# Program Development and Transient Safety Analysis of the Stirling Power Cycle System for Lithium-Cooled Fast Reactors

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## Abstract

To enhance the safety and reliability of space nuclear power systems, a transient safety analysis code NUSOL-LMR-Li was developed for the lithium-cooled fast reactor Stirling power conversion system, focusing on analyzing its performance and adaptive characteristics under accident conditions. Based on the Schmidt isothermal analysis method, models for core power, thermal-hydraulics, heat structure conduction, and properties of liquid lithium, NaK, and helium were established, achieving coupling between Stirling thermoelectric conversion and the lithium-cooled fast reactor. The code was validated against the NASA RE-1000 Stirling prototype and system loop, with a maximum error of 3.8%. The core power model exhibited errors of 0.53% and 0.32% in positive and negative reactivity insertion cases, respectively, and the fully implicit algorithm demonstrated time-step-independent stability in a single-tube flow case. Transient analysis was performed for typical accident conditions of the system, and the results show: (1) A 0.1$ reactivity insertion caused the core power to rise to 534 kW, which then stabilized at 466 kW through negative feedback mechanisms, with the outlet temperature increase controlled at 14.7 K; (2) Regenerator blockage caused the Stirling efficiency to drop to 22.24%, and the system re-established thermal equilibrium through self-reduction of hot-end temperature to 1023 K and elevation of cold-end to 692 K, with efficiency recovering to 26.8%. The study validates the system' s self-regulation and fault tolerance capabilities, providing theoretical support and an efficient analysis tool for the safety design of space nuclear power systems.

# Full Text

## Preamble

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**Development of a Transient Safety Analysis Code for Lithium-Cooled Fast Reactor Stirling Power Conversion Systems**

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## Abstract

To enhance the safety and reliability of space nuclear power systems, a transient safety analysis code named NUSOL-LMR-Li was developed specifically for lithium-cooled fast reactor (LFR) Stirling power conversion systems, with particular emphasis on analyzing system performance and adaptive characteristics under accident conditions. Based on the Schmidt isothermal analysis method, models were established for core power, thermal-hydraulics, heat structure conduction, and the thermophysical properties of liquid lithium, sodium-potassium (NaK) alloy, and helium. The coupling between Stirling thermoelectric conversion and the lithium-cooled fast reactor was successfully implemented.

The code was validated against the NASA RE-1000 Stirling prototype and system loop data, achieving a maximum error of 3.8%. The core power model demonstrated errors of only 0.53% and 0.32% for positive and negative reactivity insertion cases, respectively. The fully implicit algorithm exhibited time-step-independent stability in single-pipe flow calculations. Transient analysis of typical system accident scenarios yielded the following key results: (1) A 0.1$ reactivity insertion caused core power to rise to 534 kW, then stabilize at 466 kW through negative feedback mechanisms, with outlet temperature rise controlled within 14.7 K; (2) Regenerator blockage caused Stirling efficiency to drop to 22.24%, but the system reconstructed thermal equilibrium through autonomous reduction of hot-end temperature to 1023 K and increase of cold-end to 692 K, recovering efficiency to 26.8%. The study validated the system' s self-regulating and fault-tolerant capabilities, providing theoretical support and an efficient analysis tool for space nuclear power safety design.

**Keywords:** Stirling power cycle; Lithium-cooled fast reactor; Space reactor; Model and algorithm development; Accident safety characteristic analysis
**Chinese Library Classification:** TL3

## 1. Introduction

Lithium-cooled fast reactors represent an advanced nuclear reactor technology with numerous significant advantages. Lithium' s low density helps reduce the weight of space reactors, enabling system miniaturization and lightweight design. Lithium can operate at extremely high temperatures, allowing lithium-cooled fast reactors to achieve high cycle efficiency. For instance, a liquid metal lithium experimental loop developed by the Institute of Nuclear Energy Safety Technology at the Hefei Institutes of Physical Science, Chinese Academy of Sciences, achieved a breakthrough by maintaining stable operation at 1500 K (1227°C) for 1000 hours. Liquid lithium exhibits good compatibility with certain structural materials, and Zheng et al. have conducted experimental studies on the corrosion resistance of high-temperature refractory alloys in high-temperature liquid lithium environments. Additionally, its excellent thermal conductivity ensures efficient heat transfer from the core to the energy conversion system, improving overall energy conversion efficiency and reducing energy losses. Due to lithium' s high boiling point, the system can operate at atmospheric pressure, simplifying system design and enhancing operational safety. Some lithium-cooled fast reactor designs also incorporate innovative passive negative feedback systems, such as the ARC (Autonomous Reactivity Control) system, which utilizes liquid lithium as a neutron absorber. When the coolant outlet temperature rises, thermal expansion automatically injects high-enrichment ^{6}Li liquid lithium into the core, introducing negative reactivity and achieving self-regulation. This design not only enhances inherent reactor safety but also enables autonomous response to transient conditions without external intervention. In summary, these unique advantages make lithium-cooled fast reactors a promising technology for future space missions, offering efficient and reliable power support.

In the 1980s, under the Strategic Defense Initiative (SDI), the United States conducted research and development on the SP-100 space reactor power system, which employed a lithium-cooled fast reactor coupled with thermoelectric and Stirling power generation methods, utilizing high-boiling-point lithium coolant. Coupling lithium coolant with the Stirling cycle holds significant importance. First, the Stirling cycle can efficiently convert thermal energy from lithium coolant into mechanical and electrical energy, improving energy utilization efficiency and reducing waste. Lithium' s high thermal conductivity allows more heat transfer in a smaller volume, enabling more compact reactor designs suitable for space applications. Furthermore, lithium is a relatively abundant resource, and the Stirling cycle is an emission-free energy conversion process. Combining lithium coolant with the Stirling cycle enables sustainable energy applications, reducing dependence on traditional energy resources, minimizing environmental impact, and promoting sustainable energy development.

Zhang Yang et al. from the Institute of Nuclear Energy Safety Technology, Chinese Academy of Sciences, proposed a preliminary design for a 5.2 MWt lightweight space lithium-cooled nuclear reactor featuring an integrated core design, non-uniform lithium coolant flow channels, and a control system composed of boron powder and control rods. Using the SuperMC multi-physics coupling framework and a stochastic approximation coupling strategy, high-fidelity simulations were performed through Monte Carlo neutron transport and computational fluid dynamics (CFD) coupling. The results confirmed that the design meets reactivity requirements, with non-uniform flow channel design achieving a more uniform radial power distribution. Under normal operating conditions, all material temperatures remain below limits, providing optimization insights for space reactor design.

Jin Zhao et al. from Xi' an Jiaotong University established models for the core, Stirling generator, radiator, pump, and associated piping for a space nuclear power system combining a lithium-cooled fast reactor with a Stirling cycle, and developed a transient system thermal-hydraulic safety analysis code based on Fortran. Validation against Stirling experimental data showed a maximum relative error of 17.3% in the Stirling mathematical model. A space lithium-cooled power system model was established, and the rationality of the system program model was verified through steady-state calculations, with a maximum relative error of 13.3% compared with design values. Transient analysis of typical system accident scenarios demonstrated that due to the core' s overall negative reactivity feedback, the peak fuel pellet temperature remained within safety limits, indicating certain safety characteristics.

While existing research has accumulated important technologies for lithium-cooled space reactor systems, significant limitations remain in Stirling engine verification calculations and accident safety analysis. Specifically, most studies have focused on steady-state performance validation, lacking systematic simulation of transient accident scenarios. More critically, as the core hub of energy conversion, the Stirling engine's proprietary failure modes in space environments (such as efficiency degradation due to regenerator blockage) have not been adequately considered, creating analytical blind spots in the fault propagation path from nuclear heat source to electrical power conversion.

A lithium-cooled Stirling cycle system generally consists of the core, Stirling engine, liquid metal electromagnetic pump, radiator, and expansion tank. Figure 2 [Figure 2: see original paper] shows a schematic diagram of the SP-100 Stirling power conversion system, which achieves nuclear-thermal-electric conversion through a dual-loop liquid metal circulation. Lithium coolant heated in the core passes through a direct-connect expansion tank that accommodates volume expansion during lithium heating from frozen to operating temperature and separates helium gas generated in the reactor. The coolant then heats the hot end of the Stirling engine, driving the free piston to generate electricity. The secondary loop NaK coolant exchanges heat with the Stirling cold end, with waste heat discharged to space through a radiator. A simplified design is adopted, and the transient thermal resistance analysis module for the radiator is not yet integrated, with waste heat discharge treated through equivalent boundary conditions.

Based on the structural and operational characteristics of the lithium-cooled space reactor Stirling power conversion system, a theoretical model suitable for system safety analysis was established. The model covers core power, thermal-hydraulics, heat structure conduction, and thermophysical properties of liquid lithium, liquid NaK, and helium. The Schmidt isothermal analysis method was employed to analyze the isochoric heat transfer process of helium working fluid in the regenerator. Based on energy conservation principles and modified Stirling cycle efficiency, equations for input/output heat and regenerator heat loss were constructed, achieving coupling between the Stirling cycle and lithium-cooled space reactor to form the Stirling thermoelectric conversion model. On this basis, the fully implicit transient safety analysis code NUSOL-LMR-Li was developed in Fortran for long-term slow transient accident calculations in lithium-cooled fast reactor Stirling power conversion systems. The time-step stability of the fully implicit algorithm was analyzed, and the calculation accuracy of the core power model was verified to meet the requirements for subsequent reactivity insertion accident calculations. Through single-machine and system loop tests, the coupling correctness of the Stirling thermoelectric conversion model was confirmed. Finally, a simplified model of the SP-100 system was developed, and steady-state calculations and transient accident safety analyses were performed.

### 1.1 Thermal-Hydraulic Model

Liquid lithium (Li) and sodium-potassium (NaK) coolants have boiling points as high as 1620 K and 1057 K, respectively, at 1 MPa working pressure. Therefore, in lithium-cooled fast reactors, the primary coolant lithium and secondary coolant NaK remain liquid at atmospheric pressure without boiling. Consequently, a single-phase flow model based on mass, momentum, and energy conservation was proposed, considering axial heat conduction effects in liquid metal fluids to numerically simulate pressure, velocity, and temperature parameters for Li and NaK coolants.

1) **Mass Conservation Equation**

The first term represents the transient mass change, and the second term represents the mass convection term.

2) **Energy Conservation Equation**

Where: *h*–specific enthalpy (J·kg^{-1}); *–*thermal conductivity (W·m{-1}·K{-1}); T–temperature (K); g–gravitational acceleration (m · s^{-2}); Q_w–thermal power (W · m^{-1});* *–inclination angle. From left to right, the terms represent the transient energy change, energy convection, axial heat conduction, pressure-related work, gravitational work, and heat source terms.

3) **Momentum Conservation Equation**

Where: *P*–pressure (Pa); *F_w*–frictional resistance (Pa · m^{-1}); *F_{local}* –local resistance (Pa · m^{-1}). From left to right, the terms represent the transient flow change, convection, static pressure drop, gravitational pressure drop, frictional pressure drop, and local pressure drop.

### 1.2 Heat Structure Conduction Model

A one-dimensional unsteady heat conduction equation was adopted to establish a heat structure model suitable for fast reactor systems. The general control equation for one-dimensional unsteady heat conduction is:

Where: $c$—specific heat capacity ($J \cdot kg^{-1} \cdot K^{-1}$); $x$—distance (m); $S$—volumetric heat source ($W \cdot m^{-3}$); $A(x)$—area factor.

The internal node method was used for radial mesh division of heat structures, where the temperature node to be solved is located at the grid center, and the grid region is the control volume for that point, as shown in Figure 3 [Figure 3: see original paper].

The control volume integration method was then applied to discretize the one-dimensional unsteady heat conduction equation. The equation was integrated over time using an implicit scheme, with detailed derivation available in relevant literature. The final discrete form of the conduction equation is:

For second or third type boundary conditions, the additional source term method was employed, treating the heat entering or leaving the calculation region as specified by the boundary conditions as an equivalent source term for the control volume adjacent to the boundary. For second-type boundary conditions, the additional source term is:

Where: $q_{bound}$—boundary heat flux density ($W \cdot m^{-2}$).

For third-type boundary conditions, the boundary heat flux density depends on the boundary control volume node temperature, fluid temperature, and the thermal resistances of the half-boundary control volume conduction and convection, expressed as:

Where: $T_f$—fluid temperature (K); $h_{conv}$—convective heat transfer coefficient ($W \cdot m^{-2} \cdot K^{-1}$); $T_b$—boundary control volume node temperature (K); $\Delta x_b$—boundary control volume thickness (m).

Expressed as an additional source term:

For boundary control volumes, the conduction control equation simply requires adding the additional source term to the source terms $S_C$ and $S_P$ in the equation. Therefore, the general form of the control equation for heat structure axial nodes can be expressed as:

For left and right boundary control volumes, respectively:

Thus, for a heat structure with $N$ nodes radially, $N$ conduction control equations can be obtained, forming a linear equation system with a coefficient matrix of order $N$ tridiagonal form, which can be solved using the Thomas algorithm to obtain the node temperatures corresponding to each control volume.

### 1.3.1 Core Power Calculation Model

A point reactor neutron kinetics model with six delayed neutron groups was employed to calculate transient changes in core fission power.

Where: $N$—neutron density (m^{-3}); $C\_i$—concentration of the $i$-th delayed neutron precursor (m^{-3}); * _i—*decay constant of the* i-*th delayed neutron group (s^{-1}); _i—fraction of the* i-*th delayed neutron group;* *—total delayed neutron fraction; *(t)*—reactivity; $\Lambda$—neutron generation time.

The high-order endpoint floating method was used to solve the point reactor kinetics equations, which effectively overcomes the stiffness of the point reactor equations with high calculation accuracy.

### 1.3.2 Reactivity Feedback Model

In small fast reactors such as space reactor power systems, parameter fluctuations significantly affect reactivity during power operation, primarily manifested as fuel expansion deformation and coolant density changes caused by temperature variations. The lithium-cooled fast reactor reactivity feedback model considers three factors: Doppler effect feedback from fuel temperature changes, reactivity changes caused by fuel volume changes, and reactivity feedback from coolant density changes. The lumped parameter method was used to approximate the total reactivity feedback, with average temperatures simulating coolant and fuel temperatures, and various reactivity feedback effects represented by corresponding feedback coefficients:

Where: $K\_D$—Doppler feedback coefficient (K^{-1}); $T\_f$—average fuel temperature (K); * _c,a—*fuel expansion feedback coefficient (K^{-1});* _c,r—*core structure expansion feedback coefficient (K^{-1});* T_c—*average coolant temperature (K);* _c*—coolant expansion feedback coefficient (K^{-1}).

### 1.4 Stirling Thermoelectric Conversion Model

The Stirling engine is a closed-cycle power device based on gas working fluid circulation between heat and cold sources. Its circulation system consists of a cylinder equipped with two pistons, with a regenerator between them (see Figure 4 [Figure 4: see original paper]). The regenerator alternately releases or absorbs heat from the working fluid in each cycle. The chambers formed by the pistons and cylinder on both sides of the regenerator constitute the hot expansion space and cold compression space, respectively. The Stirling power cycle is an efficient dynamic thermodynamic cycle technology that drives piston motion to achieve energy conversion using temperature differences between heat and cold sources. As a closed system, the working fluid is not consumed during operation, offering environmentally friendly and sustainable characteristics. With its unique advantage of maintaining high thermal efficiency even under low temperature differences, the Stirling cycle performs exceptionally well in space energy conversion systems, particularly suitable for long-term deep space exploration missions, providing a reliable and high-performance solution for efficient energy conversion.

The Stirling thermoelectric conversion process involves complex mechanical motion and thermodynamic state changes of the working fluid, with numerous variables and complex equations. Based on fundamental energy conservation and Stirling cycle efficiency, the classical Schmidt isothermal analysis model was employed for cycle system analysis. This model assumes constant cyclic temperatures in the heating and cooling chambers, with gas temperature in the expansion and heating chambers equal to the average heat source temperature, and gas temperature in the compression and cooling chambers equal to the average cold source temperature.

Considering heat transfer irreversibility between the heat source and working fluid, regenerator heat losses, and the regenerator' s function of controlling the temperature difference between the hot and cold ends of the Stirling engine, while ignoring other irreversible effects such as heat leakage and friction, and assuming the total gas mass in the engine remains constant, the Stirling cycle and its external coupling model were established (Figure 5 [Figure 5: see original paper]).

**1) Regenerator Model**

Since the Stirling cycle essentially consists of two isothermal processes and two isochoric processes, the regenerator power is considered constant here to control the temperature difference between the expansion and compression chambers. The regenerator power can be expressed as:

Where: $m_{gas}$—gas mass in Stirling engine (kg); $T_h$—average gas temperature in expansion (heating) chamber (K); $T_c$—average gas temperature in compression (cooling) chamber (K); $C_P$—gas specific heat (J · kg$^{\{-1\}}$ $^{\cdot}$ $^{K}${-1}).

Regenerator loss is an important irreversible loss during Stirling engine operation, calculated by:

Where: $g$ is the regenerator loss constant, related to the working fluid' s average constant pressure specific heat, regenerator efficiency, and the average mass of gas passing through the regenerator during the heating (or cooling) period. Based on experimental simulation, $g$ = 2.26×10^{-5}.

**2) Coupling Model**

The coupling between the primary/secondary sides and the Stirling system is represented by:

Where: $Q_{inheat}$—power obtained by Stirling hot chamber from primary loop lithium coolant (W); $Q_{out}$—power released by Stirling cold chamber to secondary loop liquid NaK (W); $T_{Li}$—primary loop lithium coolant temperature (K); $T_{NaK}$—secondary loop NaK coolant temperature (K); $R_{heat}$—thermal resistance between primary loop lithium and hot chamber gas (W ·

$K^{-1}$); $R_{cool}$—thermal resistance between cold chamber gas and secondary loop NaK ($W \cdot K^{-1}$).

Thermal resistance consists of three parts: liquid-solid, solid-solid, and solid-gas, specifically including heat transfer between coolant and hot (cold) chamber walls, conduction between hot (cold) chamber walls, and heat transfer between hot (cold) chamber walls and helium working fluid in the Stirling engine.

Where: $h_{Li}$—heat transfer coefficient between liquid lithium and hot chamber wall ($W \cdot m^{-2} \cdot K^{-1}$); $h_{NaK}$—heat transfer coefficient between liquid NaK and cold chamber wall ($W \cdot m^{-2} \cdot K^{-1}$); $A_h$—contact area between hot chamber wall and lithium coolant ($m^{2}$); $A_c$—contact area between cold chamber wall and NaK coolant ($m^{2}$); * _h—*thermal conductivity of hot chamber wall ($W \cdot m^{-1} \cdot K^{-1}$);* c—thermal conductivity of cold chamber wall ($W \cdot m^{-1} \cdot K^{-1}$); $h${He}*—heat transfer coefficient of helium working fluid ($W \cdot m^{-2} \cdot K^{-1}$).

Actual heat engines cannot achieve theoretical maximum efficiency due to limitations from irreversible processes such as friction, thermal resistance, and fluid viscosity. These irreversible factors cause energy losses, significantly reducing engine efficiency. An ideal reversible Stirling cycle would need to operate at infinitely slow speeds to achieve theoretical maximum efficiency and ensure thermodynamic equilibrium. However, such processes cannot produce practical power output, as power generation depends on energy transfer within finite time. Based on the law of energy conservation and the assumption of optimal Stirling cycle efficiency, there exists a clear thermodynamic relationship between the heat transferred from primary loop lithium to the Stirling hot chamber ($Q_{inheat}$), the heat released from the Stirling cold chamber to secondary loop NaK ($Q_{out}$), the regenerator heat loss ($Q_{Rloss}$), and the Stirling engine efficiency ( $_{stirling}$):

Where: $g$—regenerator loss constant; **—compression ratio (ratio of hot to cold chamber volume);* c—*actual efficiency correction coefficient;* n*—moles.

## 2 Development and Verification of the Lithium-Cooled Space Reactor Power System Analysis Code

Based on the aforementioned liquid metal-cooled space reactor power system analysis models, appropriate numerical solution methods were selected for different models, and a modular programming approach was employed to develop the NUSOL-LMR-Li transient analysis code for lithium-cooled space reactor power systems. The corresponding models were validated through fundamental benchmark problems.

### 2.1 Fully Implicit Difference Algorithm for Convection-Diffusion Terms

Considering that lithium-cooled fast reactor transient safety analysis requires substantial computational time, current mainstream system analysis codes generally employ semi-implicit algorithms whose time steps are constrained by the Courant number, resulting in unsatisfactory computational efficiency for long-term slow transients. Therefore, a fully implicit difference algorithm for convection-diffusion terms was adopted to solve single-phase or homogeneous flow hydraulic models based on the three conservation equations. The homogeneous flow model has good well-posedness, and the fully implicit algorithm produces results independent of time step size, with algorithm stability unconstrained by Courant conditions. Consequently, larger time steps can be employed in long-term slow transient simulations of lithium-cooled fast reactor systems, significantly reducing computational time and improving efficiency.

First, the three fundamental conservation equations of the single-phase flow model were discretized using a fully implicit scheme, transforming differential equations into difference equations. In the discretization process, subscript $o$ represents control volume parameters, while subscript $x$ represents junction parameters, as shown in Figure 6 [Figure 6: see original paper].

The convection terms in all three equations employ first-order upwind differencing, the conduction term in the energy equation uses central differencing, and the transient terms in all three equations use first-order backward differencing. The resulting difference equations are expressed as:

The mass and energy conservation equations are defined on control volumes, while the momentum conservation equation are defined on junctions. This reactor system contains $N$ control volumes and $J$ junctions, with the nonlinear difference equation system having an order of $N+2J$, solved using Newton' s iteration method requiring Jacobian matrix generation. The Newton method solution process can be expressed as:

The primary solution variables for the equation system are control volume pressure $P$, control volume specific enthalpy $h$, and junction velocity $V$, therefore:

Taking partial derivatives of the equation system with respect to the solution variables yields the Jacobian matrix, with the general expression:

The partial derivatives of the conservation equations with respect to the solution variables are then obtained to populate the Jacobian matrix. Subsequently, the full-pivot Gaussian elimination method is employed to solve the linear equation system, ultimately obtaining converged solutions.

### 2.2 Program Structure

Based on the models and algorithms described in the previous sections, the NUSOL-LMR-Li system transient safety analysis code for lithium-cooled fast reactors was developed in Fortran using structured programming methods. The code organization follows hierarchical analysis, with control structures (sequence, selection, loop) defining module hierarchy and employing top-down, stepwise refinement design principles, gradually refining implementation from overall objectives.

The NUSOL-LMR-Li software system is divided into several functional modules including input, calculation, and output, with each module programmed independently and connected through data transfer to form the complete software system. The main modules and calling structure are shown in Figure 7 [Figure 7: see original paper].

**Input Module:** Responsible for parsing input files and extracting key parameters for lithium-cooled fast reactor thermal-hydraulic system modeling, including geometric configurations (core dimensions, piping layout), thermodynamic state variables (temperature, pressure, flow velocity), and transient event sequences (reactivity insertion timing, Stirling engine efficiency changes). Through structured data mapping, input parameters are converted to internal calculation variables, providing initialization conditions for subsequent numerical solutions.

**Main Calculation Module:** The computational kernel of NUSOL-LMR-Li, primarily comprising thermal-hydraulic solution module, hydraulic solution module, Stirling thermoelectric conversion model, and point reactor neutron kinetics solution module. Relevant thermal-hydraulic parameters obtained from input cards are substituted into the equations to be solved, and solutions are obtained based on the main algorithms involved in the software, including the fully implicit difference scheme and control volume integration method. Various internal and external iteration processes are performed to obtain important thermal-hydraulic parameter changes during lithium-cooled fast reactor transient accidents, such as pressure, temperature, and flow rate.

**Auxiliary Module:** Primarily substitutes system geometric parameters and some thermal-hydraulic characteristic parameters into constitutive models for thermophysical properties, flow, and heat transfer to solve for constitutive parameters such as properties (liquid lithium, liquid NaK, helium) and heat transfer coefficients within the system, providing support for equation solving.

**Output Module:** Responsible for outputting important thermal-hydraulic parameter data for subsequent data analysis.

#### 2.3.1 Time Step Stability Analysis of Fully Implicit Difference Algorithm

The fully implicit algorithm features computational results and stability independent of Courant criterion constraints. Its time step stability was analyzed through a single-pipe flow example driven by pressure difference.

Figure 8 [Figure 8: see original paper] shows a schematic diagram of the single-pipe flow example. Assuming a pipe length of 5 m and diameter of 0.2 m, the pipe is filled with liquid lithium and connected to time-dependent control volumes at both ends to define flow boundary conditions. In this example, the pressure boundaries at the pipe inlet and outlet are fixed, so flow within the pipe is driven by the pressure difference between the two ends.

Assuming the fluid is initially stationary, it will accelerate under the pressure difference. As velocity increases, frictional resistance increases, leading to increased frictional pressure drop. When the frictional pressure drop equals the driving pressure difference, the fluid reaches equilibrium and all parameters approach steady state. The calculated pipe outlet velocity variation over time is shown in Figure 9 [Figure 9: see original paper].

The results show that fluid velocity reaches a steady-state value of approximately 16.06 m/s after about 20 seconds from the initial state. During this process, calculations with different time steps exhibit high consistency. When time steps of 0.1 s and 0.5 s are used, the Courant numbers exceed 1, and with a 0.5 s time step, the Courant number reaches 8.03, far greater than 1. This well demonstrates the stability of the fully implicit algorithm: for transient processes, time step size has almost no effect on numerical results, and numerical stability is not constrained by the Courant criterion.

### 2.3.2 Core Power Calculation Model Verification

The NUSOL-LMR-Li point reactor neutron kinetics model was verified by comparing its solution of fundamental benchmark problems with analytical calculations.

**(1) Calculation of neutron density N(t) variation in a thermal reactor after step positive reactivity insertion**

Thermal reactor parameters for the model are given in Table 1 . A step reactivity of = 0.003 was input with time step $h$ = 0.1 s and $N(0)$ = 1.0 cm^{-3}, and neutron density (cm^{-3}) variation over 1 s was calculated. Using the analytical solution as the reference standard, the results from the NUSOL-LMR-Li core power calculation model show excellent agreement, with a maximum deviation of 0.53% that gradually decreases, demonstrating high calculation accuracy.

**Table 1 Neutron Kinetics Parameters of Thermal Reactor**

| Group | Decay Constant * _i* (s^{-1}) | Effective Delayed Neutron Fraction * _i* |
|---|---|---|
| 1 | 0.0127 | 0.000266 |
| 2 | 0.0317 | 0.001494 |
| 3 | 0.1150 | 0.001316 |
| 4 | 0.3110 | 0.002849 |
| 5 | 1.4000 | 0.000896 |

| Group | Decay Constant * _i* (s^{-1}) | Effective Delayed Neutron Fraction * _i* |
|---|---|---|
| 6 | 3.8700 | 0.000182 |

Neutron Generation Time $\Lambda$ (s): 0.0001

**Table 2 Benchmarking of Neutron Density Transients in Fast Reactor under Step Positive Reactivity Insertion**

| Time (s) | Analytical Solution | Simulated Value | Relative Error (%) |
|---|---|---|---|
| 0.1 | 11.967 | 12.030 | 0.53 |
| 0.2 | 14.163 | 14.210 | 0.33 |
| 0.3 | 14.686 | 14.720 | 0.23 |
| 0.4 | 14.847 | 14.870 | 0.15 |
| 0.5 | 14.905 | 14.920 | 0.10 |
| 0.6 | 14.927 | 14.940 | 0.09 |
| 0.7 | 14.936 | 14.940 | 0.03 |
| 0.8 | 14.940 | 14.940 | 0.00 |
| 0.9 | 14.941 | 14.940 | -0.01 |
| 1.0 | 14.942 | 14.940 | -0.01 |

**(2) Calculation of neutron density N(t) variation in a thermal reactor after step negative reactivity insertion**

This example uses the same thermal reactor parameters as Table 1 . A step reactivity of  = -0.007 was input with time step $h = 0.1$ s and $N(0) = 1.0$ cm^{-3}, and neutron density (cm^{-3}) variation over 1 s was calculated. Using the analytical solution as the reference standard, the results from the program's core power calculation model show excellent agreement, with a maximum deviation of 0.32%, demonstrating high calculation accuracy.

**Table 3 Benchmarking of Neutron Density Transients in Fast Reactor under Step Negative Reactivity Insertion**

| Time (s) | Analytical Solution | Simulated Value | Relative Error (%) |
|---|---|---|---|
| 0.1 | 4.900708×10^{-1} | 4.916599×10^{-1} | 0.32 |
| 0.2 | 4.809743×10^{-1} | 4.822272×10^{-1} | 0.26 |
| 0.3 | 4.727747×10^{-1} | 4.738390×10^{-1} | 0.23 |
| 0.4 | 4.652903×10^{-1} | 4.661611×10^{-1} | 0.19 |

| Time (s) | Analytical Solution | Simulated Value | Relative Error (%) |
|---|---|---|---|
| 0.5 | 4.583873×10^{-1} | 4.590609×10^{-1} | 0.15 |
| 0.6 | 4.519650×10^{-1} | 4.524387×10^{-1} | 0.10 |
| 0.7 | 4.459467×10^{-1} | 4.462189×10^{-1} | 0.06 |
| 0.8 | 4.402732×10^{-1} | 4.403429×10^{-1} | 0.02 |
| 0.9 | 4.348978×10^{-1} | 4.347644×10^{-1} | -0.03 |
| 1.0 | 4.297830×10^{-1} | 4.294464×10^{-1} | -0.08 |

In summary, under both positive and negative step reactivity input conditions, the NUSOL-LMR-Li simulation results show good agreement with analytical solutions, demonstrating the correctness of the core power calculation model in the program and its capability to meet the needs of reactivity insertion accident calculations.

### 2.3.3 Stirling Thermoelectric Conversion Model Verification

**(1) Validation of Stirling Cycle Thermal Parameters**

Sunpower designed and tested a 1 kW Stirling engine prototype named RE-1000 for NASA. The experimental results from this prototype were used to validate the Stirling model. The main parameters of RE-1000 are listed in Table 4 , which compares calculated results with experimental data, showing a maximum relative error of 1.63%. This demonstrates that the model can provide accurate data for Stirling engine analysis.

**Table 4 Comparison of Calculation Result with Experimental RE-1000 Prototype Data**

| Parameter | RE-1000 Prototype | Simulated Value | Relative Error (%) |
|---|---|---|---|
| Expansion Space Temp. (K) | 923 | 925 | 0.22 |
| Compression Space Temp. (K) | 333 | 335 | 0.60 |
| Avg. Pressure (MPa) | 7.5 | 7.4 | 1.33 |
| Stirling Cycle Efficiency (%) | 28.5 | 28.0 | 1.63 |

**(2) Lithium-Cooled Stirling Cycle Loop Calculation**

Based on a simplified SP-00 system design, the modeling diagram of the lithium-cooled fast reactor Stirling power conversion system loop is shown in Figure 10

[Figure 10: see original paper]. Fluid exiting the core flows into the upper plenum, then to the primary side of the Stirling engine. After exiting the primary side, it passes through the downcomer via the liquid metal electromagnetic pump, then enters the lower plenum and returns to the core, forming the primary loop circulation. The secondary side uses NaK coolant, with inlet flow and outlet pressure boundaries specified to simulate secondary fluid flow.

To conduct subsequent transient safety analysis of accidents in the lithium-cooled fast reactor Stirling cycle system, steady-state simulation of the reactor was performed using the NUSOL-LM-Li program. Based on system and Stirling engine parameters from literature, Table 5 presents the calculated main steady-state thermal-hydraulic parameters compared with design values. The results show that the Stirling hot and cold ends maintain a certain temperature difference, consistent with Stirling engine operating characteristics, indicating normal thermodynamic cycle operation. In summary, the steady-state results demonstrate successful modeling.

**Table 5 Steady-State Thermal-Hydraulic Parameters of LFR-Stirling System: Calculation vs Design Benchmark**

| Parameter | Design Value | Calculated Value | Relative Error (%) |
|---|---|---|---|
| Core Thermal Power (kW) | 100.0 | 100.3 | 0.30 |
| Core Outlet Temp. (K) | 1300 | 1305 | 0.38 |
| Core Inlet Temp. (K) | 1200 | 1198 | -0.17 |
| Secondary Side Outlet Temp. (K) | 650 | 652 | 0.31 |
| Avg. Stirling Hot Side Temp. (K) | 1250 | 1248 | -0.16 |
| Avg. Stirling Cold End Temp. (K) | 600 | 598 | -0.33 |
| Stirling Cycle Efficiency (%) | 40.0 | 39.8 | -0.50 |

## 3 Accident Transient Safety Analysis

To evaluate the safety performance and adaptive capability of the lithium-cooled fast reactor Stirling power conversion system under abnormal conditions, transient accident safety analysis was conducted for the lithium-cooled Stirling cycle loop system described at the end of Chapter 2. The analysis object is a simplified model based on the SP-100 space nuclear reactor design, including the core, Stirling engine, electromagnetic pump, and other important components. The system transfers core heat to the Stirling engine hot end through primary loop liquid lithium, driving helium working fluid for thermoelectric conversion. Secondary loop NaK coolant exchanges heat with the Stirling engine cold end, with waste heat discharge treated through equivalent boundary conditions.

Using the NUSOL-LMR-Li program, this chapter systematically analyzes the dynamic response characteristics of core power, temperature distribution, and Stirling cycle efficiency by simulating reactivity insertion accidents and unexpected Stirling engine performance degradation accidents, evaluating the system' s inherent safety and fault tolerance capabilities.

### 3.1 Reactivity Insertion Accident

This accident assumes a 0.1$ reactivity insertion, with the protection system not functioning. Figure 11 [Figure 11: see original paper] shows the core power transient response characteristics.

Core power rapidly rises to 534 kW within 7 seconds. The rise in core power causes rapid increases in core temperature and primary loop temperature (Figure 12 [Figure 12: see original paper]).

Figure 11 Changes in core power under Reactivity Insertion Accident

Figure 12 Core Inlet and Outlet Temperature under Reactivity Insertion Accident

For the secondary loop, additional reactivity insertion causes the Stirling cycle hot-end temperature to rise. To maintain normal circulation, the cold-end temperature rises accordingly, thereby driving up the secondary loop temperature (Figure 13). However, due to the overall negative feedback regulation mechanism, core power gradually decreases and finally stabilizes at 466 kW. As power stabilizes, temperatures eventually become stable. The temperature rise amplitudes for core inlet/outlet and secondary side outlet are 5.74 K, 14.7 K, and 2.24 K respectively.

Figure 13 [Figure 13: see original paper] Change of Secondary Side Outlet Temperature under Reactivity Insertion Accident

In summary, although additional reactivity was introduced at 0 seconds, causing instantaneous rises in core power and temperature, the system' s negative feedback regulation mechanism effectively controlled core power, causing it to finally stabilize at 466 kW. This process not only demonstrates the self-regulating capability and inherent safety of the lithium-cooled Stirling cycle system under overpower accidents, but also emphasizes the important role of negative feedback mechanisms in maintaining safe and stable reactor operation.

### 3.2 Stirling Engine Performance Degradation Accident

When operating in space, the regenerator in the Stirling power conversion system is responsible for controlling the temperature difference between the expansion and compression chambers. If due to impurity accumulation or corrosion, the regenerator becomes blocked, the thermoelectric conversion efficiency of the Stirling engine will be affected and decrease.

In this accident analysis, it is assumed that the Stirling engine operates normally from 0-10s, and from 10s to 300s the Stirling engine efficiency begins to decrease to 50% due to regenerator blockage, with no protective action. As can be seen from Figure 14 [Figure 14: see original paper], the accident occurs at 10s, Stirling cycle efficiency drops to the lowest point of 22.24% at 30s, after which the system gradually adapts to the partially blocked condition and slowly rises and stabilizes at 26.8%.

Figure 14 Change of Stirling Cycle Efficiency under Stirling Engine Unanticipated Performance Degradation Accident

Figure 15 shows the average temperature changes of the hot and cold ends of the Stirling engine during the regenerator blockage accident. During the normal operation phase of 0-10 seconds, the hot-end and cold-end temperatures remain stable at approximately 1318 K and 660 K respectively, with a temperature difference of about 658 K. After the accident (10-30 seconds), due to the sudden drop in heat transfer efficiency caused by regenerator blockage, the hot-end initially drops sharply (approximately 300 K), finally stabilizing at 1023 K. This reflects the direct cutoff effect of blockage on the working fluid' s expansion work capability, while the formation of the subsequent stable state originates from the spontaneous thermal equilibrium reconstruction mechanism of the Stirling cycle system. Meanwhile, due to the decrease in cycle efficiency, the high-temperature thermal energy carried by the helium working fluid in the Stirling engine cannot be effectively exported by the cold end, causing heat to accumulate in the regenerator and cold-end chamber in a short time, and the cold-end temperature is therefore rapidly raised to 900 K, causing the secondary side temperature to also rise. As a new cycle is established, the cold-end temperature decreases to 692 K, and the secondary side temperature also changes from rising at the beginning of the accident to decreasing and stabilizing (Figure 16), confirming the high-efficiency robustness of cold-end heat exchange: despite the sudden reduction in hot-end input causing short-term thermal coupling imbalance, the heat dissipation system still resists working fluid condensation risk by maintaining a constant temperature gradient. Notably, the temperature difference between hot and cold ends narrows from the initial 658 K to 330 K in the accident steady state. This temperature difference reconstruction phenomenon precisely corresponds to the actual efficiency value of 26.8% (lower than the original design of 40%, but higher than the Carnot theoretical lower limit of 21%), revealing the physical essence of temperature difference as the decisive factor in cycle efficiency. This dynamic evolution process highlights the thermal inertia buffering characteristics and fault self-adaptive capability of the lithium-cooled fast reactor-Stirling coupled system, providing key experimental evidence for the reliability of continuous power supply of space nuclear power under extreme operating conditions.

Figure 15 [Figure 15: see original paper] The average temperature changes in the hot and cold sides of a Stirling engine under Stirling Engine Unanticipated Performance Degradation Accident

Figure 16 [Figure 16: see original paper] Change of the Outlet Temperature of the Secondary Side under Stirling Engine Unanticipated Performance Degradation Accident

To analyze the transient safety characteristics of the lithium-cooled space reactor Stirling power conversion system, this study developed the transient analysis program NUSOL-LMR-Li, and systematically evaluated its performance and reliability through relevant model validation and accident analysis. The main conclusions of the study are as follows:

## 1. High-Precision Model Validation

The core power model demonstrated high accuracy through validation against two typical cases: step positive reactivity insertion and step negative reactivity insertion, with maximum relative errors of 0.53% and 0.32% respectively. Additionally, the stability of the fully implicit algorithm was confirmed through a single-pipe flow example driven by pressure difference, where flow velocity remained consistent even with large time steps. These validations ensure the reliability and accuracy of core power calculations and numerical methods, which are crucial for the safety and performance of lithium-cooled fast reactor Stirling power conversion systems. Steady-state validation based on NASA' s RE-1000 Stirling prototype parameters showed maximum calculation errors of 1.63% for hot-end temperature, cold-end temperature, and cycle efficiency. In the lithium-cooled Stirling cycle loop validation, the maximum error between steady-state results calculated by the program and design parameters was 3.8%, proving the high accuracy and reliability of NUSOL-LMR-Li in simulating lithium-cooled fast reactor Stirling thermoelectric conversion systems.

## 2. Accident Safety Characteristics

**Reactivity Insertion Accident:** 0.1$ reactivity insertion into the core at 0 s. Core power rises to 534 kW within 7 seconds, then self-stabilizes at 466 kW through fuel Doppler effect and coolant density negative feedback mechanisms. The core outlet temperature increase is limited to within 14.7 K, demonstrating the system' s self-regulating capability and inherent safety under overpower accidents.

**Stirling Engine Performance Degradation Accident:** Due to regenerator blockage, Stirling engine efficiency decreases from 40% to 22.24%. However, the system restores thermal equilibrium through autonomous thermal balance reconstruction (hot-end temperature decreases to 1023 K, cold-end temperature increases to 692 K), recovering efficiency to 26.8%, approaching 80% of the Carnot efficiency limit. The temperature difference between hot and cold ends narrows from 658 K to 330 K, revealing the dominant role of temperature difference in efficiency and the system' s fault tolerance characteristics.

Based on NUSOL-LMR-Li, this study achieved transient simulation of the lithium-cooled fast reactor Stirling cycle system, validating model accuracy in the program. Through accident analysis specifically targeting the characteristics of the lithium-cooled fast reactor Stirling cycle system, the study not only reveals the negative feedback self-stabilization mechanism of lithium-cooled fast reactors but also demonstrates the temperature-difference-driven efficiency characteristics of the Stirling power conversion system under the innovative accident scenario of unexpected Stirling engine performance degradation. This provides theoretical basis and technical support for safety design and fault tolerance optimization of space reactor power systems. The NUSOL-LMR-Li program serves as an efficient analysis tool that can provide important references for future space reactor transient safety assessment and engineering optimization. Subsequent work will simulate and analyze more accidents based on actual space reactor operating conditions.

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