets and two-phase jets. As in a single phase turbulent jet, the velocity is high in the centerline and decreases towards the periphery with the interaction to the ambient, whereas the RMS and droplet turbulence intensity is small in the center and increases at the periphery as long as low superheat is concerned. At higher superheat, high droplet turbulent intensity in the center is also observed.

Across a section, the distributions are narrow in the centerline and broad on the borders of the spray with more velocity classes. Moving downstream, the nozzle leads to the widening of the distributions due to the slow-down of the droplets in the aerodynamical interaction with the surrounding and the diameter decrease due to evaporation.

As explained in the previous section, increasing superheat results in a production of smaller droplets. With low or moderate superheats, no significant change is exhibited in the profiles, whereas high superheat enhances the fragmentation of the droplets thus
Chapter 6. Conclusions and perspectives

accelerates the momentum exchange of the droplets with the air leading to a general slow down in velocity. Moreover, flatter, higher RMS and droplet turbulence intensity profiles are obtained with high superheats.

Pressure increase results in an increase in mean velocity values all over the cross-section without changing the RMS values. The droplet turbulence intensity is lower for the higher pressure due to preservation of the jet core for longer distances. Velocity count distributions exhibit a shift of the peak value towards the higher values with pressure increase both at the centerline and periphery. The velocity distributions at the centerline is more narrow whereas the ones at the periphery are outspread due to the interaction with the ambient air. For high pressures, more velocity classes are observed in the distribution at the periphery than for lower pressures. At the centerline, however, the width of the distribution is preserved with pressure increase.

Changing the nozzle diameter from 1mm to 2mm only changes slightly the mean velocity, but not the RMS and droplet turbulence intensity profiles. Distributions compared at dimensional distance show identical peak value and percentages for both nozzles at the centerline with a slight increase of percentage in small velocity classes when the large nozzle is used. Nevertheless, at a higher distance from the nozzle, the small nozzle produce a better interaction with the ambient (i.e. the velocity distribution is wider).

Using three nozzle designs with different \( \frac{L}{D} \), shows that sprays formed from nozzles with \( \frac{L}{D} = 0 \) and \( \frac{L}{D} = 2 \) produce identical mean velocity, RMS and turbulence intensity profiles at \( x/D \) = 110. Profiles for \( \frac{L}{D} = 7 \) are different at the periphery than the ones obtained from the other two nozzles. At \( x/D = 220 \), nozzles with higher \( \frac{L}{D} \) produce lower mean velocity together with higher RMS and turbulence intensity at the centerline. At the periphery, all profiles are identical. The distributions obtained from three designs are identical at the centerline at \( x/D \) = 110 and shifts towards lower velocity classes for larger \( \frac{L}{D} \) at \( x/D = 220 \). At the periphery, the differences are slight such that the distributions can be called identical.

Temperature evolution of the superheated liquid jet:

Temperature evolution of the flashing jet is investigated using both non-intrusive and intrusive measurement techniques. For the nonintrusive part, Infrared Thermography is applied on the area where the liquid core still exist, i.e. very close to the nozzle exit. The infrared thermograms show that the liquid surface temperature drops very fast from the initial storage temperature to the boiling temperature of the liquid at the ambient.

Intrusive measurements include the thermocouple experiments that are performed mostly on the regions of fragmentation to give thermal information regarding the global flow. A high repeatability has been demonstrated during the measurement campaign. A rack of ten thermocouples is used to measure temperature at different distances from the nozzle. Comparisons with measurements with a single thermocouple assess a low intrusiveness of the rack. The choice of thermocouple type and wire diameter gives slight differences in the measured steady mean temperature. If the breakup of the jet
is in the proximity of the first thermocouple probe, a temperature close to the boiling point \(-26.4°C\) at atmospheric pressure is measured at the centerline. The temperature continues to drop and reaches a plateau of \(\sim -55°C\) downstream the nozzle through strong evaporation, whatever is the superheat, pressure, nozzle diameter or \(l/D\).

The effect of initial parameters on the temperature evolution of the atomized jet is also discussed. Increase in liquid temperature leads to a decrease in the measured temperatures all over the radial profiles at the measured axial distances.

When the flashing breakup occurs at the nozzle exit, the evaporation of the droplets before they reach the thermocouple probe is higher and a stronger temperature drop is observed. This is valid for the experiments with very large nozzle diameters such as 4mm or very high superheat.

The radial temperature evolution states clearly a decrease in temperature all along the radius while going towards the downstream distances. For each axial position \((x/D)\), the jet temperature is higher in the center and decreases towards the jet periphery since there is more air entrainment on the side than on the centerline together with a high population of small droplets and this increases evaporation. Going more further away in the radial distance, temperature starts to increase again due to the heating of the ambient through entrainment of air.

The increase in superheat leads to a decrease of spray temperature all over the profile, having an effect stronger on the centerline than on the sides. The higher is the liquid temperature, the more uniform and low is the spray temperatures along the radius. To recall, high superheat results in shorter breakup lengths, stronger atomisation, provide smaller and slower droplets. Therefore, when the superheat increases, the distance between the breakup point and measurements location of the first thermocouple probe will augment. Since the velocity is smaller as well, droplets will pass more time to reach the measurement point experiencing more evaporation, thus having lower temperatures uniformly across the section.

Higher pressure generally leads to higher spray temperature on the regions where it produces also larger droplets. Large droplets keep their temperature for longer time and evaporate, thus cool down, slower. Moreover, with high pressure they move faster so that they have less time to evaporate till the measurement point.

Temperature profiles show that the change of the nozzle diameter does not have an effect if compared in non-dimensional distances. However, the representation in dimensional form gives higher temperature values for the larger nozzle mostly in the core close to the centerline. This may be explained by the slight change in the other distributions such as drop size and velocity. Small-diameter nozzle creates a jet that is slightly slower, thus droplets form from the jet exiting the small nozzle have more time to evaporate till the measurement point.

Increased \(l/D\) ratios decrease the observed temperature values strongly in the centerline but not in the periphery of the spray compared to the case \(l/D = \sim 0\) at the close field.
However, the nozzles of $l/D = 2$ and $l/D = 7$ provide nearly identical temperature profiles at $x/D = 110$. Higher $l/D$ nozzle produce sprays with higher volume fraction for droplets $\leq 200\mu m$ resulting in a higher evaporation.

**1-D evaporation model for a representative droplet:**

A one-dimensional model taking into account the Stefan flow and the temperature dependent properties has been developed. It captures the overall trend of the thermodynamical behaviour of the flashing jet. The plateau value of temperature is found to be highly sensible to the mass fraction in the gas phase ($y_{d,\infty}$). Despite the metastable state of the liquid during the atomisation, the use of the saturation pressure obtained at equilibrium gives a satisfactory trend if a limitation is introduced to obtain a mass fraction lower than the unity. On the numerical point of view of the model, sensitivity to parameters such as droplet diameter, vapor mass fraction in the gas phase and temperature dependence of the film properties are found to be important.
Bibliography


BIBLIOGRAPHY


BIBLIOGRAPHY


