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Delayed Equilibrium Model and Validation Experiments for Two-Phase Choked Flows Relevant to LOCA

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Abstract

This paper deals with the 1-D Delayed Equilibrium Model (DEM) for choked or critical flow rate in steady state or quasi-steady state conditions and the selection of the relevant experimental data for assessing such models. In particular, the focus is made on thermodynamic non-equilibrium conditions, which prevail in the flashing process near the critical section. In this regard, relaxation models such as the DEM developed and tested in previous studies at UCL was revisited and improved in view of their implementation in the next CATHARE code generation during the EU NURISP (NUclear Reactor Integrated Software Platform) project. A methodology to implement the DEM into is developed. Some new results of the DEM are compared against experimental data such as Super Moby-Dick experiments done in CEA and the well-known Marviken experiments performed at quasi-real scale geometry of Nuclear Plant.

Introduction

Critical or choked Flows

A flow is said critical or choked when the mass flow rate becomes independent of the downstream flow conditions [16]. Typically when a flow is choked in a pipe connecting two vessels at different pressures, any further decrease of the pressure in the downstream vessel does not result in a change of the mass flow rate. This limit, which corresponds to the maximum mass flow rate between both vessels, exists because the acoustic signal related to the pressure decreases can no longer propagate upstream of the critical section. This condition occurs when the fluid velocity reaches the propagation velocity or the speed of sound. In the case of a quasi-linear partial differential equation system, the path lines for signal propagation are determined by an analysis of the characteristics in the sense of the gas dynamics. Considering the one-dimensional set of equations:

$$A(u)\frac{\partial u}{\partial t}+B(u)\frac{\partial u}{\partial x}+C(u)=0 \tag{1}$$

the propagation directions and velocities are determined from the roots λ of the characteristic equation:

$$\det\left(A\lambda-B\right)=0 \tag{2}$$

The real part of any root provides the propagation velocity along the associated path in the space-time domain. The imaginary part of any complex root gives the growth or decay rate of the signal along this path. Consequently, as soon as all roots are equal to - or greater than zero, no information propagates from the exterior, and the flow is choked. For the steady state case, which is considered in this paper, the analysis is reduced to the calculation of the determinant of the matrix $B(u)$. Even though such an analysis is straightforward for single-phase flows [16], the evaluation of the propagation velocity is more difficult in two-phase flows where the problem could become ill posed with the complexity of the flow model [19]. A more complete discussion can be found in the paper by Bouré et al. [7].

Relevance to Nuclear Safety

In the context of nuclear reactor safety, a pipe breach in the primary circuit is the initiator of a Loss of Coolant Accident (LOCA). The calculation of leak rates involving the discharge of water and steam mixtures plays an important role in the modeling of LOCA's for both GEN II and GEN III PWR and BWR reactors, and also for the Supercritical Water Reactor of GEN IV. Indeed, the flow through the breach determines the depressurisation rate of the system and the time to core uncovery which in turn are of major concern for when and how different mitigation auxiliary systems will be initiated and be efficient [1]. The pipe involved could be a main coolant pipe leading to a large break LOCA (LBLOCA), or a pipe connected to the main coolant loop (e.g. an ECC line, defect at a pressurizer valve) leading to an intermediate or small break (SBLOCA) [1]. The way in which the flow evolves as a function of time can be different for the case of a small broken pipe from that corresponding to a small hole in a large pipe even if the initial break flows are the same in both cases. In many licensing applications, the knowledge of the actual flow rate through a break of a given size is not required because what is of interest is the behaviour of the plant for a range of break sizes. Exceptions are the determination of the maximum flow for particular types of breaks (for instance from an instrument penetration in the pressure vessel), and the likely flow from a broken steam generator tube.
The modelling of critical flow in several of the thermal-hydraulics codes is based on semi-empirical models which in general require user defined adjustment factors to obtain a satisfactory agreement with data in individual situations [1]. In this regard, more universal models should be developed taking into account a wide range of operating and geometry conditions. In order to validate or assess such developments, appropriate benchmarks should be selected from previous tests, or new experiments should be conducted with a "model-grade approach".

1. General Assumption

The local instantaneous equations of the flow can be averaged statistically and spatially in a cross-section of the flow. The equations have then a more appropriate form with respect of their use in this section: only average values of the parameters in a channel cross-section are considered in this paper. Moreover, for the sake of simplicity, we introduce some approximations related to the following assumptions:

H0.- The flow is steady-state.
H1.- The channel is fixed, not vibrating, not elastic, not porous: it can have a non-uniform cross-section profile $A\left(z\right)$: its axis is rectilinear.

H2.- The surface and statistical correlation factors, which are the ratios between the averages of the products of the variables and the products of the averages of these variables, are all equal to unity. This implies that all transverse profiles are sufficiently flat in every cross-section, and do not vary too much statistically.
H3.- The pressure is uniform in any cross-section.

H4.- The transverse components of the velocity are neglected.
H5.- The averaging process over the cross-section is extended to the thermodynamic relationships, and in particular to the equation of state, taking H2 into account.
H6.- The effects of thermal dissipation and turbulence are not taken into account.
H7.- The wetted and heated perimeter are equal $P_w = P_H = P$.

2. Basic Modelling Technique: Homogeneous Equilibrium Model

In addition to assumptions H1 to H7 made in the previous section, several additional assumptions support the Homogeneous Equilibrium Model (HEM). The most specific assumptions are:

H8.- Mechanical equilibrium: $w_g \equiv w_f \triangleq w_m$

H9.- Thermodynamic equilibrium: $h_f = h_{fsat}(p), \rho_f = \rho_{fsat}(p)$ and $h_g = h_{gsat}(p), \rho_g = \rho_{gsat}(p)$

By choosing the pressure, the quality and the axial velocity as dependent variables, the continuity, momentum and energy equations can be written in the following matrix form:

$$\begin{bmatrix} v_{fg} & v^* & -\dfrac{v_m}{w_m} \\ 0 & 1 & \dfrac{w_m}{v_m} \\ h_{fg} & h^* & w_m \end{bmatrix} \begin{bmatrix} \dfrac{dx}{dz} \\ \dfrac{dp}{dz} \\ \dfrac{dw_m}{dz} \end{bmatrix} = \begin{bmatrix} \dfrac{v_m}{A}\dfrac{dA}{dz} \\ -\dfrac{P}{A}\tau_w - \dfrac{1}{v_m} g\cos\theta \\ -g\cos\theta + \dfrac{v_m}{w_m}\dfrac{P}{A}q_w \end{bmatrix} \tag{3}$$

where the difference between the specific volumes of the two phases is denoted:

$$v_{fg} \triangleq v_g - v_f \tag{4}$$

and the derivatives along the saturation line at constant quality are:

$$v^* \triangleq x\left(\frac{dv_g}{dp}\right)_{sat} + (1-x)\left(\frac{dv_f}{dp}\right)_{sat} \tag{5}$$

$$h^* \triangleq x\left(\frac{dh_g}{dp}\right)_{sat} + (1-x)\left(\frac{dh_f}{dp}\right)_{sat} \tag{6}$$

A necessary condition of choking (also called: critical condition) is the vanishing of the determinant of the set of equations [2]. This condition gives an expression of the speed of sound associated with the two-phase flow model. When such a condition is satisfied an additional condition, called compatibility condition is needed in order guarantee a physical solution at the critical section. According to Kramer's rule, this compatibility solution is the vanishing of secondary determinants obtained by replacing one

column vector of the system matrix by the vector of unknowns (rhs vector of Equ. 3). This second condition allows to locate the critical section.
The vanishing condition of the determinant of the set of equations gives the critical flow condition; for HEM it can be written in terms of a critical mass velocity:

$$G_c = \sqrt{\frac{h_{fg}}{v_{fg}(h^* - v_m) - h_{fg}v^*}} \tag{7}$$

It consists in a relationship between critical mass velocity G_c, the pressure and the quality at the critical section.

3. Advanced Modelling Technique: Delayed Equilibrium Model

In addition to assumptions H1 to H7 made in the previous section, some additional assumptions support the Delayed Equilibrium Model (DEM). The most specific assumptions is:

H8.- Mechanical equilibrium: $w_g \equiv w_f \triangleq w_m$

Let us consider the adiabatic expansion of a liquid in a tube (Fig. 1). Assume that the state of the liquid at the inlet of the pipe is (p_{in}, T_{in}), with $p_{in} > p_{sat}(T_{in})$. Due to the friction, the pressure decreases along the pipe, and reaches saturation at section “s”. Between section “s” and the onset of flashing, the liquid is metastable. The onset of flashing occurs at section “o”, where the pressure p_o is typically (Lackmé [12]):

$$p_o = (0.95...0.98)\, p_{sat}(T_{in}) \tag{8}$$

For turbulent liquid flows, the slope of the straight pressure line between the inlet and the onset of flashing, i.e. the pressure gradient is proportional to the square of the mass velocity. The distance between point ”o” and the tube outlet depends on the inlet subcooling and on the mass velocity. Here we will assume that the inlet subcooling and the mass velocity are such that the onset of flashing is located inside the pipe. Between point “o” and the pipe outlet, a two-phase bubbly flow develops rapidly, and the pressure gradient increases. If the pressure at the outlet is low enough, the flow is choked, and the outlet pressure is the critical pressure p_c. One can expect that the over-heating (or metastability) of the liquid phase does not vanish instantaneously at point “o”, but persists in the two-phase part of the flow, depending on one hand on the intensity of the heat transfer from the bulk of liquid to the interface and on the other hand on the rate of pressure decrease.
For large subcoolings, the onset of flashing is close to the pipe outlet, and the critical mass velocity can be approximately deduced from the single-phase pressure gradient, considering that it is constant over the whole pipe length (Lackmé [12]). For small subcoolings, accurate predictions of the critical mass velocity cannot be obtained without the complete modeling of the flow. For all cases, the accuracy on the prediction of the critical pressure is a relevant indicator of the validity of the two-phase critical flow modeling.

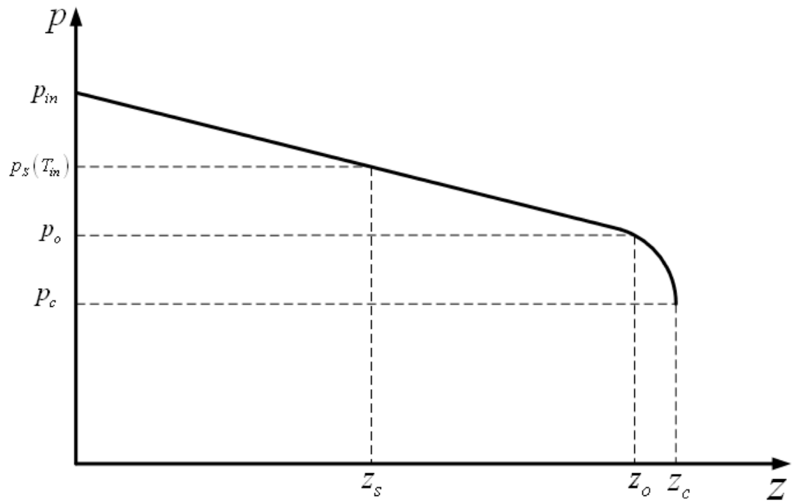


Fig. 1 A typical pressure profile along a pipe with a critical section at its end.

The Delayed Equilibrium Model (DEM, Féburie et al. [10]) assumes that, at a given cross-section, only a mass fraction y of the fluid is transformed into a saturated mixture, the remaining $(1-y)$ fraction being the bulk metastable liquid. This fraction undergoes a near-isentropic evolution since the heat of vaporization is exchanged between the saturated liquid and saturated vapor. The mass fraction of vapor in the saturated part of the mixture is denoted by x, and, consequently, the specific volume and the mixture enthalpy of this kind of "three-phase mixture" are given by:

$$v_m \triangleq (1-y)v_{fM} + xyv_{gS} + (1-x)yv_{fS} \tag{9}$$

$$h_m \triangleq (1-y)h_{fM} + xyh_{gS} + (1-x)yh_{fS} \tag{10}$$

The thermodynamic quality of the mixture in the classical sense is

$$X \triangleq xy \tag{11}$$

To distinguish the two liquid phases, we use subscript "M" for "metastable", and the subscript "S" for saturated. This new variable y requires a closure law. Féburie et al. [9] has proposed a correlation for flows through steam generator tube cracks and for subcooled inlet conditions:

$$\frac{dy}{dz} = 0.02\frac{P_w}{A}(1-y)\left[\frac{p_S(T_{fM}) - p}{p_{crit} - p_S(T_{fM})}\right]^{0.25} \tag{12}$$

This relaxation law expresses that the decrease of the mass fraction of superheated liquid $d(1-y)$ over an element of length dz is proportional to the remaining quantity of superheated liquid, and to the power 0.25 of the metastability expressed in a non dimensional way by means of the difference between the critical pressure of the fluid and the saturation pressure of the mixture. The factor P_w/A takes into account the relative importance of the wall surface, on which nucleation is supposed to be triggered, with respect to the volume of the fluid in the pipe. It has been introduced to

make Equ. 12 applicable for small as well as large diameter pipes. All results of the DEM presented in this paper have been obtained with a fixed coefficient 0.02 and a constant exponent 0.25.
This relaxation law has been generalized to take into account not only the nucleation at the wall (constant C_1) but also in the bulk of the flow (constant C_2):

$$\frac{dy}{dz} = \left(C_1 \frac{P_w}{A} + C_2\right)(1-y)\left[\frac{p_S(T_{fM}) - p}{p_{crit} - p_S(T_{fM})}\right]^{0.25} \tag{13}$$

By analogy to the equation system obtained for the HEM model and by choosing the quality X, the pressure p, the velocity w_m and the mass fraction y as dependent variables, the equations system in the framework of the DEM can be written:

$$\begin{bmatrix} v_{fg} & \begin{pmatrix} (1-y)v'_{fM} \\ +Xv'_g \\ +(y-X)v'_f \end{pmatrix} & -\frac{v_m}{w_m} & (v_{fS} - v_{fM}) \\ 0 & 1 & \frac{w_m}{v_m} & 0 \\ h_{fg} & \begin{pmatrix} (1-y)h'_{fM} \\ +Xh'_g \\ +(y-X)h'_f \end{pmatrix} & w_m & (h_{fS} - h_{fM}) \\ 0 & 0 & 0 & 1 \end{bmatrix} \begin{bmatrix} \frac{dX}{dz} \\ \frac{dp}{dz} \\ \frac{dw_m}{dz} \\ \frac{dy}{dz} \end{bmatrix} = \begin{bmatrix} \frac{v_m}{A}\frac{dA}{dz} \\ -\frac{P}{A}\tau_w - \frac{1}{v_m} g\cos\theta \\ -g\cos\theta + \frac{v_m}{w_m}\frac{P}{A} q_w \\ f(p, y, T_{fM}) \end{bmatrix} \tag{14}$$

with the following definitions of the derivatives of quantities related to the saturated mixture and to the metastable liquid:

$$v'_k = \left(\frac{\partial v_k}{\partial p}\right)_{sat} \quad \text{and} \quad h'_k = \left(\frac{\partial h_k}{\partial p}\right)_{sat}$$
$$v'_{fM} = \left(\frac{\partial v_{fM}}{\partial p}\right)_{S_{fM}} \quad \text{and} \quad h'_{fm} = \left(\frac{\partial h_{fM}}{\partial p}\right)_{S_{fM}} \tag{15}$$

where the properties of the metastable liquid and their derivatives depend only on the pressure if an isentropic evolution is assumed for this phase.
The critical condition of the model is the vanishing condition of the determinant of the system and after some manipulations it appears to be the classical definition of the speed of sound:

$$w_c = \sqrt{\left(\frac{\partial p}{\partial \rho_m}\right)_{S_m, y}} \tag{16}$$

Similar approach about the existence of a superheated liquid in a metastable state during the flashing process in two-phase flow has been developed in the Homogeneous Relaxation Model, called HRM (Z. Bilicki et al. [5]). This model has been successfully used in the WAHA code developed in the frame of the 5th European Program for simulating Water Hammer (Tiselj et al. [19] and [20], Barna et al. [4]).

4. Selection of Relevant Benchmarks

In the context of nuclear reactor safety, a pipe break in the primary circuit is the earliest mechanism for initiating a LOCA. The pipe involved could be a main coolant pipe, in which case it leads to a large break LOCA, or a pipe connected to the main coolant loop (e.g. an ECC line) which could lead to an intermediate or small break LOCA.

In a large break LOCA, the critical section can be located at the breach or at some restricted cross section in the surge line or the pump. No specific separate effects test relating to either of these last two possibilities has been found. The pressurizer surge line should in principle be covered by the database for critical flow in pipes.

As it has already been said, when a break occurs in a separating wall structure between a high and low pressure system the flow through the break will depend on conditions upstream of the break and on the break area and shape. Critical flow through a break is similar to critical flow through a nozzle, but the geometry of the break can encompass any shape, location and size from a small crack to a complete 200 percent guillotine break in a flow pipe.

The Nuclear Energy Agency has selected different experiments as reference tests for critical flows occurring during a LOCA. We have chosen three series of tests in the context of the NURISP project for assessing the CATHARE code. Two of the three series are presented below.

4.1 Super Moby-Dick test

The Super Moby-Dick experiments performed by the CEA-Grenoble (Rousseau [14,15]) consist in two-phase critical flashing flow experiments. Steady state critical flow conditions were measured in a long nozzle and in a short nozzle. The long nozzle has an elliptic convergent section at the entrance followed by a straight pipe of about 0.5 m long and of 20 mm inner diameter and ended by a 7° divergent section. The short nozzle has the same geometry without the divergent section.

Twelve reference tests have been chosen for each nozzle for four different pressures at the inlet: 20, 40, 80 and 120 bars. For each pressure, three different temperatures have been tested. The water is subcooled or quasi-saturated at the inlet of the nozzles. As far as the geometry up to the throat is slightly different between the long and the short nozzle, the critical mass flux is also slightly different.

The results of these tests are summarized in Fig. 2 where the critical flow rate is given as a function of the subcooling of the water $\Delta T_{sub} \triangleq T_0 - T_{sat}\left(P_0\right)$ at the inlet of the test section.

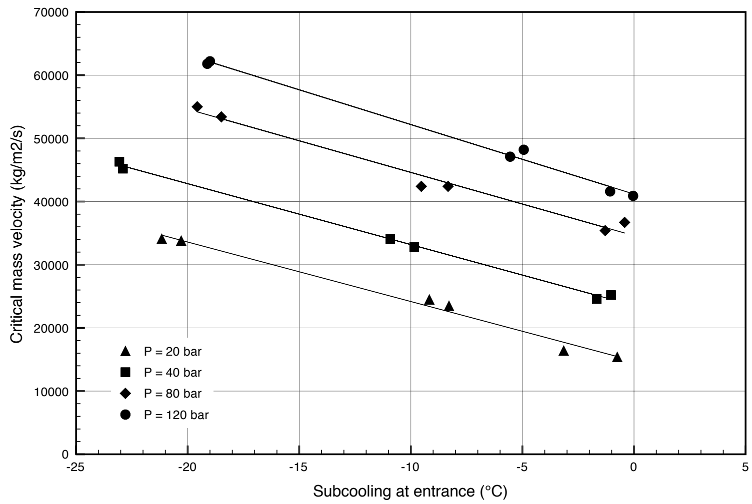


Fig. 2 Evolution of the critical flow rate, Super Moby-Dick tests.

4.2 Maviken test

The Marviken Full Scale CFT (Critical Flow Tests) [23] were conducted at the Marviken Power Station as an international project at the end of the seventies. Twenty seven (27) different experiments were performed by discharging subcooled water or steam-water mixtures from a quasi-full sized reactor vessel through a large diameter discharge pipe that supplied the flow to a test nozzle. Nine test nozzle geometries were all equipped with a rounded entrance followed by a nominally 200, 300 or 500 mm constant diameter cross-section. The nozzles ranged in lengths from 166 to 1809 mm, which correspond to so-called "short nozzles".
Most tests were conducted with a nominal initial steam dome pressure of 50 bars and with a subcooling temperature of water between 50°C and 1°C (with respect to the steam dome pressure).
The vessel, discharge pipe and nozzle were well instrumented to determine the test behavior and to provide a basis for evaluating the stagnation conditions and mass fluxes through the nozzle.
The Marviken CFT tests [23] were considered as reference tests for assessing critical two-phase flow models dedicated to LOCA of NPP.

5. Some Recent Result about the DEM model

Fig. 3 illustrates a comparison between the DEM and HEM models in terms of critical mass velocity with a wide range of data for long pipes [4], safety valves [4,6] and nozzles [14,17-18,22] produced by experiments with water and other organic fluids at pressures ranging from 1 bar to 120 bar with inlet subcoolings between 20°C and 0°C. For all presented results, the onset of flashing in the DEM approach is triggered by the law $p_o = 0{,}95 * p_{sat}\left(T_{in}\right)$. These results clearly illustrate the deficiency of the HEM model to predict the critical flow rate and they show the necessity to take into account the thermodynamic non-equilibrium. For the DEM, most of the runs provide results within 10% difference with respect to experimental data. Fig. 4 and Fig. 5 compare some results obtained with the DEM and HEM models for two different runs of the Super Moby-Dick experiments [14,15]. The friction model applied in these simulations is based on the Colebrook correlation for liquid phase and Lockhart-Martinelli correlation for two-phase flow. In the simulation of these experiments, the wall roughness has been chosen at 1 μm. The pressure at the entrance of the nozzle is supposed to be the stagnation pressure. For the first case at relatively low pressure (the saturation pressure is shown by a bold solid line), the water is subcooled at the test section inlet. For the second case, the inlet condition is close to the saturation. For both cases the DEM provides the best results for the piezometric line both qualitatively and quantitatively. As shown in Fig. 4 and 5, the pressure gradient becomes very high at the nozzle throat, which is classical for critical flow. For the subcooled inlet case (Fig. 4), the HEM fails to predict the correct pressure profile due to the non-equilibrium effects that are more important than those in the close to saturation inlet case. Indeed, in this second case, the HEM predicts a better agreement with experimental in terms of the piezometric line (Fig. 5), but not for the critical flow rate. In addition, the critical flow rate is well predicted by the DEM model for both cases even though some growing discrepancies (not shown here) have been observed for inlet pressures higher than 60 bar and for a subcooling range which increases with this pressure. However for a complete validation, additional tests would be required at different inlet qualities and even at $x_{in} > 0$. Also other parameters such as the void fraction should be assessed.

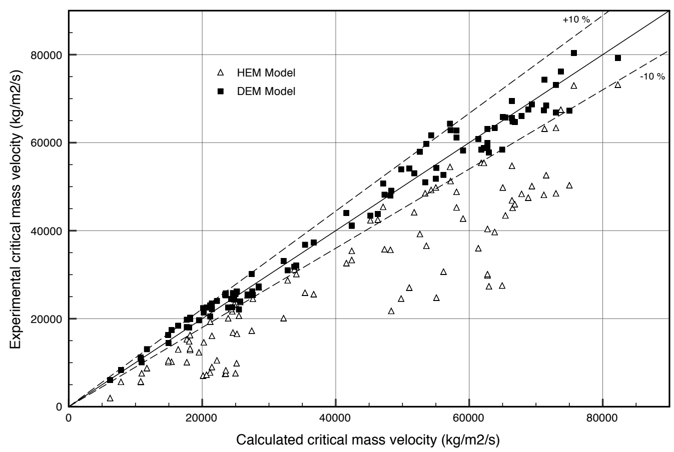


Fig. 3 Experimental versus calculated critical mass velocity for DEM and HEM

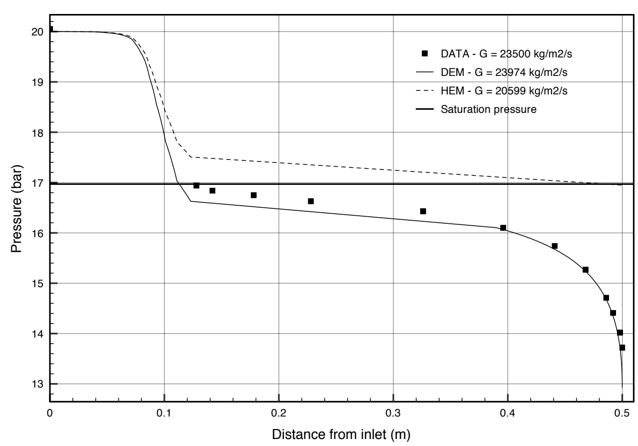


Fig.4 Piezometric line along the Super Moby-Dick nozzle. Subcooled inlet:
$P_{in} = 20\ bar, \quad T_{in} = 204.2°C$.

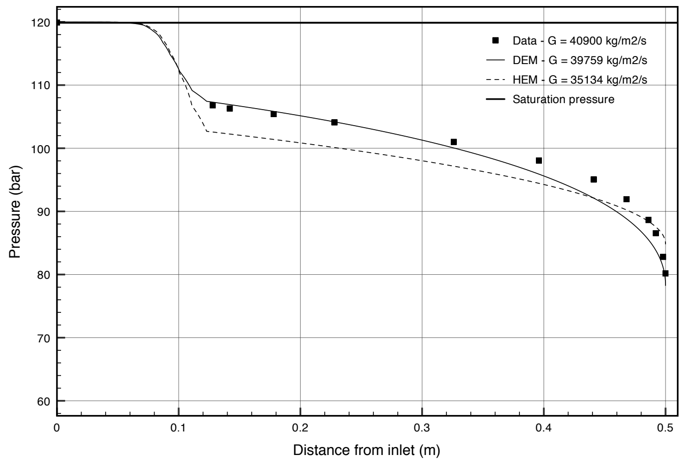


Fig.5 Piezometric line along the Super Moby-Dick nozzle. Close to saturated inlet:

6. Methodology to implement the DEM model in thermalhydraulic system codes

The system codes like RELAP or CATHARE-2 are generally based on the two-fluid model with numerous correlations for determining the mass, momentum and energy transfer between the phases.

In the framework of the NEPTUNE project, supported by CEA, EDF, IRSN and AREVA-NP, a new version of the French system code, CATHARE-3, is recently developed [23]. This version includes the two-fluid model and several new physical models such as multi-field approach (including droplets transport equations), interfacial area, turbulent kinetic energy and turbulent dissipation transport equations. In this study, CATHARE-3 is used to evaluate the DEM when is coupled with the two-fluid model (or with the multi-field model since it would behave similarly in such flows without droplet).

We will discuss here the implementation of the DEM in the CATHARE-3 code, which uses the following basic variables: pressure, both phase enthalpies, void fraction and both phase velocities:

$$p, h_g, h_f, \alpha, w_g, w_f \tag{17}$$

The DEM philosophy assumes the vapour phase is at saturation, which is equivalent to assume an infinite transfer between the vapour and the interface. The quality X can then be deduced from the following equation:

$$X = \frac{\alpha \rho_{g,sat}(p)}{\rho_m} \tag{18}$$

with the mean density of the mixture : $\rho_m = \alpha \rho_{g,sat} + (1-\alpha)\rho_f(p, h_f)$

In the CATHARE code, the enthalpy of the mixture can then be determined by:

$$h_m(CATHARE) = X h_{g,sat} + (1-X) h_f \tag{19}$$

On the other hand, the enthalpy can also be calculated for the DEM from equation (10). Combining equations (10) and (19) by making equal both mixtures enthalpies from CATHARE and DEM, we can link the vaporization index y of the DEM to the quality X by the following equation:

$$(1-y) = \frac{(1-X)\left(h_f - h_{f,sat}\right)}{\left(h_{f,m} - h_{f,sat}\right)} \tag{20}$$

In the DEM, the heat transfer between the liquid phase and the interface depends on the variation of the vaporization index y, which represents the mass transfer from the metastable liquid phase to the saturated liquid phase. In terms of energy transfer, we obtain:

$$\varphi_{f,i} = \rho_m \frac{dy}{dt}\left(h_{f,m} - h_{f,sat}\right) \tag{21}$$

Introducing the correlation (13) of the DEM in equation (21), the heat transfer between the liquid and the interface can finally be determined by:

$$\varphi_{f,i} = \frac{\rho_m (1-X)\left(h_f - h_{f,sat}\right)}{\Theta_{DEM}} \tag{22}$$

$$\Theta_{DEM} = \frac{1}{w_m \left(C_1 \frac{P_w}{A} + C_2\right)\left[\frac{p_{sat}\left(T_{fM}\right) - p}{p_{crit} - p_{sat}\left(T_{fM}\right)}\right]^{0.25}} \tag{23}$$

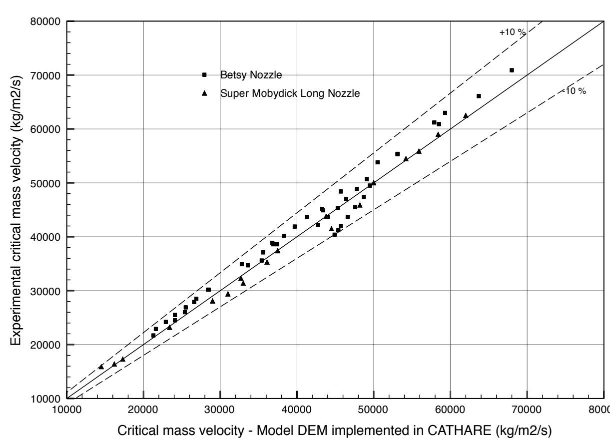
This new DEM correlation for the heat transfer has been implemented in the CATHARE-3 code with the following value for the constants:

$$C_1 = 0.051 \quad C_2 = 0.224 \tag{24}$$

First calculations of critical flashing flow were performed with the CATHARE-3 code for simulating BETHSY and Super Moby-Dick experiments. The comparison between experimental data and the CATHARE-3 code with the DEM is summarized in figures (6) and (7).

The agreement between the experimental data and the CATHARE-3 code with the DEM model seems to be very promising, but the comparison must be further analysed.

This methodology explained here for implementing the DEM model in the CATHARE-3 code could be generalized for other system codes.



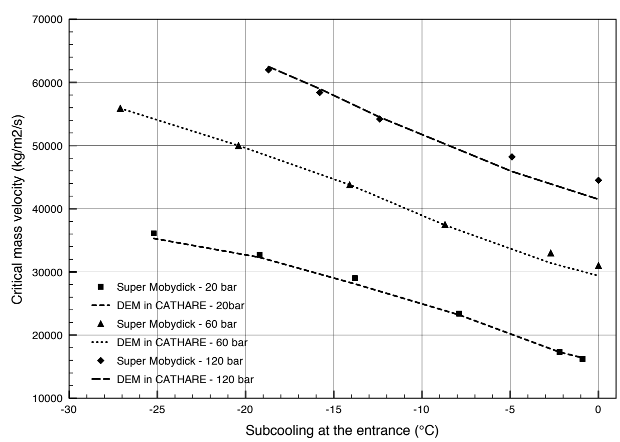


Fig.6: Simulations with CATHARE-3 and DEM of the BETHSY and Super Moby-Dick experiments

7. Conclusion

This paper revisited the modeling techniques for the computation of critical two-phase flows relevant to nuclear safety in GENII, GENIII power plants and also in supercritical water reactors of GENIV. In addition, several possible benchmarks have been reviewed and proposed for validation purposes in system codes. One of the objectives of the NURISP project is to implement such non-equilibrium models in the next generation of the system code CATHARE-3. The results presented in this paper demonstrated that the DEM model is a good candidate since it performs well and is physically consistent. It has also been observed (Fig.6) that small discrepancies appear in the prediction of the critical flow rate at very high pressures with the DEM. Up to now, there is no physical explanation for those discrepancies. The current research track is to relate turbulent pressure fluctuations to trigger the nucleation to justify or modify these empirical parameters.

Acknowledgments

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