Improvement of the RELAP5-3D Condensation Heat Transfer Model in the Presence of Noncondensable Gases

Nolan Anderson
Idaho National Laboratory,
P.O. Box 1625,
Idaho Falls, ID 83415-1341
e-mail: nolan.anderson@inl.gov

Piyush Sabharwall
Mem. ASME
Idaho National Laboratory,
P.O. Box 1625,
Idaho Falls, ID 83415-3730
e-mail: piyush.sabharwall@inl.gov

Condensation of steam on the primary side of steam generator in a pressurized water reactor is one of the means of removing decay heat during accident scenarios such as a small break loss of coolant accident. With the presence of noncondensable gases, the rate of removal of decay heat reduces, affecting the ability of the nuclear plant to remove heat in accident scenarios. Therefore, correct prediction of heat removal capability is very significant to predict the plant behavior. In this study, an analytical model is compared with a numerical solution with the use of experiments performed at University of California, Berkeley and at MIT. A modified correlation is proposed and compared to experimental observation for various noncondensable gases.
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Introduction

For most convective heat transfer problems, the fluid is considered to be in a homogeneous single-phase system state, but there are cases involving boiling or condensation in which the fluid goes through a phase change during the convective heat transfer process. During condensation, the fluid changes from a vapor to liquid state, resulting in heat transfer to a solid surface. Due to the latent heat effect associated with the phase change, the convective heat transfer coefficients and rates are generally much higher than those of convective heat transfer processes without phase change.

Condensation of steam on the primary side of a steam generator in a pressurized water reactor is one means of removing decay heat during some accident scenarios, including small break loss-of-coolant accident. However, during these scenarios, noncondensable gases may mix with steam generated in the core and affect condensation. Some of these gases include dissolved gases in the primary coolant, dissolved nitrogen in the accumulators, and hydrogen generated by cladding reacting with water during an accident.

The presence of noncondensable gases in the condensation process has been shown to have an insulating effect on the heat transfer between the vapor/gas and the wall [1,2]. Further, when vapor condenses, noncondensable gas accumulates in the steam generator tubes, increasing their concentration and further decreasing the heat transfer during the condensation process. Eventually, the tubes become filled with noncondensable gases and cease to effectively remove decay heat. Therefore, to correctly predict plant behavior, it is important that the effect of condensation in the presence of noncondensables be modeled properly.

The pioneering work of Colburn–Hougen [3] demonstrated a method of calculating the effect that noncondensables can have on the condensation heat transfer process. Other methods have been developed for calculating the effects of steam condensation on the outer wall of a vertical tube in the presence of noncondensable gases. For example, in 2008, researchers [4] developed a steam condensation model for the outside of vertical tube geometries in the presence of noncondensable gases, such as air and helium. In 2014, another group [5] analyzed experiments for the effect of noncondensable gases on steam condensation over a vertical tube external surface under low wall subcooling. The reduced effectiveness of the condensation heat transfer due to the presence of noncondensables adversely affects the ability of a nuclear plant to remove heat in accident scenarios.

The RELAP5-3D code is a computational tool used extensively in the nuclear power industry that is primarily used for transient simulation of light water reactor coolant systems during postulated accidents. The code models the coupled behavior of the reactor coolant system and the core for loss-of-coolant accidents and operational transients. A generic modeling approach is used that permits simulation of a variety of thermal hydraulic systems [6].

Influence of Noncondensable—Analytical Solution

In the condensation process, there is an additional resistance due to the presence of noncondensable gases that needs to be overcome for vapor to condense. This resistance increases as the condensing vapor must diffuse to the cooled surface through a layer of gas, which reduces the convective heat transfer coefficient considerably, and thus reduces the effectiveness of the overall heat transfer process. The noncondensable gas is carried with the vapor toward the interface where it accumulates. The total pressure remains constant, but the local saturation conditions change (due to the reduction in the vapor partial pressure), which reduces the driving temperature difference. This effect is shown in Fig. 1 [7]. The temperature at the interface, $T_{g,i}$, refers to the saturation temperature equivalent to the partial pressure of vapor ($p_{g,i}$) at the interface.

Molar flux of noncondensable gas (J_a) passing through a plane parallel to and at a distance y from the interface is given in Ref. [7]

$$D\,\tilde{c}\,\frac{\partial C_a}{\partial y} + JC_a = J_a = 0 \tag{1}$$

Similarly, molar flux of vapor (J_g) can be written as

$$D\,\tilde{c}\,\frac{\partial C_g}{\partial y} + JC_g = J_g \tag{2}$$

where D is the diffusion coefficient, C_a (p_a/p) is the molar concentration of noncondensable gas, C_g (p_g/p) is the molar concentration of vapor, $\tilde{c}$ is the total molar concentration, and J is the drift flux of the bulk fluid toward the interface.

The total pressure remains constant, and the following relations can be used [7]:

$$C_g + C_a = 1 \tag{3}$$

$$\frac{\partial C_g}{\partial y} = -\frac{\partial C_a}{\partial y} \tag{4}$$

Substituting Eq. (4) into Eq. (2), the result is

$$D\,\tilde{c}\left(\frac{-\partial C_a}{\partial y}\right) + JC_g = J_g \tag{5}$$

or

$$J_g = -D\,\tilde{c}\left(\frac{\partial C_a}{\partial y}\right) + JC_g \tag{6}$$

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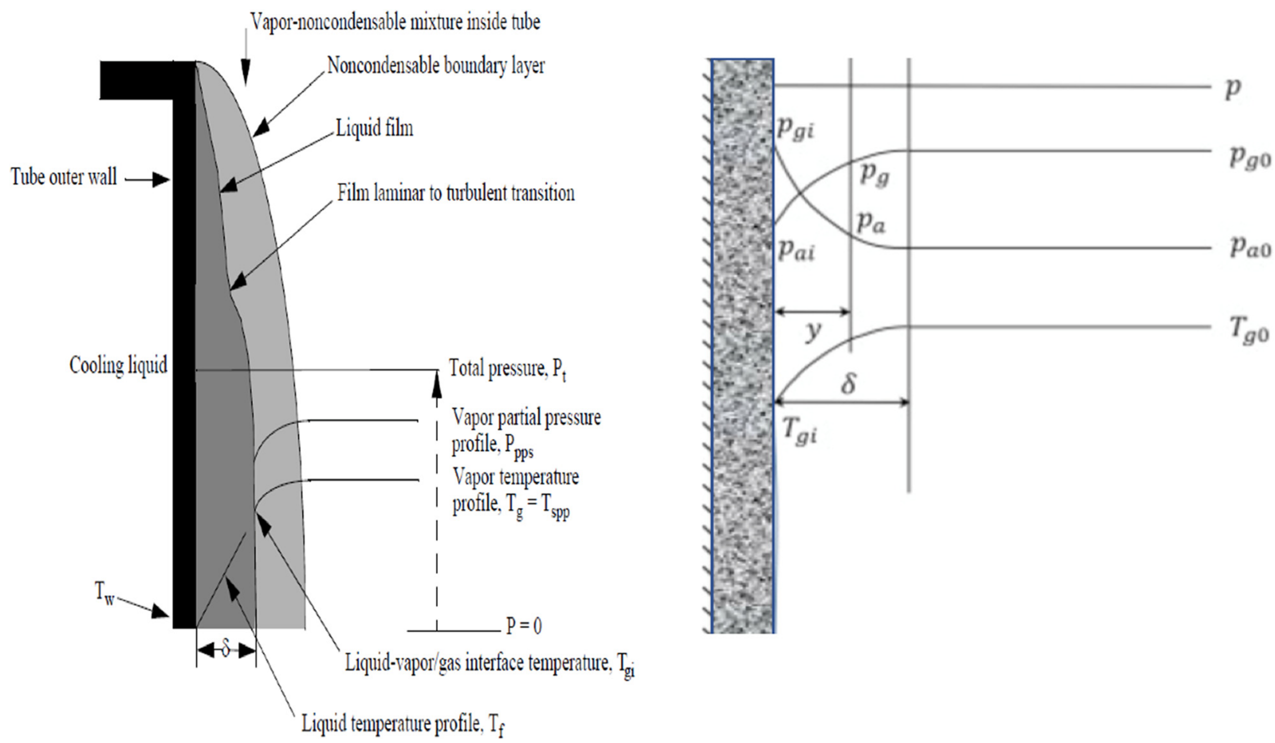

Fig. 1 Influence of noncondensable on interfacial resistance

Using Eq. (1) J can be eliminated

$$J_g = -D\,\tilde{c}\left(\frac{\partial C_a}{\partial y}\right)\left\{1 + \frac{C_g}{C_a}\right\} \tag{7}$$

Integrating between the interface ($y = 0$: $C_a = C_{a,i}$) and the edge of the diffusion layer ($y = \delta$: $C_a = C_{a,o}$)

$$J_g = \frac{D\,\tilde{c}}{\delta}\ln\left(\frac{C_{a,i}}{C_{a,o}}\right) \tag{8}$$

In terms of pressure

$$J_g = \frac{D\,\tilde{c}}{\delta}\ln\left(\frac{p_{a,i}}{p_{a,o}}\right) \tag{9}$$

where $p_{a,i}$ is the partial pressure of noncondensable gas at the interface, and $p_{a,o}$ is the partial pressure of noncondensable gas within the bulk mixture. Equation (9) can be rewritten in mass condensation flux j_g [7]

$$j_g = \frac{D}{\delta}\rho_{g,o}\ln\left(\frac{p_{a,i}}{p_{a,o}}\right) \tag{10}$$

j_g can be written in terms of the vapor pressures

$$j_g = h_m\,\rho_{g,o}\ln\left\{\frac{\left(1 - \frac{p_{g,i}}{p}\right)}{\left(1 - \frac{p_{g,o}}{p}\right)}\right\} \tag{11}$$

where h_m is the mass transfer coefficient, $h_m = (D/\delta)$, $\rho_{g,o}$ is the saturation vapor density at $p_{g,o}$, and ρ_m is the combined vapor and gas density in the bulk. The saturation vapor density $\rho_{g,o}$ can be related to the combined vapor and gas density in the bulk ρ_m by

$$\rho_{g,o} = (1 - X)\rho_m \tag{12}$$

where X is the noncondensable quality.

The heat flux due to condensation of vapor mass flux (q''_g) can be written as

$$q''_g = j_g \cdot h_{fg,b} \tag{13}$$

where $h_{fg,b} = h_{fg,\mathrm{sat}}(p_{g,o})$ is the vapor minus the saturation specific enthalpy based on vapor partial pressure in the bulk. Finally

$$q''_g = h_m\,\rho_{g,o}\,h_{fg,b}\ln\left\{\frac{\left(1 - \frac{p_{g,i}}{p}\right)}{\left(1 - \frac{p_{g,o}}{p}\right)}\right\} \tag{14}$$

Influence of Noncondensable—Numerical Solution

The University of Wisconsin [8] examined and compared the basic condensation models used by the MELCOR and RELAP5-3D codes. This examination found that RELAP5-3D should be corrected to properly account for the presence of noncondensables. The heat fluxes calculated from the two different code models were compared with AP600 test data. This comparison found that the RELAP5-3D model underestimated the condensation heat flux, but the results from MELCOR closely followed the data. The difference in the two models was found to lie primarily in the density used in the calculation of the vapor mass flux [8]. RELAP5-3D uses the variable $\rho_{g,o}$ and MELCOR uses the density of vapor at $T_{\mathrm{sat}}(p)$. Due to the results presented in Ref. [8], it was recommended that RELAP5-3D switch to the density used in MELCOR to calculate the vapor mass flux such that

$$\rho_{g,o} = \rho(T_{\mathrm{sat}}(p)) \tag{15}$$

The original RELAP5-3D source documents that describe the model implementation were investigated. The model description document [9] indicates that the saturation vapor density at the bulk vapor partial pressure ($\rho_{g,o}$) is used in the calculation of the heat flux due to condensation of vapor at the liquid–vapor interface. The implementation report indicates that the equation for the condensation heat flux comes from the second edition of Collier. However, in the third edition [7], the $\rho_{g,o}$ notation is not used. Instead, the density that is used is noted as ρ_g, which is interpreted as the density of the vapor at $T_{\rm sat}(p)$.

The derivation by Collier [7] indicates that this density should be used. In order to determine the effect of making the proposed change in the RELAP5-3D code, the alternative formulation for the density in the bulk provided in Collier was implemented and the RELAP5-3D calculated results for both methods were compared with the original experimental data.

The RELAP5 condensation model in the presence of noncondensables is compared with Eq. (14) derived above to evaluate the software solution. The effect of some of the most common noncondensable gases, helium, and air (a close approximation of nitrogen) are studied using RELAP5.

RELAP5 Input Models

RELAP5 input files are nodalized into volumes that are connected with junctions. Scalar information such as pressure, temperature, etc., are stored in the volumes. The junctions that connect the volumes handle momentum transfer between volumes such as mass flowrates, velocities, etc. Heat structures are primarily used to add or remove heat to the fluid in the volumes. A typical input nodalization is seen in Fig. 2. This nodalization is for the Christensen Test which is used in the RELAP5 Developmental Assessment [10].

Case Studies (UCB and MIT)

The UCB-Kuhn tests [11] are described in detail in Ref. [12]. The experiment is depicted in Fig. 3. The test section (condenser tube) was a vertical pipe with a downflow of steam at constant pressure and noncondensable mass fraction (for the tests with noncondensables). Cooling water was pumped upward through an annular jacket around the condenser tube to absorb energy.

The test section was instrumented with thermocouples to measure temperature and pressure transducers at the inlet and outlet. The purpose was to measure system pressure and mass flowrate. Some of the UCB-Kuhn tests were repeated to verify the results. It was found that many of the repeated tests fell outside of the original error bands, indicating that the error bands in the test were underestimated [12].

The RELAP5-3D models used for the original assessment [12] were recovered. The base input model was developed such that the volume center corresponds to the approximate location of a thermocouple. A heat structure was attached to the pipe wall with a convective boundary condition. The outer wall of the heat structure is set to a fixed temperature boundary condition, which uses the temperature values obtained from the thermocouple measurements. The inlet and outlet pressure conditions correlate to the measured system pressure, and the measured mass flowrate was specified at the top of the test section.

Various tests were run with varying values of system pressure, mass flowrate, wall temperature, and noncondensable mass fractions. The results for test cases 3.5-2 (air) and 5.2-3 (helium) are shown in Figs. 4 and 5. The inlet pressure for test case 3.5-2 was 0.2059 MPa and 0.4305 MPa for test case 5.2-3. The gas mass flowrates were 0.01661 and 0.01287 kg/s for test cases 3.5-2 and 5.2-3, respectively. The noncondensable mass fraction was 0.396 (air) and 0.00932 (helium) for the two test cases.

The analytical solution for the heat flux, Eq. (14), was also calculated independently using RELAP5 control variables. The RELAP5 control variables allow the user to independently calculate algebraic and ordinary differential equations which can be

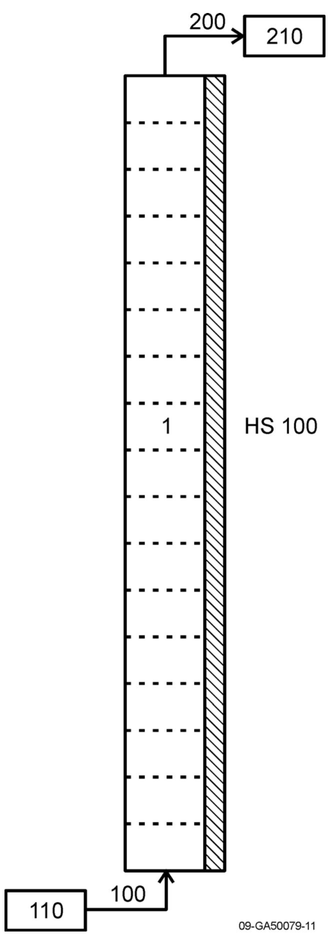

Fig. 2 Nodalization for Christensen test 15

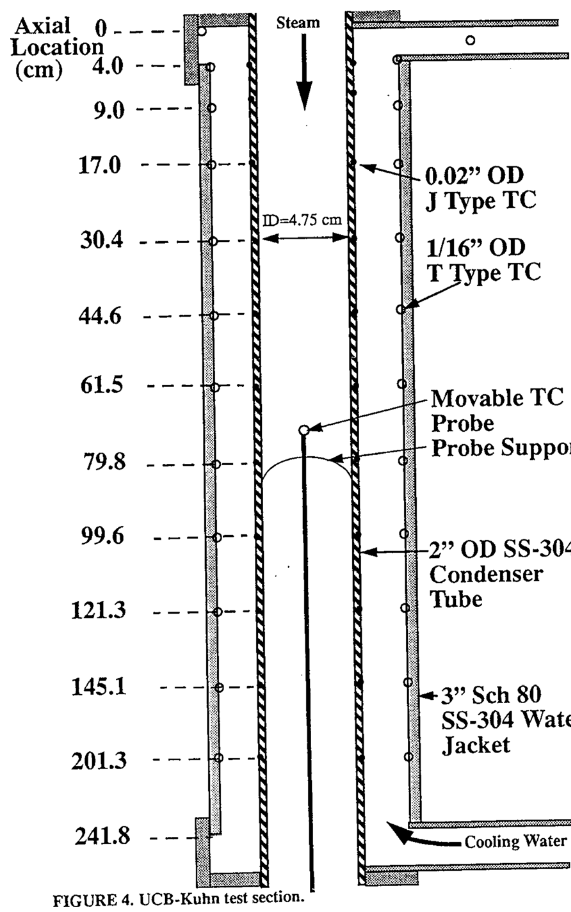

Fig. 3 UCB-Kuhn test

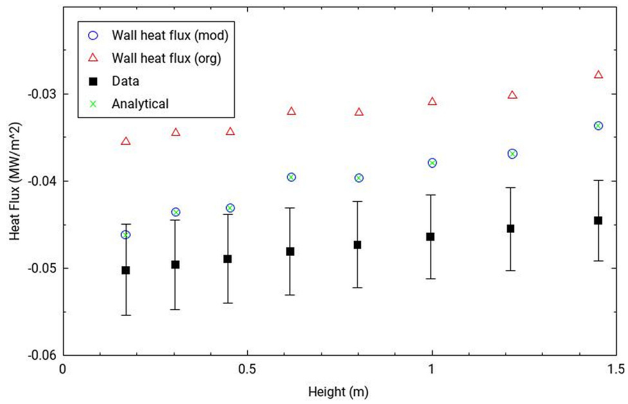

Fig. 4 Heat flux for Kuhn test 3.5-2, which used air as a noncondensable

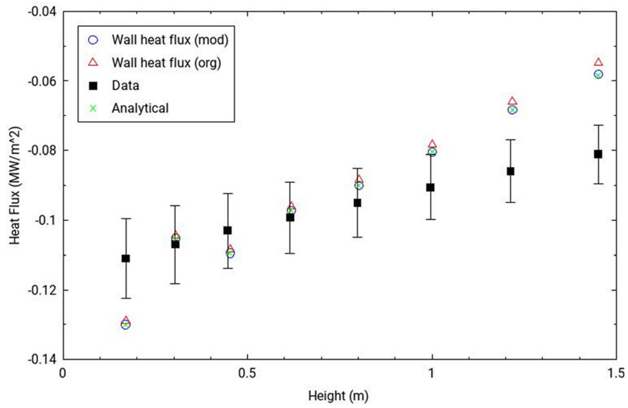

Fig. 5 Heat flux for Kuhn test 5.2-3, which used helium as a noncondensable

compared to code calculations. The mass transfer coefficient h_m is calculated as the maximum value predicted from a laminar-forced convection correlation, a turbulent-forced convection correlation, and a natural convection coefficient. The control variable calculated values are included in the plots. The analytical solution shows that the code-generated results and the control variable results were nearly identical.

For the UCB-Kuhn cases displayed here, the results are improved. However, this was not the case for all the UCB-Kuhn tests. Some of the test results showed worse results, in considering all of the results, there was little change in the results between the two models. For further information on the UCB-Kuhn test results see Ref. [13]

The MIT-Siddique tests [14] are described in detail in Ref. [12]. These tests are only summarized here. The MIT test setup was similar to the UCB-Kuhn tests. The experimental setup is shown in Fig. 6. The test section consisted of a downward flowing mixture of steam and noncondensable (either helium or air) that is cooled by a concentric cooling water jacket.

The MIT RELAP5 input decks were developed by modifying the UCB-Kuhn input decks for the different geometry and conditions. The results for two cases are included here for Tests 8A (air) and 18H (helium). The inlet pressure for test 8A was 0.217 MPa and 0.271 MPa for test 18H. The gas mass flowrates were 0.00299 and 0.00553 kg/s for test 8A and 18H, respectively. The noncondensable mass fraction was 0.137 (air) and 0.0731 (helium) for the two test cases. The results for Tests 8A and 18H are displayed in Figs. 7 and 8.

The results for test 8A are improved with the modified code. For test 18H, there is no obvious improvement to the solution based on visual observation. The results for the remaining tests

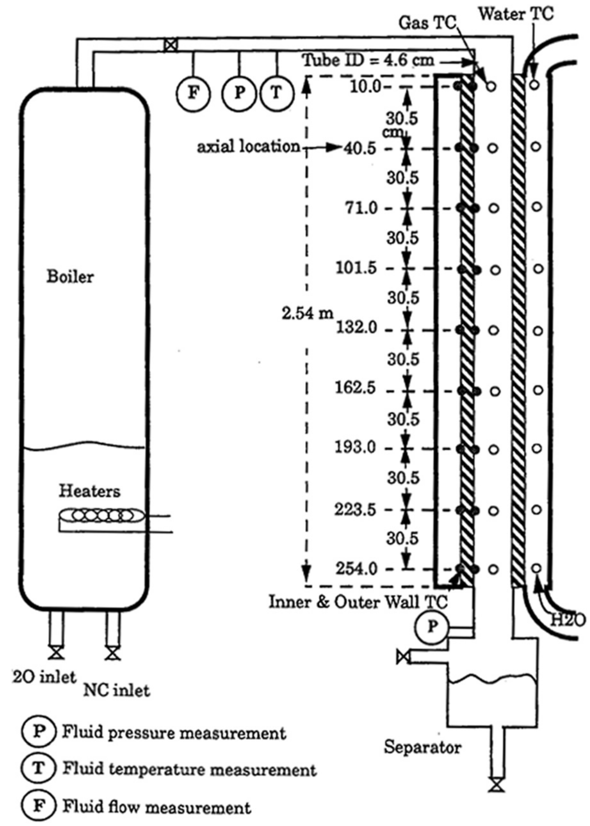

Fig. 6 MIT-Siddique experiment

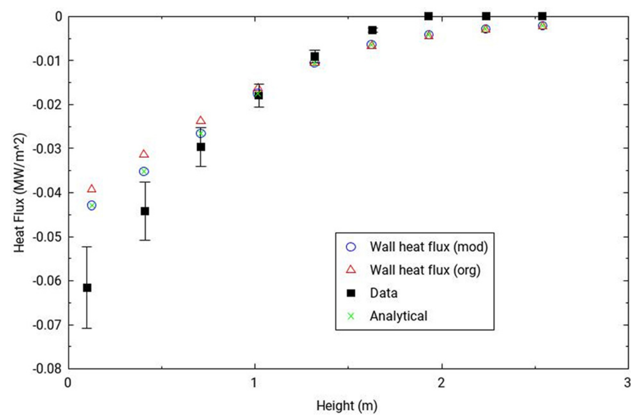

Fig. 7 Heat flux for MIT test 8A, which used air as a noncondensable

can be found in Ref. [13]. Overall the results were improved for the higher heat flux values, but worse for the lower heat fluxes.

Comparative Analysis and Discussion

Root-Mean-Square Analysis. The RELAP5-3D heat flux calculations were compared with the respective UCB-Kuhn and MIT-Siddique data for the cases. The data and calculations were compared with a root-mean-square (RMS) calculation. The root-mean-square gives an estimate of the amount of error in the prediction when compared with a dataset. The root-mean-square calculation used for this comparison is

$$\mathrm{RMS} = \sqrt{\frac{\sum_{1}^{N} \left| \frac{q''_{\text{predicted}} - q''_{\text{measured}}}{q''_{\text{measured}}} \right|^2}{N}} \quad (16)$$

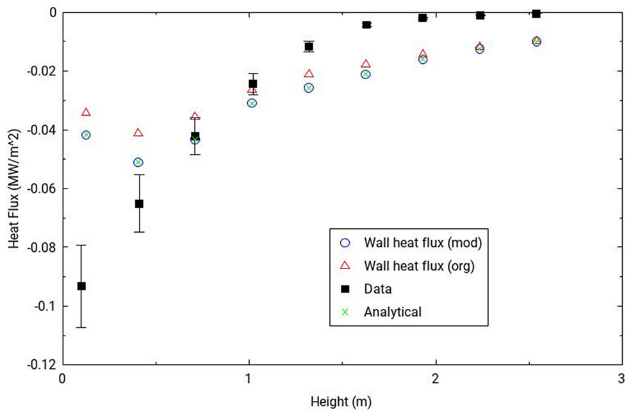

Fig. 8 Heat flux for MIT test 18H, which used helium as a noncondensable

Table 1 RMS comparison results for UCB-Kuhn and MIT-Siddique test data and RELAP5 predictions

RMS values				
	Kuhn test 3.5-2	Kuhn test 5.2-3	MIT test 8A	MIT test 18H
Modified	0.02775	0.02162	0.21265	0.36501
Original	0.10546	0.02679	0.27027	0.39750
% improved	73.69%	19.30%	21.32%	8.17%

Table 2 R^2 values for the calculations

R^2 values				
Kuhn test 3.5-2	Kuhn test 5.2-3	MIT test 8A	MIT test 18H	Total
∼1.0	0.999986	∼1.0	∼1.0	0.999994

where q'' is the heat flux, and N is the number of data points.

The values for RMS are given in Table 1. The calculated values for the original code version and the modified version are presented.

The results for the cases that were analyzed shows that the RMS is improved. The four data points below $5\,\text{kW/m}^2$ were removed from the RMS calculation for MIT test 18H because of large potential experimental errors as identified in the data report [14].

The results from the modified calculation show an 8–73% improvement. This gives further indication that the modified correlation should be implemented.

The values obtained with the modified RELAP5 computation and the independent analytical solution are also compared. This comparison was performed with an R^2 calculation. The R^2 value of a model shows the goodness of fit of a model. So a R^2 value of 1 indicates that the RELAP5 model perfectly fits the analytical solution. The lower the value of R^2, the less well the model fits the data. In this case, the RELAP5 solution is considered the "data" and the independent solution represents the model. The R^2 value is calculated using the following equation:

$$R^2 = \frac{\left[N\left(\sum xy\right) - \sum x \sum y\right]^2}{\left[N\sum x^2 - \left(\sum x\right)^2\right]\left[N\sum y^2 - \left(\sum y\right)^2\right]} \tag{17}$$

The results for the R^2 calculation are given in Table 2. The values obtained for the different cases and the total value for the sum of the four cases are presented.

The R^2 values show that the RELAP5-produced solution is essentially the same as the independent analytical solution. It is noted that the values calculated for the MIT cases fit the RELAP5 results more closely than the Kuhn results. This is attributed to the fact that in the case of the Kuhn test the flow was turbulent which required a different solution than the MIT test in which the flow was primarily in natural convection mode.

The results of the analytical solution show that the code is correctly calculating the modified equation as documented in Eq. (15). The results for the cases that were analyzed show that the modified model (15) better matches the experimental data, when compared to original model (12) for both sets of experiments that were analyzed using different noncondensable gases.

Conclusion

An error was reported that indicated that RELAP5-3D was using an incorrect density in the calculation of condensation heat flux in the presence of noncondensable gases; therefore, the model implementation documentation of RELAP5 was reviewed. Theory indicates that the density used in the calculation of the vapor mass flux should be the vapor density in the bulk at $T_{\text{sat}}(p)$, rather than the saturation vapor density at the vapor partial pressure. The alternate model was programmed, and its calculations were compared with some of the UCB-Kuhn and MIT-Siddique experimental data. For the UCB data, the alternative formulation produced lower RMS measures overall. For the MIT data, the alternative method produced lower RMS measures against data for the test cases investigated. Based on this work, the alternate formulation is added into the code.

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Funding Data

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Nomenclature

C_a = molar concentration of noncondensable gas
C_g = molar concentration of vapor
D = diffusion coefficient
$h_{fg,b}$ = vapor minus the saturation specific enthalpy based on vapor partial pressure in the bulk
$h_{fg,\text{sat}}$ = saturation specific enthalpy based on vapor partial pressure
J = mixture molar flux toward the interface
j_g = vapor mass flux
J_a = molar flux of noncondensable gas
J_g = molar flux of vapor
h_m = mass transfer coefficient
N = number of experimental data points
p = total pressure
p_a = partial pressure of noncondensable gas

$p_{a,i}$ = partial pressure of noncondensable gas at the interface
$p_{a,o}$ = partial pressure of noncondensable gas within the bulk mixture
p_g = partial pressure of the vapor
$p_{g,o}$ = vapor partial pressure in the bulk
$p_{g,i}$ = vapor partial pressure at the liquid–vapor/gas interface
q'' = heat flux
q''_g = heat flux due to condensation of vapor mass flux
$T_{g,i}$ = vapor temperature at the interface
X = noncondensable mass quality

Greek Symbols

δ = diffusion layer thickness
$\rho_{g,o}$ = saturation vapor density
ρ_m = combined vapor and gas density in the bulk at the bulk vapor/gas temperature

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