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The Code for Analysis of Thermalhydraulics during an Accident of Reactor and safety Evaluation (CATHARE) is designed to perform Pressurized Water Reactor (PWR) safety analysis. This system-scale code includes a one-dimension, two-phase, six-equation model (mass, momentum and energy balance for each phase) used to represent pipe geometries. During a Loss Of Coolant Accident (LOCA), it is essential to accurately estimate the critical mass flow at the break, requiring to model the flashing process. The current CATHARE model used to predict the vaporization rate in case of flashing is based on a semi-empirical correlation. The present study main goal is to test a mechanistic model developed by Berne. In this model, the bubbles grow first at constant number, until a critical breakup diameter is reached. This critical diameter is controlled by turbulence, assuming that breakup is due to turbulent eddies having the same size as bubbles. Then Berne characterizes the interfacial heat exchange with both a convective and a conductive mode. According to Berne, the liquid-to inter-face heat transfer seems to be mostly convective. The present study revisits this previous works by taking into account both the convective and conductive contribution of liquid to interface heat transfer, also by assessing the flashing correlation on a larger experimental data base, including nozzles such as Super Moby Dick and Bethsy. And, finally, by carrying out a sensitivity study on the most influent parameters of the Berne's model. As a conclusion of this study, the Berne convective models gives similar results for the critical mass flow in comparison with the current semi-empirical model. Moreover assumptions made to build the model are questionable at high void fraction. The sensitivity analysis showed the importance of the bubble diameter for the estimation of the critical mass flow. To go further, it may be interesting to have a better estimation of the interfacial area for improving the model. This could be done by comparing with other models of the literature, for both the initial bubble number density and the turbulent kinetic energy dissipation rate. Furthermore, a sensitivity study on the interfacial friction coefficient should be carried out for saturated inlet tests.

KEYWORDS: flashing, choked flow, CATHARE

Introduction

Safety analysis of Pressurized Water Reactor (PWR) is fundamental to achieve the design of these reactors. This analysis is carried out by studying accidents that may occur, as a Loss Of Coolant Accident (LOCA) which is initiated by a break in the primary circuit. As the flow through the break determines the depressurization rate and the coolant inventory evolution during the transient, it is a key aspect of the LOCA modeling to predict accurately the leak flowrate.

If the pressure difference between primary circuit pressure and the external one is high enough, the leak flowrate can be limited by the sonic velocity at the break: the flow is then said to be critical, or choked. The calculation of critical flowrate through the break requires the modeling of the flashing, which is the most influential phenomena in case of subcooled conditions upstream the break.

The Code for Analysis of Thermalhydraulics during an Accident of Reactor and safety Evaluation (CATHARE) is designed to perform PWR safety analysis. This system-scale code includes a one-dimension, two-phase, six-equation model (mass, momentum and energy balance for each phase) used to represent pipe geometries. The current CATHARE model used to predict the vaporization rate is based on a semi-empirical correlation, developed by A. Sekri [1], and gives satisfying results, except for some tests under low-subcooling inlet condition.

P. Berne developed a mechanistic model in his Ph.D. thesis [2], considering a bubble flow and describing the bubble diameter growth, governed by turbulence, and proposing two heat transfer modes: a conductive and a convective one. Berne study concluded that the heat transfer seems to be mostly convective. Maciaszek et al. [3] tested the convective model in CATHARE on few nozzles, the aim of the present study is to continue this work by taking into account both the conductive and convective models established by Berne and test the ability to this flashing model implemented in CATHARE 3 to predict accurately the critical mass flow rate.

Current CATHARE model

The current CATHARE model is mostly empirical, based on the Sekri analysis [1] in which the liquid to interface heat transfer is seek as a function of the local values describing the flow only, without taking any dependence on differential terms:

$$q_{le} = A(P, H_L, H_V, \alpha, V_L, V_V, D_H)(T_L - T_{sat}) \tag{1}$$

Using critical flow experiments in nozzle, the following correlation was established:

$$q_{le} = -f(\alpha) \times \frac{\rho_L^2 \lambda_L}{\mu_L^2 Cp_L} \times V_L^2 (T_L - T_{sat}) \tag{2}$$

$$f(\alpha) = 1.2\ 10^8 e^{4.5\alpha} \tag{3}$$

More precisely, the current version of CATHARE uses a modified expression of f, depending also on the pressure. Moreover, a nucleation threshold is added in the Sekri model. In order to take into account the liquid overheat needed to allow the vaporization onset, Sekri suggested, empirically and from a similar analysis as in Lackme work [4], that the vaporization really start at:

$$P_{start} = 0.98\ P_{sat} \tag{4}$$

Berne models for liquid to interface heat exchange

Bubbles diameter evolution.

Berne [2] studied the vaporization rate in nozzles, by describing the liquid to interface heat transfer and considering a bubble flow. Berne described the bubbles growth, supposed to be spherical with the same diameter. The bubbles are supposed to grow up in two successive steps: first at constant bubble number density n_{b0}, until they reached a breakup diameter $\delta_{\max}$, controlled by turbulence. Once the break-up of bubbles occurs, their diameter is assumed to be equal to the maximum one.

During the first phase, at constant bubble number density, the bubble diameter δ can be expressed as a function of the void fraction. Regarding the bubble number density, Shin and Jones [5] reviewed the literature of flashing flow using a bubble growth model, and found that the usual bubble number density varies from 10^8 to 10^{13} m^{-3} and a range of 10^9-10^{11} m^{-3} is reported in Liao and Lucas [6]. Berne suggest to take $n_{b0} = 10^{10}\mathrm{m}^{-3}$.

$$\delta = \left(\frac{6\ \alpha}{n_{b0}\pi}\right)^{1/3} \tag{5}$$

As the Reynolds number is quite high for the flow studied, the breakup phenomenon is supposed to be governed by turbulence. With two slightly different approaches, Kolmogorov [7] and Hinze [8] assumed that a bubble breakup in a turbulent flow is due to the interaction between bubbles and eddies. Especially, supposing that only the eddies having the same size as the bubble are able to cause the breakup, the breakup diameter is obtained using a condition on the Weber number defined in equation (6).

$$We = \frac{\rho_L \overline{v^2(\delta)} \delta}{2\sigma} \tag{6}$$

Where $\overline{v^2(\delta)}$ is the average velocity difference between two points of the flow distant from δ. In order to obtain as estimation of $\overline{v^2(\delta)}$, Berne [2] assumed:

- That the turbulence is locally homogeneous and isotropic;
- The eddies responsible of bubble breakup belongs to the inertia zone, which is equivalent to assume that the breakup diameter is limited by the size of the largest eddies and the Kolmogorov scale η;
- And that the turbulence on the liquid phase is not affected by the vapor phase.

It has to be noticed that the more the void fraction is important the less the last assumption is relevant. Still, in order simply estimate the Weber number, these assumptions are supposed verified and the average velocity described hereunder can be expressed as in equation (7), according to Batchelor [9].

$$\overline{v^2(\delta)} = 2 \times (\delta\epsilon)^{2/3} \tag{7}$$

Where ϵ is the turbulent kinetic energy dissipation rate. Then the maximum diameter $\delta_{\max}$ is given by:

$$\delta_{max} = C^{3/5}\left(\frac{\sigma}{\rho_L}\right)^{3/5}\epsilon^{-2/5} \tag{8}$$

Where C is the critical Weber number, taken equal to 1.216. This value obtained by Berne [2] comparing the natural frequency of a spherical globule, given by Lamb [10], with the frequency associated to a δ large eddy.

Finally ϵ needs to be evaluated, in order to do that Berne used an estimation given by Karabelas [11] or Taitel et al. [12] and established the expression given by equation (9) for ϵ, valid for fully developed steady-state monophasic flows at high Reynolds numbers. Aiming for a simple expression, to have access to an evaluation of ϵ the estimation is applied even for non-fully developed bubbly flows.

$$\epsilon = \left|\left(\frac{dp}{dz}\right)_f\right|\frac{V_L}{\rho_L} = \frac{2V_L^3C_f}{D_H} \tag{9}$$

Finally the breakup diameter can be expressed as following: according to Sevik and Park [13], Berne suggests to approximate the bubble diameter after breakup to the actual breakup diameter. Indeed the divided bubbles takes diameter from $\delta_{\max}$ to approximately $0.8\ \delta_{\max}$; so the breakup diameter is a good order of magnitude of the bubbles diameter after the breakup.

$$\delta_{max} = C^{3/5}\left(\frac{\sigma}{\rho_L}\right)^{3/5}\left(\frac{2V_L^3C_f}{D_h}\right)^{-2/5} \tag{10}$$

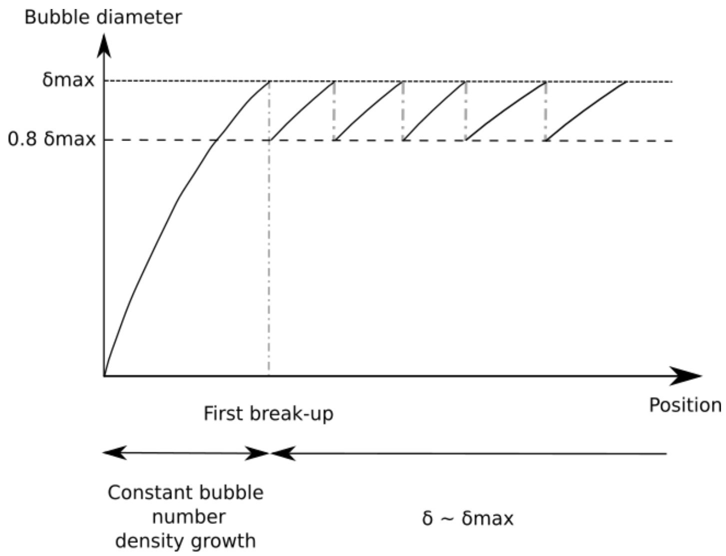

Figure 1. Schematic bubble diameter evolution according the Berne model, adapted from [2].

Fig. 1 shows the schematic bubble diameter evolution according to the Berne model: the first step at constant bubble number density, until the breakup occurs and then the diameter is assumed to be equal to the break up diameter.

Convective exchange law

Berne uses an exchange law established by Ruckenstein [14], for an independent vapor bubble in motion in the boiling liquid from which the bubble is generated. According to Ruckenstein, the heat transfer mechanism mainly depends on the bubble Reynolds number $Re_b := \frac{\rho_L \delta |V_V - V_L|}{\mu}$. Indeed for a bubble Reynolds number up to 1600-1800, the bubbles can be considered spherical and their motion in the liquid almost rectilinear, whereas for higher values of Re_b the bubbles start deforming themselves and their trajectory evolve to a spiral ascension. Ruckenstein gives a relation between the Nusselt number Nu and the Péclet number Pe, defined hereunder, with h_{li} heat transfer coefficient:

$$Nu = \frac{2}{\sqrt{\pi}} Pe^{1/2} \tag{11}$$

$$Nu := \frac{h_{li}\,\delta}{\lambda_L} \tag{12}$$

$$Pe := \frac{\delta |V_V - V_L|}{a_L} \tag{13}$$

Finally, the liquid to interface heat transfer q_{li} can be expressed as

$$q_{li,CV} = a_i h_{li,CV}(T_L - T_{sat}) = a_i \frac{2}{\sqrt{\pi}} \frac{\lambda_L}{\delta}\, Pe^{1/2}\,(T_L - T_{sat}) \tag{14}$$

Knowing the bubble diameter and the void fraction, the interfacial area density a_i can be estimated, then the liquid to interface heat transfer reads:

$$q_{li,CV} = \frac{12}{\sqrt{\pi}} \frac{\alpha}{\delta^{3/2}} \sqrt{\rho_L \lambda_L C p_L (V_V - V_L)} \tag{15}$$

Conductive exchange law

The conductive heat transfer is supposed to take place in the liquid phase, around the bubble. Berne used previous results established by Plesset and Zwick [15] on the growth of a vapor bubble in superheated liquid, in which the nucleation phase is not studied: the spherical bubble is assumed already existing, with an initial radius. The bubble diameter evolution takes place in two steps, first a brief growth is observed, controlled by the liquid inertia, and during which the diameter evolution is quite small. Then the evolution is mainly governed by the conduction, during which the growth is quite more important. In the second phase, the diameter evolution is given by the following equation.

$$\frac{d\delta}{dt} = \frac{1}{2}\sqrt{\frac{3}{\pi}}\;\lambda_L \frac{T_0 - T}{\mathcal{L}\,\rho_V\, a_L^{1/2}}\sqrt{t} \tag{16}$$

Where T_0 is the liquid temperature at an infinite distance from the bubble and T is the saturation temperature for the pressure at an infinite distance. For the present study, T_0 is assumed to be equal to T_L and T to T_{sat}. After integration of expression mentioned above, the time derivative of δ can be expressed as following, in which the time dependence does not appear.

$$\frac{d\delta}{dt} = \frac{6}{\pi}\frac{4}{\delta}\left(\frac{\rho_L}{\rho_V}\right)^2 a_L \left[\frac{Cp_L(T_L - T_{sat})}{\mathcal{L}}\right]^2 \tag{17}$$

Finally, Berne uses this expression in the vapor mass balance and find the following form for the conduction liquid to interface heat transfer.

$$q_{li,CD} = \frac{18}{\pi}\frac{4\alpha}{\delta^2}\frac{\rho_L^2 a_L}{\rho_V}\left(Cp_L(T_L - T_{sat})\right)^2 \frac{1}{\mathcal{L}} \tag{18}$$

Where Ja is the Jacob number, defined as:

$$Ja := \frac{\rho_L Cp_L (T_L - T_{sat})}{\rho_V \mathcal{L}} \tag{19}$$

The conductive heat transfer mode can be characterized by a specific Nusselt number

$$Nu_{CD} = \frac{12}{\pi} Ja \tag{20}$$

Interfacial friction model consistent with the bubble flow

As the flow is supposed to be a bubbly one, the interfacial friction should be adapted to this configuration. Ishii and Mishima [16] established a correlation for the drag coefficient of a bubble in a viscous regime.

$$C_D = \frac{24\ (1 + 0.1\ Re_b^{0.75})}{Re_b} \tag{21}$$

As the present study mostly aim at assessing the Berne models implemented in CATHARE, this model is not further detailed here as the liquid to interface heat transfer is preponderant for subcooled inlet flows, and the interfacial friction process is more important for saturated inlet flows.

Critical flow experiments

The Super Moby Dick (SMD) and Bethsy nozzles are critical flow experiments, carried out by CEA-Grenoble [17-18], covering conditions occurring in SBLOCA and IBLOCA. These separate effect tests are used in the literature for flashing model assessment, as in the Berne Ph. D thesis for the SMD tests or in recent work on Delayed Equilibrium Model (DEM) for flashing flow [19].

Super Moby Dick: SMD nozzle consists of an inlet elliptic convergent section leading to a 372 mm long straight pipe with a 20.13 mm inner diameter, followed by a 7° divergent ending in a condenser. The tests analyzed by Berne were carried out at different inlet pressure, 20, 40, 80 and 120 bar, and several inlet subcooling, from about 20°C to 0°C.The experimental critical mass flow rate is measured to an accuracy of 2% to 4%, whereas the inlet pressure is known with ± 0.2 bar and the inlet temperature with ±0.2°C.

Bethsy 2 and 6 inch nozzles: These two nozzles were used in CEA-Grenoble [18-19] in order to carry out critical flow experiments, and were designed to simulate respectively a 2-inch and a 6-inch pipe break in a reactor. BETHSY experimental loop was designed at f=1/100 scale in volume with respect to a reference reactor and the break diameter at $f^{1/2}$ = 1/10 scale with respect to a reference break comprise between 1-10 inches. Thus, the BETHSY 2-inch and the 6-inch nozzles inner diameter are respectively 5.24 mm and 15.73 mm. The nozzles present a sharp convergent inlet followed by a 150 mm long, respectively 80 mm, constant section pipe. A quite large range of inlet pressure is covered by the experiment: 100, 70 and 30 bar tests series are considered here, for several subcooled inlet conditions varying from 30°C to 0°C. The experimental critical mass flow rate is measured to an accuracy of 1%, whereas the inlet pressure is known with ± 0.2 bar and the inlet temperature with ±0.2°C.

Results

In Berne Ph.D. thesis, the two heat transfer models were assessed, separately, on the SMD test series. For these tests, the nozzle is instrumented in order to have access to the void fraction and the pressure evolution from about 300 mm before the 7° divergent to the end of the straight pipe. Using the measurements, Berne estimated the relative velocity, $\Delta V := V_V - V_L$, between the two phases, the super-heat temperature and then the bubble diameter. According to Berne analysis, the convective model seems to be preponderant in the SMD experiments.

Still, it has to be noticed that the convective model cannot be significant at low void fraction. Indeed at low void fraction the bubbles are carried by the liquid continuous phase, then the Péclet number is quite low. According to the Nu_{CV} and Nu_{CD} expressions, the conductive transfer mode is dominant at low Péclet number, see Figure 3.

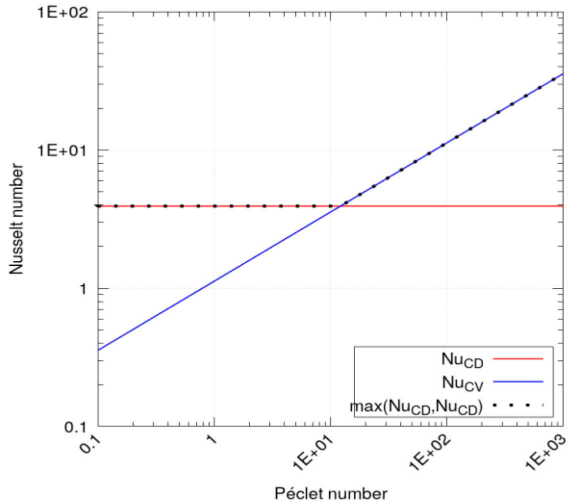

Figure 3. Nusselt numbers against the Péclet number value, for $Ja = 1$.

So the two models are combined in our study by taking the most significant heat transfer mode

$$Nu = \max(Nu_{CD}\,; Nu_{CV}\,) \tag{22}$$

which is equivalent to

$$q_{li} = \max(q_{li,CD}\,; q_{li,CV}\,) \tag{23}$$

Moreover, as in the standard CATHARE model, the onset of flashing is set to $P_{flashing} = 0.98\, P_{sat}$.

Fig. 4 and Fig. 6 show the critical mass flow rate calculated by CATHARE with both the CATHARE standard model and the new model based on Berne work, for the 2-inch Bethsy nozzle. From these figures, it can be seen that the two models seems to overestimate the critical flowrate for high-subcooled inlet conditions. The new model gives similar or slightly better results for this group of tests than the CATHARE standard model. Moreover, for the lowest subcooled inlet conditions, the two models present a quite significant discrepancy, indeed the standard CATHARE model systematically underestimate the critical mass flow rate whereas the combined Berne models predict the critical mass flow rate with a better accuracy.

As it can be seen from figures 4 and 5, the experimental critical mass flow rate evolution against the inlet subcooling shows a significant change of slope around -10°C, whereas this tendency is not observed for SMD test series presented in figure 6. Indeed this slope variation is due to two-dimensional effects introduced by the sharp convergent at the Bethsy nozzles inlet, which is smoother for the SMD nozzles, causing a flow separation. Previous work [20] has established that the value of the slope strongly depends on the reaching, or not, of the saturation pressure P_{sat} in the convergent. As highlighted by figures 4 and 5, the presented models (CATHARE standard model or new model) do not take into account these two-dimensional effects and thus cannot capture this behavior.

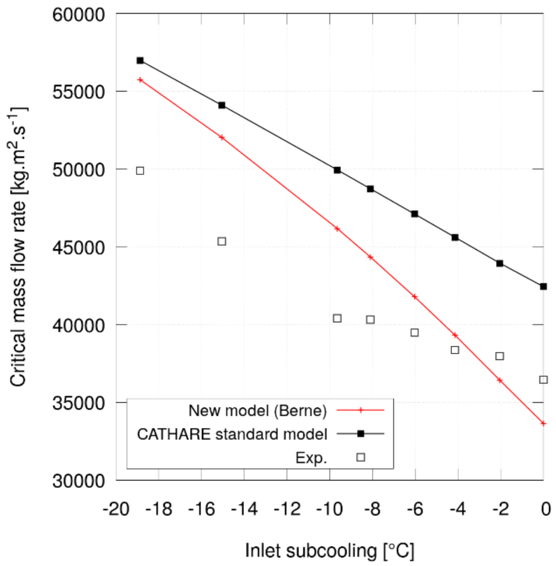

Figure 4. **Bethsy 2-inch nozzle – 70 bar test series - Critical mass flow rate against the inlet subcooling**

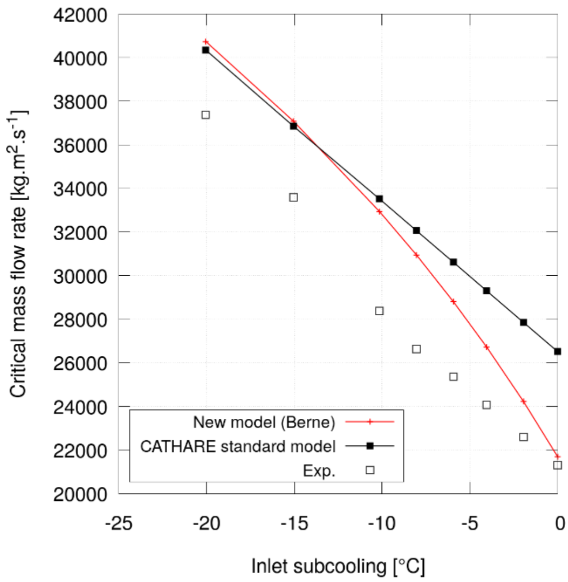

Figure 5. **Bethsy 2-inch nozzle – 30 bar test series - Critical mass flow rate against the inlet subcooling**

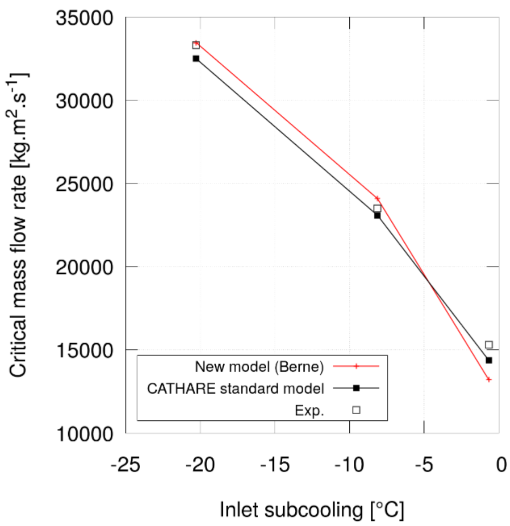

Figure 6. Super Moby Dick – 20 bar test series – Critical mass flow rate against the inlet subcooling

Regarding the SMD test series, the two models gives quite similar results, as it is shown in figure 6, and seem to present closely the same trends: the critical mass flow rate is accurately or slightly overestimated at high subcooled inlet conditions whereas at low subcooled inlet conditions both the models tend to underpredict the critical mass flow rate.

Moreover, it must be noticed that the convective law suggested by Ruckenstein [14] supposes that the bubbles are independent and that the bubble Reynolds number Re_b is lower than 1600 – 1800, in order to assume the bubbles spherical and having a vertical ascension. This assumption is always verified for the SMD tests. For Bethsy nozzles tests, the present validation shows maximum Re_b between 3000 and 4000, which is acceptable, except for the Bethsy 2-inch nozzle test series at 70 bar, for which the predicted Re_b reaches values around 10^5 for few tests with different inlet subcooling conditions.

Sensitivity to bubble diameter

The proposed models, based on the work for flashing carried out by Berne, mainly relies on the bubble evolution prediction, especially on the initial bubble diameter and the breakup one, which is governed by turbulence. The initial bubble diameter is determined both by the initial void fraction and the bubbles number density, which usually, as exposed before, can take values from 10^8 to $10^{13}\ \mathrm{m}^{-3}$. Regarding the initial void fraction, usually, as reported by Shin and Jones [4], this value is taken negligible compared to 1% in the literature of flashing flow using a bubble growth model. In CATHARE, the initial void fraction is taken equal to 10^{-5}.

For instance Rivard and Travis [21] established a model for non equilibrium vapor production for critical flow, describing the bubble evolution, where the initial bubble number density suggested is equal to $10^9\ \mathrm{m}^{-3}$ and the initial void fraction to 2.10^{-4}, for blowdown experiments in nozzles. Nozzles diameters vary from 1.8 to 51 cm and inlet sub-cooling from 30°C to 0°C. Figure 6 compares the results obtained with the combined Berne models with different parameters for the initial diameter evaluation: first $n_{b0} = 10^9\ \mathrm{m}^{-3}$ and the initial void fraction is the CATHARE value, 10^{-5}, and then with the same bubble number density but the initial void fraction is set to 2.10^{-4}, according to Shin and Jones study.
Finally another value for the initial bubble number density is tested, $10^{11}\ \mathrm{m}^{-3}$, with 10^{-5} as the initial void fraction.

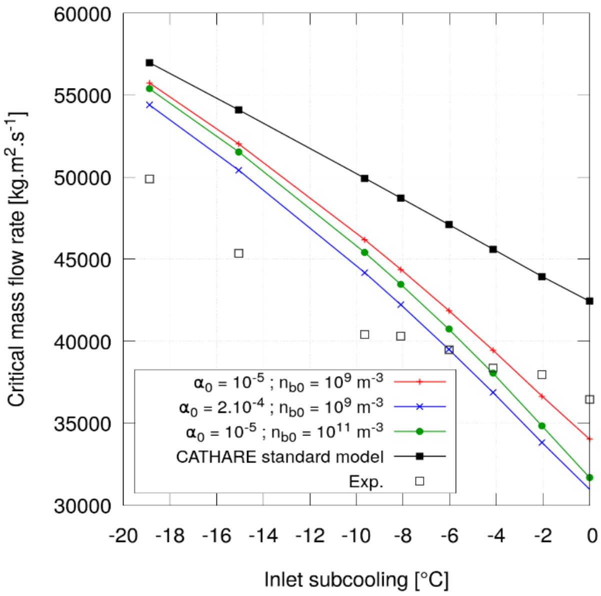

Figure 7. Bethsy 2-inch nozzle – 70 bar test series - Critical mass flow rate against the inlet subcooling with different initial value for void fraction and bubbles number density

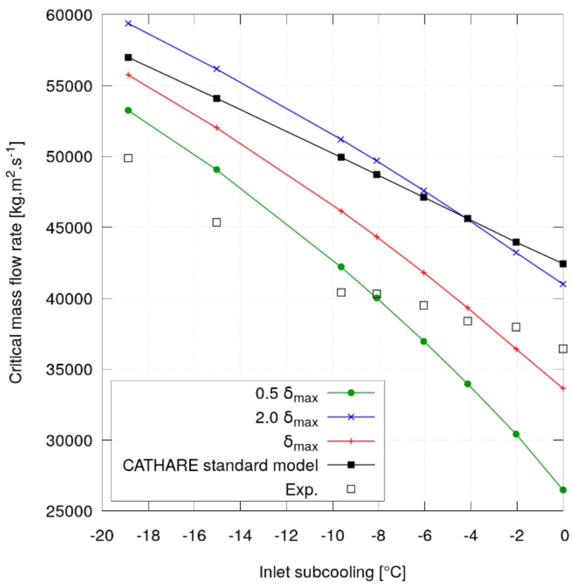

Figure 8. Bethsy 2-inch nozzle – 70 bar test series - Critical mass flow rate against the inlet subcooling Sensitivity to breakup diameter

Figure 7 shows that the critical mass flow rate obtained with the Berne models strongly depends on the initial bubble diameter. Regarding the initial bubbles number density, the influence of this parameter on the choked flow prediction depends on the distance, from the nozzle throat, at which the breakup of bubbles occurs. Indeed, the more the first step of bubble growth ends close to the throat the more it is determining for flashing. This is highlighted by figure 6, for the highest sub-cooled test, which shows any particular sensitivity to n_{b0}: for this test the breakup diameter is quite small and the first growing phase is rapidly ended.

Also, figure 7 highlights the influence of the initial value of void fraction for the critical mass flow rate calculation. This parameter has a more global effect on the results, indeed the interfacial area concentration depends on the void fraction and the bubble diameter as following.

$$a_i = \frac{6\alpha}{\delta} \tag{24}$$

The bubble diameter only depends on the bubbles number density during the first step of growth, whereas the interfacial area is still influenced by the value of the initial void fraction even if the breakup happens really early in the nozzle.

Figure 8 shows the significant dependence of the critical mass flow rate calculation on the bubble breakup diameter. As the Berne model only estimates an order of magnitude of the breakup diameter, a sensitivity study on this parameter is carried out. Calculations are performed by taking arbitrary a 2 times greater, and respectively a 2 times smaller, breakup diameter. For the Bethsy 2-inch, 70 bar test series, it can be seen from Fig. 8 that a higher breakup diameter systematically leads to an overestimation of the critical mass flow rate, whereas a smaller one leads to an under prediction of the critical flowrate.

Conclusion

The main goal of the current study was to assess a flashing mechanistic model in CATHARE 3 to predict accurately the critical mass flow on choked flow experiments in nozzles. This model is derived from the Berne Ph.D. thesis, based on a bubble growth model.

The analysis carried out has identified the importance of bubble diameter evaluation for the flashing estimation, in particular the parameters determining the initial bubble diameter and the breakup diameter. Moreover the model suggested by Berne for bubble diameter estimation only gives an order of magnitude, especially for the breakup diameter. So improvements on the bubble diameter estimation are needed in order to improve the simulations. For instance the initial bubble number density should be better described, as it is a quite uncertain parameter of the model, indeed it should be a function at least of the nozzle diameter, the pressure and the liquid superheat in order to take into account the nucleation process. Furthermore the turbulent kinetic energy dissipation rate estimation model suggested by Berne should be compared to other models from the literature.

Finally a smoother way to combine the conductive and the convective heat transfer modes is required. A solution could be, as suggested by Wolfert et al. [22], to take the sum of the two contributions. Or, according to Saha et al. [23] study, to use the following expression for the global Nusselt number.

$$Nu = \sqrt{Nu_{CD}^2 + Nu_{CV}^2} \tag{25}$$

Regarding the Ruckenstein exchange law [14], it has been highlighted in the present study that some assumptions on which it relies are not verified for some validation tests, and could be not applied in our case of interest for a Nuclear Power Plant. Hence, further work must be carried to investigate the effects of bubble deformation on the heat transfer, by studying Clift et al. [24] work for instance, but also to take into account the influence of other bubbles, as detailed on the Ruckenstein study [14].

Moreover, because of the presented tests only cover a part of the conditions occurring in LOCA (mainly IBLOCA and SBLOCA), the assessment of these models will be extended to other experiments already included in the CATHARE test matrix [3,20], namely MARVIKEN tests, representative of a postulated full-scale reactor LOCA with larger break diameters (up to 500 mm), and to MOBY DICK tests performed at low pressure (2 – 7 bar) [25]. In addition to this, a sensitivity study on the interfacial friction model should be carried out for saturated inlet tests.

Nomenclature

a_i	Interfacial area concentration [m^{-3}]
a_l	Thermal diffusivity [m^2.s^{-1}]
C	Critical value of Weber number [-]
C_D	Drag coefficient for bubble flow [-]
$q_{li,CD}$	Liquid to interface heat transfer, convective heat transfer mode [W.m^{-3}]
$q_{li,CV}$	Liquid to interface heat transfer, convective heat transfer mode [W.m^{-3}]
Re_b	Reynolds number associated to the bubbles [-]
T_L	Liquid temperature [K]

C_f	Wall friction coefficient [-]
Cp_L	Specific heat capacity [J.kg^{-1}.K^{-1}]
D_H	Hydraulic diameter [m]
H_L	Liquid enthalpy [J.kg^{-1}]
H_V	Vapor enthalpy [J.kg^{-1}]
Ja	Jacob number [-]
$\mathcal{L}$	Latent heat [J.kg^{-1}]
n_{b0}	Bubble number density [m^{-3}]
Nu	Nusselt number [-]
Nu_{CD}	Nusselt number associated to the convective heat transfer [-]
Nu_{CV}	Nusselt number associated to the conductive heat transfer [-]
P	Pressure [Pa]
P_{sat}	Saturation pressure [Pa]
q_{li}	Liquid to interface heat transfer, conductive heat transfer mode [W.m^{-3}]
T_{sat}	Saturation temperature [K]
V_L	Liquid velocity [m.s^{-1}]
V_V	Vapor velocity [m.s^{-1}]
We	Weber number [-]
α	Void fraction [-]
δ	Bubble diameter [m]
δ_{max}	Breakup bubble diameter [m]
ϵ	Turbulent kinetic energy dissipation rate [m^2.s^{-1}]
λ_L	Liquid conductivity [W.m^{-1}.K^{-1}]
μ_L	Liquid viscosity [Pa.s]
ρ_L	Liquid density [kg.m^{-3}]
σ	Surface tension [N.m^{-1}]

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