Study on Heat Transfer Effects of Inner Barrier Failure in Heat Exchanger Tubes of Emergency Salt Drainage Cooling Systems for Molten Salt Reactors

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Abstract

Liquid fuel molten salt reactors represent one of the candidate reactor types for Generation IV advanced nuclear energy systems, with emergency salt drainage cooling systems constituting a special safety feature that may be implemented. This study investigates the impact of inner barrier damage in heat transfer elements on their normal residual heat removal function, using the emergency salt drainage cooling system of the Molten Salt Reactor Experiment (MSRE) as the research subject. Based on computational fluid dynamics simulation methodology, heat transfer and multiphase flow models are constructed to analyze post-damage thermal-hydraulic phenomena, with sensitivity analysis performed on key influencing parameters. The results demonstrate that following inner tube damage, 18.4% of cooling water flows out through the gas gap layer at the damage location, the power of a single heat transfer element increases to 29.434 kW, and local temperature minima appear on the outer tube near the damage site. Sensitivity analysis reveals that damage size, damage location, and gas gap layer pressure variations all significantly affect heat transfer in the heat transfer elements, with heat transfer exhibiting the strongest sensitivity to damage size. These analytical results can provide research data to support the engineering design of emergency salt drainage cooling systems for molten salt reactors.

Full Text

Preamble

Study on Heat Transfer Effects of Inner Barrier Damage in Heat Exchange Tubes of the Fuel Drain System for Molten Salt Reactors

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Abstract

[Background]: The liquid-fueled molten salt reactor is one of the candidate reactor technologies for Generation IV advanced nuclear energy systems, and the fuel drain system represents a special safety design feature that can be incorporated. [Purpose]: This study investigates the Molten Salt Reactor Experiment (MSRE) fuel drain cooling system to examine how damage to the inner barrier of a heat exchange element affects its normal decay heat removal function. [Methods]: Computational fluid dynamics simulations were employed to construct heat transfer and multiphase flow models for analyzing post-damage thermal-hydraulic phenomena, followed by sensitivity analyses of key influencing parameters. [Results]: Following inner tube damage, 18.4% of cooling water flows out through the annular gas space at the breach location, increasing the power of a single heat exchange element to 29.434 kW and creating a local temperature minimum on the outer thimble near the breach. Sensitivity analysis reveals that breach size, location, and annular gas space pressure all significantly affect heat transfer, with breach size having the strongest influence. Conclusions: These results provide valuable insights for the engineering design of fuel drain cooling systems in molten salt reactors.

Keywords: Molten salt reactor; Fuel drain system; Heat exchange element; Coolant leak; Computational fluid dynamics

Introduction

Liquid-fueled molten salt reactors, which use thorium and/or uranium as nuclear fuel and high-temperature molten salt as the primary coolant, represent one of the Generation IV nuclear energy systems. Research on molten salt reactors began in the 1950s, culminating in the construction and operation of the Molten Salt Reactor Experiment (MSRE) at Oak Ridge National Laboratory in 1964, after which global development largely stagnated. In the 21st century, molten salt reactors have regained international attention as a focus of advanced reactor technology research due to their advantages in economics, safety, sustainability, and proliferation resistance, as well as their capability for high-temperature, low-pressure operation and suitability for the thorium-uranium fuel cycle and high-temperature integrated applications. Notable examples include the 2MW liquid-fueled thorium-based molten salt experimental reactor designed by the Shanghai Institute of Applied Physics, Chinese Academy of Sciences, the Molten Salt Research Reactor (MSRR) at Abilene Christian University in the United States, and the Integral Molten Salt Reactor (IMSR) in Canada.

Passive safety represents a key trend in next-generation reactor development. Passive safety designs, which operate without reliance on external power or inputs, offer high reliability and have been widely applied in nuclear energy systems, including the primary loop compartment cooling system of the HTR-10 high-temperature gas-cooled reactor, the passive residual heat removal system in AP1000, and the passive cooling system for the lower cavity of molten salt reactors. In molten salt reactor development, the dual role of liquid molten salt as both coolant and nuclear fuel carrier enables a unique passive safety design not found in conventional water reactors: the core emergency fuel drain cooling system. This system primarily comprises a freeze valve, drain tank, cooling system, valves, and piping. The emergency fuel drain cooling system ensures rapid removal of fuel salt from the core through the freeze valve during abnormal conditions to achieve emergency shutdown, while the drain tank cooling system provides cooling to prevent fuel salt overheating and avoid potential safety hazards. Additionally, this system can serve as a backup decay heat removal option when the normal shutdown heat removal system fails.

The water-cooled emergency fuel drain cooling system adopted by MSRE offers advantages in forming passive natural circulation and engineering feasibility, has been validated in practice, and remains a focus of current research. Therefore, this study investigates the MSRE emergency fuel drain cooling system as its research object.

The core component of the MSRE emergency fuel drain cooling system is the heat exchange element inserted into the fuel salt. This element features a double-wall tube design where cooling water removes fuel salt decay heat through boiling. Previous research based on the MSRE heat exchange element includes: Sun Lu and Chen Kailun, who analyzed natural circulation startup and internal flow heat transfer phenomena through combined experimental and numerical methods; Wu Xiangcheng and Yang Zonghao, who investigated instability and flashing phenomena; and Huang Wanjue and Qu Pengrong, who conducted studies on passive decay heat removal system design based on this element type.

These studies primarily assume intact heat exchange element structures, with limited research addressing damage and leakage scenarios. The thermal-hydraulic changes following damage remain unclear and require analysis. The inner tube serves as the water boundary for the heat exchange element, and its integrity failure can cause coolant water leakage and affect normal decay heat removal from the fuel salt. This study investigates the MSRE water-cooled emergency fuel drain cooling system to analyze heat transfer characteristics following inner barrier damage, exploring the effects of potential inner tube breach under extreme conditions. This analysis can provide research data for molten salt reactor engineering design, consequence assessment, and response strategies.

1. System Structure Description

The MSRE emergency fuel drain cooling system comprises multiple core components including the drain tank, freeze valve, heat exchange elements, steam drum, feedwater tank, and condenser. [Figure 1: see original paper] illustrates the MSRE drain tank and its cooling system. After core fuel salt enters the drain tank through the freeze valve, decay heat is removed through this system.

During normal operation, fuel salt decay heat transfers to the heat exchange elements, causing internal cooling water to boil. The resulting saturated steam-water mixture flows into the steam drum for separation, with liquid water returning to the heat exchange elements and steam flowing to the condenser. Condensed water returns to the elements by gravity, while the feedwater tank stores cooling water and controls steam drum level.

The MSRE heat exchange element structure is shown in [Figure 2: see original paper]. The element employs a double-wall tube design consisting of a downcomer, inner tube, and outer thimble. The annular space between the downcomer and inner tube forms the riser channel, while the space between the inner and outer tubes forms the annular gas space, which connects to the drain tank atmosphere and is filled with nitrogen that remains stable relative to the fuel salt under study conditions. This double-wall design prevents excessive fuel salt cooling in the drain tank while providing dual barriers between fuel salt and cooling water to ensure system safety.

During normal operation, fuel salt decay heat transfers to the element interior through conduction, convection, and radiation. Inside the element, cooling water circulates naturally driven by density differences between steam and liquid water. Structural parameters for the heat exchange element are listed in Table 1 , with the heated length defined as the portion inserted into the fuel salt.

2. Theoretical Model

The heat exchange element contains multiple fluids including fuel salt, cooling water, and nitrogen, with phase change occurring in the cooling water. The governing equations are as follows:

(1) Continuity equation:

$$\frac{\partial}{\partial t}(\alpha_q \rho_q) + \nabla \cdot (\alpha_q \rho_q \mathbf{v}_q) = \sum_{p=1}^{n} (\dot{m}_{pq} - \dot{m}_{qp}) + S_q$$

where α_q represents the volume fraction of phase q, ρ_q is the density of phase q, $\mathbf{v}_q$ is the velocity vector of phase q, and S_q is the mass source term for phase q. Due to phase change processes, the source terms represent mass transfer between liquid water and steam, with the sum of all source terms equal to zero.

(2) Momentum equation:

$$\frac{\partial}{\partial t}(\alpha_q \rho_q \mathbf{v}_q) + \nabla \cdot (\alpha_q \rho_q \mathbf{v}_q \mathbf{v}_q) = -\alpha_q \nabla p + \nabla \cdot \;_q + \alpha_q \rho_q \mathbf{g} + \mathbf{F}_q + \mathbf{S}_{M,q}$$

where p is pressure, $\;_q$ is the stress tensor, $\mathbf{g}$ is gravitational acceleration, $\mathbf{F}_q$ represents interphase forces, and $\mathbf{S}_{M,q}$ is the momentum source term.

(3) Energy equation:

$$\frac{\partial}{\partial t}(\alpha_q \rho_q E_q) + \nabla \cdot (\alpha_q \rho_q \mathbf{v}_q E_q) = -\nabla \cdot \mathbf{q}_q + \nabla \cdot (\;_q \cdot \mathbf{v}_q) - p \nabla \cdot \mathbf{v}_q + S_{E,q} + Q_{pq}$$

where E_q is the specific total energy of phase q, $\mathbf{q}_q$ is heat flux, $\;_q$ is the stress tensor, Q_{pq} represents interphase heat transfer, and $S_{E,q}$ is the heat source term including decay heat.

Additionally, this study employs the Continuum Surface Force (CSF) model to calculate surface tension. The CSF model is widely used for computing surface tension at liquid-gas interfaces by considering surface forces and droplet shapes, making it suitable for various liquid-gas interactions. The Lee model is used for phase change calculations, which is commonly applied to describe vapor-liquid phase transition processes in multicomponent and multiphase flow systems, with validated predictions in experimental studies. The relationship between saturation temperature and pressure is calculated using the Antoine equation, a classical empirical formula fitted from experimental data that is widely applied to describe the relationship between saturation temperature and pressure during liquid evaporation. The phase change medium in this study is water, which satisfies the applicable conditions for this equation.

The Antoine equation is expressed as:

$$\log_{10} P_{sat} = A - \frac{B}{C + T}$$

where T represents temperature, P_{sat} represents water saturation vapor pressure, and A, B, and C are empirical coefficients for water' s Antoine equation.

For radiation heat transfer within the heat exchange element, the Surface-to-Surface (S2S) model is employed. The S2S model calculates radiation exchange between surfaces, which is particularly important between the outer and inner tubes. This model accurately simulates internal radiation phenomena in the heat exchange element. The annular gas space primarily contains nitrogen, a symmetric diatomic gas that satisfies the requirements for S2S model calculations. The inner and outer tubes are made of Hastelloy N alloy with an emissivity of 0.625. The radiation heat transfer between inner and outer tubes is calculated using:

$$Q_{rad} = \frac{\sigma(T_1^4 - T_2^4)}{\frac{1-\varepsilon_1}{\varepsilon_1 A_1} + \frac{1}{A_1 F_{12}} + \frac{1-\varepsilon_2}{\varepsilon_2 A_2}}$$

where ε_1 and ε_2 are the emissivities of the inner and outer tubes, A_1 and A_2 are their radiation areas, T_1 and T_2 are their absolute temperatures, F_{12} is the view factor between the two surfaces, and σ is the Stefan-Boltzmann constant.

The Todreas & Kazimi model is used to calculate fuel salt decay heat, which shows good agreement with MSRE experimental data. Calculations use decay heat after 1,000 hours of normal operation with a drain time of 1,800 s. The decay heat formula is:

$$\frac{Q}{Q_0} = 0.066\left[(t_s^{-0.2} - (t_s + t_0)^{-0.2})\right]$$

where Q and Q_0 represent decay heat power after shutdown and reactor power before shutdown, respectively, while t_s and t_0 represent time after shutdown and continuous operation time before shutdown. The decay heat power during the first hour after drain completion is shown in [Figure 3: see original paper].

The calculated Rayleigh number for natural convection of cooling water in the heat exchange element is on the order of 10^{11}, with Reynolds numbers exceeding 10,000, indicating fully developed turbulent flow. The k- turbulence model is selected for its simplicity, computational efficiency, and suitability for moderate-to-high Reynolds number flows, meeting the requirements for modeling turbulent flow in this study.

The turbulence model equations are:

$$\frac{\partial}{\partial t}(\rho k) + \nabla \cdot (\rho \mathbf{v} k) = \nabla \cdot \left[\left(\mu + \frac{\mu_t}{\sigma_k}\right)\nabla k\right] + G_k + G_b - \rho\varepsilon + S_k$$

$$\frac{\partial}{\partial t}(\rho \varepsilon) + \nabla \cdot (\rho \mathbf{v} \varepsilon) = \nabla \cdot \left[\left(\mu + \frac{\mu_t}{\sigma_\varepsilon}\right)\nabla \varepsilon\right] + C_{1\varepsilon}\frac{\varepsilon}{k}(G_k + C_{3\varepsilon}G_b) - C_{2\varepsilon}\rho\frac{\varepsilon^2}{k} + S_\varepsilon$$

where k is turbulent kinetic energy, ε is turbulent dissipation rate, μ is molecular dynamic viscosity, μ_t is turbulent viscosity, G_k and G_b are source terms from turbulent kinetic energy and buoyancy, respectively, and $C_{1\varepsilon}$, $C_{2\varepsilon}$, $C_{3\varepsilon}$, σ_k, and σ_ε are model constants.

3.1 Computational Model

Commercial CFD software Fluent is used for modeling and analysis. The geometric model represents a simplified single heat exchange element and surrounding fuel salt. The lower heated section matches the MSRE prototype, while the inlet bend at the top is omitted. The drain tank contains 32 heat exchange elements, and the modeled fuel salt volume is 1/32 of the total drain tank volume. Due to structural symmetry, half of the element is selected for computation. The specific model for a single heat exchange element and surrounding fuel salt is shown in [Figure 4: see original paper].

3.2 Boundary Conditions and Property Parameters

Based on reference data, the inlet and outlet pressures of the heat exchange element are atmospheric, with an outlet saturated steam temperature of 373 K. The cover gas pressure in the drain tank is 34,470 Pa (5 psig) maximum, the annular gas space outlet pressure is -13,788 Pa (-2 psig), the initial fuel salt temperature is 923 K, and the inlet subcooling is 3 K with water temperature set at 370 K. Other thermophysical properties are listed in Table 2 .

3.3 Grid Independence Verification

Since other studied parameters interact with mass flow rate and power, grid independence verification is performed using mass flow rate and heat transfer power as benchmarks. When the mesh count reaches 455,000, both inlet/outlet mass flow rates and heat transfer power cease to change. Therefore, this mesh count is selected for subsequent analyses, with local 1/4 refinement applied near the breach location.

3.4 Steady-State Simulation and Model Validation

Due to time-dependent decay heat power and the inherent instability of two-phase natural circulation, transient simulations are conducted to obtain quasi-steady-state operating parameters. Steady-state natural circulation is achieved when inlet and outlet mass flow rates no longer fluctuate.

The mass flow rates from initial heating to steady state are shown in [Figure 5: see original paper]. During segment A-B, mass flow rate remains zero because cooling water temperature gradually increases but has not yet reached saturation. In segment B-C, outlet mass flow rate begins to increase, followed by inlet mass flow rate, as heating causes vaporization in the riser annulus and natural circulation initiates due to density differences between the downcomer and riser. In segment C-D, outlet mass flow rate decreases because faster circulation reduces heating time per unit mass of water, lowering the vaporization fraction. Steady state is reached when outlet and inlet mass flow rates balance at 0.102 kg/s.

The steady-state temperature distribution is shown in [Figure 6: see original paper], clearly demonstrating distinct temperature zones: high-temperature regions in the fuel salt and outer thimble, low-temperature regions in the cooling water and inner tube, and a transition region in the annular gas space. The overall heat transfer path proceeds from fuel salt to outer thimble, through the annular gas space to inner tube, and finally to cooling water. Thermal resistance is concentrated in the annular gas space, creating large temperature drops there while temperature differences across the fuel salt side and element interior are relatively small.

Model accuracy is validated by comparing the MSRE rated power per element (3.125 kW) with the calculated steady-state power (3.102 kW), yielding an error of only 0.74%.

3.5 Inner Barrier Damage Analysis

Inner barrier damage analysis is initiated using the steady-state results as initial conditions. The breach location is selected at the element bottom where pressure differential across the inner tube is maximum. Conservatively assuming a through-wall crack with circular geometry and neglecting crack flow resistance, the breach diameter is calculated as 3 mm using Formula 10. Additionally, inner barrier damage is assumed not to affect outer barrier structural integrity.

After inner tube damage, cooling water flows into the annular gas space due to pressure differential. The flow vector diagram in [Figure 7: see original paper] shows cooling water rapidly exiting at 3.98 m/s at the breach location.

The liquid water volume fraction after breach is shown in [Figure 8: see original paper]. Upon entering the annular gas space, cooling water contacts the outer thimble directly, rapidly vaporizing and exiting through the annular gas space outlet.

Mass flow rates at inlets, outlets, and annular gas space outlet are shown in [Figure 9: see original paper]. Inlet mass flow increases from 0.102 kg/s to 0.125 kg/s (22.5% increase), outlet mass flow remains at 0.102 kg/s, and 0.023 kg/s of cooling water leaks through the annular gas space, representing 18.4% of the total flow. The outlet mass flow variation results from combined effects of reduced pressure at the breach location and increased overall heat transfer.

The void fraction distribution on the breach and non-breach sides is shown in [Figure 10: see original paper]. On the breach side, liquid water fraction increases rapidly from the bottom to the breach location, reaching 1.0 at the breach, then decreasing to 0.73 above it. Further vaporization occurs due to outer thimble heating, with decreasing vaporization rate along the length. At the annular gas space outlet, liquid water volume fraction is 0.03. On the non-breach side, liquid water fraction decreases gradually from 0.56 to 0.03 at the outlet. Steam void fraction shows the opposite trend, reaching 0.97 at the outlet. Liquid water and steam occupy the entire annular gas space, completely displacing nitrogen. At 0.2 m height, steam void fraction is higher on the breach side due to direct contact vaporization at the breach.

Flow vectors after water enters the annular gas space are shown in [Figure 11: see original paper]. Water velocity increases along the vertical direction, reaching 2.44 m/s at the outlet. Near the breach location, most cooling water flows downward after contacting the outer thimble, fills the element bottom, then flows upward along the non-breach side. A vortex appears at the element bottom due to velocity differences across the breach.

Heat transfer changes significantly after breach. The damaged element power increases from 3.102 kW to 29.434 kW (848.8% increase), while the other 31 elements operate normally. Overall drain tank power increases from 99.264 kW to 125.596 kW (26.5% increase), which is insufficient to cause bulk fuel salt solidification in the short term.

Temperature distribution after inner tube damage is shown in [Figure 12: see original paper]. The lateral temperature distribution comparison at the breach location is presented in [Figure 13: see original paper]. Within the inner tube, temperature remains unchanged due to cooling water saturation temperature limitation. In the annular gas space, overall temperature decreases due to cooling water inflow, with breach side temperatures lower than non-breach side and temperature gradients shifting toward the outer thimble. In the fuel salt, near-wall temperatures decrease due to enhanced heat removal.

The inner wall temperature distribution of the outer thimble on the breach side is shown in [Figure 14: see original paper]. A temperature minimum of 439.48 K appears at the breach location due to rapid heat transfer from direct cooling water contact. Above the breach, steam void fraction increases and local flow velocity is slower, creating a higher temperature point of 762.35 K on the outer thimble inner wall.

The temperature variation at the breach location over time is shown in [Figure 15: see original paper]. The outer thimble temperature at the breach (0.01 m height) drops rapidly by 449.32 K within 6 seconds due to direct cooling water contact, then gradually decreases to 444.35 K.

4.1 Effect of Breach Location

The relationship between mass flow rates and breach location height is shown in Figure 16: see original paper, while power and breach velocity versus height are shown in Figure 16: see original paper. The analysis assumes a 3 mm breach size and -13,788 Pa annular gas space outlet pressure. As breach height increases from 0.01 m to 1.5 m, the damaged element power decreases from 29.434 kW to 9.858 kW, inlet mass flow decreases from 0.125 kg/s to 0.109 kg/s (relative change from 22.5% to 6.8%), annular gas space outlet flow decreases from 0.023 kg/s to 0.007 kg/s, and leak fraction decreases from 18.4% to 6.4%. Both heat transfer power and mass flow rates decrease with increasing breach height because higher locations experience smaller hydrostatic pressure differentials, reducing flow through the breach. Notably, at 1.5 m height, breach velocity shows a different trend due to higher steam volume fraction and lower flow resistance in the riser annulus at that elevation.

4.2 Effect of Annular Gas Space Pressure

Mass flow rates versus annular gas space pressure are shown in Figure 17: see original paper, with power and breach velocity versus pressure in Figure 17: see original paper. The analysis assumes a 3 mm breach at 0.1 m height. As annular gas space pressure increases from -2 psig to 2 psig, damaged element power decreases from 29.434 kW to 13.788 kW, inlet mass flow decreases from 0.125 kg/s to 0.103 kg/s (relative change from 22.5% to 0.98%), annular gas space outlet flow decreases from 0.023 kg/s to 0.009 kg/s, and leak fraction decreases from 18.4% to 8.7%. Both heat transfer power and mass flow rates decrease with increasing annular gas space pressure due to reduced pressure differential across the inner tube, which decreases breach flow and overall heat transfer.

4.3 Effect of Breach Size

Mass flow rates versus breach size are shown in Figure 18: see original paper, with power and breach velocity versus size in Figure 18: see original paper. The analysis assumes -13,788 Pa annular gas space pressure and 0.1 m breach height. As breach size increases from 3 mm to 8 mm, damaged element power increases from 29.434 kW to 98.420 kW, inlet mass flow increases to 0.170 kg/s (relative change from 22.5% to 66.7%), annular gas space outlet flow increases from 0.023 kg/s to 0.056 kg/s, and leak fraction increases from 18.4% to 32.9%. Both heat transfer power and mass flow rates increase with breach size, while breach velocity decreases.

Conclusions

This study investigates heat transfer characteristics following inner tube damage in the MSRE water-cooled emergency fuel drain cooling system heat exchange element. Based on conservative assumptions of a through-wall circular breach neglecting crack flow resistance, a multiphase flow model incorporating radiation, evaporation, and decay heat is developed to analyze post-damage thermal-hydraulic behavior and key phenomena. Sensitivity analyses are performed for breach location, size, and annular gas space pressure.

1) For a 3 mm breach at the bottom location, 0.023 kg/s of cooling water leaks through the annular gas space, increasing inlet mass flow to 0.125 kg/s. Rapid vaporization in the annular gas space increases single element power to 29.434 kW, causing temperature reductions in the annular gas space and near-wall fuel salt. A local temperature minimum of 439.48 K appears on the outer thimble near the breach. The rapid temperature change in the outer thimble suggests that appropriate materials should be selected in design to prevent potential thermal stress.

2) Lower breach location, lower annular gas space pressure, and larger breach size all increase effects on element power and flow conditions, with breach

size having the strongest influence. During heat exchange element manufacturing, advanced fabrication processes should be employed to minimize weld quantities and reduce leakage risk.

Author Contributions Statement

SUN Liangjie: Developed damage model, performed calculations, and wrote the manuscript; LIANG Rongjian, JIANG Wentao: Provided literature support and suggestions; WANG Chaoqun: Contributed technical and data support; YANG Qun, ZOU Yang: Provided research direction, technical guidance, and review; HE Zhaozhong, XU Hongjie: Responsible for project management and review.

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