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Improvement of the Subcooled Boiling Model in the MARS Code

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1. Introduction

Subcooled boiling is characterized by local boiling adjacent to the heated surface while the bulk liquid is at a subcooled condition. Subcooled boiling may occur in a hot channel under a steady state as well as a transient or accident in nuclear reactors. The void behavior in subcooled boiling has a great effect on flow and heat transfer characteristics and, thus, it is important to predict well the subcooled boiling phenomena in nuclear reactors [1].

In most of the best-estimate thermal-hydraulic system codes, such as RELAP5/MOD3.3 [2] and MARS 3.1 [3], the subcooled boiling model consists of several sub-models; a net vapor generation (NVG), a wall evaporation model, interfacial condensation model, etc. Among them, a lot of studies concerned with the NVG model [4, 5, 6, 7] have been conducted over the last several decades.

The point of net vapor generation (NVGP) is the starting point where the void fraction increases rapidly along the heated length. It has been known that the ability to predict NVGP is mandatory for accurate prediction of the void fraction. Saha and Zuber [4] suggested a NVG correlation composed of low- and high-velocity regions. Although some problems of the correlation have been pointed out [1, 5], the correlation has been still used in thermal-hydraulic system codes such as RELAP5 and MARS.

In this paper, a new NVG correlation, which is based on convective heat transfer characteristics for laminar and turbulent flow, is proposed. In addition, the wall evaporation model in MARS is modified for a more accurate prediction of axial void fraction profile. These are implemented in MARS, and the results are discussed.

2. Subcooled boiling model of MARS code

2.1 The original model

In MARS, the subcooled boiling model includes NVG model and a wall evaporation model. In a flow channel with a heated surface, bubbles can be generated at the surface although the cross-section averaged liquid is subcooled. Initially, the bubbles may coalesce and condense by the subcooled liquid, thus maintaining negligible void fraction along the channel. The NVGP is generally defined as the point in the axial direction (i.e., in the direction of the flow) where the void fraction increases significantly.

MARS adopts the Savannah River Laboratory (SRL) model [6], consisting of NVG model and wall evaporation model package, which shows better results in predicting the subcooled flow boiling under low-pressure conditions. The SRL NVG model is similar to the model presented by Saha and Zuber [4]. The NVG correlation of SRL model is given by:

$$Nu = \frac{\ddot{q} D_h}{k_f (T_{sat} - T_{NVGP})} = 455 , \quad \text{for Pe} \le 70{,}000 \qquad (1)$$

$$St = \frac{Nu}{Re \cdot Pr} = \frac{\ddot{q}}{G c_{pf} (T_{sat} - T_{NVGP})} = 0.0055 - 0.0009 F_{press}$$

$$\text{for Pe} > 70{,}000 \qquad (2)$$

where

$$Pe = Re \cdot Pr = \frac{G D_h}{\mu} \cdot \frac{C_{pf} \mu}{k} ,$$

where T_{NVGP} is the bulk liquid temperature at NVGP, F_{press} is a pressure dependent multiplier:

$$F_{press} = \frac{1.0782}{1.015 + \exp[(P/P_{psia}\text{-}140.75)/28.0]} ,$$

where

$P_{psia} = 6.894 \times 10^3$ for units conversion.

In Eqs. (1) and (2), Peclet number Pe is used to determine the division between thermally (Pe ≤ 70,000; low velocity) and hydro-dynamically (Pe > 70,000; high velocity) controlled region.

Recognizing that the thermal equilibrium quality at the point of net vapor generation is given by:

$$X_{eq,NVGP} = -\frac{c_{pf} (T_{sat} - T_{NVGP})}{h_{fg}} , \qquad (3)$$

the Eqs. (1) and (2) can be also expressed as:

$$X_{eq,NVGP} = -\frac{1}{455} Nu' \quad \text{for Pe} \le 70{,}000, \qquad (4)$$

$$X_{eq,NVGP} = -\frac{1}{0.0055 - 0.0009 F_{press}} \left(\frac{Nu'}{Re \cdot Pr} \right)^{1.0} ,$$

$$\text{for Pe} > 70{,}000 \qquad (5)$$

where

$$Nu' = \frac{\ddot{q} D_h}{k_f} \frac{C_{pf}}{h_{fg}} .$$

Once the point of NVG is determined by Eqs. (1) and (2), the wall evaporation rate is calculated from the point to downstream along the heated wall. The wall evaporation is represented as:

$$\Gamma_W = \frac{q_w \cdot A_w}{V \cdot h_{fg}} \left(\frac{1}{1 + \varepsilon_{SRL}} \right) \left[M + F_{press} \left(F_{gam} - M \right) \right], \qquad (6)$$

where

$$M = \frac{h_f - h_{cr}}{h_f^s - h_{cr}} ,$$

$$F_{gam} = \min[1.0, 0.0022 + 0.11M - 0.59 \times M^2 + 8.68 \times M^3 - 11.29M^4 + 4.25M^5],$$

$$\varepsilon_{SRL} = \frac{\rho_f (h_f^s - h_f) \times F_{eps}}{\rho_g h_{fg}},$$

$$F_{eps} = \min\left[1.0, \frac{1.0}{0.97 + 38.0 \times \exp[-(P/P_{psia} + 60.0)/42]}\right].$$

The multiplier, F_{eps}, is applied to the pumping factor to correct the effect of the density ratio at the low-pressure condition.

2.2 Deficiencies of original subcooled boiling model

The deficiencies of the original subcooled boiling model have been pointed out in previous studies:

(i) According to Rogers et al. [5], the effect of inlet liquid velocity on subcooled boiling has been shown in their experimental data under Pe ≤ 70,000. However, the original model of Eq. (1) does not take into account the effect of inlet liquid velocity. Those can be seen in Figs 1 and 2, respectively.

(ii) In MARS, the SRL model has been assessed by Ha et al. [1]. The results showed that the calculated void fraction is underpredicted when the hydraulic diameter is small ($D_h \le 6$ mm). Adversely, the calculated void fraction is overpredicted in case of $D_h \ge 20$ mm. Those can be shown in Figs 3(a) and (b), respectively.

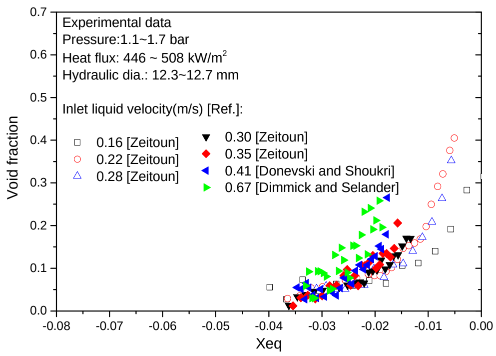


Fig. 1. The effect of liquid velocity on void fraction in experimental data.

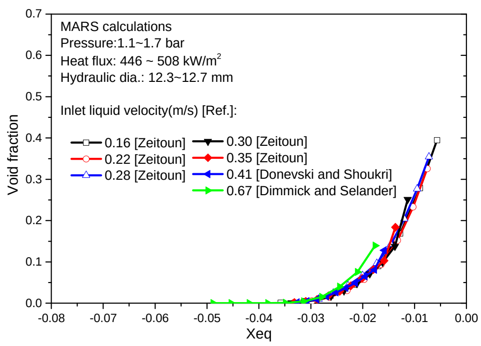
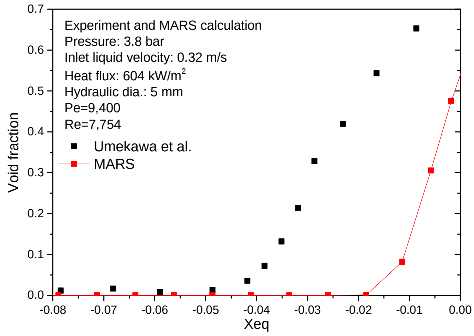
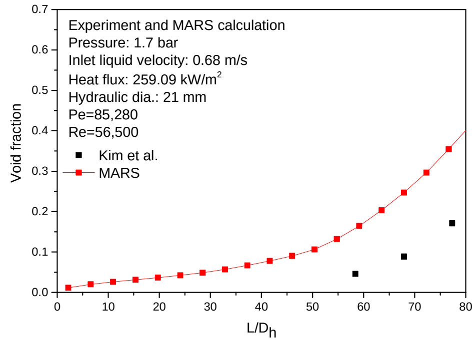


Fig. 2. The calculation results of the experiment.



(a) $D_h = 5$ mm



(b) $D_h = 21$ mm

Fig. 3. The comparison of experimental data and calculated result.

3. Improvement and assessment of the subcooled boiling model

3.1 Improvements of NVG model and wall evaporation model

A lot of studies related to the point of NVG have been conducted over several decades [4, 5, 6, 7]. Some of them [5, 7] have developed NVG models by postulating that the region before the point of NVG is a single-phase flow. Based on these studies, we anticipated that the point of NVG would be closely related to convective heat transfer characteristics of single-phase for laminar and turbulent flow. In references [8, 9], the local Nusselt numbers for the laminar and turbulent flow have been well known:

(i) In case of the laminar flow in a circular tube with constant heat flux, the local Nusselt number is mathematically solved as follows:

$$Nu = \frac{2}{\frac{11}{24} + \sum_{n=1}^{\infty} C_n \exp\left(-\frac{\beta_n^2 x}{r_0}\frac{1}{Pe}\right) R_n(1)}. \quad (7)$$

For the fully developed flow, the exponential term in Eq. (7) disappears and, the local Nusselt number becomes a constant 4.36. Generally, the constant value is depending on geometric types (e.g., rectangular, plate and, annulus, etc.).

(ii) In case of the fully developed turbulent flow, the local Nusselt number is a function of Re and Pr numbers. The correlation proposed by Dittus-Boelter has been used widely. It is given by:

$$\frac{Nu}{\mathrm{Re}^{0.8}\,\mathrm{Pr}^{0.4}} = 0.023\,. \qquad (8)$$

It should be noted that the Nusselt number of Eq. (1) is constant and, the Nusselt number of Eq. (2) is functions of Re and Pr numbers (in case of constant pressure). These correlations correspond to the heat transfer correlations of the fully developed laminar and turbulent flows.

Generally, the criterion for the transition between the laminar and turbulent flows is generally Re=10,000. In SRL model, the criterion between the low-and high-velocity regions is Pe=70,000. As shown in Fig. 3, we can confirm that Pe=85,280 is corresponding to Re=56,500. The value of 70,000 is very high as a criterion for the transition between the laminar and turbulent flows and, some literature [10] has also raised this issue.

In reference [1], a new criterion between the low-and high-velocity regions was proposed using the non-dimensional bubble rise velocity u^*. We used the new criterion to propose a new NVG correlation. The criterion is given by:

$$\mathrm{u}^* = \frac{u_i}{1.53\times\left(\dfrac{g\sigma(\rho_L-\rho_v)}{\rho_L^2}\right)^{0.25}} = 1.2\,, \qquad (9)$$

where the denominator is a bubble rise velocity correlation [11] used in MARS code and, u_i is a velocity to estimate the inlet liquid velocity defined as:

$$u_i = \frac{\dot{m}}{\rho_f A}\,. \qquad (10)$$

Table 1 shows the collected subcooled boiling experimental conditions. From total 103 cases of 13 experiments, the NVGP was fitted for the low-and high-velocity regions, respectively. As a result, we propose the following new NVG correlation:

$$Nu = \frac{1}{0.0901-0.0893\exp\left(-158\dfrac{1}{Pe}\right)} \quad \text{for } u^* \le 1.2 \qquad (11)$$

$$\frac{Nu}{(\mathrm{Re\,Pr})^{0.5833}} = 1.09 \quad \text{for } u^* > 1.2 \qquad (12)$$

$X_{eq,NVGP}$ in experimental data is compared with the calculated results by SRL and new models using Eq. (3), as shown in Figs. 4 and 5 (a, b), respectively. It is shown that the results by the new NVG model show better agreements with experimental data.

The SRL wall evaporation model was also modified empirically to consider the effects of inlet liquid velocity and hydraulic diameter. In Eq. (6), $\mathrm{F_{gam}}$ is modified as follows:

$$\mathrm{F_{gam}} = \min[1.0, 0.9\mathrm{M}^2 + 0.1\mathrm{M} + \mathrm{f}(\mathrm{u}^*,\mathrm{D}^*)\sin(\pi\mathrm{M})]\,, \qquad (13)$$

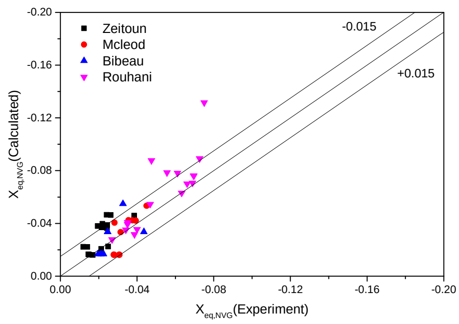
where

$$\mathrm{f}(\mathrm{u}^*,\mathrm{D}^*) = \min[0.103\mathrm{u}^{*0.266}\mathrm{D}^{*2}, 1.0] \text{ for } u^* \le 1.2\,,$$

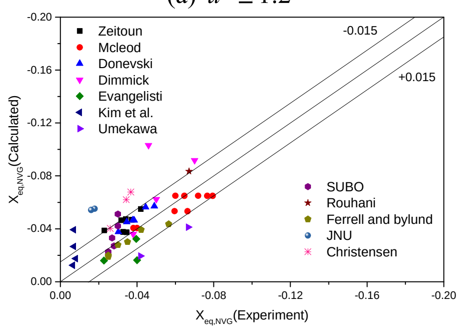
$$\mathrm{f}(\mathrm{u}^*,\mathrm{D}^*) = \min[0.491(\mathrm{u}^*-1.2)^{0.545}\mathrm{D}^{*2}, 1.0]\,, \quad \text{for } u^* > 1.2$$

$$\mathrm{D}^* = \frac{\mathrm{D_{ref.}}}{\mathrm{D_h}}\,,\ \mathrm{D_{ref.}} = 12\mathrm{mm}\,.$$

Hereinafter, the model package of Eqs. (11) through (13) is called as "New subcooled boiling model".

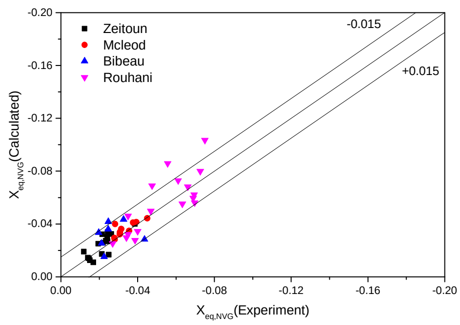


(a) $u^* \le 1.2$

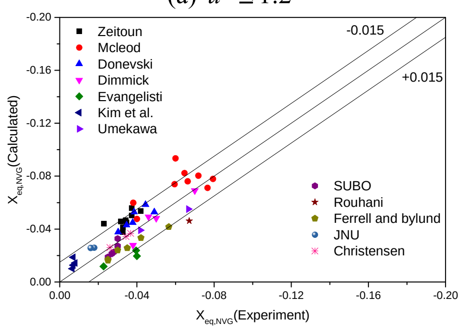


(b) $u^* > 1.2$

Fig. 4. Comparison of $X_{eq,NVG}$ between the experiment and calculated results (SRL).



(a) $u^* \le 1.2$



(b) $u^* > 1.2$

Fig. 5. Comparison of $X_{eq,NVG}$ between the experiment and calculated results (new NVG model).

Table 1. Experimental conditions used for the new subcooled boiling model.

Experiment	No of tests	Press. (bar)	Pe	Geometry D_h(mm)
Zeitoun	25	1.1 ~1.7	12,000 ~32,500	Annular (12.7)
Mcleod	19	1.55	3,600 ~26,600	Annular (8.9)
Bibeau	6	1.55	3800 ~14,200	Annular (9.1)
Donevski and Shoukri	6	1.5 ~2.1	25,000 ~35,500	Annular (12.7)
Dimmick and Selander	4	1.65	48400 ~86,500	Tube 12.29
Evangelisti and Lupoli	3	1.2	22,600 ~52,600	Annular (6.0)
Kim et al.	4	1.3 ~1.7	43800 ~85,700	Annular (21.0)
SUBO	5	1.9 ~2.0	177,100 ~329,000	Annular (25.5)
JNU	2	1.4 ~1.7	53,400 ~85,000	Annular (20.0)
Umekawa et al.	2	3.8 ~5.0	9,400 ~18,900	Tube (5~10)
Ferrell and Bylund	6	4.1 ~8.2	33,600 ~41,000	Tube (11.9)
Rouhani	18	9.8 ~50	8,070 ~45,188	Tube (13.0)
Christensen	3	28~69	81,700 ~135,900	Rect. (17.8)
Total	103	1.1 ~69	3,600 ~329,000	5~25.5

3.2 Simulation results

The subcooled boiling experiments have been simulated using MARS with the new subcooled boiling model. Fig. 6 shows the simulation results against the experiments presented in Fig. 1. It can be seen that the simulation results can represent the effect of inlet liquid velocity very well in contrast to the results of the original MARS code with SRL model.

Fig. 7(a) shows the simulation results for the subcooled boiling experiment at a small D_h . The modified model shows better agreement with the experimental data than the original MARS. In the case of a large D_h in Fig. 7(b), the simulation result also shows better agreement with the experimental data than the original MARS.

For quantitative assessment, averaged absolute errors of void fraction was calculated for each experimental case:

$$\varepsilon_{mean} = \frac{1}{n}\sum_{i=1}^{n}\left|\alpha_{exp,i} - \alpha_{cal,i}\right| \quad (14)$$

where, $\alpha_{exp,i}$ is a measured void fraction. $\alpha_{cal,i}$ is a calculated void fraction at the position of experimental measurement, which is obtained by a linear interpolation of the void fractions at two adjacent computing cells.

The average absolute errors for all the experiments are summarized in Table 2. The results showed that, when the new subcooled boiling model was used, the absolute error was reduced in most experiments (except for Rouhani's experiment. The averaged absolute error for the thirteen experiments was reduced by 3.7 %. Furthermore, the reduction of the relative error is approximately 34%.

Table 2. Comparison of averaged absolute errors between SRL model and new subcooled boiling model.

Experiment	No. of data points	No of tests	SRL	New S.B. model
Zeitoun	308	25	0.076	0.052
Mcleod	239	19	0.079	0.052
Bibeau	39	6	0.074	0.055
Donevski and Shoukri	62	6	0.061	0.041
Dimmick and Selander	59	4	0.069	0.041
Evangelisti and Lupoli	44	3	0.212	0.173
Kim et al.	6	4	0.173	0.093
SUBO	16	5	0.045	0.029
JNU	3	2	0.147	0.086
Umekawa et al.	16	2	0.263	0.145
Ferrell and Bylund	30	6	0.099	0.078
Rouhani	67	18	0.029	0.031
Christensen	36	3	0.071	0.052
Average	925	103	0.108	0.071

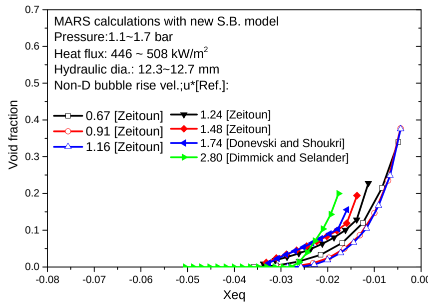
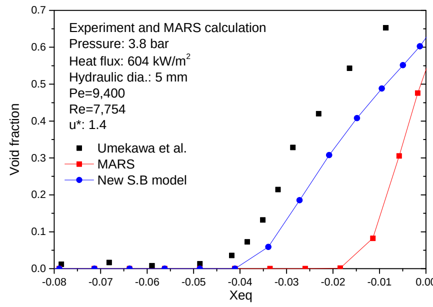
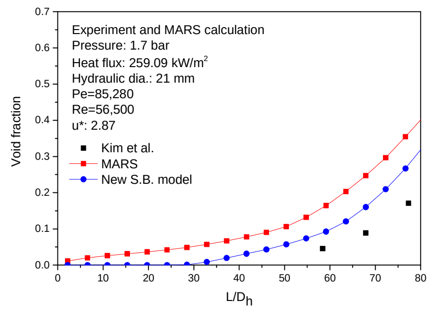


Fig. 6. The calculation results with new model (See Fig. 1).



(a) $D_h = 5$ mm



(b) $D_h = 21$ mm.

Fig. 7. The comparison of experimental data and calculated result with new model (See Fig. 3).

4. Conclusions

In this work, the following deficiencies of the MARS subcooled boiling model were presented.

- MARS cannot consider the effect of inlet liquid velocity on axial development of void profile.
- MARS considers the effect of hydraulic diameter incorrectly.

To solve these problems, we proposed the new subcooled boiling model, consisting of new NVG model and modified wall evaporation model. The new subcooled boiling model was implemented in MARS and, it has been assessed against various subcooled boiling experiments. The results showed better agreement with experimental data than the original MARS code.

Acknowledgment

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