PROGRESS IN SYSTEM THERMOHYDRAULIC CODE

HYDRA-IBRAE/LM MODELS DEVELOPMENT FOR FAST REACTOR SIMULATION

V.M. ALIPCHENKOV, V.V.  BELIKOV, A.E. VLASENKO, YA.V. GRUDTSYN, I.N. DROBYSHEVSKAYA, N.A. MOSUNOVA, A.V. PALAGIN

Nuclear Safety Institute of the Russian Academy of Sciences (IBRAE RAN)

52, B.Tulskaya st., Moscow 115191, Russia

Email contact of corresponding author: palagin@ibrae.ac.ru

**Abstract**

New models of the system thermohydraulic code HYDRA-IBRAE/LM are presented. Since last (FR-17) conference the models for dispersed phase transport, post dryout heat exchange, lead coolant solidification as well as turbine model were developed and implemented in the code, the possibility of water behavior modeling at supercritical pressure was realized.

## INTRODUCTION

The system thermohydraulic code HYDRA-IBRAE/LM [1, 2] is designed for the simulation of non-stationary thermohydraulic processes in liquid metal and water circuits of fast reactors under normal operating conditions, anticipated operational occurrences and accidents. The code uses a two-fluid model in all flow regimes except for dispersed annular flow, where a three-fluid model is applied. Besides advanced mathematical models, the code has pre- and postprocessor and utility for performing multivariative calculations, uses MPI and OpenMP parallelization. The code is being developed in “Codes of New Generation” subproject of “Proryv” project. Water and steam properties are described according to the International Association for the Properties of Water and Steam recommendations (IAPWS-IF97) [3].

During years 2018-2020 new models were developed and implemented in the code:

* dispersed phase transport model;
* post dryout heat exchange model;
* turbine model;
* lead coolant solidification model.

Also the work was performed on providing the possibility of description of water behavior at supercritical pressure. The first three models are described in the present paper.

## Dispersed phase transport model

One of the most notable trends in the thermalhydraulic code development is the improvement of the disperse phase characteristics description. The correct determination of dispersed phase particle sizes or, in other terms, interfacial area concentration is necessary for the correct description of heat and mass exchange processes and interfacial friction as well as for predicting the distribution of the disperse phase in the liquid metal coolant circuit (disperse phase flow direction).

Three approaches are used to determine the size of dispersed particles: empirical correlation, transport equation for interfacial area concentration and multi-group (fraction) method. The first one corresponds to the dynamically equilibrium value depending on the parameters of the two-phase flow and channel geometry. The disadvantage of the approach is that an instantaneous change in diameter is assumed whereas the real relaxation time can be significant. Another approach is to solve an equation for the interfacial area concentration. It is implemented in the advanced thermalhydraulic codes (e.g. [4]). Interfacial area concentration is an important parameter using which interfacial mass, momentum and heat transfer rate could be determined. Such approach allows describing gradual change in sizes of dispersed phase particles, but frequently only one-group approximation is used. The last is not sufficient for a number of applications such as modeling of the phase separation in the downflow, when small bubbles are captured by the flow, and the bigger ones float up. More detailed description of an ensemble of interacting particles could be given on the basis of kinetic equation for particles size distribution function. For numerical solution of kinetic equation the group (fraction) method is widely used. Within the framework of this method all particles are divided into groups according to their sizes. For each group the system of equations with respect to concentration based on kinetic equation is written. These equations account for particles interactions (break-up, coalescence and other processes).

In the HYDRA-IBRAE/LM code all three mentioned above approaches for the determination of dispersed phase particles (bubbles) size in the flow of heavy liquid metal are implemented.

In the first approach the bubble diameter is determined by the following equation

|  |  |
| --- | --- |
| , | (1) |

where

|  |  |
| --- | --- |
| , | (2) |

is the turbulent energy dissipation rate; ; , ,  are superficial velocities of gas, fluid and mixture, correspondingly;  – fluid velocity; – mixture density;  – pressure drop due to friction;  – hydraulic diameter;; z – coordinate; σ – surface tension; g – acceleration due to gravity. Correlation (1) for turbulent flow was obtained by evaluation of experimental data on flow of water in round pipes. The performed analysis showed that it adequately describes the gas bubble diameter in a heavy liquid metal coolant as well. The advantage of this approach is the ability to perform quick calculations with satisfactory results if monodisperse distribution of particles prevails (for example, when a small break in the steam generator tube occurs and separate bubbles enter the flow of a heavy liquid metal) and for fully developed turbulent flow where the relaxation time is small. In other cases this approach can give the results that differ qualitatively from the real picture.

The second approach in the HYDRA-IBRAE/LM code is the transport equation for the interfacial area concentration in the following form [6]:

|  |  |
| --- | --- |
| , | (3) |

Here  is the interfacial area concentration;  is void fraction;  – gas phase velocity;  – rate of interfacial area concentration change due to coagulation of bubbles;  – rate of interfacial area concentration change due to break-up process;  accounts for mass source contribution. The notation corresponds to those used in [6]. Equation (3) describes bubble size variation in one-group approximation with its gradual adjustment to flow parameters change. This is the next step in more realistic modeling in comparison with (1).

The advantage of this approach in comparison with empirical correlation (1) can be demonstrated by the following simple test problem. The rise of injected bubbles in a column of liquid lead is considered. The hole through which gas enters the lead is located at the point 1 m from the bottom, mass flow rate – 2·10-4 kg/s. The vessel height is 16 m, inner diameter – 0.1 m. Temperatures of lead and gas are equal to 700 K. The diameter of bubbles versus height of the vessel obtained using the transport equation for the interfacial area (3) and correlation (1) is shown in Fig. 1.



1 – transport equation (3); 2 – correlation (1)

Fig. 1. Distribution of the bubble diameter along the height of the vessel

Fig. 1 demonstrates that with the use of the transport equation an increase in the bubble diameter during rising due to a decrease in pressure can be correctly modeled. Diameter rises as P-1/3 (isothermal process). The use of correlation (1) in this case leads to incorrect results.

Multi-group model is based on kinetic equation for particles size distribution function, and the group method is used for its solution. Corresponding finite-differences relations are described in [7]. The additional system of equation for the groups of particles and their momentum in every group was implemented to the HYDRA-IBRAE/LM code. The details can be found in [8]. Heterogeneous multi-group model allows describing processes, in which the bubble size distribution is important, e.g. downflow of heavy metal coolant where bubbles can be captured by the flow or move in the opposite direction depending on their size. And this model, as well as the model based on transport equation, accounts for finite bubble size relaxation time.

To demonstrate the advantages of this model, the following problem is considered. A vertical tube (internal diameter – 10 cm, height – 9 m) is filled with lead, flowing downward with velocity equal to 0.2 m/s. At the elevation 3.6 m gas bubbles are injected to the lead from two sources with mass flow rate 0.0002 kg/s for each source. Bubbles diameter from the first source is equal to 8 mm, second source – 1 mm. Void fraction distribution after 10 second of gas injections is shown in Fig. 2.

|  |  |  |
| --- | --- | --- |
|  |  | |
| *a)* | | *b)* | |

1. *Schematic representation of the task; b) Distribution of the volume fraction of gas along the tube, 10 seconds after the beginning of injection: 1 – calculation results using the multi–group model, 2 – calculation results using equation (3).*

Fig. 2. Gas bubbles transport in the lead downward flow

It can be seen that the multi-group model calculation results are more realistic. The bubbles flow downward and upward depending in their sizes, whereas according to (3), all bubbles are captured by the flow of liquid lead and move down.

## Post dryout heat transfer model

Post dryout heat transfer regime arises downstream from the point where liquid loses contact with hot wall and boiling crisis takes place. This regime corresponds to the normal operation conditions of FR steam generator. In such region a dispersed flow occurs – liquid droplets are dispersed in continuous steam flow. At the start point of post dryout regime temperatures of liquid and steam are equal to saturation temperature at a given pressure.

In post dryout region wall temperature rises sharply. Steam is in contact with the hot wall, and its temperature gradually grows along the channel. Temperature of liquid droplets is constant and equal to saturation temperature. Deposition of droplets from the flow core on the hot wall takes place.

Heat flux from the wall is given by the sum of heat fluxes to the steam and to the depositing droplets. At the same time heat transfer from the superheated steam to the droplets takes place. All the heat transferring to the droplets from the steam and from the wall goes to evaporation.

Due to evaporation and steam heating up the velocity of flow increases along the channel. Steam and droplet velocities can be considered to be close to each other.

Presented post dryout heat transfer model accounts for the described above specific features. The model is based on three closure relations for the heat fluxes (i) from the superheated steam to the droplets, (ii) from the hot wall to the depositing droplets, and (iii) from the hot wall to the steam.

All droplets are assumed to have the same size, droplet diameter at the post dryout region inlet is determined according to [9]

|  |  |
| --- | --- |
| . | (4) |

Here  – surface tension coefficient,  – density,  – void fraction, *u* – velocity,  – Reynolds number,  – viscosity, subscript ‘*g*’ stands for steam (gas phase), ‘*f*’ for fluid. As the liquid fraction decreases due to evaporation droplet diameter goes down.

For the heat flux from the steam to the droplets Nusselt number correlation proposed in [10] is used

|  |  |
| --- | --- |
| . | (5) |

( – Prandtl number, subscript ‘*s*’ means “at saturation temperature”). Since droplets are moving in the steam flow with the same velocity *u*, the corresponding value in Reynolds number  is determined not by *u*, but by its turbulent pulsation magnitude. The characteristic size of the problem is given by a combination of droplet and channel diameters.

Heat flux from the hot wall to the depositing droplets is determined by the heat transfer in the thin steam layer between droplet and wall surface. Steam flow in this layer is always laminar. Characteristic time of droplet-wall interaction (contact time) is close to a droplet free oscillation time [11], determined by its size.

Thus, the two of three closure relations were expressed in terms of flow parameters. The third flow relation for the heat flux from the wall to the steam was obtained using data from Becker experiments.

Becker tests on post dryout heat transfer in vertical round heated tube [12] were performed at the Royal Institute of Technology (Stockholm, Sweden). Tube parameters were: length 7 m, internal diameter 14.9 mm, outer diameter 20.8 mm. In the steady state phase of the test the temperature along the tube was measured (TC were located 10 cm from each other). Location where the temperature showed sharp change was identified as the dryout point.

For the present work of the most interest are the tests with the mass flowrate about 500 kg/(m2s) and pressure 3, 5, 7, 10, 12, 14, 16 MPa because these are the working parameters of the steam generator of demonstration reactor with the lead coolant developed in Russia. For each pressure several tests with heat flux density in the range 20–56 W/cm2 were performed, in total 27 tests. Such experimental data obtained at the same facility in wide parameters range are quite representative and could be the basis for the elaboration of post dryout heat transfer model.

Since liquid temperature is constant and equal to the saturation one, and droplets and steam velocities coincide, one can reduce the system of two-phase thermalhydraulic equations to the single equation with respect to quality. This equation corresponds to the enthalpy growth along the channel. In this equation the hot wall temperature appears as one of the parameters. Usage of experimental data [12] allows obtaining the solution of this equation for the conditions (pressure, inlet subcooling, power) of each test and perform calculation of the full set of flow thermalhydraulic values (void fraction, velocity, steam enthalpy).

With the use of these values the heat fluxes from the steam and the wall to the droplets can be found. The heat flux from the wall to the steam as the functions of the coordinate along the channel was calculated as the difference between the total heat flux from the wall (determined by heating power) and the above heat fluxes to the droplets. And the correlation for the corresponding Nusselt number was found using regression analysis methods.

In Fig. 3–6 the comparison of the calculation results with the experimental data [12] concerning tube wall temperature for different pressures and heat fluxes is presented. Average difference between the results of calculations obtained with the new model and experimental data of the Becker tests [12] in the 3-16 MPa pressure range and 500 kg/(m2s) mass flowrate does not exceed 5.3%.

|  |  |
| --- | --- |
|  |  |
| *Fig. 3.* *Test 157. Pressure 13.98 MPa, mass flowrate 502.2 kg/(m2·s), heat flux 20.5 W/cm2* | *Fig. 4. Test 199. Pressure 11.94 MPa, mass flowrate 502.0 kg/(m2·s), heat flux 25.2 W/cm2* |

|  |  |
| --- | --- |
|  |  |
| *Fig. 5. Test 201. Pressure 11.93 MPa, mass flowrate 501.8 kg/(m2·s), heat flux 35.6 W/cm2* | *Fig. 6. Test 246. Pressure 9,98 MPa, mass flowrate 501,9 kg/(m2·s), heat flux 48,9 W/cm2* |

## Turbine model

The turbine model describes thermodynamic processes taking place in a sequence of turbine stages as the thermal energy of superheated steam is converting to mechanical work: steam expansion, density and enthalpy decrease from stage to stage, entropy rise and liquid fraction formation. The model is based on universal thermodynamic relations connecting the values of enthalpy, entropy, density, mass fraction of water and pressure in turbine stages.

A sequence of real turbine stages located between two bleed points (where turbine steam is extracted to preheat the feedwater) is henceforth referred to as a (simulated) stage.

Based on the values of mass flowrate and effective hydraulic resistance coefficients  the pressure drops in the stages are determined:

|  |  |
| --- | --- |
|  | (6) |

( – effective cross-section area of a stage). Important point is that there is a significant change in the enthalpy and pressure at a stage. To find average density  one needs to know the average values of pressure and enthalpy at a stage, the later in turn can be calculated using pressure drop values. Here the iterative method is used: some expected average pressure values are specified, pressure drop values (6) and average values of pressure and enthalpy at a stage are calculated, average pressure values is corrected and the calculation is repeated required number of times. Making use of turbine outlet pressure  on the basic of obtained values of pressure drops the pressure at the turbine inlet  and the outlet pressure of each stage are calculated.

When operating with a nominal or reduced load, the steam enthalpy may appear to be lower the saturation value for the outlet pressure of some stages. That will lead to liquid fraction formation.

Water mass fraction at a stage outlet for the isentropic expansion (flow without irreversible losses) is given by

|  |  |
| --- | --- |
|  | (7) |

where  and are steam and water entropies at the saturation line at the pressure ,  is the entropy of working body at the stage  outlet.

The value of enthalpy at the isentropic expansion is determined by

|  |  |
| --- | --- |
|  | (8) |

Here  and  are steam and water enthalpies at the saturation line at the pressure , and  is the solution of the equation

|  |  |
| --- | --- |
| , | (9) |

where is the entropy as a function of pressure an enthalpy. Equation (9) is the mathematical formulation of the following problem: find such enthalpy that corresponds to the pressure  at the stage outlet and the value of entropy  at the stage inlet, i.e., for isentropic expansion. Direct solution of this equation with the use of water/steam properties database in accordance with IAPWS-IF97 [3] is an important advantage of the described model in comparison with other approaches, where various simplifying assumptions concerning steam density variation in the process of expansion are used.

The available heat drop is given by the difference

|  |  |
| --- | --- |
| . | (10) |

The real enthalpy change at the stage  corresponding to mechanical work is lesser due to irreversible losses. It can be determined using relative efficiency of the stage :

|  |  |
| --- | --- |
| . | (11) |

Real enthalpy at the stage outlet:

|  |  |
| --- | --- |
| . | (12) |

Real water mass fraction:

|  |  |
| --- | --- |
|  | (13) |

Entropy at the stage outlet:

|  |  |
| --- | --- |
|  | (14) |

The values of  (12) and  (14) are used in the relations similar to (7)–(13) in order to find working body parameters at the  stage.

Power output of the stage  is given by

|  |  |
| --- | --- |
| , | (15) |

and total turbine power developed

|  |  |
| --- | --- |
| , | (16) |

where  represents the irreversible losses.

In Fig. 7 relative load time dependence used in the test calculation is shown. In Fig. 8 the corresponding average density values at the stages of the turbine working with lead cooled fast reactor at different time moments are presented.

|  |  |
| --- | --- |
|  |  |
| *FIG. 7. Relative load time variation* | *FIG. 8. Working body average density in the turbine elements at different time moments* |

## Other improvements

The model for the description of lead coolant solidification was developed and implemented in the code. A solid lead layer (crust) formed on the channel wall can partially or completely block channel cross-section causing heat transfer drop and increase of local hydraulic resistance. Such situation can occur in reactor primary coolant system under emergency cooldown conditions, main circulation pump failure and the associated excessive heat removal by the secondary circuit. Variation of solid lead mass is calculated in accordance with Stefan condition as being proportional to the difference of heat flux in the crust and heat flux in the liquid. To account for the fast variations of channel cross-section area thermalhydraulic equations were modified and the new terms with time derivative of cross-section area were added. This model is described in the paper “Models of the integral EUCLID/V2 code for numerical modeling of different regimes of lead-cooled fast reactor” (paper #114 [13]) at the FR21 conference.

Also the work was performed on providing the description of water behavior at supercritical pressure by implementing the improved closure relations for wall friction and heat transfer [14]. This modification is important for correct modeling of the experiments with supercritical pressure in the water loop.

## Conclusion

The new models described in the present paper expand the code ability to describe processes and phenomena taking place in liquid metal and water circuits of fast reactors under normal operating conditions, anticipated operational occurrences and accidents. Based on solid physical grounds the new models allow perform the reactor safety assessment in more realistic approximations.

References

1. BOLSHOV L.A., STRIZHOV V.F., MOSUNOVA N.A., Codes of new generation for safety justification of power units with a closed nuclear fuel cycle developed for the “PRORYV” project, Nuclear Energy and Technology **6** (3) (2020) 203–214. <https://doi.org/10.3897/nucet.6.54710>
2. MOSUNOVA N.A., ALIPCHENKOV V.M., PRIBATURIN N.A., STRIZHOV V.F., USOV E.V., LOBANOV P.D., AFREMOV D.A., SEMCHENKOV A.A., LARIN I.A., Lead coolant modeling in system thermal-hydraulic code HYDRA-IBRAE/LM and some validation results. Nuclear Engineering and Design **359** (1104631) (2020). <https://doi.org/10.1016/j.nucengdes.2019.110463>
3. W.WAGNER and H.-J. KRETZSCHMAR, International Steam Tables, Properties of Water and Steam Based on the Industrial Formulation IAPWS-IF97, Second edition, Springer, 2008.
4. SALKO R.K., AVRAMOVA M.N., CTF Theory Manual. – CASL-U-2016-1110-000, The Pennsylvania State University, USA, 2016.
5. HIBIKI T., ISHII M., Interfacial area concentration of bubbly flow systems. Chemical Engineering Science **57** (2002) 3967–3977.
6. YAO W., MOREL C., Volumetric interfacial area prediction in upward bubbly two-phase flow. International Journal of Heat and Mass Transfer, **47** (2004) 307–328.
7. HOUNSLOW M.J., RYALL R.L., MARSHALL V.R., A discretized population balance for nucleation, growth, and aggregation. AIChE Journal, **34** (1988) 1821–1832
8. ALIPCHENKOV V.M., GRUDTSYN YA.V., MOSUNOVA N.A., Modeling of the gaseous phase transport in the heavy liquid metal coolant in HYDRA-IBRAE/LM system thermohydraulic code. Atomic energy, **129** (3) (2020) 134-141.
9. KATAOKA I., ISHII M., MISHIMA K. Generation and Size Distribution of Droplet in Annular Two-Phase Flow. Transactions of the ASME, Journal of Fluids Engineering, **105** (1983) 231-238.
10. WHITAKER S., Forced convection heat transfer correlations For flow in pipes, past flat plates, single cylinders, single spheres, and for flow in packed beds and tube bundles, AlChE Journal, **18** (2) (1972) 361–371.
11. BIANCE A.-L., CHEVY F., CLANET C., LAGUBEAU G., QUÉRÉ D. On the elasticity of an inertial liquid shock. J. Fluid Mech., **554** (2006) 47–66.
12. BECKER K. M., et al. An Experimental Investigation of Post Dryout Heat Transfer. KTH-NEL-33, Royal Institute of Technology, Stockholm, Sweden, May 1983.
13. ALIPCHENKOV V.M., BELOV A.A., BEREZNEV V.P., BOLDYREV A.V., BUTOV A.A., VEPREV D.P., DROBYSHEVSKAYA I.N., ILYASOVA O.KH., KLIMONOV I.A., KOLTASHEV D.A., KUDASHOV I.G., MOSUNOVA N.A., NAZAROV D.A., OZRIN V.D., PALAGIN A.V., STRIZHOV V.F., SOROKIN A.A., TARASOV O.V., TARASOV V.I., SYCHEVA T. V., USOV E.V., TSAUN S.V., CHUKHNO V.I., Mathematical models of the EUCLID/V2 integral code for the description of various modes of operation of reactor with lead coolant, International Conference on Fast Reactors and Related Fuel Cycles: Sustainable Clean Energy for the Future FR22 (Proc. Int. Conf., Beijing, China, 2022) Paper CN291-114.
14. KURGANOV V.A., ZEIGARNIK Y.A. AND MASLAKOVA I.V. On calculating heat transfer and pressure drop of supercritical-pressure coolants. Journal of Nuclear Engineering and Radiation Science, **2** (3) 031012 (2016).