Int. J. Heat Mass Transfer. Vol. 35, No. 12, pp. 3291–3299, 1992
Printed in Great Britain

0017-9310/92 $5.00+0.00

Prediction of pressure drops in forced convection subcooled boiling water flows

M. A. HOFFMAN and C. F. WONG
Department of Mechanical, Aeronautical and Materials Engineering, University of California, Davis, CA 95616, U.S.A.

(*Received* 14 *August* 1991 *and in final form* 10 *February* 1992)

Abstract—A semi-empirical model is described for the prediction of the pressure drop in subcooled convective boiling of water flows in tubes subject to uniform high heat fluxes. The flow in the single-phase-liquid (SPL) regime, the partially-developed boiling (PDB) regime and the fully-developed boiling (FDB) regime is modeled including the effects of nonequilibrium quality and void fraction and property variations along the tube. Improved models for the PDB and FDB regimes and extensive comparisons with experimental data are described. Pressure drop predictions of the model are compared with over 100 experimental runs and found to agree within approximately $\pm 20\%$.

INTRODUCTION

CONVECTIVE cooling of energy system components and electrical and computer components using subcooled nucleate boiling (SCB) is becoming increasingly important as the heat fluxes to which these components are subjected increase. Subcooled boiling has long been recognized as a very effective cooling mechanism as a result of the heat transfer enhancement due to the action of the bubbles generated along the heated surface. Cooling with subcooled boiling is generally limited by two main constraints. The first constraint is for the temperature of the working fluid to remain below the onset of bulk boiling so that there is no net bulk vapor generated (which would have to be condensed in a heat rejection system). The second constraint is to remain safely below the critical heat flux condition to avoid any possibility of tube burnout. In addition, it is generally desirable to minimize the pressure drop in order to be able to use lower inlet pressures and smaller pumps.

In order to predict when either of the above two constraints might be violated in a cooling system design, it is absolutely essential to accurately predict the two-phase pressure drop along the tubes of the heat exchanger. Dormer and Bergles [1] and Owens and Schrock [2] were among the first to generate a significant amount of low-uncertainty subcooled boiling pressure drop data. They also produced correlations of their data which are widely used in design work. These correlations are reliable when used for applications which fall within the range of the parameters covered in the experiments upon which the correlations are based, but the authors do not recommend that the correlations be used outside this range.

The research reported in this paper summarizes several years of effort to develop an accurate model for convective subcooled boiling water flows in tubes which would be of more general applicability for design purposes over a wide range of cooling system parameters, particularly at high heat fluxes. This study started with the work of Hoffman *et al.* [3] in connection with high heat flux cooling systems for future fusion reactor components, and was continued in the work of Mokhtarani [4], Kline [5] and Wong [6]. The goal has been to produce a well-validated computer code using a combination of theoretical equations and empirical correlations which would predict the pressure drops as accurately as the existing empirical correlations of the experimental data and which would be easily extendable to a wider range of subcooled boiling parameters as more data become available.

Results of the first thoroughly-validated version of this computer code (SR-2F) were reported by Hoffman and Kline [7]. This code predicted the pressure drops for a large part of the selected data base to within ± 25–30%, but there were some sets of data for which the predictions were worse than this. Jia and Schrock [8] suggested several improvements in the model and reported good agreement with several sets of data. The purpose of this paper is to report on some significant modifications in the earlier subcooled model which improve the prediction of the subcooled-boiling pressure drops. In all, about 110 experimental runs were selected from the data of ref. [1] for horizontal tubes and ref. [2] for vertical tubes to form our reference data base for validating the improved SCB model. Over 3200 computer runs were made during this study to select and validate the models incorporated in the final code version ASCB53 [6]. The basic models used in this computer code in each flow regime are described first and comparisons with typi-

NOMENCLATURE

Acronyms

- BD bubble departure point
- FDB fully developed boiling regime
- OBB onset of bulk boiling
- ONB onset of nucleate boiling
- OSNVG onset of significant net vapor generation (assumed the same as the BD point)
- PDB partially developed boiling regime
- SCB subcooled nucleate boiling regime
- SPL single phase liquid regime.

Symbols

- C_o radial void distribution parameter
- c_p specific heat per unit mass
- D inner diameter of tube
- f fanning friction factor
- g acceleration of gravity
- G mass flux
- i enthalpy
- k thermal conductivity
- K_{red} reduction factor for near-wall void fraction
- L heated length of tube
- p pressure
- q'' heat flux
- T temperature
- ΔT_{SAT} temperature difference, $T_w - T_{SAT}$
- ΔT_{SUB} temperature difference, $T_{SAT} - T_f$
- u velocity
- v specific volume
- x' nonequilibrium quality
- z distance along tube from heated inlet.

Greek symbols

- α' nonequilibrium void fraction
- α_w near-wall void fraction
- μ dynamic viscosity
- ρ density
- σ surface tension coefficient
- $(\phi_{fo})^2$ two-phase friction multiplier.

Subscripts

- accel acceleration
- f liquid or bulk mean liquid
- fo liquid only
- fg vapor minus liquid property
- fric friction
- g vapor
- grav gravity
- i inner
- in inlet
- iso isothermal
- SAT saturation
- SUB subcooling.

cal experimental data are given. Then the critical heat flux correlation selected for incorporation in the code is discussed.

REVIEW OF FLOW REGIMES ALONG A UNIFORMLY HEATED TUBE

Figure 1 shows the flow entering the tube in the single phase liquid (SPL) regime. After the flow reaches the onset of nucleate boiling (ONB) point, the flow enters the subcooled boiling (SCB) regime. The SCB regime is conventionally divided into the high-subcooling, partially developed boiling (PDB) regime and the low-subcooling, fully developed boiling (FDB) regime for the purposes of better modeling the physical phenomena.

In the PDB regime, small bubbles generated from nucleation sites remain on or near the wall. This near-wall bubble layer grows in thickness as the flow is heated until the bulk temperature reaches a sufficiently high value to allow bubbles to depart from the surface and exist in the cooler fluid in the core flow. This PDB regime is modeled as a region of growing flow blockage as a result of the growing annular void in the near-wall region. The model for the PDB regime includes the effects of the growing near-wall void on the friction and acceleration pressure gradients, as will be described in more detail shortly.

The point at which a 'significant' number of bubbles have detached from the near-wall region is usually called the point of onset of significant net vapor generation (OSNVG) or simply the bubble-departure (BD) point; these two terms will be used interchangeably in this paper. While the term 'significant' has never been defined quantitatively to the authors' knowledge, it nonetheless has been found to be crucially important to predict this OSNVG or BD point accurately in order to obtain a good prediction of the pressure drop in the FDB regime following this point. The end of the FDB regime and of subcooled boiling occurs at the onset of bulk boiling (OBB). The model for the FDB regime includes the effects of the growing volumetric void on the friction, acceleration and gravity pressure gradients, as will be described in more detail shortly.

The code employs a straightforward marching scheme to integrate the pressure gradient, where accuracy is maintained by selecting a sufficiently small axial step size. For a typical run with 1000 axial steps, the run time on a PC/AT compatible computer with a 6 MHz 80286 microprocessor and an 80287 math

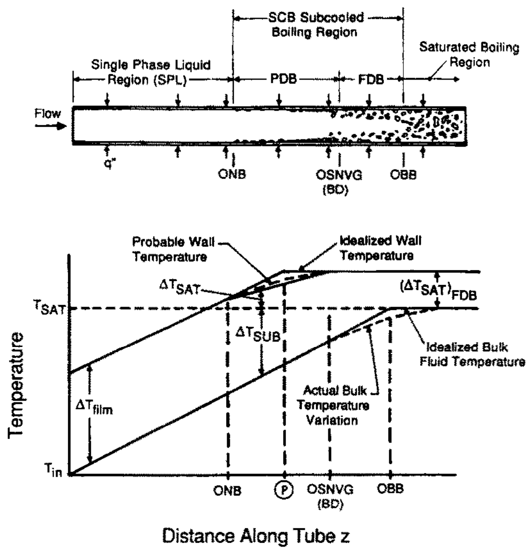

FIG. 1. Definition of the flow regimes and typical temperature profiles along the heated tube for water coolant and uniform axial heat flux.

coprocessor is on the order of 4 min. More modern personal computers would be significantly faster.

The models used to characterize the SPL, the PDB and the FDB regimes will now be described in the logical sequence of occurrence in a heated tube flow, as shown in Fig. 1. It should be noted that logic built into the code allows it to start the computation in any of the three regimes as determined by the geometry, inlet conditions and the heat flux. However, no provision for inlet flow or thermal development is included in the code yet.

SINGLE PHASE LIQUID REGIME

In the original SR-2F code version developed by Hoffman and Kline [7], the Petukhov equation was used to correct the isothermal friction coefficient for the effects of heating

$$\frac{f}{f_{\rm iso}} = \frac{1}{6}\left[7 - \frac{\mu_{\rm bulk}}{\mu_{\rm wall}}\right]. \qquad (1)$$

However, Wong [6] discovered that under certain conditions of high heat flux, the Petukhov correction could become negative. Dormer and Bergles [1], Owens and Schrock [2] and others have suggested using the wall viscosity divided by the bulk viscosity to some power to correct for the effect of heating. The revised code version now incorporates this correction factor with the exponent, n, chosen as 0.3

$$\frac{f}{f_{\rm iso}} = \left[\frac{\mu_{\rm wall}}{\mu_{\rm bulk}}\right]^n. \qquad (2)$$

Agreement of the pressure drop predictions with the SPL data is excellent in almost every case, with overall agreement within about $\pm 20\%$. The SPL heat transfer coefficient used in the code is that specifically recommended for water in ref. [9].

ONSET OF NUCLEATE BOILING

The PDB regime is defined to start at the onset of nucleate boiling (ONB). In the original code version SR-2F [7], the PDB regime was assumed to start at point P on Fig. 1 as a first approximation; this is at the intersection of the extrapolated SPL wall temperature and the FDB wall temperature curves. In order to improve upon this approximate model, the Bergles and Rohsenow correlation [10] and the Davis and Anderson equation [11] for prediction of the true ONB point were compared. It was found that the two equations gave almost exactly the same prediction of ONB when the critical cavity radius, $r_{\rm CRIT}$, was used in the Davis and Anderson equation in place of the maximum-active cavity radius, $r_{\rm C(MA)}$

$$\Delta T_{\rm SAT(ONB)} = \frac{B}{r_{\rm C(MA)}} + \frac{q'' r_{\rm C(MA)}}{k_{\rm f}} \qquad (3)$$

where

$$B = \left[\frac{2\sigma T_{\rm SAT} v_{\rm g}}{i_{\rm fg}}\right] \qquad (4)$$

and

$$r_{\rm CRIT} = \sqrt{\left(\frac{Bk_{\rm f}}{q''}\right)}. \qquad (5)$$

The Davis and Anderson equation also permits use of the actual maximum active cavity radius in place of the critical cavity radius when this is known for a particular surface/fluid combination. Our improved SCB model now incorporates the Davis and Anderson equation.

PARTIALLY DEVELOPED BOILING REGIME

In order to model the growing near-wall bubble layer in the PDB regime, the original code version SR-2F used Rouhani's correlation [12] to estimate the radius of bubbles at the bubble departure (BD or OSNVG) point for the prediction of the effective near-wall void fraction at bubble departure

$$\alpha_{\rm w(BD)} = \frac{4}{D_{\rm i}}\left[1.59 \times 10^{-4}\left(\frac{p}{10^5}\right)^{-0.237}\right]. \qquad (6)$$

The improved model uses this equation, but the effective wall void fraction, $\alpha_{\rm w}$, is now assumed to grow linearly from zero at ONB (rather than point P in the old model [7]) to the above value at the BD point. The SPL friction coefficient was used in the PDB regime with no enhancement for the effective roughness of the bubble layer, since it is uncertain how to include the effect of sliding bubbles in the near wall region on the friction coefficient. However, there is a strong enhancement of the SPL friction pressure

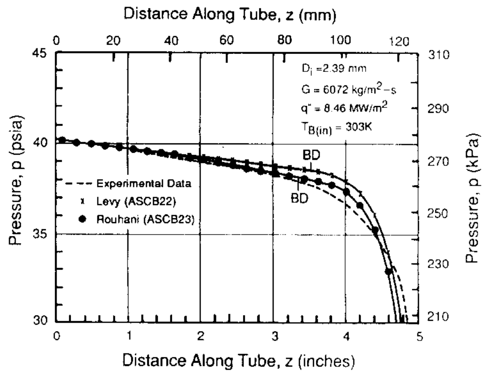

FIG. 2. Comparison of the predicted pressure variation to one experimental run from ref. [1], Fig. 11. (ONB is predicted to occur upstream of the inlet.)

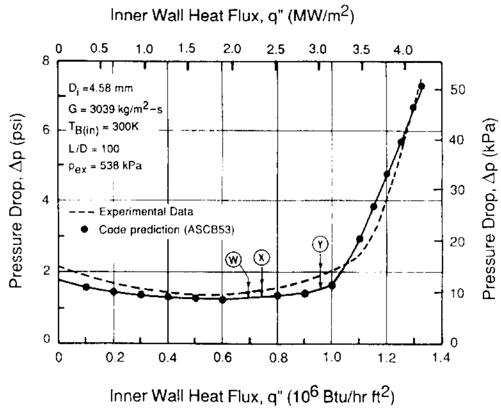

FIG. 3. Comparison of the predicted pressure drops with a set of experimental runs from ref. [1], Fig. 19. Points W, X and Y are explained in the text.

gradient in the PDB regime through the use of the core-flow mass flux, G_c, instead of the inlet mass flux, G, to calculate the pressure gradient

$$G_c(z) = \frac{G(z)}{1-\alpha_w(z)}. \tag{7}$$

This core-flow mass flux increases due to the blockage created by the growing wall void fraction, and the friction pressure gradient depends on this mass flux to the 1.8 power.

An alternate PDB model proposed by Jia and Schrock [8] used the Levy bubble departure model [13] to predict the bubble size at OSNVG (BD) and the Hirata equation [14] for the enhanced friction coefficient due to the bubble layer effective roughness. This model gave very nearly the same results as our model, as illustrated by a typical comparison run in Fig. 2. Until there are better data which permit us to separately evaluate the effects of wall blockage and enhanced friction due to the near-wall bubble layer, it is not possible to decide which model is correct. The final code version continues to use our original model because Hino and Ueda [15] suggest that the Levy equation underpredicts the size of the bubbles at bubble departure based on measurements in their photographic studies.

Figure 3 shows a typical comparison of our model with an entire set of runs at different heat fluxes of Dormer and Bergles [1] for a tube inner diameter of 4.58 mm. Dormer and Bergles presented most of their results in this type of plot, where the dashed curve represents an entire series of runs for a wide range of heat fluxes. On this plot, point W signifies the heat flux for the particular run where the ONB point is predicted to be at the tube exit; single phase liquid exists everywhere else in the tube. At point X, the ONB point is predicted to be at the tube inlet and the entire flow is predicted to be in the PDB regime. At point Y, the BD point is predicted to be at the tube exit; this is the highest heat flux where the flow is entirely in the PDB regime. It can be seen that the agreement between our model and the data is quite good throughout the PDB regime, and the agreement is also good in the SPL and FDB regimes as well. The overall agreement for this particular set of predictions is about $\pm 15\%$.

The top curve in Fig. 4(a) shows the predictions of our model for the smallest diameter tubes (1.575 mm inner diameter) used in the experiments of ref. [1]; this was the poorest agreement of the model with any of the data in our selected data base. The predicted pressure drops can be seen to be too high by as much as 50% in the PDB regime between points W and Y. In order to improve the agreement with these small-diameter-tube runs, we introduced an empirical

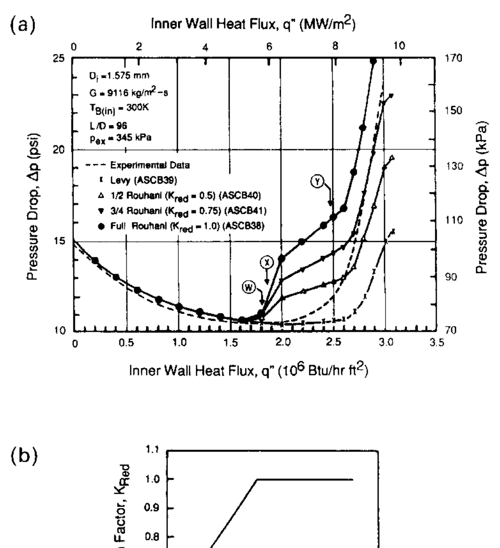

FIG. 4. (a) Comparison of the predicted pressure drops for various values of the near-wall void reduction factor, K_{red}, with a set of experimental runs from ref. [1], Fig. 28. (b) Final variation of the near-wall void reduction factor selected for incorporation in the new SCB Code ASCB53.

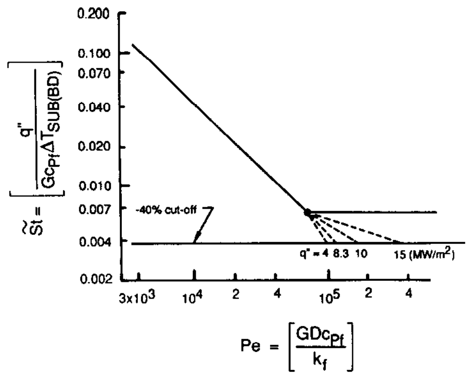

FIG. 5. Modifications of the Saha–Zuber BD (bubble departure) correlation incorporated in the new SCB Code ASCB53.

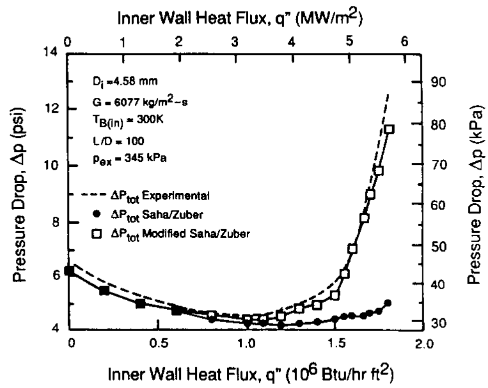

FIG. 6. Comparison of the predicted pressure drops using the unmodified and the modified Saha–Zuber correlation with a set of experimental runs from ref. [1], Fig. 19.

reduction factor, K_{red}, to reduce the effective near-wall void fraction, α_w. This did give some improvement in the agreement in the PDB regime, particularly for the lower value of K_{red} of 1/2. However, in order to obtain good agreement in the FDB regime past point Y, a final compromise value of K_{red} close to 3/4 was chosen for this small tube diameter. The final choice of the reduction factors incorporated in our model is shown by the curve in Fig. 4(b). The use of some reduction in α_w is consistent with the recent experimental results of Inasaka *et al.* [16], which suggest that the effective wall void fractions in tubes with diameters around 1 mm are lower than predicted by current models. The fact that our agreement is still only within about 20% in the PDB regime indicates that further research is required on the modeling of the PDB regime, particularly for small diameter tubes.

As indicated on Fig. 1, a linear wall temperature variation was assumed in the PDB regime between the ONB point and the BD point in the improved code (instead of using the asymptotes to point P). This is clearly only a first approximation to the actual wall temperature variation; it is adequate for the prediction of the PDB pressure drop, because the wall temperature only affects the pressure drop very slightly through its effect on the wall viscosity in equation (2). However this wall temperature variation only gives a rough estimate of the PDB heat transfer coefficient. An improved equation for the wall temperature variation in the PDB regime is now being developed.

ONSET OF SIGNIFICANT NET VAPOR GENERATION

The prediction of the OSNVG or BD point was based on the empirical correlation of Saha and Zuber [17] in the original code version SR-2F. In an analysis of the most probable locations of the OSNVG points for the Dormer and Bergles data, we detected a weak trend with tube diameter and a stronger trend with heat flux [6]. With only a few exceptions the estimated OSNVG points for each of four heat fluxes fell on the dashed lines shown in Fig. 5. However, since none of the estimated OSNVG points fell below the horizontal line 40% below the Saha–Zuber correlation, we chose this as a lower bound.

Equations were fitted to these lines and are incorporated in the final code version. These modifications to the Saha–Zuber correlation were found to either give improved agreement with the experimental data in our data base or to give a more conservative estimate of the pressure drop (i.e. to predict a somewhat higher pressure drop). In some cases such as the one shown in Fig. 6, the improvement over the original SR-2F code prediction was dramatic. (It should be noted that the agreement in Fig. 6 is typical of that obtained with most of the data of ref. [1].) As a result, it was decided to include this modification of the Saha–Zuber correlation in our model until more data become available. We also feel that some modification of the Saha–Zuber correlation for the smaller diameter tubes will improve the model, but more research is needed to quantify this effect.

The alternative model of Shah [18] for predicting the bubble departure point was also examined briefly. A typical result is shown in Fig. 7. For this run, the

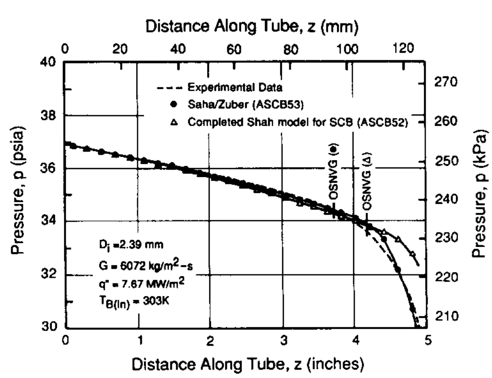

FIG. 7. Comparison of the predicted pressure variation using the Saha–Zuber correlation versus the Shah correlation with one experimental run from ref. [1], Fig. 11. (ONB is predicted to occur upstream of the inlet.)

Shah model predicted bubble departure (i.e. OSNVG) to occur further downstream in the tube than the Saha–Zuber correlation. This in turn caused the code version employing the Shah model to underpredict the pressure drop in the FDB regime. Since the present model using the Saha–Zuber bubble-departure correlation gave better results, the Shah model was not adopted.

For many high heat flux situations, the wall at the heated tube inlet is already superheated above the value required for ONB. For these cases, we assume that the near-wall bubble layer growth starts at the inlet. This approximation appears to give good agreement with the data for experimental runs which had subcooled boiling at or very close to the heated inlet (e.g. see Figs. 2, 7, 8 and 10). Our model does not account for thermal development explicitly, as mentioned earlier. However, based on the good agreement of the predictions with the pressure drop data near the heated inlet, we speculate that the effective development lengths in subcooled boiling may only be a few tube diameters.

FULLY DEVELOPED BOILING REGIME

In the original code version SR-2F of ref. [7], the near-wall void fraction in FDB was calculated using equation (6). The core mass flux was calculated using equation (7) and this value was used as the effective mass flux in all the FDB equations. This implied that the effective blockage created by the near-wall void stayed almost constant in the FDB regime. This is clearly one possibility, but we believe that it is not the most likely one. Another possible limiting case is to set the wall void fraction to zero immediately after the BD point and use the full mass flux, G, in all the FDB equations; however, this case gave poor agreement with the data. The actual situation probably lies in between these two limiting cases. Since it was not possible to distinguish the wall void from the volumetric void in the FDB regime based on the available experimental data, we decided to treat the wall void variation and the blockage after the bubble departure point in a purely empirical way.

A set of linearly decreasing wall void profiles was evaluated and it was found that a profile that decreased to zero at a point one-quarter of the distance from the BD point to the predicted location of the onset of bulk boiling (OBB) gave the best overall agreement with the experimental data in our data base. However, this treatment of the transition between PDB and FDB is purely empirical and should be improved upon when reliable data on the actual behavior of the wall void fraction become available.

The basic analytical equations used in the original code version SR-2F [7] for the pressure gradients due to friction, flow acceleration and gravity in the FDB regime were based on the separated flow model [19] modified for use in the subcooled boiling regime

$$\left[\frac{\mathrm{d}p}{\mathrm{d}z}\right]_{\mathrm{fric}} = -\left[\frac{2f_{\mathrm{fo}}G^2v_{\mathrm{f}}}{D}\right](\phi_{\mathrm{fo}})^2 \tag{8}$$

$$\left[\frac{\mathrm{d}p}{\mathrm{d}z}\right]_{\mathrm{accel}} = -G^2\frac{\mathrm{d}}{\mathrm{d}z}\left[\frac{(x')^2v_{\mathrm{g}}}{\alpha'}+\frac{(1-x')^2v_{\mathrm{f}}}{(1-\alpha')}\right] \tag{9}$$

$$\left[\frac{\mathrm{d}p}{\mathrm{d}z}\right]_{\mathrm{grav}} = -g\sin\theta\left[\frac{\alpha'}{v_{\mathrm{g}}}+\frac{(1-\alpha')}{v_{\mathrm{f}}}\right]. \tag{10}$$

The main modification for use of the separated flow model in the SCB regime was to replace the equilibrium quality and void fraction normally used in the bulk boiling regime by the nonequilibrium quality, x', and the nonequilibrium void fraction, α', in the SCB regime. The total pressure drop was obtained by integrating each of these pressure gradient terms along the tube and summing them.

In order to model the nonequilibrium volumetric void fraction, α', the drift flux model developed by Kroeger and Zuber [20] was used

$$\alpha'(z) = \frac{x'(z)}{\left[\dfrac{C_{\mathrm{o}}(\rho_{\mathrm{f}}-\rho_{\mathrm{g}})}{\rho_{\mathrm{f}}}\right]x'(z)+\left[C_{\mathrm{o}}+\dfrac{\bar{u}_{\mathrm{gj}}}{u_{\mathrm{fo}}}\right]\dfrac{\rho_{\mathrm{g}}}{\rho_{\mathrm{f}}}}. \tag{11}$$

The nonequilibrium quality was evaluated from the model of Kroeger and Zuber [20] using an empirical equation for the variation of the bulk fluid temperature in the FDB regime

$$x'(z) = \left[\frac{c_{p\mathrm{f}}\Delta T_{(\mathrm{BD})}(Z^+-T^*)}{i_{\mathrm{fg}}-c_{p\mathrm{f}}\Delta T_{(\mathrm{BD})}(1-T^*)}\right] \tag{12}$$

where

$$T^* = \left[\frac{T_{\mathrm{f}}(z)-T_{\mathrm{f}}(z_{\mathrm{BD}})}{T_{\mathrm{SAT}}-T_{\mathrm{f}}(z_{\mathrm{BD}})}\right] = \tanh\,[Z^+] \tag{13}$$

and

$$Z^+ = \frac{z-z_{\mathrm{BD}}}{z_{\mathrm{OBB}}-z_{\mathrm{BD}}} \tag{14}$$

and

$$\bar{u}_{\mathrm{gj}} = 1.41\left[\frac{\sigma g(\rho_{\mathrm{f}}-\rho_{\mathrm{g}})}{\rho_{\mathrm{f}}^2}\right]^{0.25} \tag{15}$$

which is the weighted mean drift velocity for vertical upflow in tubes. For horizontal tube orientations, this velocity was set to zero. This model has been retained in the improved code with the minor modifications described below.

One problem encountered with this model was the prediction of too large nonequilibrium void fractions near the tube exit for some runs, particularly at lower exit pressures on the order or less than 2×10^5 Pa. In fact, in some cases the predicted α' was over 0.80, which is probably too high a value in subcooled boiling.

One of the most crucial adjustable parameters in the nonequilibrium void fraction equation, the distribution parameter, C_{o}, was examined first. The original code version SR-2F [7] used a constant value of 1.13, corresponding to a bubbly flow, because Kroeger and Zuber [20] recommended that this value be used in the absence of experimental data on the void fraction radial profiles in subcooled boiling. However, since other authors have recommended constant values up to about 1.3 for C_o, these other values were evaluated.

Jia and Schrock [8] suggested the use of the empirical equation of Hancox and Nicoll [21] for the distribution parameter, C_o, instead of a constant value

$$C_o = \left[\frac{1-\exp(-C_{01}\alpha')}{1-\exp(-C_{01})}\right](1+C_{02})-C_{02}\alpha' \quad (16)$$

where $C_{01} = 19$ and

$$C_{02} = 1.164-1.6534\times10^{-7}p+7.5098\times10^{-15}p^2. \quad (17)$$

Note that the pressure is in Pascals in this and all other equations. We compared the Hancox–Nicoll equation to various constant values for C_o and found that a constant value for C_o of 1.25 gave the best all around agreement with the runs in our data base. A typical result from this comparison study is shown in Fig. 8. The Hancox–Nicoll equation often resulted in an underprediction of the pressure drop by as much as 20%. This was probably a direct result of the fact that the Hancox–Nicoll equation often predicted high values for C_o; in some cases C_o peaked at values as high as 1.8 along the tube and had a value as high as 1.7 at the tube exit.

Several alternative correlations first studied by Kline [5, 7] were reexamined to be certain that the revised FDB model described above was indeed the best choice. First, the tanh (Z^+) empirical equation for the bulk temperature profile in the FDB regime was compared with the $[1-\exp(Z^+)]$ alternative equation suggested by Kroeger and Zuber [20]; the tanh (Z^+) was found to be clearly superior. Next the Levy empirical equation for the nonequilibrium quality [13] was tried in place of the Kroeger and Zuber equation (12). The results showed that the Kroeger and Zuber equation gave much better agreement with the experimental pressure drop data.

The weighted mean drift velocity, $\bar{u}_{gj}$, in equation (11) is normally set equal to zero for horizontal tubes. A number of horizontal tube runs were made including a nonzero $\bar{u}_{gj}$ and the effect was found to be negligible. This was due to the fact that the $\bar{u}_{gj}$ term was always very small compared to the C_o terms in the denominator of the nonequilibrium void fraction equation (11).

Finally, the homogeneous two-phase friction multiplier, $(\phi_{fo})^2$, used in the original SR-2F model [7] was replaced by the Martinelli–Nelson friction multiplier [22] to be more consistent with the separated flow model. However, this change had only a very slight effect on the predicted pressure drops because the acceleration pressure drop dominated the friction pressure drop contribution in the FDB regime.

ADDITIONAL TESTS OF THE MODEL

After careful validation of the new model using the selected experimental runs of Dormer and Bergles [1] for horizontal tubes, additional comparisons were made against other sets of experimental data. Figures 9–11 show comparisons with some of the experimental data of Owens and Schrock [2] in small diameter tubes with vertical upflow of water. These runs were made at much higher pressures than the Dormer and Bergles runs. The predicted pressure drops can be seen to be in good agreement with the data. This also constitutes a good test of equation (10) for the gravity contribution to the total pressure gradient.

Finally, we compared our predictions to the data of Reynolds [23] for the relatively large diameter horizontal tubes (inner diameter of 9.53 mm). A typical result is shown in Fig. 12, and the agreement can be seen to be reasonably good. (It should be noted that all of Reynolds experiments showed an unexplained discontinuity in the pressure profile around an axial distance of about 1.4 m.)

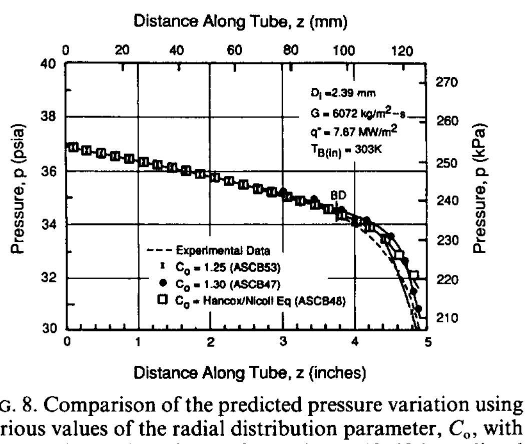

FIG. 8. Comparison of the predicted pressure variation using various values of the radial distribution parameter, C_o, with one experimental run from ref. [1], Fig. 11. (ONB is predicted to occur upstream of the inlet.)

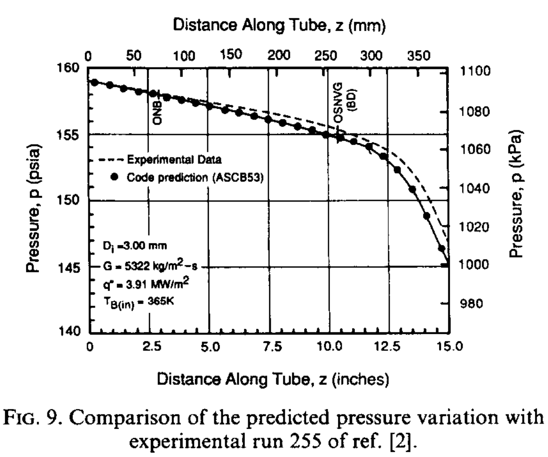

FIG. 9. Comparison of the predicted pressure variation with experimental run 255 of ref. [2].

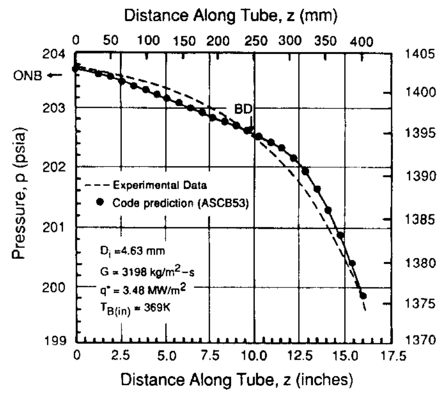

FIG. 10. Comparison of the predicted pressure variation with experimental run 210 of ref. [2]. (ONB is predicted to occur upstream of the inlet.)

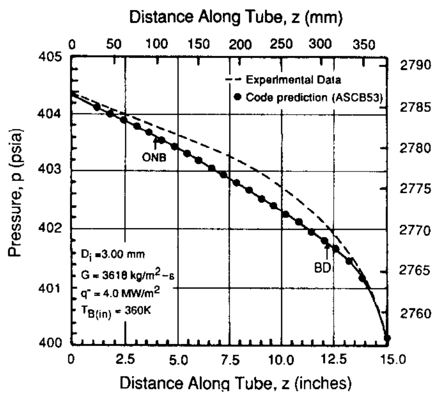

FIG. 11. Comparison of the predicted pressure variation with experimental run 253 of ref. [2].

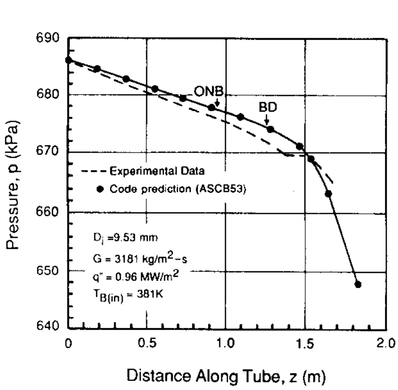

FIG. 12. Comparison of the predicted pressure variation with experimental run 129 of ref. [23].

RANGE OF VALIDITY OF THE FINAL MODEL ASCB53

The new code version ASCB53 now gives agreement with almost all the runs in the selected experimental data base to within $\pm 20\%$ with the exception of cases where the acceleration pressure gradient exceeds about 3 MPa m^{-1}. As mentioned in the previous section, the probable cause is that equation (11) seems to over-predict the nonequilibrium void fraction for the lower pressure cases (around 2×10^5 Pa), and the void fraction grows to very large values as OBB is approached. We have not yet found any simple correction for this problem, although many ideas were studied. For the present, the code warns users when this value of the acceleration pressure gradient of 3 MPa m^{-1} has been exceeded and cautions the user that the predicted pressure drop beyond that point may be too large. Further work on this problem is warranted.

The only other situation where the predicted pressure drops differed from the experimental values by more than $\pm 20\%$ occurred for some vertical tube runs with mass fluxes less than about 2500 kg m^{-2} s^{-1}. Since the model tends to overpredict the pressure in the SPL and PDB regimes for these cases, the code is only accurate for values of G greater than 2500 kg m^{-2} s^{-1}. However, most water cooling systems of interest for high heat flux applications employ higher mass fluxes than this value, so this should not seriously restrict the use of the code for the intended application.

In summary, the SCB pressure drop model has been validated over the following range of parameters for water flows in round tubes and was found to agree with our data base consisting of about 110 runs to within $\pm 20\%$:

$$q'' = 0\text{–}9.7 \text{ MW m}^{-2}$$
$$G = 2500\text{–}10\,000 \text{ kg m}^{-2} \text{ s}^{-1}$$
$$p_{\text{in}} = 0.2\text{–}2.8 \text{ MPa}$$
$$\Delta T_{\text{SUB(in)}} = 10\text{–}200^\circ\text{C}$$
$$D_{\text{i}} = 1.6\text{–}9.5 \text{ mm}$$
$$L/D_{\text{i}} = 49\text{–}192$$
$$[\text{d}p/\text{d}z]_{\text{accel}} < 3.0 \text{ MPa m}^{-1}.$$

The range of validity can be extended as more low-uncertainty pressure drop data in the subcooled boiling regime become available. However, even in the absence of such data, the cooling system designer can use this model for preliminary design purposes, since the model is based on a set of sound physical equations and well-validated empirical correlations. In any case, a good designer should recognize that the predictions must be checked by an experiment before commitment to the final design whenever the design parameters are outside the range of experience.

CHOICE OF A CRITICAL HEAT FLUX CORRELATION

After an examination and comparison of many subcooled boiling CHF correlations, the Gambill correlation [24] was chosen for use in the computer code. This correlation is based on one of the largest sets of data for uniform heat flux on tubes and channels of any of the CHF correlations examined. It should be noted that the Gambill correlation tends to be somewhat more conservative (i.e. to predict a lower CHF) than many of the other correlations examined.

One modification was applied to the Gambill CHF correlation. For tubes smaller than 8 mm in inner diameter, the Gambill CHF was modified as follows:

$$\mathrm{CHF} = \mathrm{CHF}_{(\mathrm{Gambill})} \sqrt{\left(\frac{0.008}{D_{\mathrm{i}}}\right)}. \qquad (18)$$

This modification is based on the correction suggested for the USSR Academy of Sciences CHF data [19] and is supported by CHF data of Bergles [25] on small diameter tubes.

CONCLUSIONS

A semi-empirical model of subcooled convective boiling of water in round tubes based on sound physical models and well-validated empirical correlations has been developed. The model has been compared to over one hundred experimental runs for water coolant in round tubes subject to uniform heat fluxes. The model has been found to predict the overall pressure drop for our data base to within $\pm 20\%$. The model has also been extended to nonuniform circumferential and axial heat fluxes. Work on these aspects will be reported in a separate paper.

Acknowledgements—This study was supported by a grant from Engineering Research at the Lawrence Livermore National Laboratory, Livermore, California, under Intramural Purchase Order No. 9222705.

REFERENCES

1. T. Dormer, Jr. and A. E. Bergles, Pressure drop with surface boiling in small diameter tubes, Technical Report No. 8767-31, M.I.T., Dept of Mech. Engng, Cambridge, Massachusetts (1964).
2. W. L. Owens and V. E. Schrock, Local pressure gradients for subcooled boiling of water in vertical tubes, ASME Paper No. 60-WA-249 (1960).
3. M. A. Hoffman, R. W. Werner, T. R. Roose and G. A. Carlson, Fusion reactor first-wall cooling for very high energy fluxes, *Nucl. Engng Des.* **36,** 37–46 (1976).
4. A. Mokhtarani, Analytical study of subcooled water cooling of nonuniformly heated tubes, M.S. Thesis, Dept of Mech. Engng, Univ. of California, Davis (1976).
5. C. T. Kline, Pressure drop estimation techniques for convective subcooled nucleate boiling, M.S. Thesis, Dept of Mech. Engng, Univ. of California, Davis (1984).
6. C. F. Wong, A computer code for calculating subcooled boiling pressure drop in forced convective tube flows, M.S. Thesis, Dept of Mech. Engng, Univ. of California, Davis (1988).
7. M. A. Hoffman and C. T. Kline, Evaluation of several empirical models for predicting subcooled flow-boiling pressure drops. In *Multiphase Flow and Heat Transfer*, ASME HTD-Vol. 47, pp. 151–160 (1985).
8. D. Jia and V. E. Schrock, A generalized procedure for predicting the pressure drop in a subcooled boiling channel, *Proc. 4th Int. Symp. on Multi-phase Transport and Particulate Phenomena*, Miami (1986).
9. *Heat Transfer and Fluid Flow Data Books.* Genium, Schnenectady, New York (1973).
10. A. E. Bergles and W. M. Rohsenow, The determination of forced convection surface boiling heat transfer, ASME Paper 63-HT-22, 6th Natn. Heat Transfer Conf., Boston (1963).
11. E. J. Davis and G. H. Anderson, The incipience of nucleate boiling in forced convection flow, *A.I.Ch.E. Jl* **12**(4), 774–780 (1966).
12. S. Z. Rouhani, Calculation of steam volume fraction in subcooled boiling, *ASME J. Heat Transfer* 158–165 (February 1968).
13. S. Levy, Forced convection subcooled boiling prediction of vapor volumetric fraction, *Int. J. Heat Mass Transfer* **10,** 951–965 (1967).
14. M. Hirata and N. Nishiwaki, Skin friction and heat transfer for liquid flow over a porous wall with gas injection, *Int. J. Heat Mass Transfer* **6,** 941–949 (1963).
15. R. Hino and T. Ueda, Study on heat transfer and flow characteristics in subcooled flow boiling—Part 1, *Int. J. Multiphase Flow* **11**(3), 269–281 (1985).
16. F. Inasaka, H. Nariai and T. Shimura, Pressure drops in subcooled flow boiling in narrow tubes, *Heat Transfer—Jap. Res.* **18**(1), 70–82 (1989).
17. P. Saha and N. Zuber, Point of net vapor generation and vapor void fraction in subcooled boiling, *Proc. 5th Int. Heat Transfer Conf.*, Japan, Vol. 4 (B4.7), pp. 175–179 (1974).
18. M. Shah, A general correlation for heat transfer during subcooled boiling in pipes and annuli, *ASHRAE Trans.* **83**(1), 202–216 (1977).
19. J. Collier, *Convective Boiling and Condensation*, 2nd Edn. McGraw-Hill, New York (1981).
20. P. Kroeger and N. Zuber, An analysis of the effects of various parameters on the average void fractions in subcooled boiling, *Int. J. Heat Mass Transfer* **11,** 211–223 (1968).
21. W. T. Hancox and W. B. Nicoll, A general technique for the prediction of void distribution in non-steady two-phase forced convection, *Int. J. Heat Mass Transfer* **14,** 1377–1394 (1971).
22. R. C. Martinelli and D. B. Nelson, Prediction of pressure drop during forced circulation boiling of water, *Trans. ASME* **70,** 695–702 (1948).
23. J. B. Reynolds, Local boiling pressure drops, Rpt ANL-5178, Argonne Natn. Lab. (1954).
24. W. R. Gambill, Generalized prediction of burnout heat flux for flowing, subcooled, wetting liquids, *Chem. Engng Progress Symp. Series* **59**(41), 71–137 (1963).
25. A. E. Bergles, Subcooled burnout in tubes of small diameter, ASME Paper 63-WA-182 (1963).