Transient Startup Analysis of a Space Lithium-Cooled Reactor Coupled with a He-Xe Closed Brayton Cycle

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Full Text

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Keywords: Space Nuclear Power, Brayton cycle, Safe transient startup

Introduction

For space exploration missions, three fundamental energy sources are currently available: chemical, photovoltaic, and nuclear-based solutions [?]. Photovoltaic systems utilize semiconductor junctions to convert solar electromagnetic radiation into electrical energy through the photovoltaic effect. However, their operational efficacy exhibits strong correlation with solar flux density variations, thereby restricting their practical deployment to near-Earth orbital regimes [?]. Electrochemical energy storage systems, despite their capability for rapid energy discharge, are fundamentally constrained by specific energy limitations that preclude their application in sustained deep-space operations [?]. In contrast, nuclear power systems demonstrate unique advantages including environmental invariance and exceptional energy density characteristics, establishing them as the enabling technology for deep-space exploration initiatives.

The development of space nuclear power systems was accelerated during the Cold War as the U.S. and Soviet Union sought to surpass the limitations of chemical propulsion. In 1955, the U.S. established the SNAP program, which led to the successful deployment of the first orbital nuclear reactor with thermoelectric conversion technology in 1965 [?]. Building on this success, subsequent projects included the Mini-BRU program for testing improved thermoelectric components [?], followed by the SP-100 project developing a lithium-cooled reactor with Brayton cycle conversion for kilowatt-level power generation [?, ?]. This technological progression culminated in NASA's 2002 Prometheus Program, which advanced the design to a high-temperature gas-cooled reactor coupled with a He-Xe Brayton cycle [?].

While U.S. space reactor development has predominantly focused on dynamic thermoelectric conversion systems, the Soviet Union initially pioneered static thermoelectric conversion technologies. This approach was exemplified by the TOPAZ-I and TOPAZ-II thermionic emission reactors [?]. Following the dissolution of the Soviet Union, Russia transitioned toward high-power dynamic conversion systems. A significant milestone in this shift was the 2009 proposal by Rosatom (Russian State Atomic Energy Corporation) of the Transportable

Energy Module (TEM) project. This initiative features a Brayton cycle-based thermoelectric conversion system designed to enable megawatt-class nuclear-powered spacecraft for deep-space exploration [?]. In parallel, Chinese research institutions have made substantial progress in developing He-Xe Brayton cycle technology for space applications [?].

Substantial progress has also been made in theoretical research on space Brayton cycles. For working fluid selection in the cycle, researchers have developed property calculation models for various inert gas mixtures using virial coefficient expansion [?, ?]. Their work confirmed that a helium-xenon (He-Xe) mixture with a molecular weight of 40 g/mol is the optimal working fluid for space Brayton cycles. Additionally, performance evaluation studies have been conducted for turbine designs under different working fluid conditions [?].

Regarding system modeling approaches, Wright et al. [?] performed comprehensive analysis of both steady-state and dynamic thermodynamic models employing lumped-parameter methods for integrated systems. Subsequent research has further advanced component-level design and modeling techniques to address the inherent limitations of lumped-parameter approaches [?].

Extensive research has been conducted on system optimization to analyze parameter interactions and identify optimal configurations for achieving design objectives. Specifically, Ma et al. [?] investigated the impact of working fluids on system performance and mass, with the dual objectives of maximizing cycle efficiency while minimizing total mass. Their study explored optimal parameter combinations, including the He-Xe molar ratio and component inlet temperatures.

Regarding transient startup optimization, Li et al. [?] improved Wright's startup process design [?], reducing both the time to steady-state and peak power demand. Liu et al. [?] analyzed the optimal startup strategy for heat sources with high thermal inertia, addressing slow thermal response challenges. Liao et al. [?] proposed a pressure-controlled startup method, leveraging inventory control in later phases to elevate system pressure and achieve the target power output. Regarding system safety, Wang et al. [?] explored reasonable startup and shutdown procedures while accounting for constraints such as surge margin, temperature change rate, heater mass flow rate, and maximum motor power consumption.

Recent research endeavors into space-based Brayton cycle systems have focused predominantly on gas-cooled reactors. In contrast, research addressing lithium-cooled reactor coupled Brayton cycles, particularly with regard to transient startup characteristics, remains comparatively limited. This study consequently focuses on the transient startup behavior and safety assessment of a Brayton cycle system coupled with a lithium-cooled reactor. The research encompasses the following:

1. The establishment of a numerical simulation framework for the lithium-cooled reactor coupled Brayton cycle system, incorporating three typical

control strategies.
2. The evaluation of safety-relevant operational constraints during transient startup conditions.
3. The formulation of an optimized startup sequence compliant with the identified safety constraints.

System Description and Model Construction

System Description

This study constructs a thermoelectric conversion device for space nuclear power applications, coupling a lithium-cooled reactor with a closed Brayton cycle. As illustrated in [Figure 1: see original paper], the system primarily consists of three parts: the reactor loop (marked 9 and 10), the Brayton cycle loop which is also referred to as the secondary loop (marked 1 to 6), and the radiator loop (marked 7 and 8). Its operation is summarized as follows:

1. The reactor loop: In the reactor loop, the reactor generates heat, which is transferred to the hot-side inlet (marked 10) of the intermediate heat exchanger. After heat exchange at the cold side, the temperature decreases, and the coolant flows into the reactor inlet (marked 9) to cool the reactor, ensuring its normal operation. The working fluid in this loop is liquid lithium.

2. The Brayton cycle loop: The Brayton cycle loop employs a He-Xe gas mixture. The cold side of the intermediate heat exchanger absorbs heat from the reactor. The He-Xe mixture exits the cold-side outlet (marked 4) and enters the turbine, where it expands and performs work, resulting in a temperature drop. It then flows into the hot-side inlet (marked 5) of the recuperator for heat recovery, improving cycle efficiency. After further cooling at the cold side of the recuperator, the gas exits the hot-side outlet and enters the hot-side inlet (marked 6) of the cooler for additional cooling. The cooled He-Xe then enters the compressor inlet (marked 1) for compression, raising its temperature. The compressed gas flows into the cold-side inlet (marked 2) of the recuperator for reheating. The reheated gas then enters the cold-side hot inlet (marked 3) of the intermediate heat exchanger, completing the cycle.

3. The radiator loop: In the cooler, the hot-side working fluid is He-Xe, while the cold-side working fluid is a Na-K alloy, which is connected to the radiative heat radiator. The Na-K alloy enters the cold-side inlet (marked 8) of the cooler, absorbs heat from the He-Xe gas, and then flows into the radiator inlet (marked 7) to dissipate the waste heat into space via the radiative heat radiator, completing the heat rejection process.

Reactor Model

The reactor model consists of two parts. The first part is the point kinetics equations describing the relationship between reactor power and reactivity, while the second part is the core heat transfer equations characterizing the variations in core power and fuel temperature.

The reactor neutron kinetics equations are used to describe the generation of fission power in the core and are widely applied in nuclear power systems [**?**]. Equations 1-3 represent the point kinetics equations.

$$\frac{dP_{R,th}}{dt} = \frac{\rho - \beta}{\Lambda} P_{R,th} + \sum_{i=1}^{6} \lambda_i C_i$$

$$\frac{dC_i}{dt} = \frac{\beta_i}{\Lambda} P_{R,th} - \lambda_i C_i$$

where $P_{R,th}$ is the core power of the lithium-cooled reactor, Λ is the neutron lifetime, λ_i is the decay constant of delayed neutrons, β_i is the fraction of delayed neutrons, C_i is the concentration of delayed neutron precursor nuclei, the subscript i corresponds to the neutron group designation, and ρ is the reactivity.

When $\rho > 0$, the power level increases; when $\rho < 0$, the power level decreases; and when $\rho = 0$, the power remains stable. The reactivity ρ can be described by equation 4:

$$\rho = \rho_0(t) - \alpha(T_{fuel} - T_0)$$

where ρ_0 is the initially introduced reactivity, T_{fuel} is the fuel temperature, T_0 is the reference temperature at rated power, and α is the temperature feedback coefficient representing reactivity changes due to fuel expansion or other effects.

The total energy generated by reactor fission is partially transferred to the liquid lithium coolant and partially absorbed by the reactor fuel. This heat transfer process can be calculated using the fuel temperature and the average temperature of the liquid lithium metal (equations 5-7):

$$P_{R,C} = \dot{m}_{Li} c_{p,Li} (T_{coolant,out} - T_{coolant,in})$$

$$\frac{dT_{fuel}}{dt} = \frac{k_f P_{R,th} - \gamma_{fuel}(T_{fuel} - T_{aver})}{M_{fuel} c_{p,fuel}}$$

$$T_{aver} = \frac{T_{coolant,out} + T_{coolant,in}}{2}$$

where $\dot{m}_{Li}$ is the lithium mass flow rate, $c_{p,Li}$ is the constant-pressure specific heat capacity of lithium, k_f is the fuel heat capacity coefficient, γ_{fuel} is the fuel-to-coolant heat transfer time constant, $P_{R,C}$ is the thermal power transferred from core to lithium coolant, $T_{coolant,out}$ is the outlet temperature of liquid lithium metal, and $T_{coolant,in}$ is the inlet temperature of liquid lithium metal.

Heat Exchanger Model

The heat exchanger transfers thermal energy from the primary fluid to the secondary fluid, and its rational use can enhance the efficiency of the space Brayton cycle [?]. In the lithium-cooled reactor coupled with a closed space Brayton cycle system, three heat exchangers are required: the intermediate heat exchanger, the recuperator, and the cooler. All three are printed circuit heat exchangers (PCHEs), which exhibit excellent performance under high-temperature and high-pressure conditions. Due to their highly compact structure, they are particularly suitable for space-constrained Brayton cycle applications.

In the intermediate heat exchanger, the hot-side working fluid is lithium, while in the cooler, the cold-side working fluid is a Na-K alloy. For all other components, the working fluid is a He-Xe gas mixture with a molecular weight of 40 g/mol.

The heat exchanger model consists of two parts: the heat transfer module and the pressure drop module [?]. For the heat transfer module, the heat exchange processes of the hot-side channels, cold-side channels, and the heat exchanger itself are separately considered and determined by equations 8-10:

$$\dot{m}_h c_{p,h} \frac{dT_h}{dx} = \dot{m}_h c_{p,h}(T_{h,i} - T_{h,o}) - h_h A(T_h - T_{HX})$$

$$\dot{m}_c c_{p,c} \frac{dT_c}{dx} = \dot{m}_c c_{p,c}(T_{c,i} - T_{c,o}) - h_c A(T_{HX} - T_c)$$

$$M_{HX} c_{p,HX} \frac{dT_{HX}}{dt} = h_h A(T_h - T_{HX}) - h_c A(T_{HX} - T_c)$$

where T_{HX} is the structural temperature of the heat exchanger, A is the heat-exchange area, M_{HX} is the mass of the heat exchanger, m is the mass of the working fluid in the flow channel, $\dot{m}$ is the mass flow rate of the working fluid in the flow channel, $T = \frac{T_i + T_o}{2}$ is the average inlet-outlet temperature of the corresponding flow channel. The subscript h denotes the hot-end flow channel, the subscript c denotes the cold-end flow channel, and h is the heat transfer coefficient. The heat transfer coefficient calculation references the literature [?].

The pressure drop model describes the pressure difference between the inlet and outlet of the three heat exchangers, primarily determined by the pipe characteristics and the thermophysical properties of the working fluid, and can be calculated using equations 11-13:

$$\Delta P = f \frac{\rho u^2 L}{2d}$$

$$f = \frac{0.079}{Re^{0.25}}$$

where f is the pipe friction factor, ρ is the working fluid density, μ is the working fluid viscosity, L is the pipe length, and d is the hydraulic diameter of the flow channel.

TAC Model

As illustrated in [Figure 2: see original paper], the TAC model refers to the integrated coaxial assembly of the turbine-alternator-compressor system, comprising the turbine, compressor, alternator, and rotating shaft [?]. The TAC serves as the core component of the Brayton cycle, providing the driving force for the He-Xe working fluid circulation in the secondary loop while simultaneously powering shaft rotation and electricity generation through the alternator. Its power balance equation is expressed as equation 14:

$$P_{Shaft} = P_T - P_C - P_L$$

where P_{Shaft} is the shaft surplus power, P_T represents the turbine-generated power, P_C denotes the compressor-consumed power, and P_L corresponds to the generator power (electrical load).

In the Brayton cycle, the turbine expands the working fluid to perform work, generating power expressed as equation 15:

$$P_T = \dot{m}_{HeXe} c_{p,t}(T_4 - T_5)$$

The compressor consumes power while compressing the working fluid, with the power consumption expressed as equation 16:

$$P_C = \dot{m}_{HeXe} c_{p,c}(T_2 - T_1)$$

where $\dot{m}_{HeXe}$ is the mass flow rate of the He-Xe gas mixture, $c_{p,t}$ and $c_{p,c}$ represent the mean specific heat at constant pressure in the turbine and compressor respectively, T_1 and T_2 denote the compressor inlet and outlet temperatures, while T_4 and T_5 indicate the turbine inlet and outlet temperatures.

P_{Shaft} is the power source driving the shaft rotation, and the shaft speed N of the generator shaft satisfies equation 17:

$$\frac{dN}{dt} = \frac{P_{Shaft}}{I \cdot (2\pi)^2}$$

where I is the moment of inertia and N represents the shaft speed of the shaft.

The shaft power P_{Shaft} governs the shaft speed variation: when $P_{Shaft} > 0$, the shaft speed N increases gradually; when $P_{Shaft} < 0$, the shaft speed N decreases progressively.

The TAC model requires obtaining the outlet temperatures and pressures as they vary with shaft speed N and mass flow rate $\dot{m}_{HeXe}$. These relationships can be characterized using the performance curves of turbines and compressors.

Generally, there are three main types of performance curves for turbines and compressors: pressure ratio curves, temperature ratio curves, and efficiency curves. This study references [**?**] performance curves for qualitative research, including the pressure ratio curve and polytropic efficiency.

The outlet pressure P_o can be obtained from the inlet pressure P_i through the pressure ratio, and the outlet temperature T_o can be derived from the inlet temperature T_i by calculating the temperature ratio using the polytropic efficiency formula.

The compressor characteristic curve ([Figure 3: see original paper]) and temperature calculation can be expressed by equations 18-20:

$$\frac{P_2}{P_1} = f_{prC}(\dot{m}_{HeXe}, N, P_1, T_1)$$

$$\eta_{pC} = f_{\eta pC}(\dot{m}_{HeXe}, N, P_1, T_1)$$

$$\frac{T_2}{T_1} = \left(\frac{P_2}{P_1}\right)^{(\gamma-1)/(\gamma\eta_{pC})}$$

where T_1 and P_1 represent the compressor' s inlet temperature and pressure respectively, T_2 and P_2 denote the compressor's outlet temperature and pressure, η_{pC} is the compressor' s polytropic efficiency, and γ is the specific heat ratio of the He-Xe gas mixture.

Similarly, the turbine' s characteristic curves ([Figure 4: see original paper]) and temperature calculation can be expressed by equations 21-23:

$$\frac{P_4}{P_5} = f_{prT}(\dot{m}_{HeXe}, N, P_4, T_4)$$

$$\eta_{pT} = f_{\eta pT}(\dot{m}_{HeXe}, N, P_4, T_4)$$

$$\frac{T_5}{T_4} = \left(\frac{P_5}{P_4}\right)^{(\gamma-1)/\gamma\cdot\eta_{pT}}$$

It should be noted that in actual modeling, the turbine' s pressure ratio characteristic curve is typically expressed in an equation-equivalent form (equation 24), where the mass flow rate of the He-Xe gas mixture in the loop is calculated based on the pressure ratio:

$$\dot{m}_{HeXe} = f_{mprT}(N, P_4, P_5, T_4)$$

Radiator Model

As illustrated in [Figure 1: see original paper], the He-Xe gas exiting the cold end of the regenerator passes through a cooler before entering the compressor. The waste heat extracted by the cooler is rejected into space via a radiative radiator.

The thermal coupling between the cooler and radiative radiator is achieved using flowing Na-K alloy. The alloy absorbs heat at the cooler and transports it to the radiator panels, where the heat is ultimately radiated into space according to the Stefan-Boltzmann law (equation 25):

$$Q_{radi} = \varepsilon \sigma A_{radi}(T_7^4 - T_{ref}^4)$$

where Q_{radi} is the radiated waste heat, ε is the emissivity, σ is the Stefan-Boltzmann constant, A_{radi} is the radiator panel area, T_7 is the radiator inlet temperature, and T_{ref} is the space environment temperature (set to 225 K in this study).

Steady Point Design and Verification

The component models are interconnected as illustrated in [Figure 1: see original paper] to form a preliminary Brayton cycle system, which is then validated.

The system is designed with a reactor outlet temperature of 1485 K, a shaft speed of 45,000 rpm, a turbine outlet pressure of 1.8 MPa in the low-pressure loop, and a cycle pressure ratio of 2.1. Following Wright' s steady-state design methodology [**?**], the steady-state operating conditions are derived.

The initial conditions are input into the constructed system, and the results are compared with theoretical values, as presented in . The discrepancies are all within acceptable tolerances.

Control Strategy and Controller Model

The safe and stable startup of the system requires a series of control strategies. Currently, there are six main control methods for space reactor-coupled Brayton cycles: reactivity control, shaft speed control (also referred to as parasitic load control), inventory control, bypass valve control, throttle valve control, and radiator control. This study analyzes safe startup using the first three control strategies.

Reactivity Control

Reactivity control refers to the regulation of reactor power and outlet temperature by adjusting the inserted reactivity, thereby achieving power modulation and safety constraints. Generally, as illustrated in [Figure 5: see original paper], reactivity control can be implemented in two modes:

1. Predefined outlet temperature T_{ref}: As illustrated in [FIGURE:5(a)], the reference outlet temperature T_{ref} is initially calculated by the T_{ref} calculator based on the designed startup process and safety constraints. Both T_{ref} and the actual outlet temperature T_{actual} are then fed into the PID controller to generate the reactivity insertion ρ_{in} command. This command is transmitted to the control rod assembly to dynamically adjust the inserted reactivity ρ_{in}, thereby ensuring precise tracking of the actual outlet temperature to the reference temperature profile.

2. Predefined inserted reactivity curve $\rho_{in,ref}$: As illustrated in [FIGURE:5(b)], following the acquisition of the reference outlet temperature profile T_{ref}, this parameter is processed by the control rod worth calculator to calculate the insert reactivity command ρ_{in}. This command is then transmitted to the control rod assembly to dynamically adjust the inserted reactivity $\rho_{in,ref}$, thereby generating the actual outlet temperature ρ_{actual}. The observed temperature differential may optionally initiate an iterative feedback correction cycle to compute optimized insert reactivity adjustments, contingent upon system control requirements and operational constraints.

It should be noted that reactivity control is typically achieved by moving reactor control rods. Frequent rod movements may compromise component longevity and reliability. Although repeated temperature fluctuations could also impact fuel reliability, their effects can be mitigated by rationally pre-setting the reactivity insertion profile. Therefore, this study adopts the predefined inserted reactivity $\rho_{in,ref}$.

Shaft Speed Control

According to the relationship between the shaft surplus power P_{Shaft} and shaft speed N (equations 14, 17), the surplus power can influence shaft speed variation. In practical systems, considerations of lower power consumption and system stability necessitate regulating the generator’ s electrical output power P_L to adjust the surplus power, thereby controlling shaft acceleration and deceleration. This method is termed shaft speed control or parasitic load control, as the means of adjusting generator output power involves controlling the parasitic load connected to the generator.

As illustrated in [Figure 6: see original paper], a PMAD controller is connected to the alternator. As illustrated in [Figure 7: see original paper], the PID controller receives the reference shaft speed N_{ref} and actual shaft speed N_{actual} to derive the command for parasitic load, then transmits this command to the parasitic load regulator to modify the parasitic load of the system. Meanwhile, the alternator model calculates the actual shaft speed based on the turbine power, compressor power, and parasitic load power according to equations 14 and 17.

It should be noted that when N starts changing from zero (i.e., when the TAC begins operating), the power of the turbine and compressor is relatively low. To achieve a stable increase in shaft speed ($P_{Shaft} > 0$), it is necessary to maintain a state where $P_L < 0$ for a certain period, meaning the generator consumes external power rather than generating it. After the TAC operates for some time and the turbine' s power output significantly exceeds the compressor' s power consumption, the shaft speed can increase without requiring external power consumption.

Inventory Control

In the Brayton cycle loop of the system, the working fluid He-Xe fills the pipelines. As illustrated in [Figure 8: see original paper], the entire secondary loop pipeline is divided into 10 segments. To satisfy the mass conservation of the working fluid, the filling mass of the working fluid is constrained by the volume of each pipeline segment and the temperature and pressure of the working fluid within each segment. This constraint is referred to as the inventory constraint (equation 26):

$$\sum_{i=1}^{10} \frac{P_i V_i}{R T_i} = m_{total}$$

where V_i, P_i, T_i represent the volume, average pressure, and average temperature of the gas in the i-th pipeline segment respectively, R is the gas constant, and m_{total} is the total mass of He-Xe gas mixture.

By appropriately regulating the total mass m_{total} filling the pipeline, the loop pressure can be controlled. An increase in pressure enhances the He-Xe mass flow rate while maintaining constant pressure ratio, temperature ratio, and shaft speed in the TAC components, thereby enabling the generator to produce higher power. Inventory control allows flexible adjustment of the system' s stable operating point for load-following operations. It can also prevent shaft overspeed during load rejection by reducing the total gas mass in the loop, thereby decreasing turbine power.

As illustrated in [Figure 9: see original paper], a He-Xe storage tank is connected to the low-pressure loop (e.g., turbine outlet). An inventory controller is connected to the tank, as illustrated in [Figure 10: see original paper]. The control scheme first calculates the reference turbine outlet pressure $P_{5,ref}$ profile through startup procedures and the $P_{5,ref}$ calculator. Subsequently, a PID controller regulates the fill mass of He-Xe gas mixture command and then transmits to the tank to ensure the actual turbine outlet pressure $P_{5,actual}$ trajectory tracks the $P_{5,ref}$. In this study, this approach aims to coordinate with the shaft speed control to achieve a safe and stable compressor working line.

Description of Safety Startup Constraint

The operation of the entire system involves coupling between multiple components, requiring consideration of necessary constraints for the safe operation of different parts. This study primarily focuses on reactor safety and TAC safety, with emphasis on TAC safety, while temporarily disregarding safety constraints for pipelines and heat exchange components.

Reactor Safety Constraints

The reactor requires consideration of numerous design details, such as control rod lifespan, cooling pipe dimensions, fuel cladding integrity, etc. These issues necessitate repeated testing by specialized designers of space reactors. However, the model constructed in this study is a lumped-parameter model and cannot delve into such depth. Therefore, this study only addresses the reactor' s temperature threshold and temperature rate of change, which will be incorporated into the steady-state and transient startup processes of the system design.

TAC Safety Constraints

The TAC machinery is the key component for energy conversion in the Brayton cycle, and ensuring its safety is critical for the stability of the entire system. The following issues must be considered to guarantee stable TAC operation.

First, TAC components generally employ gas bearings, which rely on a gas film formed during high-speed rotation to support the shaft and prevent metal-to-metal contact. During startup, when the shaft is at low speed, insufficient gas film thickness may lead to shaft wear, reduced lifespan, or even damage. Thus, the shaft speed must be rapidly increased to the design value (typically tens of thousands of rpm or higher) to quickly establish a stable gas film. This requires the TAC to accelerate to the target shaft speed as quickly as possible during startup.

Additionally, surge effects must be considered during TAC operation. Surge is one of the most hazardous unstable operating conditions, arising from an imbalance between gas flow and compressor energy transfer, leading to self-excited oscillations. Its primary manifestation occurs when the mass flow rate falls below a critical value $\dot{m}_{surge}$, causing rotating stall cells at the impeller inlet, propagating at approximately 30-50% of the impeller speed. In multistage compressors, these stall cells form a positive feedback loop between blades, eventually evolving into full-system surge. Therefore, the TAC must strictly maintain the mass flow rate of He-Xe gas mixture $\dot{m}_{HeXe} > \dot{m}_{surge}$ during operation.

Transient Startup Design and Analysis

For the startup process of a space Brayton cycle, there are generally two approaches: one is to first activate the TAC device to initiate the flow of the He-Xe working fluid, followed by reactor heating; the other is to first start the reactor heating and, once the temperature reaches a certain level, activate the TAC device. For gas-cooled reactors, both methods are feasible. However, for lithium-cooled reactors, the only viable approach is to first start the reactor and then the TAC, as the reactor' s initial temperature is 225 K, at which point the lithium coolant is in a solid state. Only after the reactor is activated and heats the lithium to its liquid phase can normal operation proceed.

Given that this study employs a lithium-cooled reactor, the startup process is divided into four phases: reactor preheating phase, TAC startup and low-power operation phase, shaft speed ramp-up phase, and temperature rise and final steady-state phase.

Reactor Preheating Phase

In this phase ([Figure 11: see original paper]), the reactor must first be preheated, with the focus on safely bringing the reactor to a suitable state for subsequent TAC startup. Initially, an inserted reactivity of 0.008 is introduced to allow the reactor power to increase slowly. By approximately 500 s, sufficient latent heat is generated. Subsequently, the inserted reactivity is linearly increased, causing a step change in the total reactivity. At this point, the reactor outlet temperature begins to rise. The rate of increase in inserted reactivity is controlled to prevent excessive temperature escalation. By around 2500 s, the inserted reactivity reaches the design value of 0.01, and the reactor temperature stabilizes at approximately 650 K, marking the end of the reactor preheating phase. The next step involves initiating the TAC device.

TAC Startup and Low-power Operation Phase

When the reactor preheating phase concludes, the TAC shaft is activated to initiate the Brayton cycle. To ensure the safety of TAC components, the shaft speed must be rapidly increased to a sufficiently high level through shaft speed control to minimize gas bearing wear. In this phase, the shaft speed is designed to linearly rise to 20,000 rpm within 100 s, accompanied by appropriate inventory control to ensure a rapid increase in the He-Xe mass flow rate, thereby avoiding surge.

The rise in mass flow rate enables the secondary loop to begin operation, activating heat exchange components. This leads to an increase in the reactor inlet temperature and a corresponding rapid rise in the outlet temperature. Once the temperature reaches a certain level, the radiator is activated at 3000 s. It should be noted that this study does not account for natural heat transfer when the TAC is inactive or the thermal inertia of the He-Xe gas in the secondary loop after TAC startup. As a result, as illustrated in [Figure 12: see original paper], the temperatures exhibit an abrupt increase at 2500 s.

After the shaft speed reaches 20,000 rpm, the insert reactivity remains at 0.01, and the shaft speed is also maintained until 5800 s. This phase represents a low-speed, low-temperature operational stage where the net power output is relatively low, approximately 55 kW. It is typically used for system inspection and maintenance, marking the first stable operational phase of the entire system.

As illustrated in [Figure 13: see original paper], the initial power output is negative, requiring an external power source to drive the shaft and rapidly increase the He-Xe mass flow rate in the loop. Only when the turbine’ s power output exceeds the compressor’ s power consumption does the net power become positive, signifying the start of electricity generation.

Shaft Speed Ramp-up Phase

Following the low-power operation phase, the shaft speed and temperature must be gradually increased to bring the system to its steady-state design point. To ensure system stability and safety, the shaft speed ramp-up and temperature rise must be executed in a staggered manner.

Starting at 5800 s, the shaft speed increases to the steady-state design value of 45,000 rpm over 2000 s. Since the shaft speed is already relatively high at 5800 s, a rapid acceleration is unnecessary. During this phase, the reactor reactivity remains at 0.01. The increase in shaft speed raises the mass flow rate in the loop to its final state, and the higher mass flow rate enhances heat removal by the heat exchangers, causing a slight decrease in the turbine inlet temperature ([Figure 14: see original paper]).

Meanwhile, the turbine temperature ratio is increased, but since the reactor insert reactivity remains unchanged, the turbine inlet temperature does not rise significantly, unlike in gas-cooled reactors. Consequently, the turbine outlet temperature also experiences a slight decline, widening the turbine temperature difference and increasing turbine power output. After this phase, an additional 2000 s is allocated to allow the system to reach steady state.

Temperature Rise and Final Steady-State Phase

At 10,000 s, the insert reactivity of the reactor is increased to 0.0185 over 1000 s and then remains constant. The reactor power rises, leading to an increase in the outlet temperature, which in turn elevates the temperatures of all components in the loop. The turbine and compressor power outputs increase, and the net power stabilizes at approximately 900 kW. Subsequently, the system operates at full power for 3000 s until 14,000 s. The transient startup process of the entire system is illustrated in [Figure 15: see original paper]-[Figure 18: see original paper].

Analysis of Control Strategy and Safety

The transient startup process of this system utilizes three control strategies: reactor control, shaft speed control, and inventory control.

For reactor control, this study primarily focuses on designing the timing and progression of insert reactivity changes to achieve precise temperature regulation. As a lithium-cooled reactor, the system exhibits low sensitivity to secondary loop mass flow variations, which are predominantly governed by inserted reactivity and lithium coolant mass flow rate. Consequently, the design can be rationally optimized based on target outlet temperature requirements and reactor safety constraints ([Figure 15: see original paper]).

For shaft speed control, this study regulates the parasitic load to achieve precise shaft speed management. This approach serves two purposes: First, it enables flexible adjustment of shaft speed according to operational requirements. As illustrated in [Figure 16: see original paper], the mass flow rate exhibits nearly linear correlation with shaft speed variation, allowing indirect mass flow control through shaft speed regulation to protect TAC components. Second, it maintains low-shaft speed steady-state operation with minimal energy penalty, thereby enhancing system efficiency [**?**].

For inventory control, regulating the total He-Xe gas inventory in the secondary loop enables flexible adjustment of loop pressure, which is critical for power regulation. Additionally, the loop pressure can be pre-designed to ensure controlled pressure variations that safeguard system stability. [Figure 19: see original paper] illustrates the pre-designed pressure management in the low-pressure loop section. [Figure 20: see original paper] indicates how the combined shaft speed control and inventory control strategy rapidly elevates the mass flow rate to a sufficiently high level, keeping the compressor operating line safely away from its surge line and preventing surge effects in TAC components.

Conclusions

This paper establishes a dynamic model of a space nuclear power system coupling a lithium-cooled reactor with a He-Xe Brayton cycle, comprising lithium-cooled reactor, heat exchange components, TAC assembly, radiator, and three control systems. Three control strategies were employed to design the transient startup process, with key achievements as follows:

1. A complete space Brayton cycle system was developed, achieving a megawatt-scale steady-state operating condition with a reactor outlet temperature of 1,485 K, shaft speed of 45,000 rpm, cycle pressure ratio of 2.1, and rated power output of 0.902 MW. The system’ s validity was verified through comparison with theoretical values.

2. Controller modules for the three control strategies were implemented and integrated with the safety constraints of the space Brayton cycle startup, ensuring logically sound control design.

3. A comprehensive transient startup scheme for the space Brayton cycle was developed with full consideration of system safety constraints.
4. A control strategy combining inventory control and shaft speed control was used. The results indicate that this strategy effectively constrains the TAC operating line to maintain sufficient margin from the surge line, thereby ensuring reliable TAC component safety.

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