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Validation of the system code CATHARE3 on critical flow experiments in the framework of the OECD-NEA ATRIUM project

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ABSTRACT

When applying the Best Estimate Plus Uncertainty (BEPU) methodology for the safety analyses of nuclear reactors, one of the major issues is to quantify the uncertainties associated to the physical models in thermal-hydraulic codes. A good practice guideline for Inverse Uncertainty Quantification (IUQ) was therefore developed during the OECD-NEA SAPIUM (Development of a Systematic APproach for Input Uncertainty quantification of the physical Models in thermal-hydraulic codes) project in 2020. A first application of this guideline is now carried out within the OECD-NEA ATRIUM (Application Tests for Realization of Inverse Uncertainty quantification and validation Methodologies in thermal-hydraulics) project, which was launched in 2022. The goal is to perform practical IUQ benchmark exercises to evaluate the applicability of the SAPIUM best-practices and suggest possible improvements.

In this article, we describe part of the work performed at CEA on the first benchmark exercise on critical flow. In particular, we focus on the validation of the system code CATHARE3 against the available experimental data and the associated sensitivity analyses performed to better understand the simulation results and prepare the IUQ process. The 324 choked flow experiments come from three different facilities: Sozzi-Sutherland, Super Moby-Dick and Marviken-CFT. The simulations are in very good agreement with the experimental data (maximum discrepancy of 23.3% on the critical flowrate). Based on the sensitivity analyses, two main influential parameters are identified: the wall-to-liquid friction and the flashing models in CATHARE3. The flashing is dominant for relatively short nozzles ($L/D \leq 18$). For longer nozzles, the wall-to-liquid friction becomes more and more influential.

KEYWORDS

Critical flow, ATRIUM, CATHARE, Validation, Sensitivity

1. INTRODUCTION

The use of Best Estimate Plus Uncertainty (BEPU) methodologies for the safety analyses of nuclear reactors is growing [1]. The accidental scenarios are therefore simulated using a best-estimate thermal-hydraulic system code (e.g. ATHLET, CATHARE, RELAP, SPACE, TRACE, ...), but the related uncertainties should be quantified. One of the challenges is to quantify the uncertainties associated to the physical models implemented in the code, usually referred to as Inverse Uncertainty Quantification (IUQ). To tackle this issue, the OECD-NEA ATRIUM (Application Tests for Realization of Inverse Uncertainty quantification and validation Methodologies in thermal-hydraulics) 4-years project was launched in 2022, drawing the interest of more than 20 institutions worldwide [2]. The goal is to carry out IUQ benchmark exercises to demonstrate the applicability of, and eventually improve, the OECD-NEA SAPIUM (Systematic APproach for Input Uncertainty quantification Methodology) guideline [3, 4], which details the necessary steps and best-practices to perform IUQ. The guideline proposes a systematic approach where IUQ should be performed as a global process involving 5 key elements: a clear specification of the problem, an efficient strategy to construct an adequate experimental database, a development and assessment of the simulation models, the quantification of the uncertainty ranges and distributions with adequate IUQ methods, and finally their validation. The first exercise of ATRIUM aims at quantifying the uncertainties associated to the critical/choked flow phenomenon, which is very important during a Loss Of Coolant Accident (LOCA).

In this article, we describe part of the work performed at CEA on this first exercise. In particular, we focus on the validation of the system code CATHARE3 against the available experimental data and the related sensitivity analyses performed to better understand the simulation results and prepare the IUQ process (step 3 of the SAPIUM guideline).

The paper is organized as follows. In Section 2, the available experimental database is briefly described and analyzed. In Section 3, the modelling with the code CATHARE-3 is presented. The results of the simulations and the associated sensitivity analyses are discussed respectively in Section 4 and 5. Finally, Section 6 summarizes the main conclusions.

2. DESCRIPTION AND ANALYSIS OF THE EXPERIMENTAL DATABASE

The organizers of the benchmark provided a list of critical flow experiments to the participants. It consisted of three experimental setups: Sozzi-Sutherland (Nozzle 2, 3 and 4) [5], Super Moby-Dick [6, 7, 8] and Marviken CFT (tests 13, 17 and 24) [9].

In the Sozzi-Sutherland (Sozzi) tests, the subcooled or saturated water inside a pressurized vessel is discharged to the atmosphere through different horizontal nozzles and the critical mass flowrate is measured. Three nozzles (2, 3 and 4) are studied in ATRIUM for a total of 439 available experiments. Nozzle 2 and 4 consist of long well-rounded inlet convergents, where the internal diameters are progressively reduced from 43.2 mm to the final throat diameter D , 12.7 and 19 mm respectively. Downstream the throat, Nozzle 2 is followed by a straight pipe of length L ($0 \leq L/D \leq 140$), while an abrupt expansion ($L/D = 0$) is present in Nozzle 4. Nozzle 3 is a 12.7 mm sharp-edge orifice, 4.7 mm long.

In the Super Moby-Dick (SMD) experiments, stable steady-state measurements of the critical flowrate are obtained thanks to a dedicated thermal-hydraulic loop. Two campaigns are provided in ATRIUM. However, only the so-called “SMD Divergent” one was considered by CEA since the second campaign reproduced almost the same experiments obtaining practically identical results. The test section consists of a 100 mm long smooth convergent (20.13 mm throat diameter), followed by a 363 mm straight pipe ($L/D = 18$) and a 437 mm long divergent (conical expansion with an angle of $6^{\circ}57'$). The inlet convergent is designed to minimize any two-dimensional effect affecting the critical flow.

The Marviken CFT experiments were performed to obtain critical flow measurements under conditions that may occur in a full-scale reactor LOCA. Subcooled or saturated water is discharged from a full-scale reactor vessel to the atmosphere. The discharge flowrate (not necessarily in critical conditions) is registered during the transient. The smooth converging nozzle is placed vertical at the bottom of the vessel and it is followed by a straight pipe ($0.33 \leq L/D \leq 3.7$). In ATRIUM, three Marviken CFT tests (13, 17 and 24) are considered.

According to the SAPIUM guideline, the adequacy of the experimental database for the intended purpose (i.e. the quantification of the uncertainties applicable to the problem of interest) should be assessed [10]. In ATRIUM, the problem of interest consists of an Intermediate Break LOCA (IB-LOCA) in a Pressurized Water Reactor (PWR). In particular, to simplify the analyses and allow the validation of the obtained uncertainties, a specific IB-LOCA transient reproduced in the LSTF Integral Effect Test (IET) facility is taken as reference. It is the LSTF Intermediate Break-Hot Leg-01 (LSTF IB-HL-01) experiment [11], which simulates a double-ended guillotine break of the pressurizer surge line in a PWR.

As explained in [12], CEA performed the adequacy analysis and concluded that the Super Moby Dick experiments are the most adequate ones. This is due to:

- their high representativeness both in terms of nozzle geometry and thermal-hydraulic conditions (coverage of the LSTF IB-HL-01 target domain);
- extensive documentation and good quality of the experimental data;
- capability to provide useful information for system code validation and IUQ, thanks to its nozzle geometry avoiding 3D fluid flow effects and the possibility to have steady-state conditions (high separability).

The other experiments were ranked in order of representativeness as follows: Sozzi-Sutherland Nozzle 2, Marviken-CFT 17, 13 and 24, Sozzi-Sutherland Nozzle 4 and 3. Based on these analyses, it was decided to discard the least representative experiments (i.e. Sozzi-Sutherland Nozzle 3 and 4, and Marviken CFT 13 and 24) from the next steps of the IUQ process. The main liability of the excluded tests lies in the short nozzle length L/D , which causes the presence of non-equilibrium effects in the nozzle [13, 14]. The critical flow for small L/D is therefore highly dependent on the form loss associated to the nozzle or orifice (local pressure loss coefficient). This was not evaluated experimentally and it is therefore very difficult to model accurately in system codes. Furthermore, the short nozzles are not representative of LSTF, whose L/D is equal to 12. For the same reasons, all the Nozzle 2 tests with $L/D < 3$ were ruled out.

All the retained experimental data are therefore plotted to study the dependencies and identify potential outliers. This experimental analysis allowed the identification and exclusion of one outlier (point 75 of the table Sozzi N2 $L/D = 140$ in the original publication [5]). Two tests (points 66 and 67 of the table Sozzi N2 $L/D = 140$) present identical inlet conditions and, consequently, critical flowrates. The presence of duplicated experimental points may bias the IUQ, so one of them is removed.

Thus, the final experimental database consists of 323 experimental points coming from Super Moby-Dick and Sozzi-Sutherland Nozzle 2 with $L/D \geq 3$, which are used for IUQ, and the Marviken CFT test 17. The Marviken test is employed exclusively for the validation of the quantified uncertainties, since it is a transient. A summary of the database is reported in Table 1. It contains the main geometric characteristics of the nozzle (throat diameter D and length-to-diameter ratio L/D), the number of experiments, the ranges of pressure, thermodynamic quality at the nozzle inlet, and critical mass flux (i.e. the Quantity of Interest – QoI).

Table 1: Summary of the experimental database and of the target domain.

	D [mm]	L/D	N° tests	Inlet pressure [bar]	Inlet thermodynamic quality [-]	Critical mass flux [kg/m ² /s]
Sozzi –Sutherland N2	12.7	3 – 140	296	56.0 – 71.3	-0.1439 – 0.0065	17528 - 75824
SMD	20.13	18	27	20.0 – 120.1	-0.0991 – -0.0005	15300 - 62200
Marviken CFT 17	300	3.7	1	31 – 51.4	< 0	23600 - 61700
Target domain LSTF	41	12	1	10 – 155	-0.15 – 1.0	1500 – 46000

3. CATHARE MODELLING

The simulations in this work were performed with the system code CATHARE3 v2.2.160, developed at CEA. CATHARE solves the 6-equations model (mass, momentum and energy balance for the two phases) [15, 16]. As described in [17], the critical flowrate is reached when the mass flowrate becomes independent of the downstream flow conditions. This occurs when the acoustic signal associated to the pressure can no longer propagate upstream the nozzle throat, i.e. the fluid velocity is larger than the speed of sound (propagation velocity of the pressure (sound) waves).

CATHARE determines the propagation velocities calculating the roots of the characteristic equation. The characteristic equation is obtained from the system of conservation equations, which can be rewritten as:

$$A \frac{\partial X}{\partial t} + B \frac{\partial X}{\partial x} = C \quad (1)$$

where X is the vector of variables (e.g. p , v), A and B are the associated matrices and C the vector of source/sink terms. The characteristic equation is a sixth-order polynomial function with six roots λ which is obtained from:

$$\det(B - \lambda A) = 0 \quad (2)$$

The sonic speed is deduced from the roots. Thus, no specific empirical model, like 0-D choked flow models, is used in CATHARE to calculate the critical mass flow rate in an axial element [14].

As already observed in [17], the determination of the thermodynamic non-equilibrium sound velocity of the two-phase fluid is not an easy task. The critical flowrate depends on the flashing phenomenon and the associated flashing delay in the presence of subcooled inlet conditions. In CATHARE, the flashing is modelled as a semi-empirical correlation used to estimate the interface to liquid heat flux [18].

The critical flowrate in CATHARE was previously validated over several SET experiments, which cover a wide range of conditions, i.e. $D \sim 5 - 500$ mm; $L/D \sim 0 - 20$; $p = 2 - 120$ bar; subcooled, saturated and air-water tests. Furthermore, CATHARE was validated over several IET transients, including LSTF. The application of CATHARE to the experiments of interest is therefore justified due to the wide validation matrix, which includes also the Super-MobyDick and Marviken CFT tests.

The Sozzi, SMD and Marviken nozzles are modelled as a 1D element with a fine nodalization. The size of the mesh at the throat is smaller or equal to 1 mm according to CATHARE user guidelines. The inlet pressure, liquid enthalpy and void fraction are imposed as inlet boundary conditions. The atmospheric pressure is imposed at the outlet. In case of saturated inlet conditions, the inlet void fraction is estimated using the homogenous hypothesis (i.e. slip ratio between phases equal to 1) [19]. The Chisolm formula [19] for the slip ratio was also tested. The change in void fraction was judged non-significant due to the low void fraction values ($\alpha < 0.2$) in the experiments. Thus, the homogenous hypothesis is retained.

A wall roughness of $4.5 \cdot 10^{-5}$ m (typical value for commercial steel) is used in the reference calculations for Sozzi. Conversely, based on previous works at CEA and the experimenters' claim of a very smooth nozzle, the wall roughness is set to zero for SMD and Marviken.

Due to the smooth convergent section, no local pressure loss coefficients are used at the throat section for Sozzi and SMD. In the case of Marviken, the determination of the localized pressure loss coefficient (k_{loc}) is difficult due to the particular shape of the nozzle (see Figure 1) that couldn't be clearly identified in [20]. After a sensitivity analysis (see Section 5.2), it was decided to retain a value of 0.182 as reference since it gave the best results in the CATHARE simulations. This value is associated to the sharp-edge contraction between 752 and 600 mm [20], while the loss related to the smooth nozzle is neglected.

As suggested in the SAPIUM guideline, the geometrical fidelity of the CATHARE model was carefully verified.

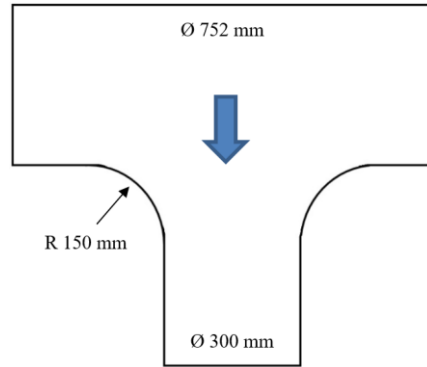


Figure 1: Nozzle geometry (Marviken CFT 17).

4. RESULTS OF THE SIMULATIONS

The calculated ($G_{c,CATH}$) and experimental ($G_{c,exp}$) critical mass fluxes are compared using the residuals in percentage:

$$r = 100 * \frac{G_{c,CATH} - G_{c,EXP}}{G_{c,EXP}} \quad (3)$$

A summary of the results for the Sozzi-Sutherland and SMD experiments is reported in Table 2 and Figure 2. In the table, the mean of the residuals indicates how centered on the best-estimate value of zero the

predictions are, while the standard deviation ($\sigma(r)$) measures the dispersion. The minimum and maximum values of the residuals provide the largest prediction errors. The figure plots the residuals as a function of the thermodynamic quality.

Table 2: CATHARE - experiment residuals (Sozzi-Sutherland N2 and SMD).

<i>L/D</i>	Sozzi-Sutherland N2										SMD	All tests
	3	5	9	15	18	25	40	50	140	All	18	
Mean(<i>r</i>) [%]	10.1	10.0	6.1	0.6	1.0	3.5	0.4	3.3	-3.7	2.1	-2.5	1.6
$\sigma(r)$ [%]	8.7	4.2	4.1	2.4	3.2	5.0	4.1	5.8	7.9	7.5	5.8	7.5
Min(<i>r</i>) [%]	-2.7	0.4	0.0	-3.5	-9.4	-0.9	-8.6	-10.8	-18.1	-18.1	-14.3	-18.1
Max(<i>r</i>) [%]	23.3	15.7	13.3	4.1	4.5	18.8	5.8	17.0	9.8	23.3	4.4	23.3

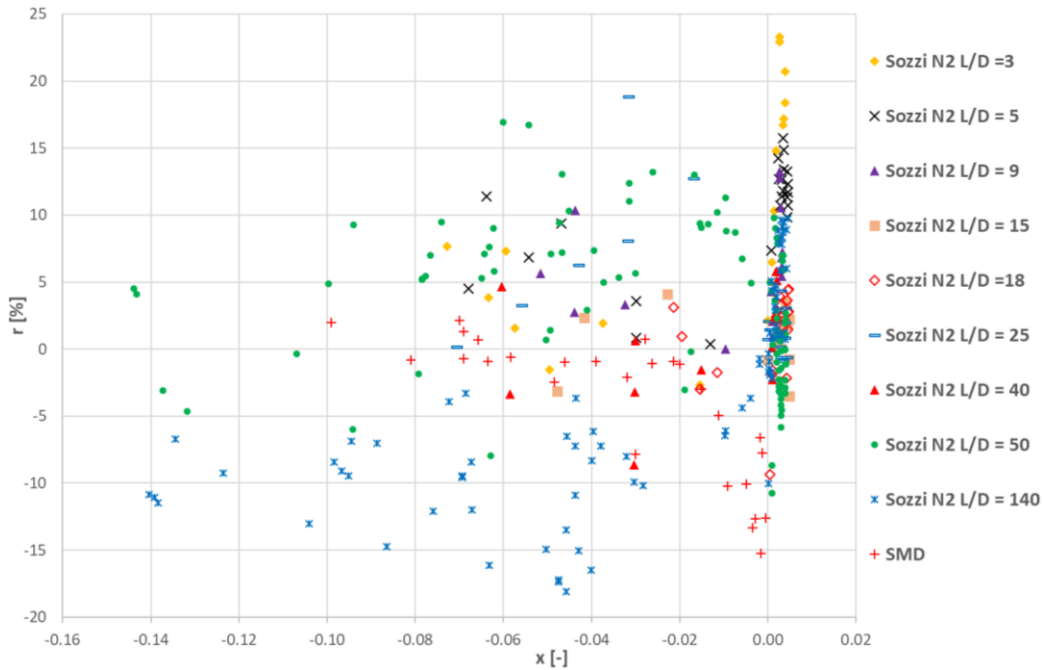


Figure 2: CATHARE - experiment residuals as a function of the inlet thermodynamic quality.

The critical mass flux predictions are well centered (average residual of 1.6 %) and a relatively low standard deviation (7.5 %) is obtained over all the tests. All of the points are within the ± 20 % error bar, except 3 tests Sozzi N2 $L/D = 3$. The largest biases are associated to the tests with $L/D \leq 9$ and CATHARE tends to overestimate the critical mass flux in these cases. This is reasonable since the critical flow for small L/D is dependent on the form loss associated to the nozzle (see discussion in Section 2) and, in our simulations, no local pressure drops were considered. However, the errors remain acceptable. We decided not to include any local pressure drop in the reference calculations since the convergent is pretty smooth and it would be difficult to quantify exactly the pressure drop coefficient, especially in two-phase conditions (e.g. vena contracta?). A brief sensitivity analysis will be presented in Section 5.

A tendency of CATHARE to under-predict slightly the critical mass flux at low qualities and, conversely, to over-predict slightly at qualities around 0 seems to appear visually in Figure 2. However, no clear conclusion can be achieved on this statement because this may depend on other factors, e.g. the availability of tests at different conditions for different L/D .

The results of the simulation of the Marviken CFT 17 test are compared to the experimental measurements in Figure 3. The figure displays also the experimental uncertainty band of ± 10.4 %, which was reported as maximum uncertainty value for the Pitot static measurements of break mass flowrate [21]. This value is

consistent with the variability around the average value of the experimental measurements in the last part of the transient. For easier presentation and analysis, the uncertainty band of $\pm 10.4\%$ is applied to the average experimental behavior of the critical flowrate fitted with a polynomial function of order 2. This also allows to have a direct comparison of the simulations to the average experimental behavior. This can give a qualitative idea of the residuals between experiment and CATHARE calculation.

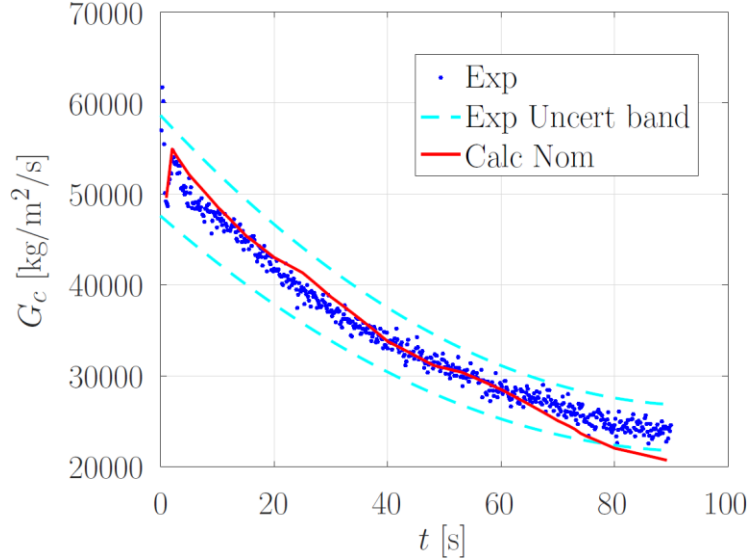


Figure 3: Experimental vs calculated time evolution of the mass flux (Marviken CFT 17).

The simulated critical mass flux qualitatively follows the experimental behavior pretty well with some discrepancy at the end of the transient. The calculation falls within the experimental uncertainty bands for most of the transient except the last 10 seconds ($t > 80$ s). The first flowrate value at $t = 0$ s in the simulation corresponds to the transient opening of the valve (fully open at $t = 0.05$ s in the simulation). This value shouldn't be compared to the experimental one, since the dynamic of the valve opening is unknown.

The residuals between calculated and experimental critical mass flux (polynomial fit) remain comparable with the ones observed in Sozzi N2 and SMD. The critical mass flux predictions are fairly centered with a slight tendency to underestimate (average residual of -1.3%) and a standard deviation (5.4%) comparable to the experimental uncertainty. The minimum and maximum discrepancies are respectively -14.7% and 5.9% .

Based on these considerations, we consider CATHARE to be best-estimate and validated. The main source of the discrepancies is most probably the relatively large experimental uncertainties on the critical mass flux, the inlet temperature and the localized pressure loss coefficient. The impact of these uncertainties is discussed in the next section.

5. SENSITIVITY ANALYSES

In this section, several sensitivity analyses are presented and discussed.

5.1. Sensitivity to the wall roughness

As shown in Table 3, a sensitivity to the wall roughness is performed on all the tests Sozzi N2 $L/D = 3$, 50 and SMD. The wall roughness is an important parameter impacting the critical mass flow, especially for the tests with large L/D . The increase of wall roughness determines a reduction of the critical mass flux in the CATHARE simulations. Conversely, the wall roughness plays a little role for small L/D , such Sozzi N2 $L/D = 3$.

Table 3: Sensitivity to the wall roughness (Sozzi-Sutherland N2 L/D = 3, 50 and SMD).

Wall roughness [m]	Sozzi N2 L/D = 3		Sozzi N2 L/D = 50			SMD	
	0	$4.5 \cdot 10^{-5}$ (ref)	0	$1.3 \cdot 10^{-5}$	$4.5 \cdot 10^{-5}$ (ref)	0 (ref)	$4.5 \cdot 10^{-5}$
Mean(r) [%]	11.7	10.1	16.4	9.0	3.3	-2.5	-9.1
σ (r) [%]	8.6	8.7	9.0	7.0	5.8	5.8	4.1
Min(r) [%]	-1.2	-2.7	-2.7	-7.2	-10.8	-14.3	-18.4
Max(r) [%]	24.9	23.3	37.7	25.9	17.0	4.4	-4.0

A simulation of the Marviken CFT 17 test using the roughness for commercial steels ($4.5 \cdot 10^{-5}$ m) was also performed (no roughness in best-estimate). No significant impact was observed on the critical mass flowrate. This confirms that the wall roughness is not important for small L/D .

5.2. Sensitivity to the local pressure loss coefficient

We performed a sensitivity analysis on the local pressure loss coefficient at the throat. The results are reported in Table 4. The local pressure loss coefficient k_{loc} is varied from 0 (nominal value) to 0.5 and 1. Even if the loss coefficients according to [20] are negligible, it is interesting to explore its impact since other local phenomena (e.g. vena contracta) may induce a local pressure loss. The use of a loss coefficient leads to the reduction of the mass fluxes predicted by CATHARE. The largest impact is observed for small L/D tests, as expected. For $L/D = 3$, an average reduction of the simulated mass fluxes of approximately 10% is obtained for an extra k_{loc} of 0.5. A 5 % average reduction is instead observed for $L/D = 50$. These results confirm the fact that the critical flow for small L/D is highly dependent on the form loss associated to the nozzle, while the length of the nozzle/pipe is more important for larger L/D .

Table 4: Sensitivity to the local pressure loss at the throat (Sozzi N2 L/D = 3 and 50).

k_{loc}	Sozzi L/D = 3			Sozzi L/D = 50		
	0	0.5	1.0	0	0.5	1.0
Mean(r) [%]	10.1	-1.7	-10.2	3.3	-2.9	-7.4
Std(r) [%]	8.7	9.5	9.9	5.8	4.9	5.7
Min(r) [%]	-2.7	-14.2	-23.1	-10.8	-16.5	-23.0
Max(r) [%]	23.3	11.9	3.5	17.0	6.1	3.4

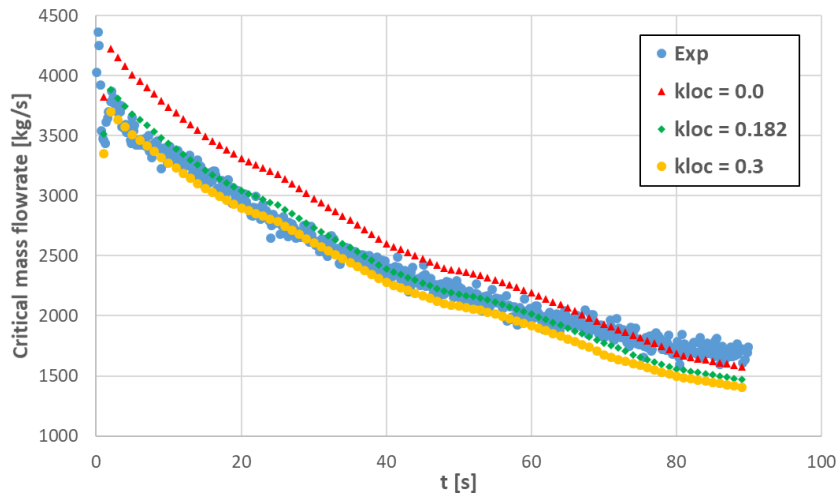


Figure 4: Sensitivity to the local pressure loss at the throat (Marviken CFT 17).

A sensitivity was also performed on the Marviken test, as shown in Figure 4. The local pressure loss coefficient is varied from 0 to 0.182 (reference value) and 0.3 (value obtained for a sharp-edge contraction between 752 and 300 mm [20], see Figure 1). A significant reduction of the mass fluxes predicted by CATHARE is obtained increasing the localized pressure loss coefficient, confirming the importance of this parameter to the prediction of the critical flow in short nozzles.

5.3. Sensitivity to the inlet temperature

In the Marviken report [21], several uncertainties are reported to affect the measured inlet temperature. A systematic bias was estimated indicating that the measured temperatures were most probably lower than the real ones by 1 – 2.5 °C. Furthermore, the experimentalists estimated a random error of ± 2 °C on the temperature measurements. Thus, a sensitivity to the temperature at the nozzle inlet is also performed. The measured inlet temperature is increased by 2 °C to test its impact on the simulations. In Figure 5, we can see that a relatively small variation of the inlet temperature has a strong impact on the critical mass flowrate, especially at the end of the transient. This tells us that the experimental uncertainties on the inlet temperature are extremely impactful and can explain most of the experiment-code discrepancies.

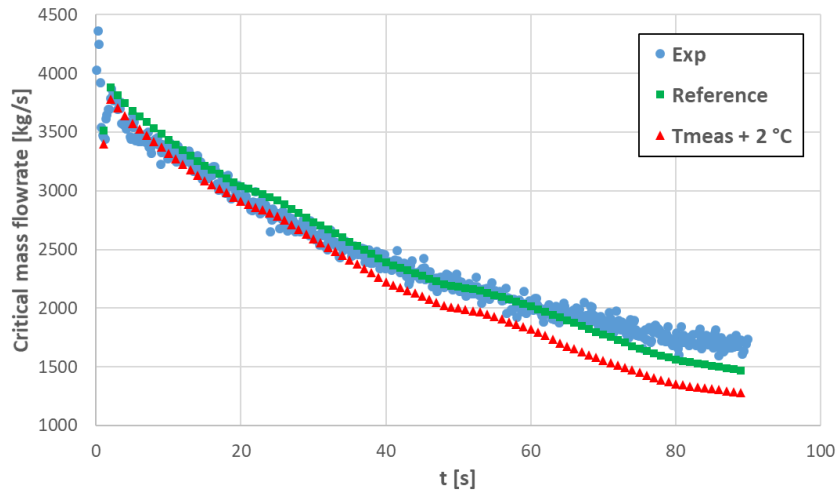


Figure 5: Sensitivity to the inlet temperature (Marviken CFT 17).

5.4. A priori sensitivity analysis to CATHARE closure laws

Even though the choked flow is basically calculated by solving the 6 balance equations (see Section 3), some closure laws (or physical models) in the CATHARE code may impact the result. Based on previous works at CEA (e.g. [18]), the critical flowrate may depend on the following physical models in CATHARE:

- Wall-to-liquid friction (CATHARE keyword: SP1CL)
- Wall-to-steam friction (SP1CG)
- Flashing (P1QLF)
- Delay to flashing (SP3QLF)
- Interfacial friction (SP1TOI)
- Steam-to-interface heat transfer (SP1QVE)

In this work, we perform a *a priori* sensitivity analysis on these physical models to identify the most influencing ones. The list of postulated uncertainties is reported in Table 5. These uncertainties are determined by expert judgement.

Table 5: Selected input uncertainties for the *a priori* sensitivity analysis.

	Distribution	Parameters or range
Wall-to-liquid friction (SP1CL)	Normal	$\mu = 1.0$ $\sigma = 0.1$
Wall-to-steam friction (SP1CG)		
Flashing (P1QLF)	Uniform	[0.5; 2.0]
Delay to flashing (SP3QLF)		
Interfacial friction (SP1TOI)		
Interfacial heat transfer (SP1QVE)		

We carry out a propagation of uncertainties varying simultaneously the 6 uncertainties sampled with a Simple Random Sampling (SRS). A total number of 260 calculations was performed for each experimental test of the quantification database (260 x 323 = 83980 CATHARE simulations). The value 260 corresponds to a Wilks order 8 [22]. For each test, the Pearson and Spearman correlation coefficients are calculated. The Pearson correlation coefficient measures the strength of the linear relationship between two samples [23]. The Spearman rank correlation coefficient is based on a non-parametric approach, which treats the sample values in terms of their ordered ranks. No linear assumption is therefore needed and the coefficient measures how strongly monotonic the relationship between the two variables is [23].

The Pearson coefficients in absolute values of the influencing parameters are plotted in Figure 6 for the Sozzi and SMD tests. From the analysis, we observe that:

- The Wall-to-steam friction (SP1CG), the Interfacial friction (SP1TOI) and Steam-to-interface heat transfer (SP1QVE) do not impact the simulations (Pearson < 0.2 for almost all tests);
- The sum of the 6 squared Pearson coefficients is always very close to unity, indicating that the code response is linear modifying the sensitivity parameters;
- The Spearman coefficients are almost identical to the Pearson one, further confirming the linearity of the code response;
- Both the Flashing (P1QLF) and the Wall-to-liquid friction (SP1CL) are characterized by a negative Pearson and Spearman coefficients. An increase of the friction/flashing in the nozzle determines the decrease of the critical mass flow.
- The Delay to flashing (SP3QLF) is influential only for some of the SMD tests, while it is always negligible in the Sozzi tests. A positive relationship (positive Pearson/Spearman) is observed.
- The Flashing (P1QLF) is the only and dominant parameter for all the Sozzi simulations with $L/D \leq 18$. This is reasonable, since we expect the flashing to play an important role in relatively short nozzles due to the presence of non-equilibrium phenomena.
- Progressively, for longer nozzles $L/D > 18$, the Wall-to-liquid friction (SP1CL) becomes more and more influential. Until it becomes the only and dominant parameter for the tests with $L/D = 140$. For $25 \leq L/D \leq 50$, both the flashing and the wall-to-liquid friction play an important role. In particular, it is observed that the importance of the flashing increases with increasing inlet quality while the opposite is true for the friction. This is reasonable since the closer to the saturation the more flashing should be expected.
- The dependencies are more complex in the SMD tests, mainly due to the wide pressure range of the tests. For $x < -0.04$, all three models are influential to approximately the same extent. Getting closer to saturation ($x = 0$), the flashing (P1QLF) becomes dominant while the other 2 parameters (SP1CL and SP3QLF) become negligible. This behavior is consistent with the Sozzi tests.
- These results are fully consistent to the ones found in [18].

The linear response of the code and therefore the applicability of the Pearson coefficients was verified performing a Sobolj sensitivity analysis for the SMD test n°1, using the same approach explained in [22]. The Sobolj analysis provided results fully consistent with the ones obtained using the Pearson and Spearman coefficients confirming the linearity of the code response. The Pearson analysis is therefore preferred due to the limited number of simulations needed.

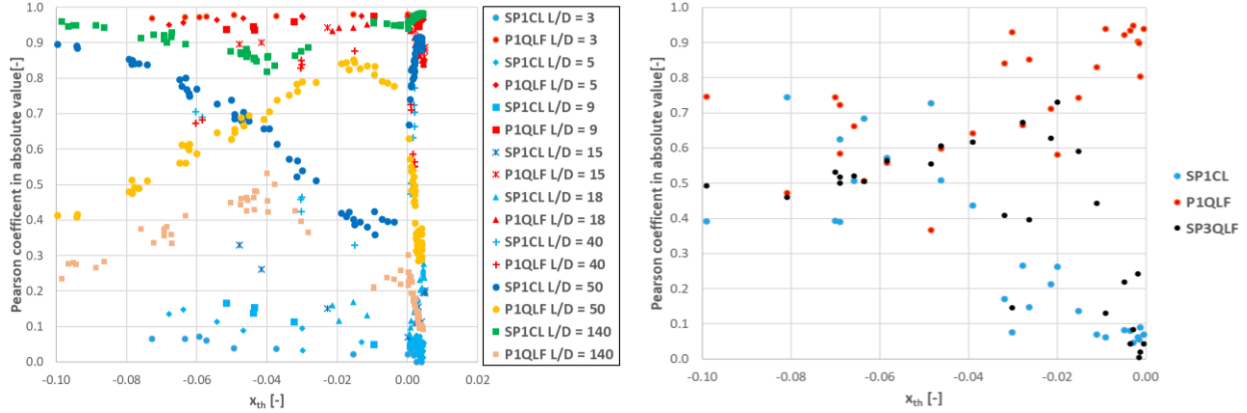


Figure 6: Pearson coefficients in absolute value: Sozzi (left) and SMD (right).

As suggested in SAPIUM, we verified that a variation of the input parameters resulted in a variation of the QoI (i.e. the critical mass flux). For each propagation of uncertainties, we computed the variation of the output QoI in terms of a percentage:

$$\text{Output Variation [\%]} = 100 \cdot \frac{G_{c,max} - G_{c,min}}{G_{c,nom}} \quad (1)$$

where $G_{c,max}$ is the maximum value of the critical mass flux calculated during the propagation, $G_{c,min}$ is the minimum and $G_{c,nom}$ is the nominal value. The results are shown in Figure 7. In SMD, the higher variations are observed for $x > -0.04$ when the flashing model is the most influential one. In Sozzi, the output variation is in the range 10-30% for all tests with $L/D \leq 40$ (again the flashing is dominant or very influential). For $L/D \geq 50$, the output variation is significant in subcooled conditions, while it decreases rapidly getting closer to saturation since the flashing model stops to play a role. At saturation, the output variation is around 5-15 %.

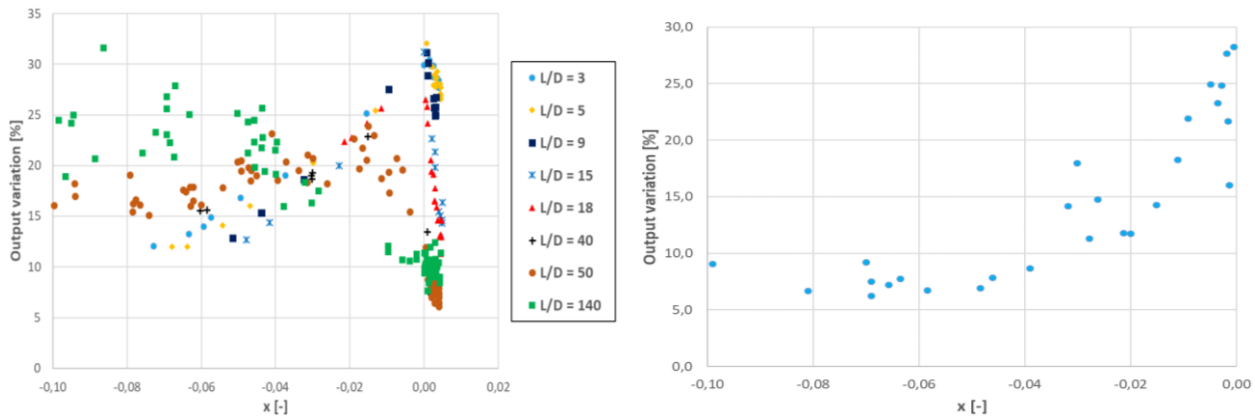


Figure 7: Output variation as a function of the inlet quality: Sozzi (left) and SMD (right).

In conclusion, two main influential parameters are identified from the *a priori* sensitivity analysis: the Wall-to-liquid friction (SP1CL) and the Flashing (P1QLF). The flashing is dominant for relatively short nozzles ($L/D \leq 18$). For longer nozzles, the wall-to-liquid friction becomes more and more influential. The Delay to flashing (SP3QLF) is influential only for some of the SMD tests with $x < -0.02$, whereas it is always negligible in the Sozzi tests. In these SMD tests, the output variation of the QoI is limited. Thus, these tests do not seem pertinent and usable for the IUQ of the delay to flashing model.

6. CONCLUSIONS

In this paper, the validation of the system code CATHARE3 against a database of critical flow experiments and the associated sensitivity analyses are presented. This work is part of CEA's contribution to the OECD/NEA ATRIUM project, whose goal is to perform Inverse Uncertainty Quantification benchmark exercises. Three-hundred and twenty-four choked flow experiments, coming from three different facilities: Sozzi-Sutherland, Super Moby-Dick and Marviken-CFT, are computed with CATHARE3. The simulations are in very good agreement with the experimental data (maximum discrepancy of 23.3% on the critical flowrate). The largest biases are associated to the short nozzle tests ($L/D \leq 9$), due to the difficulties in quantifying the local pressure loss coefficients at the nozzle. Several sensitivities studies are performed to better understand the simulation results and prepare the IUQ process. The wall roughness is an important parameter impacting the critical flow in the tests with large L/D , while it becomes negligible in short nozzles. On the contrary, the local pressure loss coefficient at the throat is mostly influential in short nozzle tests, as expected, due to the importance of non-equilibrium phenomena. The experimental uncertainties on the inlet temperature are also found to be extremely impactful. *A priori* sensitivity analysis is finally carried out to identify the most influencing CATHARE closure laws. The flashing (P1QLF) is dominant for relatively short nozzles ($L/D \leq 18$). For longer nozzles, the Wall-to-liquid friction (SP1CL) becomes more and more influential, until it becomes the dominant parameter for the tests with $L/D = 140$. For $25 \leq L/D \leq 50$, both the flashing and the wall-to-liquid friction play an important role. The goal of the next part of the project will therefore be to quantify the uncertainties of SP1CL and P1QLF using the CIRCE methodology developed at CEA.

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