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SNAP 8 AUTOMATIC STARTUP
AND PCS MERCURY INJECTION
SIMULATION TESTS

AEC Research and Development Report

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By
R. F. AUDETTE

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ABSTRACT

SNAP 8 reactor dynamic tests in the SNAP 8 Developmental Reactor system have demonstrated the capability for fully automatic startup over eight decades from shutdown and for automatically furnishing the heat load demand imposed by automatic startup of the flight system power conversion loop following reactor automatic startup.

Analyses of test results indicate a fuel temperature coefficient of reactivity = -0.105 ± 0.005 $\text{¢}/^{\circ}\text{F}$, a lower grid coefficient = -0.080 ± 0.005 $\text{¢}/^{\circ}\text{F}$, and no measurable upper grid coefficient. The lack of an upper grid coefficient is attributed to two probable conditions, coolant by-pass at the core periphery, and/or lack of contact between upper control drum supports and the core vessel adjacent to the upper grid. Test results have also confirmed the existence of near vacuum conditions in the space between the fuel and cladding tube at low temperatures. The increase in hydrogen gas pressure in the space, due to fuel hydrogen migration with increasing fuel rod temperature, progressed quite closely to theoretical estimates.

Due to test limitations imposed by the ground test heat exchanger (IHx) in the heat rejection loop, reactor dynamics tests to the full predicted capability for the SNAP 8 Nuclear System were not performed. The heat exchanger was originally designed only for steady state heat transfer service. However, the tests performed approximate predicted flight system operation, and it can be reasonably extrapolated from the test results that the SNAP 8 reactor will provide the dynamic response characteristics required for the flight system.

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I. INTRODUCTION

SNAP 8 reactor automatic startup tests, including tests simulating Power Conversion System (PCS) mercury-injection load demand to the primary loop coolant, have been performed on the SNAP 8 Developmental Reactor (S8DR) system to evaluate SNAP 8 response characteristics during an Electric Generating System (EGS) automatic startup. Test results show that the reactor can be automatically brought to power over an eight decade range from shutdown, in a well-controlled manner, without excessive power overshoot on entry into the useful power range (>1 kwt), and without excessive reactor fuel or coolant temperature transients. Following a period of loop coolant heating to design operating level (1300°F), test results also show that mercury loop bootstrap load demand on primary loop coolant can be met in an adequate and stable manner to provide automatic EGS system startup within the desired time interval.

In addition to dynamic response characteristics, the test results have also provided measurements of the reactor component temperature coefficients of reactivity and of the overall core heat transfer coefficient. Coefficients determined from test results were: fuel = -0.105 ± 0.005 $\text{¢}/^{\circ}\text{F}$, and lower grid = -0.080 ± 0.005 $\text{¢}/^{\circ}\text{F}$. The test transients, and separate flow oscillation tests designed to isolate upper grid coefficient effects, indicated no effective upper grid coefficient. It is believed that this condition is due to flow by-pass around the core periphery and/or free play between the upper grid and control drum supports. Evidence of flow by-pass greater than predicted was provided by measurement of the upper grid exit coolant temperature radial profile. Such by-pass flow would attenuate much of the upper grid radial expansion and contraction produced by core outlet coolant temperature oscillation during the flow oscillation tests. No direct measurement of the contact between the core tank surface at the upper grid level and the control drum supports was feasible during the planned test program.

The overall core heat transfer coefficient (UA) at low fuel temperatures corresponded to a near vacuum in the gas space between the fuel and cladding. As higher pressure and heat transfer rates had been anticipated, this condition produced an appreciably different power excursion on power range entry during

automatic startup than had been predicted from computer model studies. The very low hydrogen pressure resulted in a rapidly damped power oscillation below 10 kw in the startup power transient. The subsequent power rise produced hydrogen migration into the gas gap from increased fuel temperature and resulted in a core UA variation with fuel temperature that closely followed the theoretically predicted variation.

Due to thermal stress limitations on the ground test IHX used in the heat rejection loop, the tests performed on S8DR approximated predicted flight system transients imposed in an automatic EGS startup. The heat exchanger was originally designed for steady-state heat transfer service but had sufficient transient capability to satisfy the objective of these tests. Another inherent limitation in the simulation response speed was the greater heat storage capability of the S8DR primary loop relative to the flight system primary loop, the difference appreciably attenuating reactor inlet temperature change rates for the mercury-injection simulation tests. Though not designed for this type of test, the S8DR heat transfer system did, however, contain sufficient transient test capacity to provide an adequate indication of SNAP 8 dynamic response under approximate flight system transient conditions.

The proposed flight system automatic startup sequence and the S8DR capabilities for testing the SNAP 8 reactor are discussed below.

A. EGS AUTOMATIC STARTUP SEQUENCE

EGS automatic startup consists of two primary operations: automatic startup of the thermal power source (SNAP 8 nuclear system), followed by rapid and stable bootstrap of the PCS within specified time and thermal stress limits. Studies performed on the SNAP 8 system by AI and Aerojet-General Corporation (AGC) have defined the proposed startup sequence of Figure 1.⁽¹⁾

SNAP 8 startup consists of a programmed reactor control drum operation from shutdown, through criticality, and into the useful power range until reactor outlet coolant temperature reaches the lower limit of the automatic temperature controller deadband (1300 ± 20 °F). At this point, programmed drum operation is terminated and automatic temperature control is effected. Programmed control drum operation consists of positioning three of the six control drums from

Figure 1. SNAP 8 PCS Startup Sequence

full out (110°) position to the startup position ($\sim 68^\circ$) at the most rapid drum rotation rate available: 2-1/2 sec/step or 2.08 $^\circ/\text{min}/\text{drum}$. With the startup drums in position, the remaining three control drums are rotated from the full out position at the maximum rate to a position predetermined from drum worth measurements to result in a 50¢ subcritical core. The combination of startup and control drum positions for this condition is selected to result in reactor criticality when all six drums are near the same (68°) position. At the 50¢ subcritical position, the control drum stepping rate is decreased to a considerably lower rate for the neutron flux excursion through criticality and on into the useful power range. This stage of the automatic reactor startup imposes the most severe reactor fuel and coolant temperature transients experienced during the EGS startup, and occupies the major portion of the reactor automatic startup time. As a result, drum rate selection is essentially specified by startup time requirements and reactor thermal stress limits. In S8DR, the ground test heat transfer system component stress considerations limited the drum insertion rate to $\sim 120 \text{ sec/step}$ ($\sim 0.5 \text{ ¢/min}$) for an 8-hour startup time. In the flight system

SNAP 8, this rate could safely be doubled to 1 ℓ /min to obtain a startup time within the desired 5-hour interval shown on Figure 1.

Following the power range entry transient that occurs below 10 kw, the power rises smoothly up to ~ 100 kw before appreciable coolant temperature rise occurs. Lower grid temperature rise, produced by the coolant inlet temperature rise, reduces the power rise rate and eventually terminates in a slightly oscillatory power increase which is due to the interplay between fuel and grid reactivity coefficients and the drum reactivity insertion rate. Reactor outlet temperature control then establishes power level control to furnish system heat losses at operating temperature, marking the end of the nuclear startup phase.

PCS startup⁽¹⁾ is initiated by raising primary loop flow from 20 to 50% rated in 2 minutes followed by a 7.7-min period for reactor power and temperature stabilization by the reactor outlet temperature controller. At 8.3 min in the PCS startup program, the mercury loop gas valve is opened and the line to the mercury boiler is allowed to fill. Sixty seconds later, at 9.3 min, the mercury flow control valve is opened, in accordance with the program shown on Figure 1, to start mercury flow to the boiler. Primary loop flow is ramped from 50 to 100% rated in 30 sec at the 9.7-min point in the program (20 sec after opening of the mercury flow control valve) to obtain the required secondary loop bootstrap to power.

B. S8DR - SNAP 8 PCS SIMULATION CAPABILITY

Simulation of the SNAP 8 PCS in S8DR tests was primarily concerned with the simulation of the thermal load demand to the primary loop NaK during the PCS Hg-injection to full flow. The S8DR reactor is a flight configuration SNAP 8, and the principal S8DR approximation to the flight configuration primary loop consisted of the slightly greater NaK inventory in S8DR. The significance of this difference would be that slightly shorter startup times would occur for the flight system at a given drum rate, due to the smaller NaK inventory heat-up to operating levels and to closer coupling of the reactor and heat sink during PCS startup.

A schematic of the relationship of the principal components for each system is shown by Figure 2. The IHX used for primary loop heat rejection in S8DR was

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Figure 2. SNAP 8 PCS System and S8DR System Schematic

designed for high temperature steady-state heat rejection operation and provided transient test capability not specifically designed into the component. As a result, reactor testing to the desired flight system conditions was prevented by IHX transient thermal stress limitations for the ground test component. In terms of IHX primary side inlet temperatures, test transients were limited to excursions of 150°F at 20°F/min, maximum, or full range at 10°F/min, maximum. With an allowable rate of 150°F/min over the full temperature range for the SNAP 8 vessel and nozzles, the IHX transient limitations severely restricted the test transients which could be imposed on SNAP 8; however, the allowable IHX transients permitted approximate flight system transient conditions and provided an adequate indication of SNAP 8 response to flight system startup requirements.

IHX thermal stress limitations primarily affected the SNAP 8 startup tests as the simulated PCS startup transients at the IHX inlet—though higher in rate—were lower in magnitude than the 150°F - 20°F/min excursion limitation and were shown by stress history studies to be acceptable for the number of test cycles to be imposed.

The basic simulation required of the S8DR system consisted of the imposition of a thermal load demand on the IHX primary side NaK approximating the demand generated by mercury injection to the boiler of the SNAP 8 PCS. Heat removal from the S8DR secondary NaK at the air blast heat exchanger (ABHX) could be readily obtained for a reasonable simulation of the required heat removal transient; however, conveying this demand through the secondary loop surge tank and the NaK-to-NaK IHX at a somewhat inherently lower heat transfer rate appreciably attenuated the magnitude and rate of the ABHX exit temperature transient by the time the transient was conveyed to the IHX primary outlet. To obtain the desired heat removal transient, it was therefore decided to reduce surge tank temperature as rapidly as possible during low coolant flow operation and to follow the surge tank chill by a rapid secondary flow increase. By this procedure, the surge tank contents were transported through the IHX as rapidly as possible, thereby approximating the mercury injection load demand to the extent possible with the S8DR.

To obtain an indication of S8DR primary loop response to the desired Hg-injection load demand, a computer model calculation was made of S8DR response to the heat removal transient shown on Figure 3. The predicted reactor excursion is shown on Figure 4. The heat removal curve of Figure 3 assumes complete mercury vaporization for the mercury flow program shown. The computed transient shows reactor coolant temperature rates well within specified limits; the IHX inlet temperature transient, however, though within the specified limit for magnitude, is relatively high in rate and would have been unacceptable for the planned test program stress history. For the reasons discussed, other pre-test calculations, including secondary loop effects, showed a considerably less severe IHX inlet temperature excursion, so the IHX inlet temperature did not impose a limiting influence on the actual Hg-injection simulation tests.

Comparison on Figure 5 of the predicted S8DR response for an idealized Hg-injection with the predicted SNAP 8 PCS response prepared by AGC⁽²⁾ shows the slightly slower response expected for the S8DR primary loop because of the greater coolant and piping inventory.

Figure 3. S8DR-IHX Heat Removal for Mercury-Injection Simulation

Figure 4. PCS-Mercury Injection Simulation Computed Response of S8DR Primary Loop and SNAP 8 Reactor to Assumed Hg-Injection Load Demand at IHX

Figure 5. PCS-Mercury Injection Primary Loop Response Comparison

II. TEST RESULTS

S8DR system tests of SNAP 8 reactor response characteristics for the two principal transient stages of EGS automatic startup were performed from mid-December 1968 to mid-January 1969. Five tests of each transient stage were completed to obtain adequate test data for evaluation of transient characteristics and to assess transient response sensitivity to minor variations in test conditions. The repeatability of the transients observed in these tests, and reported in the following subsections, shows that the SNAP 8 reactor response is negligibly sensitive to the range of uncertainties or tolerances predicted for measured or implied system parameters such as shutdown flux level, reactivity, load demand rate, and flow rise rate.

A. SNAP 8 AUTOMATIC STARTUP TO OPERATING TEMPERATURE

The five automatic startups of the SNAP 8 reactor in S8DR were performed during the period January 10 to January 15, 1969 with the results for reactor power, coolant outlet, and coolant inlet temperature transients shown on Figures 6 to 10. S8DR initial conditions for each startup are listed below.

- 1) Primary and secondary loop coolant isothermal at 200°F.
- 2) Primary and secondary loop coolant flow constant at 20% rated (9760 lb/hr)
- 3) Trickle flow through the ABHX; secondary loop heater off
- 4) Initial shutdown reactivity estimates as shown in Figures 6 to 10.

Automatic startup consisted of a nearly constant reactivity insertion rate near 0.5 β /min from the shutdown reactivity levels shown until reactor coolant outlet temperature reached the lower end of the outlet temperature control dead-band ($1300 \pm 20^\circ\text{F}$ - 10°F). At this temperature, automatic controller action disengaged continuous sequential stepping of the three control drums by placing the three drums in the automatic temperature control mode.

Drum stepping time intervals producing reactivity insertion rates near 0.5 β /min for the drum positions, and the range of drum rotation required for automatic startup, resulted in actual reactivity insertion rates near 0.484 β /min

Figure 6. S8DR Automatic Startup to Operating Temperature, Test B-1, January 10, 1969

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Figure 7. S8DR Automatic Startup to Operating Temperature, Test B-2a, January 12, 1969

Figure 8. S8DR Automatic Startup to Operating Temperature, Test B-3, January 13, 1969

INITIAL CONDITIONS: $P(0) = 4.63 \times 10^{-4}$ KW
 $\rho(0) = -73.3\%$
 $T(0) = 200^\circ\text{F}$
 $W_P(0) = W_S(0) = 20\%$ RATED
 DRUM RATE = 0.524% /MIN.

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Figure 9. S8DR Automatic Startup to Operating Temperature, Test B-4a, January 14, 1969

Figure 10. S8DR Automatic Startup to Operating Temperature, Test B-5, January 15, 1969

for a 130 sec/step interval, and 0.524 ¢/min for a 110 sec/step interval. The two initial startups used the longer stepping interval, and the following three startups used the shorter interval to reduce startup time and assess the effect of increased reactivity insertion rate on SNAP 8 response in view of the appreciable margin between measured IHX primary inlet temperature rise rates and specified limits. The five automatic startup power excursions are replotted in Figure 11 as a single plot to demonstrate the repeatability between startups of similar reactivity insertion rate and the effect on startup time for a reactivity insertion rate increase from 0.484 ¢/min to 0.524 ¢/min, or a rate increase of 0.04 ¢/min.

The initial portion of the power range (> 1 kw) startup transient was found to be oscillatory but well damped below 10 kw before continued drum reactivity insertion raised power to 15 kw. The oscillatory behavior shown for the power transients in this power range is inherently due to the negligible transfer of heat from fuel to coolant for average fuel temperatures below 700 to 800°F. Low heat

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Figure 11. S8DR Automatic Startup to Operating Temperature, Test B-1 to B-5 Power Transients

transfer rates result in most of the fission thermal energy absorbed in the fuel with high fuel heating rates and consequent high fuel feedback reactivity rates. The combination of rapid fuel heating and low control drum reactivity insertion permits the fuel reactivity feedback to overcome excess reactivity present at power range entry (~ 8 to $9\text{ } \$/\text{min}$) and the small amount inserted ($\sim 3\text{ } \$/\text{min}$) by drum movement before the 10 kw level is reached ~ 6 minutes later. The resulting subcritical ($\sim -5\text{ } \$/\text{min}$) reactor rapidly terminates the power rise, a power reduction ensues until the combination of reduced rate of fuel temperature rise and continued reactivity insertion restores reactor criticality and eventual supercriticality to reestablish the power rise. Two cycles of this behavior were observed for each of the five startup transients, with the second cycle considerably damped due to heat transfer increase from fuel heating.

Data obtained during this portion of the test transient were particularly useful since the interaction of fuel reactivity feedback and the core fuel-to-coolant heat transfer rate (UA) could be clearly separated. The magnitude of the first

oscillation cycle was primarily a function of the fuel temperature coefficient of reactivity, while the frequency of oscillation was primarily a function of the UA value. Determination of each of these parameters is discussed further in Section III.

The increase in secondary loop flow indicated for each test near 30 kwt was introduced to reduce primary loop heat losses to the secondary coolant system. These losses added to the sluggish response of the primary loop; so, to improve the simulation where possible, it was decided to introduce pump heating to the secondary loop to minimize the heating load on the primary loop. Test results indicated that the pump heating effect was minimal and had a negligible influence on the startup transients.

The reactor coolant temperature curves of Figures 6 to 10 show a characteristic peak temperature rise rate when power is in the 50 to 100 kwt range of the power transient. This temperature rise is followed by a uniform temperature rise rate up to the operating power level. The peak reactor coolant temperature rise rates obtained during the early portion of the startup transient (8 to 11°F/min) are due to the variation of core UA with fuel temperature for the fuel temperatures predicted during this part of the transient. Average fuel temperature is estimated from model calculations to be in the range 700 to 900°F during this time, so core UA is undergoing the rapid increase shown on Figure 12. The UA increase produces an increased heat flow to the coolant and a consequent reduced negative reactivity feedback from the fuel coefficient. The reduced feedback reactivity permits an increased positive reactivity insertion rate from the drum input with the resulting power rise rate increase, shown on the test results, as the 100 kwt power level is approached. The combination of increased heat transfer rate and increasing power rise rate produce the peak coolant temperature rise rates observed during this stage of reactor startup. Primary loop temperature rise introduces lower grid feedback reactivity, adding to the fuel feedback effects to produce the power rise rate reduction shown near the 100 kwt level on all startup transients.

Test data analyses, discussed in Section III, show that reactor response may also be controlled by additional effects on feedback reactivity introduced when fuel temperatures are between 800 and 900°F. The probable sources of this

Figure 12. S8 Core Fuel-to-Coolant Heat Transfer Coefficient, UA

additional reactivity feedback are discussed in Section III, however, identification of such sources has not so far been possible because of the probable interaction of several effects and of the difficulty of separately measuring each effect.

B. PCS MERCURY-INJECTION SIMULATION

Five simulated mercury-injection thermal transients to the primary loop were performed on January 25, 1969, and test results for reactor power, inlet, and outlet temperatures are shown on Figures 13 to 17. Simulation of the mercury-injection load demand to the S8DR primary NaK loop, through manipulation of secondary NaK loop heat rejection, was performed from the initial conditions listed below.

- 1) Primary NaK flow at 50% rated (24,400 lb/hr)
- 2) Secondary NaK flow at 20% rated (9,760 lb/hr)
- 3) Reactor outlet temperature controlled below 1300°F (1245 to 1275)
- 4) Reactor power stabilized and furnishing primary and secondary loop heat losses (175 to 190 kw)

Figure 13. S8DR Mercury Injection Simulation, Test D-1, January 25, 1969

Figure 14. S8DR Mercury Injection Simulation, Test D-2, January 25, 1969

Figure 15. S8DR Mercury Injection Simulation, Test D-3, January 25, 1969

Figure 16. S8DR Mercury Injection Simulation, Test D-4, January 25, 1969

Figure 17. S8DR Mercury Injection Simulation,
Test D-5, January 25, 1969

The sequence of secondary loop manipulation used to obtain a simulated Hg-injection load transient to the primary side of the IHX was as follows:

- 1) ABHX louvers were manually opened, and the fan motor was simultaneously activated with fan speed preset for 450 kw ABHX heat injection.
- 2) Ninety seconds after initiation of Step 1), the fan motor was turned off and primary and secondary loop NaK flows were increased in 1 min to rated flow (48,800 lb/hr) by manual ramp of the electromagnetic pump voltage rheostats.
- 3) The resulting reactor power transient was observed, and upon passing through the first power peak about 3 min after fan motor tripout, the fan speed was ramped back up to the 450 kw heat rejection speed over a 5-min interval.
- 4) System response was observed and recorded over the next 20 min with no manual or automatic control action.

Return of the ABHX fan to the 450 kwt heat removal speed of Step 3 required a manual turn down of the fan speed control from the 450 kwt speed setting to the minimum speed control position during the 3-min interval between the end of Step 2 and power peaking. At minimum fan speed, ABHX heat rejection at design operating temperatures and full secondary loop NaK flow is ~ 150 kwt eliminating the possibility of a smooth heat removal ramp from 0 to 150 kwt. To simulate the first 150 kwt of the heat removal ramp, the fan motor was cycled on and off five times with successive increases in the time-on intervals and fan speed control set for the minimum speed setting. Above 150 kwt, fan speed was manually ramped to the 450 kwt fan speed by means of the speed control rheostat. Repeatability of the simulated Hg-injection transient through this sequence of manual operations can be seen by comparing system transients on Figures 13 to 17 and from the power and reactor inlet transient temperatures for Tests D-1 and D-4 on Figures 18 and 19 which bracket the transients for the five test series.

The most severe coolant temperature excursion on Figures 13 to 17 occurs at the reactor outlet as a result of primary loop flow increase up to rated level producing coolant temperature decrease rates between -67 and $-90^{\circ}\text{F}/\text{min}$. Coolant excursions at the IHX were adequately protected from these change rates by the surge tank thermal capacitance which appreciably attenuated the reactor outlet temperature excursions in magnitude and rates as shown on Figure 15 for Test D-3.

Comparison of the reactor composite test power transient results shown by Figure 20 with the computed power transients for the S8DR using the desired load demand at the IHX, and for the SNAP 8 PCS Hg-injection, shows a relatively sluggish test approximation to the predicted load transient imposed by PCS Hg-injection. The somewhat more sluggish response of S8DR indicated by this comparison demonstrates the effect of the primary and secondary loop NaK inventories and the inability to duplicate the high heat transfer rates of the PCS Hg boiler in the S8DR IHX. The first power peak shown for the test results was intentionally limited near 400 kwt to avoid excessive temperature transients at the IHX primary outlet, however, the second peak was allowed to approach the full load demand as a result of the lower peak rise rate with full coolant

Figure 18. S8DR Mercury Injection Simulation Test,
Transient Power Envelope

Figure 19. S8DR Mercury Injection Simulation Test,
Transient Reactor Inlet Temperature Envelope

Figure 20. S8 Reactor Power and Inlet Temperature Response to PCS Mercury Injection

flow in the NaK loops. It will be noted that initial heat loss power level is near 190 kwt for the test power transient while computed results were based on an estimated 70 kwt initial heat loss conditions for the flight system. The heat loss difference is not unexpected due to the longer primary loop lines in S8DR, the additional secondary loop losses, and the existence of the more efficient heat removal mechanism presented by the ambient ground level atmosphere for S8DR relative to the near vacuum environment for the SNAP 8 PCS flight system.

The slight power reduction shown near two minutes in the transient is due to the initial increase in reactor inlet temperature in the early stage of flow increase. This occurs as a result of coolant heating in the electromagnetic pump, some of which is lost to the atmosphere in the 65 feet of line between the pump and reactor inlet. With coolant flow increase in the first two minutes of the transient, the small temperature gradient between pump and reactor is compressed in its conveyance to the reactor producing a momentary temperature rise and the resulting power dip shown for the test results.

III. ANALYSIS

Analytical studies conducted in support of the test program included preparation of pretest predictions of test transients; concurrent analyses to evaluate startup test data and update of Hg-injection simulation pretest predictions; and posttest evaluation of the test results. The basic analytical tool assembled for transient studies was the mathematical model described in the Appendix and programmed for computation with the AIROS⁽³⁾ code designed specifically for reactor system dynamic studies. The conclusions reached for each test series, based on evaluation of the test data with a corrected computer model for system heat losses and piping insulation heat capacity, are discussed in the balance of this section.

A. SNAP 8 AUTOMATIC STARTUP

Pretest computer model predictions of the automatic startup transient were based on the predicted⁽⁴⁾ core overall heat transfer variation (UA) shown on Figure 21 and on these theoretically predicted reactor component temperature coefficients of reactivity:⁽⁵⁾ fuel = 0.09 ± 0.02 $\text{¢}/^{\circ}\text{F}$, upper grid = 0.07 ± 0.02 $\text{¢}/^{\circ}\text{F}$, and lower grid = 0.06 ± 0.02 $\text{¢}/^{\circ}\text{F}$. The difference between predicted and

Figure 21. S8 Core Fuel-to-Coolant Heat Transfer Coefficient, UA

theoretical⁽⁶⁾ UA of Figure 21 was based on an anticipated residual hydrogen pressure in the gas gap between fuel and cladding after fuel fabrication. As discussed in Reference 4, at low fuel temperature, a relatively small amount of hydrogen in the gap would have the significant effect shown by Figure 21 between the predicted and theoretical curves. Startup transient calculations with the predicted UA produced a power excursion into the useful power range, with a power peak near 60 kwt and no cyclic power oscillations. The initial startup test transient, on the other hand, deviated appreciably from the power peak prediction, due to power peaking well below 60 kwt, and resulted in test termination shortly after power range entry.

Analysis of test results, and a brief test of power decay for a small reactivity change to measure fuel thermal time constant, clearly indicated a core UA close to the low temperature theoretical value. A computer model prediction using the low temperature theoretical UA provided a good approximation to the test results obtained up to that time, and in the subsequent startup acceptably predicted the startup transient almost to the end of the heat up to operating temperature. An increasing power rise rate in the last hour of the startup transient, observed for all five transients, was not predicted by the computer model when using constant fuel and grid temperature coefficients. Neither the apparent reduction in feedback reactivity rate nor the increase in reactivity insertion rate due to control drum rotation into higher worth positions was investigated further during the test program as the reactivity rate balances required to produce the difference between predicted and measured response were expected to be quite small, an expectation confirmed by posttest analysis.

The oscillatory power behavior observed for all five startup tests upon power range entry provided a well-defined indication of fuel temperature coefficient of reactivity and core UA because of the isolated interaction of these parameters at low temperature. The fuel coefficient of reactivity determines the power peaks and valleys for a given UA due to reactivity feedback. The UA determines the oscillation frequency due to control of the heat transfer rate or fuel thermal time constant. Posttest analysis using the simulation model provided a rapid convergence on definitive values for the two parameters due to the sensitivity of the core power response to these parameters. The following values

were used in simulator calculations to provide the match with test results shown in Figure 23: $\alpha_f = -0.105 \pm 0.005 \text{ } \epsilon/\text{ }^\circ\text{F}$, $\alpha_{LG} = -0.080 \pm 0.005 \text{ } \epsilon/\text{ }^\circ\text{F}$, $\alpha_{UG} = 0.000 \text{ } \epsilon/\text{ }^\circ\text{F}$, and $UA = 0.010 \pm 0.002 \text{ kw/ }^\circ\text{F}$ at 200°F following the estimated variation shown on Figure 21.

Rapid damping of power oscillations as reactivity insertion is continued in the startup transient is a measure of the UA increase with fuel temperature increase along the estimated curve shown on Figure 21. Fuel thermal time constant as a function of the fuel temperature acting through the UA variation is shown on Figure 22, and is approximately inversely proportional to UA. The fuel heat capacitance variation⁽⁷⁾ with fuel temperature results in an appreciable influence on the inverse proportionality between UA and the fuel time constant.

Continuation of the transient into the power range is reasonably duplicated by the simulation model using the fuel coefficient and core UA curve estimated from the low temperature test data. Near the 100 kwt level, however, additional negative reactivity feedback effects apparently occur and produce a more

Figure 22. S8 Fuel Thermal Time Constant Variation with Average Fuel Temperature

rapid reduction in the power rise than predicted by the model using a constant fuel or grid coefficient. The possibility of a core heat transfer reduction mechanism as the source for added reactivity feedback through the fuel temperature coefficient is discounted due to the removal of hydrogen required in the gas gap for such a change. Both thermal expansion of the single-phase fuel and additional hydrogen diffusion in the gap with rising fuel temperature would act to increase core UA. This UA increase would reduce fuel temperature rise per unit power rise and permit an increased power rise rate instead of producing the behavior observed. Nonlinearities in nuclear components of the fuel temperature coefficient of reactivity would not be expected on a theoretical basis, so it is believed that the probable source of the observed power transient is due to the combination of (1) nonlinear fuel expansion coefficient variation produced by fuel material phase changes⁽⁸⁾ occurring in the 800 to 850°F range of fuel temperatures without an appreciable change in hydrogen migration from the fuel, and (2) piping transport delay approximations which result in less abrupt reactor coolant inlet temperature changes in the computed results than occur in actual tests. The additional negative reactivity required to match the test power transient with the computer model during this portion of the startup transient is shown on Figure 23. Correspondence between the fuel temperature behavior during this part of the transient and the additional reactivity required to produce the power transient observed is the basis for the contention that the fuel phase change with corresponding fuel expansion exerts some nonlinear reactivity feedback effect during this part of the startup. The difference between calculated reactor inlet temperature and test data over the time interval of interest for both startups is shown by Figure 24.

B. PCS Hg-INJECTION SIMULATION

Pretest computer model predictions of the secondary loop operation required to match the desired Hg-injection simulation transient of Figure 4 were based on the reactor temperature coefficients determined from preliminary analysis of the startup test results. The apparent lack of an upper grid coefficient in the startup test results was investigated further through flow oscillation tests and showed no significant reactivity response, therefore the coefficients

Figure 23. S8DR Startup Test Power Transient Match with Computer Model Calculations

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Figure 24. S8DR Startup Test Reactor Inlet Temperature Transient Match with Computer Model Calculations

used for test prediction were: fuel = 0.110 ± 0.010 $\phi/^{\circ}\text{F}$, upper grid = $0.00^{+0.000}_{-0.005}$ $\phi/^{\circ}\text{F}$, and lower grid = 0.100 ± 0.010 $\phi/^{\circ}\text{F}$. The estimated core UA shown by Figure 12 at the high temperature end of the curve was assumed operative for these predictions.

The sequence of secondary loop manipulation predicted to approximate the reactor coolant inlet temperature transient of Figure 4, and the corresponding computer transient results are shown on Figure 25.

The slightly more sluggish simulation transient obtained from this secondary loop manipulation was anticipated as a result of secondary loop heat capacitances and the inherently lower S8DR-IHX heat transfer rate compared to the PCS boiler rate for mercury vaporization. The approximation to the desired Hg-injection simulation was considered to be the best that could be obtained in S8DR to demonstrate SNAP 8 response to the Hg-injection load demand.

In performing the Hg-injection simulation tests, the rate and extent of secondary surge tank chilling that could be imposed was restricted by transient thermal stresses on ABHX exit piping and the surge tank inlet nozzle. The reactor inlet coolant temperature excursion resulting from the permissible surge tank chill excursion was less severe than the excursion required to duplicate the model prediction of Figure 25, but provided a reasonable approximation for simulation test purposes and adequately demonstrated the stable response of the SNAP 8 reactor to reactor inlet temperature excursions.

Figure 26 compares the computer model prediction for the S8DR Hg-injection simulation, which did not include surge tank chill limitations, with the test results and shows that the chill limitation was not overly restrictive in obtaining a simulation test with S8DR. Computer model changes required to obtain the test match shown by Figures 27 and 28 consisted of the duplication of secondary loop manipulation used in the tests and use of the fuel and lower grid temperature coefficient of reactivity determined from startup test analyses (-0.105 $\phi/^{\circ}\text{F}$ and -0.080 $\phi/^{\circ}\text{F}$).

Figure 25. S8DR Computed Mercury Injection Simulation Transient Compared to Idealized Transient for Mercury Injection Load Demand at IHX Primary

Figure 26. S8DR Composite Mercury Injection Simulation Experimental Results Compared to Computed Transient Prediction

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Figure 27. S8DR Computed Mercury Injection Simulation Match with Experimental Results (Reactor Power)

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TIME (min)

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Figure 28. S8DR Computed Mercury Injection Simulation Match with Experimental Results (Reactor Inlet Temperature)

IV. SUMMARY

Testing of the SNAP 8 reactor in S8DR to determine reactor automatic startup and transient load demand characteristics has shown the capability of the reactor to meet requirements for the proposed automatic startup of the EGS system. The tests conducted in the S8DR were restricted by ground test system component stress considerations to approximations of the desired automatic startup and Hg-injection simulation; however, these approximations were close enough to predicted flight conditions to indicate conclusively that the SNAP 8 reactor would respond as required in the flight system.

Analysis of test results has determined the following quantitative values for the principal parameters controlling reactor transient response characteristics:

1) Temperature coefficients of reactivity

a) Fuel = -0.105 ± 0.005 $\text{¢}/^{\circ}\text{F}$

b) Upper Grid = $0.00 \begin{matrix} + 0.000 \\ - 0.005 \end{matrix}$ $\text{¢}/^{\circ}\text{F}$

c) Lower Grid = 0.080 ± 0.025 $\text{¢}/^{\circ}\text{F}$

2) Fuel-to-coolant heat transfer coefficient, UA (approximating theoretical as shown in Figure 12)

Based on the reactor parameters given above, computer model predictions of SNAP 8 automatic reactor startup and idealized Hg-injection load demand to the S8DR IHX primary NaK were made as shown on Figures 29 and 30. A drum rate of 1 $\text{¢}/\text{min}$ was used for automatic reactor startup to obtain completion of this phase of EGS startup within the desired five-hour interval.

Figure 29. Predicted S8 Automatic Startup at 1 c/min Compared with S8DR Automatic Startup Near 0.5 c/min

Figure 30. Predicted Mercury Injection Transient in S8DR to Assumed Full Load Demand at IHX Primary

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APPENDIX

S8DR MATHEMATICAL MODEL

The scope of the mathematical representation of the S8DR system was constrained by conventional considerations for time, parametric detail required to adequately describe the transient behavior of the S8DR system, and the computer code used to perform the system simulation studies. The number of components comprising the S8DR system and requiring some degree of description in the mathematical model limited consideration of some components to only first order effects. Some intentional approximations of this kind are listed below.

- 1) Coolant piping transport delay approximated as first order time constants
- 2) Constant film heat transfer coefficient in the piping, independent of coolant flow velocity
- 3) Constant coolant, piping, and component metal thermal conductivity and heat capacity coefficients, independent of temperature
- 4) Constant core flow cross-sections and coolant density, independent of temperature
- 5) Dry, 100°F air at the inlet to the ABHX.

An additional deterrent to an extensively detailed model was the selection of a digital computer code⁽³⁾ designed to compute the sum of 100 differential or algebraic equations in addition to the basic neutron point kinetics equations. System simulation on a digital, rather than on an analog, computer (traditionally used for this type of study), was selected due to the anticipated intermittent requirements for simulator results over the period of pretest to posttest analyses and to the availability of an existing operational code specifically designed for reactor simulation studies.

The mathematical model assembled to simulate the S8DR system consisted of a set of equations describing the relationship between the S8DR components shown schematically in Figure 31. The computer model equations representing each system component are presented below.

Figure 31. S8DR Mathematical Model Schematic

A. REACTOR

Reactor neutron kinetics were based on the conventional point kinetics representation using the 6 delayed neutron group data for ^{235}U fission.⁽⁹⁾

$$\dot{N} = \left(\frac{\rho - \beta}{\ell^*} \right) N - \sum_{i=1}^6 \lambda_i C_i^*$$

$$\dot{C}_i = \frac{\beta_i}{\ell^*} N - \lambda_i C_i$$

$$\rho = K_\theta \Phi_D + \alpha_{UG} \Delta T_{GU} + \alpha_{LG} \Delta T_{GL} + \alpha_f \sum_{i=1}^5 W_i \Delta T_{fi}$$

To include the influence of axial neutron flux distribution on feedback reactivity weighting and obtain greater accuracy in the description of transient temperature behavior axially in the core, a distributed core heat balance representation using five axial nodes was used. A relatively flat radial distribution and radial flow orificing in the reactor design to obtain uniform coolant temperature rise across the core obviated radial heat balances. Because of the gas gap between fuel and cladding, the principal source of core heat flow resistance and overall heat transfer variation was dominated by this region so that fuel cladding thermal behavior was closely coupled to the coolant behavior. As a result, clad heat capacity was lumped with coolant heat capacity in the model to obtain the following core nodal heat balance equations:

$$M_f C_f \dot{\bar{T}}_{fi} = K_l F_i \left(\frac{N}{N_o} \right) - UA_R (\bar{T}_{fi} - \bar{T}_{Ci}) \quad (i = 1 - 5)$$

$$M_{CR} C_{CR} \dot{\bar{T}}_{Ci} = UA_R (\bar{T}_{fi} - \bar{T}_{Ci}) - 2W_{CR} C_{CR} (\bar{T}_{Ci} - T_{Ci-1}) - K_R \bar{T}_{Ci}$$

$$M_{CR} C_{CR} \dot{T}_{Ci} = UA_R (\bar{T}_{fi} - \bar{T}_{Ci}) - W_{CR} C_{CR} (T_{Ci} - T_{Ci-1}) - K_R \bar{T}_{Ci}$$

*See List of Nomenclature and Figure 31

The third equation shown is redundant for steady state thermal balances, but was used in the computer model to avoid the loss of computed transient accuracy and the potential computational instability inherent with the use of the steady state algebraic relationship ($T_{ci} = 2\bar{T}_{ci} - T_{i-1}$) when generating the node-to-node inlet temperatures.

Representation of the inlet and outlet plenums assumed uniform coolant volumetric mixing to produce first order time lag response to inlet temperature changes. Heat loss to the plenum walls was also included and assumed simple heat conduction for the NaK in contact with the plenum walls. These equations took the form:

$$M_{CP}C_{CP}\dot{T}_{UP} = W_{CP}(T_{C5} - T_{UP}) - UA_P(T_{UP} - T_{UM})$$

$$M_{MP}C_{MP}T_{UM} = UA_P(T_{UP} - T_{UM})$$

Upper and lower grid plate thermal response characteristics were assumed to be governed primarily by heat transfer in the flow passages through the grid plates, where the principal heat transfer areas for the plates are located. Neither flow-dependent heat transfer for this region nor heat transfer to low velocity NaK on the grid plate surfaces was included, and the following grid plate thermal representation resulted:

$$M_{UG}C_{UG}\dot{T}_{GU} = UA_G(T_{C5} - \bar{T}_{GU}) \quad .$$

B. PRIMARY AND SECONDARY LOOP PIPING AND SURGE TANKS

Analytically exact representation of piping slug flow transport delays was not within the scope of the computer program capability. To approximate these delays, first order time constant relationships between pipe segment outlet and inlet temperature were assumed, with the time constant assigned the transport time for the pipe length at the given coolant flow rate. Primary and secondary loop piping were each represented by three such pipe segments. Heat absorbed by the pipe metal and insulation were also included using a non-flow-dependent

heat transfer coefficient. Temperature-dependent heat losses to the atmosphere, based on loss characteristics measured in steady state tests, were included. A typical pipe segment heat balance was represented by:

$$M_{CP} C_{CP} \dot{T}_{Si} = W_{CP} C_{CP} (T_{UP} - T_{Si}) - UA_P (T_{Si} - T_{P1})$$

$$M_{MP} C_{MP} \dot{T}_{P1} = UA_P (T_{Si} - T_{P1}) - K_{P1} T_{P1} \quad .$$

Surge tank coolant outlet temperature equations assumed idealized volumetric mixing in the passage of coolant through the tanks to give primary and secondary loop surge tank equations of the form:

$$M_{CS} C_{CS} \dot{T}_{So} = W_{CS} (T_{Si} - T_{So}) \quad .$$

C. INTERMEDIATE HEAT EXCHANGER (IHX)

The counterflow shell-and-tube heat exchanger used to transfer primary NaK loop heat to the secondary NaK loop was described by 10 nodal equations of the form:

$$M_{CPX} C_{CPX} \dot{T}_{XP1} = W_{CP} C_{CP} (T_{XPi} - T_{XP1}) - UA_X (T_{XP1} - T_{XS10})$$

$$M_{CSX} C_{CSX} \dot{T}_{XS10} = UA_X (T_{XP1} - T_{XS10})$$

$$- W_{CS} C_{CS} (T_{XS10} - T_{XS9}) - K_X T_{XS10}$$

where the last term of the second equation considers heat loss from the shell side of the IHX to the atmosphere. Ten segments were used to obtain steady state balances within 98% of theoretical temperature balances at low flow (20% rated). This requirement was of particular importance for the startup simulation studies. Heat transfer correlation calculations have shown that coolant film heat transfer coefficient variation with flow between 20% and full-rated flow was approximately linearly related to flow rate.

D. AIR BLAST HEAT EXCHANGER (ABHX)

The cross-flow heat exchanger used to dump heat to the atmosphere was described by four nodal equations, each representing a NaK piping pass through the air flow channel of the exchanger. The nodal equations were of the form:

$$M_{CA} C_{CA} \dot{\bar{T}}_{CA1} = 2W_{CS} C_{CS} (T_{CAi} - \bar{T}_{CA1}) - UA_A (\bar{T}_{CA1} - \bar{T}_{A4})$$

$$M_{CA} C_{CA} \dot{\bar{T}}_{CA1} = W_{CS} C_{CS} (T_{CAi} - T_{CA1}) - UA_A (\bar{T}_{CA1} - \bar{T}_{A4})$$

$$M_A C_A \dot{\bar{T}}_{A4} = UA_A (\bar{T}_{CA1} - \bar{T}_{A4}) - W_A C_A (\bar{T}_{A4} - T_{A3}) \quad .$$

Because of the relatively negligible effect of NaK side heat transfer resistance compared to air side resistance, the variation of UA_A was dependent on air flow alone, as shown on Figure 32.

Figure 32. S8DR ABHX Overall Heat Transfer Rate as a Function of Airflow

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NOMENCLATURE

C_i	= Delayed neutron group concentration ($n/cm^2/sec$)	<u>Subscripts</u>	
		A	= ABHX, Air
C	= Specific heat (Btu/lb-°F)	C	= Coolant
F_i	= Axial power distribution factor	f	= Fuel
K_l	= Thermal energy conversion factor (Btu/sec)	G	= Grid
K_θ	= Control drum reactivity function with drum position ($\phi/degree$)	i	= Delay neutron group, node number, inlet
K	= Heat loss coefficient (Btu/sec-°F)	L	= Lower
l^*	= Prompt neutron lifetime (sec)	M	= Metal
M	= Mass (lb)	N	= ABHX (sodium side)
N	= Neutron flux ($n/cm^2/sec$)	o	= Outlet
Q	= Heat losses	P	= Plenum, piping, primary
T	= Temperature (°F)	R	= Reactor
$\bar{T}$	= Average temperature (°F)	S	= Surge tank, secondary
UA	= Overall heat transfer rate (Btu/sec)	U	= Upper
W_i	= Weighted importance factor	X	= IHX
W	= Mass flow (lb/sec)	<u>Acronyms</u>	
α	= Temperature coefficients of reactivity ($\phi/°F$)	NS	= Nuclear System
β	= Effective delayed neutron fraction	PCS	= Power Conversion System
λ	= Delay neutron group decay constant (sec^{-1})	EGS	= Electric Generation System
θ_D	= Control drum angle (degree)	IHX	= S8DR Intermediate Heat Exchanger
τ	= Time constant (sec)	ABHX	= Air Blast Heat Exchanger
ρ	= Reactivity	AI	= Atomics International
		AGC	= Aerojet-General Corporation
		S8DR	= SNAP 8 Developmental Reactor