Analysis of the CABRI-BI1 Loss-of-Flow Experiment Based on ISAA-Na

Authors: Tang Shaowei, Zhang Bin, Feng Siqi, Lei Yi, Shan Jianqiang, Zhang Bin

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Abstract

With the development of commercial applications of sodium-cooled fast reactors, there is a need to develop integrated analysis codes to enhance safety assessment capabilities. Sodium boiling is a critical phenomenon preceding severe accidents in sodium-cooled fast reactors, and accident analysis requires accurate prediction of coolant thermodynamic parameters during boiling. This paper presents the core thermal-hydraulic model of the ISAA-Na integrated analysis code for sodium-cooled fast reactors currently under development, and employs ISAA-Na to simulate and analyze the CABRI-BI1 loss-of-flow experiment. Comparison with experimental data demonstrates that ISAA-Na achieves higher accuracy than SAS4A, ASTEC-Na, and SIMMER in predicting coolant temperature and pressure prior to boiling. However, after boiling initiation, the absence of fuel melting and cladding failure models, and consequently the neglect of phase transitions in fuel and cladding, leads to overestimation of bubble growth velocity and two-phase interface propagation velocity. Overall, the multi-bubble liquid-slug boiling model implemented in ISAA-Na exhibits reasonable and reliable performance in the CABRI-BI1 experimental analysis.

Full Text

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TANG Shaowei[1], **ZHANG Bin**[1,2], **FENG Siqi**[1], **LEI Yi**[1], **SHAN Jianqiang**[1,2]

[1] School of Energy and Power Engineering, Xi'an Jiaotong University, Xi'an 710049, China

[2] State Key Laboratory of Multiphase Flow in Power Engineering, Xi'an Jiaotong University, Xi'an 710049, China

Abstract

[Background] As commercial deployment of sodium-cooled fast reactors (SFRs) advances, integrated analysis codes are needed to enhance safety assessment capabilities. [Purpose] Sodium boiling is a critical precursor phenomenon in SFR severe accidents, requiring accurate prediction of coolant thermodynamic parameters during boiling initiation. [Methods] This paper presents the core thermal-hydraulic models of the integrated SFR analysis code ISAA-Na under development, and applies ISAA-Na to simulate and analyze the CABRI-BI1 loss-of-flow experiment. [Results] Comparison with experimental data demonstrates that ISAA-Na predicts coolant temperature and pressure prior to boiling more accurately than SAS4A, ASTEC-Na, and SIMMER. However, after boiling onset, the absence of fuel melting and cladding failure models leads to overestimation of bubble growth rates and two-phase interface movement velocities. [Conclusions] Overall, the multi-bubble liquid slug boiling model implemented in ISAA-Na performs reasonably and reliably in the CABRI-BI1 experiment analysis.

Keywords: Sodium fast reactor, CABRI experiment, Loss of flow, ISAA-Na

1. Introduction

Sodium-cooled fast reactors possess inherent safety characteristics due to sodium's favorable thermophysical properties, making them one of the Generation IV advanced reactor types. According to China's "three-step" development strategy of thermal reactors, fast reactors, and fusion reactors, developing breeder and transmutation reactors represented by fast reactors is crucial for addressing national energy challenges.

As SFR technology progresses, new methods employing advanced computational tools and techniques are urgently needed to analyze SFR safety and severe accident phenomena. Historically, limited computer performance and incomplete understanding of complex coupled phenomena during accidents constrained SFR computational analysis to mechanistic and specialized codes such as the spray sodium fire code SOFIRE, pool sodium fire code NACOM, sodium-cooled fast reactor transient analysis code SAS4A, and SFR disassembly accident analysis code SIMMER. To reduce uncertainties in SFR severe accident analysis and more accurately quantify reactor safety margins, countries planning to deploy SFRs have initiated development of integrated analysis codes, including Europe's ASTEC-Na, Russia's EUCLID, and the United States' BRISC. These codes are currently under development with the ultimate goal of achieving full-scope, full-process analysis and simulation of SFRs using integrated codes.

To meet China's future SFR safety analysis requirements, the sodium-cooled fast reactor version of ISAA (Integrated Severe Accident Analysis code), designated ISAA-Na, is currently under development. The ISAA-Na development plan encompasses reactivity models, thermal-hydraulic models, fuel rod thermomechanical models, core melting and relocation models, debris bed cooling

models, and sodium fire models.

Two-phase thermal-hydraulic behavior of sodium is critical for SFR severe accident analysis. Sodium boiling typically occurs during the initial phase of core disassembly accidents, and the velocity and pressure fields developed during this stage directly influence subsequent molten material relocation, making their prediction highly significant. This paper introduces the core channel two-phase model within the ISAA-Na thermal-hydraulic framework. The pre-boiling single-phase liquid slug employs one-dimensional heat conduction and flow equations, while post-boiling bubbles are categorized as either small or large bubbles with different models solving for their thermodynamic parameters. The liquid slugs between bubbles are treated using the same heat transfer and flow equations as the pre-boiling single-phase liquid slug. The code is then applied to model and calculate the CABRI-BI1 loss-of-flow experiment, with results compared against experimental data and other code predictions.

2. ISAA-Na Core Channel Thermal-Hydraulic Model

2.1 Overview

In severe accident analysis for light water reactors (LWRs), ISAA simulates the entire core by dividing it into multiple radial rings. ISAA-Na follows this approach but employs a channel modeling method for each radial ring, utilizing more sophisticated nodalization schemes within each channel as needed to meet the higher accuracy requirements of SFR accident analysis. Each channel represents a fuel pin, its associated coolant, and a portion of the subassembly duct wall. The channel is divided into seven regions: coolant, reflector, structure, plenum, cladding, blanket, and fissile column.

Core thermal-hydraulic calculations in ISAA-Na are performed within this framework. Extensive sodium boiling experimental results indicate that during boiling, a small bubble first forms in the channel, which then grows continuously and leaves liquid films on cladding and structures. Simultaneously, when fuel rod temperatures become sufficiently high, additional bubbles form at other locations. These bubbles may either collapse or coalesce into larger bubbles. When wall surfaces dry out, the cladding exchanges heat directly with vapor.

Based on these phenomena, ISAA-Na's multi-bubble slug boiling model was developed, as illustrated in [Figure 1: see original paper]. Prior to boiling, all coolant in the channel is treated as a single liquid slug, with temperatures and pressures calculated for each mesh within this slug. When coolant temperature in any mesh exceeds the saturation temperature by a superheat value of 10 K, boiling is considered to initiate—this constitutes the initial condition for bubble formation. Bubbles are classified as small bubbles when their length is less than the mesh length or a specified minimum length; for small bubbles, the internal vapor pressure is assumed uniform and the vapor is treated as a single entity. Otherwise, bubbles are considered large bubbles with internal vapor pressure gradients, where vapor bubbles at different mesh positions have

different thermodynamic parameters. The liquid between bubbles is treated as a liquid slug, employing the same heat transfer and flow equations as the pre-boiling single-phase liquid slug.

2.2 Single-Phase Heat Transfer Equations

The fundamental transient heat transfer equation for fuel in ISAA-Na is:

$$\rho c \frac{\partial T}{\partial t} = \frac{1}{r} \frac{\partial}{\partial r} \left(rk \frac{\partial T}{\partial r} \right) + Q$$

where T is temperature (K), ρ is density ($\mathrm{kg \cdot m^{-3}}$), c is specific heat ($\mathrm{J \cdot kg^{-1} \cdot K^{-1}}$), r is radius (m), k is thermal conductivity ($\mathrm{W \cdot m^{-1} \cdot K^{-1}}$), t is time (s), and Q is volumetric heat source ($\mathrm{W \cdot m^{-3}}$). This heat transfer equation also applies to cladding and structures.

For coolant, the fundamental heat transfer equation is:

$$\rho_c c_c A_c \frac{\partial T_c}{\partial t} = -\frac{\partial}{\partial z}(w c_c T_c) + Q_c A_c + Q_{ec} + Q_{sc}$$

where ρ_c is coolant density ($\mathrm{kg \cdot m^{-3}}$), c_c is coolant specific heat ($\mathrm{J \cdot kg^{-1} \cdot K^{-1}}$), T_c is coolant temperature (K), A_c is coolant flow area ($\mathrm{m^2}$), w is coolant mass flow rate ($\mathrm{kg \cdot s^{-1}}$), Q_c is heat generated directly in the coolant ($\mathrm{W \cdot m^{-3}}$), Q_{ec} is heat flux from cladding to coolant ($\mathrm{W \cdot m^{-3}}$), and Q_{sc} is heat flux from structure to coolant ($\mathrm{W \cdot m^{-3}}$). Q_{ec} and Q_{sc} are calculated identically; using Q_{ec} as an example:

$$Q_{ec} = h_c S_e (T_e - T_c)$$

where T_e is cladding outer surface temperature (K) and S_e is cladding perimeter (m). The coolant heat transfer coefficient employs a semi-empirical correlation derived by Lyon, suitable for low Prandtl number convection:

$$h_c = \frac{k}{D_h} \left(7 + 0.025 \mathrm{Pe}^{0.8} \right)$$

where h_c is coolant heat transfer coefficient ($\mathrm{W \cdot m^{-2} \cdot K^{-1}}$), D_h is hydraulic diameter (m), k is coolant thermal conductivity ($\mathrm{W \cdot m^{-1} \cdot K^{-1}}$), Pe is Peclet number, and Pr is Prandtl number.

During transient calculations, these heat transfer equations are discretized in space and time. Equations for nodes at the same elevation are solved simultaneously using a semi-implicit scheme with Gaussian elimination to obtain temperatures for all nodes in fuel, cladding, coolant, and structures.

2.3 Single-Phase Flow Equations

ISAA-Na's single-phase flow equations solve for coolant flow rate and pressure distribution within the channel. Transient hydraulic calculations require specification of coolant inlet/outlet boundary conditions, such as pressure or coolant flow rate. Prior to bubble formation in the channel, coolant is assumed incompressible, with the fundamental momentum equation:

$$\frac{\partial p}{\partial z} = -\frac{f\rho_c v^2}{2D_h} - K\frac{\rho_c v^2}{2} - \rho_c g$$

where p is pressure (Pa), z is axial position (m), v is coolant velocity ($\mathrm{m}\cdot\mathrm{s}^{-1}$), and g is gravitational acceleration ($\mathrm{m}\cdot\mathrm{s}^{-2}$). The right-hand side terms represent frictional pressure drop, form pressure drop, and gravitational pressure drop, respectively.

Frictional pressure drop is calculated using a correlation for annular channels with 3% uncertainty:

$$f = \begin{cases} 0.316\mathrm{Re}^{-0.25} & \mathrm{Re} > \mathrm{Re}_L \\ \frac{64}{\mathrm{Re}} & \mathrm{Re} \le \mathrm{Re}_L \end{cases}$$

where Re is Reynolds number and Re_L is the Reynolds number for transition from turbulent to laminar flow.

Integrating the momentum equation over the coolant channel length yields an equation for coolant flow rate variation, which is solved using a semi-implicit scheme to obtain flow rate changes within a time step. Pressure at each coolant node is then calculated from these flow rate changes to obtain the final pressure distribution.

2.4 Small Bubble Model

ISAA-Na employs the small bubble model based on Cronenberg's research. For small bubbles, energy conservation yields:

$$E_{es} + E_i = \Delta E$$

where E_{es} is energy from cladding and structures (J), E_i is energy from the vapor-liquid interface (J), and ΔE is the change in bubble internal energy (J), used to raise vapor temperature and generate new vapor. The cladding and structure term can be approximated as:

$$E_{es} = \int_{z_l}^{z_h} (Q_e + Q_s)\, dt = \int_{z_l}^{z_h} (P_e q_e + P_s q_s)\, dz$$

where Q_e is heat flux from cladding (W), Q_s is heat flux from structure (W), z_l is lower bubble interface elevation (m), z_h is upper bubble interface elevation (m), P_e is cladding perimeter (m), P_s is structure perimeter (m), q_e is heat flux density from cladding to vapor ($\mathrm{W \cdot m^{-2}}$), and q_s is heat flux density from structure to vapor ($\mathrm{W \cdot m^{-2}}$). Heat flux density is calculated using Newton's law of cooling.

Since convective heat transfer coefficient calculations for vapor and liquid films are complex, the model assumes that if cladding is more than 100 K hotter than vapor, the liquid film is assumed to be at vapor temperature (equivalent to neglecting vapor thermal resistance). If cladding is more than 100 K cooler than vapor, the liquid film is assumed to be at cladding temperature (neglecting film thermal resistance). For intermediate temperatures, interpolation is applied. The final heat transfer coefficient is calculated as:

$$h_{ec} = \frac{1}{\frac{w_{fe}}{k_l} + \frac{1}{h_{cond}}} \exp\left(-\frac{w_{fe}}{k_l} h_{cond}\right)$$

where h_{ec} is the combined heat transfer coefficient accounting for liquid film and vapor thermal resistances ($\mathrm{W \cdot m^{-2} \cdot K^{-1}}$), k_l is liquid film thermal conductivity ($\mathrm{W \cdot m^{-1} \cdot K^{-1}}$), w_{fe} is liquid film thickness (m), and h_{cond} is condensation heat transfer coefficient ($\mathrm{W \cdot m^{-1} \cdot K^{-1}}$).

Energy from the vapor-liquid interface is the sum of upper interface term I_{iu} and lower interface term I_{il}:

$$I_{ix} = \int_{z_l}^{z_h} h_c P_c (T_l - T_{sat}) \exp\left(-\frac{\xi}{\delta}\right) d\xi$$

where x represents u (upper interface) or l (lower interface), ξ is axial distance to the interface (m), T_l is liquid temperature near the interface, and δ is a characteristic length.

Heat flow into the bubble control volume is used both to generate new vapor and to raise existing vapor temperature. During time step Δt, vapor temperature changes from T to $T + \Delta T$, pressure from p_v to $p_v + \Delta p$, density from ρ_v to $\rho_v + \Delta \rho_v$, and bubble volume from V_v to $V_v + \Delta V$. The vapor energy change can be expressed as:

$$\Delta E = \rho_v V_v \left(c_{p,v} \Delta T + \lambda \frac{\Delta \rho_v}{\rho_v}\right) + \Delta \rho_v V_v \lambda$$

where λ is latent heat of vaporization ($\mathrm{J \cdot kg^{-1}}$).

Substituting the energy terms into the conservation equation and solving yields bubble temperature, from which internal bubble pressure is obtained using saturation conditions.

2.5 Large Bubble Model

In ISAA-Na's large bubble model, each node has different thermodynamic parameters. Assuming saturation conditions, the vapor energy equation can be combined with mass continuity and solved simultaneously for vapor continuity and momentum equations at each bubble node using a fully implicit finite difference scheme. The vapor continuity equation is:

$$\frac{\partial \rho_v}{\partial t} + \frac{\partial}{\partial z}(\rho_v v_v) = \frac{Q_v}{\lambda}$$

The vapor momentum equation is:

$$\frac{\partial}{\partial t}(\rho_v v_v) + \frac{\partial}{\partial z}(\rho_v v_v^2) = -\frac{\partial p_v}{\partial z} - \frac{f_v \rho_v v_v^2}{2D_h} - F_c$$

where w_v is vapor mass flow rate ($\mathrm{kg \cdot s^{-1}}$), Q_v is volumetric heat flux in vapor ($\mathrm{W \cdot m^{-3}}$), f_v is vapor friction factor, F_c is condensation momentum loss term ($\mathrm{Pa \cdot m^{-1}}$), and k_{or} is orifice pressure loss coefficient.

Volumetric heat flux calculation is similar to the small bubble model, but energy from the interface is added only to meshes near the interface. The model assumes momentum is lost when vapor condenses but not when liquid evaporates, so the condensation momentum loss term is decomposed into contributions from cladding and structures:

$$F_c = \frac{Q_e}{A_v \lambda} v_v + \frac{Q_s}{A_v \lambda} v_v$$

The vapor friction factor is calculated as:

$$f_v = \begin{cases} 0.316 \mathrm{Re}_v^{-0.25} & \mathrm{Re}_v > 2000 \\ \frac{64}{\mathrm{Re}_v} & \mathrm{Re}_v \leq 2000 \end{cases}$$

where Re_v is vapor Reynolds number.

After discretizing the equations using finite differences, advanced time terms are linearized. Assuming saturation conditions restricts the solution variables to mass flow rate changes and pressure changes. Solving these yields the pressure distribution within the bubble, from which sodium vapor temperature and other thermodynamic parameters are calculated.

3. CABRI-BI1 Experiment Simulation

3.1 CABRI-BI1 Experiment Description

The CABRI facility is a pool-type research reactor that investigates accident phenomena by observing the behavior of a single fuel pin under transient conditions, as shown in [Figure 2: see original paper]. The BI1 experiment was a loss-of-flow test conducted in the CABRI facility using a mixed-oxide test fuel pin irradiated in the PHENIX reactor with a maximum burnup of 1 at.%.

During the BI1 experiment, steady-state conditions were first established at a peak linear power of 662 W/cm. While maintaining constant power, coolant inlet flow rate was gradually reduced starting at t = 0 s, causing coolant boiling in the channel at approximately 20 s. Bubbles then increased progressively, with the two-phase interface extending upward and downward. Cladding failure occurred at approximately 25.8 s, followed by fuel melting and ejection into the channel. The reactor was subsequently shut down, and the experiment terminated at approximately 30 s.

3.2 Numerical Modeling of the BI1 Experiment

Fuel pin parameters for the BI1 experiment are listed in , with initial and boundary conditions provided in , sourced from reference [21]. BFC denotes Bottom of the Fissile Column, and TFC denotes Top of the Fissile Column.

The CABRI-BI1 experiment was modeled using ISAA-Na with nodalization shown in [Figure 3: see original paper]. ISAA-Na meshing is case-specific; for this study, a 20$\times$10 mesh was employed in the fissile fuel region with large temperature gradients, while only a single radial node was used in the fission gas plenum where temperature gradients are small. The reflector and other regions are omitted from the figure. [Figure 4: see original paper] shows the axial power factor distribution, featuring a noticeable dip at 60 cm caused by gaps between fuel pellets resulting from transportation from PHENIX to CABRI.

3.3 Simulation of the CABRI-BI1 Experiment

The CABRI-BI1 simulation consists of steady-state and transient phases. The steady-state calculation aims to replicate experimental conditions prior to transient initiation. Using the boundary conditions from , steady-state results are presented in [Figure 5: see original paper], [Figure 6: see original paper], and [Figure 7: see original paper], which include comparisons with SIMMER, ASTEC-Na, and SAS4A results from references [21-22] (SIMMER results from JRC, ASTEC-Na results from ENEA).

[Figure 5: see original paper] compares steady-state coolant pressure drop, with all four code results showing good agreement. [Figure 6: see original paper] presents steady-state axial cladding temperature distributions; SAS4A and ISAA-Na, which modeled the fuel gap at 60 cm, show corresponding temperature dips at this location. [Figure 7: see original paper] displays steady-

state axial coolant temperature distributions. All four codes accurately predict temperature rise in the fissile region, but only ISAA-Na and SAS4A correctly simulate the temperature plateau above the blanket region. SIMMER overestimates this plateau by approximately 25 K, while ASTEC-Na shows a decreasing coolant temperature trend.

Transient calculations begin at t = 0 s, with the loss-of-flow transient implemented by varying inlet flow rate according to:

$$\dot{m}(t) = \dot{m}_0 \left(1 - \frac{t}{\tau}\right) \quad \text{with} \quad \tau = 7.8 - 8\text{s}$$

ISAA-Na calculations use $\tau = 7.8$ s.

4. Results and Discussion

4.1 Inlet and Outlet Flow Rates

[Figure 8: see original paper] and [Figure 9: see original paper] compare inlet and outlet coolant flow rates (volumetric flow). Boiling begins in the test section at approximately 20 s, causing outlet flow oscillations starting at 20 s and inlet flow oscillations beginning at 22 s. All four codes accurately predict boiling initiation, but ISAA-Na and ASTEC-Na show faster flow rate decreases at boiling onset, while SIMMER and SAS4A results better match experimental values during the 20-22 s period.

4.2 Coolant Temperature and Pressure

Axial coolant temperature distribution before boiling is shown in [Figure 10: see original paper]. Among the four codes, only ISAA-Na simultaneously predicts both the coolant temperature peak and the temperature distribution in the upper fuel region. SIMMER overpredicts coolant temperature, while SAS4A and ASTEC-Na underpredict it.

[Figure 11: see original paper] and [Figure 12: see original paper] present coolant temperature evolution at 77 cm and 39 cm, respectively. The 77 cm location is where boiling initiates experimentally, so coolant temperature reaches saturation immediately after boiling onset. The thermocouple measurement spike at 25 s results from fuel ejection after cladding failure, which affects data acquisition. Except for SIMMER, all codes show good agreement with experimental values. SIMMER's higher predicted channel pressure after boiling leads to higher corresponding saturation temperature, as shown in [Figure 13: see original paper]. At 39 cm, ISAA-Na and ASTEC-Na overpredict post-boiling temperature rise rates, while SIMMER underpredicts this rate, with SAS4A showing the closest match to experimental data.

4.3 Two-Phase Interface Position

[Figure 14: see original paper] shows the two-phase interface evolution in the BI1 experiment. TC denotes thermocouple measurements, and VD denotes void detector measurements. Code-calculated two-phase interfaces refer to the highest and lowest bubble interfaces in the channel. Compared with experimental data, ISAA-Na overestimates the upward and downward movement velocity of the two-phase interface, while SAS4A underestimates it. ASTEC-Na shows inaccurate prediction of the upper interface position, and SIMMER results are closest to experimental values. Since ISAA-Na currently does not model fuel melting and cladding failure, all energy generated in the core is used to increase coolant enthalpy rather than being partially consumed by fuel and cladding phase changes, resulting in excessive post-boiling coolant temperature rise rates (as shown in [Figure 12: see original paper]) and accelerated two-phase interface movement.

5. Conclusions

To enhance SFR severe accident analysis capabilities, the integrated sodium-cooled fast reactor analysis code ISAA-Na has been developed. Based on observations from sodium two-phase boiling experiments, a multi-bubble slug boiling model was developed for ISAA-Na, building upon single-bubble sodium boiling models to calculate thermal-hydraulic phenomena in the SFR core. The model was applied to simulate the CABRI-BI1 experiment.

Comparisons of ISAA-Na results with experimental data and other code calculations demonstrate that ISAA-Na accurately predicts boiling onset time and location, as well as pre-boiling coolant temperature and pressure. However, due to the absence of fuel melting and cladding failure models, predictions of post-boiling two-phase interface movement are less accurate.

Future ISAA-Na development will continue to simulate additional sodium boiling experiments to further validate the core thermal-hydraulic model, while implementing a transient fuel thermomechanical module with fuel melting and cladding failure models to improve simulation capabilities for sodium boiling experiments.

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Note: Figure translations are in progress. See original paper for figures.