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NS SAVANNAH UPGRADING ANALOG COMPUTER ANALYSIS

By
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November 1962

The Babcock and Wilcox Company
Atomic Energy Division
Lynchburg, Virginia

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NS SAVANNAH UPGRADING
ANALOG COMPUTER ANALYSIS

November, 1962

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ABSTRACT

This extensive transient analysis of the NS Savannah power plant used analog computer technique to determine the control and safety system actions required to prevent plant damage following operating and accident transients. This work amplified previous design studies by considering accidents of greater severity, automatic limiting devices to preclude safety action, and more realistic parameters based on operating experience.

The following transients were analyzed:

1. Startup accident.
2. Rod withdrawal accident in the power range.
3. Excessive steam demand.
4. Accidental closing of both steam stop valves.
5. Primary flow coastdown.
6. Capsize ship accident.
7. Ejection of one control rod.
8. Determination of maximum and minimum rates of reactivity change that should be available to the control system.
9. Power change from 100 kw-t to 86 MW-t to determine the reactivity required to maintain a 20-second period.

The study determined the minimum rate of reactivity removal necessary to prevent plant damage considered to occur (1) when heat flux exceeds the steady-state level corresponding to departure from nucleate boiling, (2) when the central fuel temperature exceeds 5000 F, (3) when primary system pressure exceeds 2200 psia, and (4) when the core is not subcritical following a primary system rupture. The effects of changing set points were also analyzed.

The graded protective system automatically limits excursions from normal operating conditions by initiating corrective actions that, in order of increasing severity, are (1) stop rods from withdrawing, (2) insert rods

at normal control speeds, and (3) scram rods at high speed. Analysis of the graded protective system involved the selection of setpoints, with the setpoint for the least severe corrective action located closest to the operating value of the parameter.

The analysis showed that overpressure considerations dictate the maximum required rate of reactivity removal, which is 3% δk per second. This rate is recommended for other safety trips.

Elimination of several safety inputs, installation of a high pressure safety trip, and the relaxation of some setpoints are recommended. On the basis of operating experience to date and the results of this analysis, installation of the graded protective system does not appear justified.

The analysis indicates that the present rod rates available to the controller are adequate and safe.

Comparison between computer results and actual Savannah testing results indicates that the analog simulation is reliable for transient studies.

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1. INTRODUCTION

An analog computer was used to analyze transient performance of the NS Savannah reactor plant. The primary objectives were to determine the control and safety system actions required to prevent plant damage following plant operating transients and accidents transients, and to determine the possible simplification of the safety system. The analysis also included a study to determine the effectiveness of automatic limiting action (corrective action of less severity and reliability than a scram) in reducing the probable frequency of safety action. The automatic limiting action is initiated at setpoints that are nearer to normal operating range than are the safety-system setpoints. The automatic limiting action stops undesirable rod withdrawal or inserts rods at normal control speeds.

The combination of automatic limiting action and safety action is referred to as the graded protective system. The protective action that occurs nearer to the normal operating levels does not have to be as drastic as that required by ultimate safety action, so normal operation can be reestablished without the inconvenience of a complete shutdown. Because the ultimate safety action is retained, the first line of protective action does not need the high degree of dependability of the ultimate safety action.

Major results have indicated δk removal rates and δk removal trip points for various possible situations. In evaluating and applying the results, the degree of dependability is very important. Even though safety action less drastic than the existing scram rate is adequate and automatic control limiting action can prevent a safety trip, the present scram action may have to be retained to provide the desired degree of safety unless the limiting action can be achieved with the same degree of reliability, in which case the limiting action could become the safety action.

Part of the analysis is similar to the analog work performed during the Savannah design phase at Battelle Memorial Institute (BMI). Reference 1 summarizes the BMI analog program. By comparison, the present analysis

placed greater emphasis on the limit of plant safety, added the graded protective concept, and considered additional types of transients. In addition, nuclear parameters used in the BMI simulation were reevaluated, and final design data and more precise computational techniques were used. As a result, some of the nuclear parameters such as Doppler and moderator coefficients, delayed neutron fractions, and the power split between core passes differ from those used for analog studies made during the design phase. It is felt that the present parameters better describe the physical characteristics of the core. However, to allow for present uncertainties in Core I and design changes for future cores, the effect of varying each significant parameter is studied whenever the uncertainty in that parameter affects the accident result.

The major part of the analysis deals with the following accidents:

1. Startup.
2. Rod withdrawal in the power range.
3. Excessive steam demand.
4. Sudden closing of steam stop valves.
5. Primary flow coastdown.

In the existing plant, the safety system protects against these five accidents, by initiating scram rod insertion when various unsafe and abnormal conditions such as those described in Section 4.6 are approached.

The present analysis considers variations in rod-insertion trip point, rod insertion rate, Doppler coefficient, moderator coefficient, primary flow, core life, rod withdrawal rate, and steam demand. In evaluating the results, the criteria for plant damage are burnout, central fuel-pin melting, and overpressurization of the primary system.

The normal third-pass average heat flux at reactor power of 69 MW-t is 70,500 Btu/hr-ft². Calculations indicate that burnout occurs in the hot channel when the third-pass average heat flux and reactor power levels are as follows:

<u>Pumps running full speed</u>	<u>Third-pass average heat flux, Btu/hr-ft²</u>	<u>Steady state reactor power, MW-t</u>
	<u>At 1750 psia</u>	
4	190,000	186
3	173,000	169
2 (2 loops)	152,000	148
1	120,000	118
	<u>At 1500 psia</u>	
4	179,000	175
3	160,000	157
2 (2 loops)	138,000	135
1	107,000	105

Fuel melting is assumed to begin at 5000 F. Melting at the center of the hottest fuel pin occurs when the third-pass average heat flux reaches approximately 140,000 Btu/hr-ft². Overpressure damage is assumed to occur when pressurizer or primary system pressure reaches 2200 psig. This is the maximum pressure allowed by the ASME code for a system designed for 2000 psig. However, the analysis determined the action required to prevent primary system expansion beyond the rate that exceeds the core-valve relief capacity. Therefore, unless otherwise specified, overpressure is defined as any pressure in excess of 2000 psia.

In addition to the five major accidents already listed, the report covers other transients of a special nature.

The capsized ship accident was analyzed because of its particular application to the Marvel-Schebler control rod drives. At some point beyond a 45-degree list the effectiveness of gravity scram is reduced, and the rods must be driven in by their motors. Also, an extreme list would eventually drain all secondary mass from the boilers, so all heat generated by the reactor would have to be absorbed by the primary fluid. The accident was analyzed with a range of δk removal rates in order to evaluate safety of the Marvel-Schebler drives under ineffective gravity scram.

Ejection of one control rod was also analyzed with application to the Marvel-Schebler drives. This accident was assumed to occur owing to a large leak in a control rod drive housing developing when the restraining devices were functioning improperly. A step addition of reactivity followed by safety action equivalent to a scram was simulated.

Analyses were conducted to determine (1) the rate of δk addition or removal that should be available to the automatic control system for maneuvering, and (2) the rate of δk removal that should be available to the automatic control system to prevent the opening of the secondary-steam relief valves. The control system was further studied in an analysis to determine what limit should be placed on the rate of δk change available to prevent instability of control. Also determined was the δk addition rate from rod withdrawal that would maintain the reactor on a 20-second period between 100 kw-t and 86 MW-t (125% of 69 MW-t).

2. SUMMARY OF CONCLUSIONS

2.1. Reactivity Removal Rates Necessary to Prevent Plant Damage With Present Safety System Setpoints

<u>Accident</u>	<u>Reactivity removal required, $\delta k/\text{sec}$</u>	<u>Controlling damage criteria</u>
Startup	2.9×10^{-3}	Exceeding relief valve capacity
Rod withdrawal at power	3×10^{-2}	Exceeding relief valve capacity
Excessive steam demand	3×10^{-4}	Central fuel melting
Coastdown	2.5×10^{-2}	Departure from nucleate boiling
Capsize ship	3.0×10^{-4}	Exceeding relief valve capacity
Ejection of one control rod	1×10^{-1} is adequate - absolute safety limit not demonstrated	Departure from nucleate boiling

With the powerful Doppler and moderator coefficients and the highly reliable scram system, the existing plant has far more safety shutdown capability than is required to prevent plant damage.

2.2. Requirements and Limitations for Reactivity Rates Available to the Rod Controller

<u>Criteria</u>	<u>Reactivity requirement or limit, $\delta k/\text{sec}$</u>
Maintain primary temperature within ± 6 F of setpoint during extreme maneuvering	1.67×10^{-4}
Prevent opening of secondary relief following turbine trip	1.25×10^{-4}
Maintain stable operation, on-off control	$\ll 6.6 \times 10^{-4}$
Maintain stable operation with pulsed rod movement	Control by 4×10^{-5} δk pulses is satisfactory Larger pulses were not investigated.
Maintain 20 second period from 100 kw-t to 86 MW-t	Variable rate required. Maximum rate = 8.0×10^{-4}

2.3. New Safety System Setpoints

If an overpressure safety trip at approximately 2000 psi or increased primary relief capacity is provided, the overpower trip point could be raised to 117 MW-t for four-, three-, and two-pump operations. This would reduce the required rate of reactivity removal for rod withdrawal accidents at power to $3.0 \times 10^{-4} \delta k/\text{sec}$. For one-pump operation overpower protection should be provided at less than 95 MW-t.

2.4. Elimination of Safety Setpoints

There are several conditions with the present plant that initiate safety action that can be eliminated because: (1) other safety trips provide essentially duplicate protection or (2) the trip is from an equipment failure that does not jeopardize the safety of the plant.

2.5. Graded Protective System

The probability of safety action can be reduced by the provision of a graded protective system that initiates control action at levels below the safety trip with reactivity removal rates less than those

provided by the safety system. However, the scram-type rod insertion cannot be eliminated unless the milder type of corrective action is made as reliable as the scram.

At present the added complexity of the graded protective system is not justified.

2.6. Parameters Used to Initiate Safety Action

Although existing technology dictates the parameters presently preferred for initiation of safety action, consideration should be given to all of the following parameters in the development of new designs.

2.6.1. Overpressure

Neutron flux and UO_2 fuel temperature would anticipate overpressure from high reactor-power excursions. Pressure, rate of primary-coolant temperature change, and rate of pressurizer level change would indicate excessive primary-coolant expansion. Primary-coolant temperature or pressurizer level could be used to prevent filling of the pressurizer.

2.6.2. Underpressure

Pressure, primary-coolant temperature, or pressurizer level could be used to protect against underpressure.

2.6.3. Excessive Fuel and Clad Temperatures

Electric current to primary pumps would anticipate loss of primary flow that could cause excessive clad temperatures. Primary-coolant flow rate and reactor core outlet temperature could be used to protect against clad damage from loss of primary flow. Neutron flux level or UO_2 fuel temperature could be used to protect against damaging fuel and clad temperatures when flow is normal.

2.7. Nuclear Parameters

1. The actual plant testing results demonstrate that the present core has Doppler and moderator coefficients approximately equal to the nominal values used in the analysis.

2. No serious control or safety problems will be encountered in future cores having either Doppler or moderator coefficients that are one-half to two times the nominal values used in the

analysis if all other parameters are within the required limits.

3. The plant has far more than the required safety protection for a start-up accident, even if the system is cold and the moderator coefficient is zero.

3. RECOMMENDATIONS FOR THE PRESENT SAFETY SYSTEM

3. 1. Actions to be Eliminated:

1. Period scram below 10% of 69 MW-t.
2. Flux-level scram at 21 MW-t for power below 10% of 69 MW-t.
3. Low-flow scram from ΔP cell flow measurement.
4. Loss of rod-drive-system voltage scram.
5. Cabinet-door-interlock scram.
6. Velocity-monitor-trip fast-insertion scram (this action not applicable without group demand unit in operation).
7. Period-initiated fast-insertion scram.
8. Manual-initiated fast-insertion scram.

3. 2. Actions to be Retained:

1. Neutron-flux-level scram at 95 MW-t.
2. High reactor-outlet temperature, T_h , scram at 540 F.
3. Low primary-system pressure scram at 1500 psia.
4. Undercurrent scram upon loss of power to all pumps.
5. Manual scram.
6. Loss of hydraulic-supply-manifold pressure scram at 2715 psia.
7. Nuclear instrumentation failure scram.
8. Rod test switches scram.

3. 3. Actions to be Added:

1. Either a high primary-system pressure scram with a setpoint at approximately 2,000 psia or a primary relief valve with capacity of 40,000 lb/hr (steam at 2000 psia).
2. Tight administrative control of access to cabinets (e. g. padlocks on cabinet doors with access to keys by high-level supervisors only).
3. Tight administrative control of access to rod test switches.

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4. PLANT DESCRIPTION

4.1. Reactor Core

The reactor core consists of 32 fuel elements, approximately 8.5-inches square and 92-inches long vertically. Each fuel element contains 164 half-inch diameter stainless steel tubes filled with enriched uranium-oxide pellets. Pressurized helium gas fills the gaps within the fuel pins. The reactor heat generation is 69 MW at 100% rated power. The moderator and coolant is demineralized light water under a pressure of 1750 psia.

4.2. Control Rod Drives

Reactivity is controlled by 21 cruciform shaped rods located between the fuel elements. Each rod has three sections; a stainless steel extension, a neutron absorbing portion, and a Zircaloy-2 follower. The neutron absorbing section has a boron center enclosed by a stainless steel sheath. The mechanical rod-drive system is located on the reactor vessel top. Withdrawing the rods upward removes the neutron absorbing section, thereby increasing reactivity and power output. Inserting the rods downward tends to shut down the reactor.

Normally the control rods are moved in groups by manual control or by the automatic control system. The total rod stroke is 58 inches, and the maximum rod speed for normal operation is approximately 15 inches per minute. For emergency shutdown either by manual operation or the safety system, all rods can be driven into the core in approximately one second by means of hydraulic pressure from oil accumulators, one for each rod. Alternate rod drives use gravity for scram insertion.

4.3. Reactor Vessel

The reactor vessel is a cylindrical shell with a hemispherical bottom head and a spherical dished top head. The vessel height is 26-1/2 feet and the inside diameter is 98 inches. Material is carbon

steel, clad with stainless steel on the inside. The vessel has three water-flow passes. Water enters through two bottom nozzles, flows up through the thermal shields in a concentric peripheral pattern, down through the second concentric pass, then up through the center pass, leaving through the upper plenum and two upper nozzles. There are 16 fuel elements in each of the two fuel passes. Consequently the two passes have equal heat-transfer areas and fuel-pin heat capacitances. The coolant receives negligible heat during the first pass through the thermal shields. The total second and third pass heat-transfer area is approximately 3780 square feet.

4. 4. Primary Coolant System

4. 4. 1. General

Figure 1 shows the reactor plant schematically. The cooling water is circulated in two identical loops. Progressing from the reactor outlet in the direction of flow, each loop consists of 12-9/16 inch ID piping, the boiler, two canned pumps in 8-9/16 inch ID parallel piping, 12-9/16 inch ID piping, and the reactor vessel inlet nozzle. A check valve is located immediately downstream from each pump. Each loop has a gate valve between the reactor and the boiler, and a second gate valve between the check valves and the reactor. A pressurizer is connected to the reactor outlet leg of one loop.

When all four pumps are operating at full speed the total coolant flow is 9.4×10^6 pounds per hour. The average of the boiler inlet and outlet temperatures is 508 F at steady state for all power levels. The temperature drop across the boiler is approximately 20 F at 69 MW-t. The primary water mass is approximately 63,500 pounds.

4. 4. 2. Pressurizer

The pressurizer is designed to maintain a pressure of 1750 psia on the primary coolant system. During steady-state conditions the pressurizer contains 62 cubic feet of water and 92 cubic feet of saturated steam. Submerged electric heaters in the bottom head of the vessel provide 222 kw-e to counteract losses and to raise pressure. As the primary coolant cools and contracts, water flows out of the pressurizer, additional steam is flashed, the pressure drops, and the heaters activate automatically. When the primary coolant heats and expands, water

flows into the pressurizer, steam is condensed, pressure rises, and cooling water is automatically sprayed into the steam space to reduce pressure. The cooling water is piped from the boiler outlet.

Two relief valves are installed on the pressurizer. One is a diaphragm pilot-operated valve controlled by pressure instrumentation to start opening at approximately 1915 psig. The other valve is spring loaded, self-actuated, code approved; and opens at approximately 2000 psig. There are two code valves, but only one is intended to be in use at a time. The pressurizer relief valves discharge to a tank in a closed system.

4. 4. 3. Boilers

Each of the two boilers has a U-tube heat exchanger with primary water flowing inside the tubes to form secondary steam outside the tubes. An upper drum, filled with water and steam, is connected to the heat exchanger by downcomers, through which the secondary water flows down to be heated and evaporated, and risers, through which the water and steam mixture flows up. Propulsion steam is taken from the top of the upper drum. The fluid weight in the secondary system remains essentially constant at about 17,000 pounds. The heat transfer area in this system is approximately 7300 square feet.

Relief valves on the boilers are spring loaded, self-actuated, and code approved. The valves are set to open at 800 psig (520 F saturated), and discharge directly into the containment vessel.

4. 5. Secondary Steam System

Steam generated in the two boilers is discharged through two 8-inch pipes that join a 10-inch header. Installed in each of the 8-inch lines is a quick-closing steam stop-valve that can be closed in 1 second. Steam is piped to the turbine throttle-valve, high-pressure turbine, low-pressure turbine, and condenser. Steam for astern power bypasses the high-pressure turbine and flows to a special section of the low-pressure turbine, known as the backing turbine. Condensate is piped through the first-stage heater, deaerator, third-stage heater, feed pumps, and then to the boiler drum.

There is a steam dump valve installed in a separate steam line to enable steam to be discharged directly from the boilers to the condenser. The dump valve can be controlled manually or automatically to maintain

a certain steam demand when the flow to the main turbines is reduced.

Since the primary temperature is essentially constant, secondary temperature must vary with load. At zero load the secondary pressure is 730 psia. At 69 MW-t the secondary pressure is approximately 460 psia.

4.6. Safety System

The safety system collects input information from the nuclear instrumentation, non-nuclear instrumentation, and auxiliary sources; and causes a scram as required for safe operation. The delay between the time a scram trip is reached and the time the rods start moving into the core is 240 milliseconds. This quantity is used in the analog analysis, unless otherwise specified.

With the General Electric (GE) rod drive system installed in the ship, a scram signal causes full insertion of all control rods in less than 1 second. The insertion is accomplished by hydraulic accumulators, one for each rod. These accumulators do not depend on the normally used control system and have an extremely high degree of reliability. The Marvel-Schebler rod drive system, which is an alternate design for the Savannah, provides for gravity insertion of rods following a scram signal. This scram system is designed to insert rods at approximately the same speed as the GE system, and is also highly reliable.

The safety system causes a scram when any of the following events occur:

1. The reactor power increases to 90 MW-t (130% of 69 MW-t when the plant is in the OPERATE mode, or increases to 21 MW-t (30% of 69 MW-t) when the plant is in the START mode.
2. The reactor outlet temperature (T_h) increases to 540 F.
3. The pressurizer pressure decreases to 1485 psig.
4. The number of operating primary pumps drops to zero.
5. The reactor period decreases to 8.7 seconds while the reactor power is below 10%.
6. The rod drive hydraulic pressure drops below 2700 psig.
7. The normal 2-phase rod drive power supply fails.
8. There is a failure in the nuclear instrumentation.
9. The doors on the rear of the GE control rod drive cubicle or the doors on the primary pump controller cabinet are opened.

10. Any of the 21 rod test switches are actuated. These switches are provided to defeat automatic fast insertion so that rod carriages may be tested when all rods are fully inserted.

The GE rod drive system presently installed in the Savannah is designed to automatically "Fast Insert" rods when any of the following conditions exist:

1. The group demand unit, which controls four servo rods as a group, fails to follow the velocity demand signal (either in the automatic or manual mode).
2. The reactor period decreases to 13 seconds while the reactor power is below 10%.
3. Control rods disengage from their carriages. This ensures that carriages are always run in whenever a scram occurs.

Assuming that some rods are fully withdrawn, approximately 4 minutes is required to "Fast Insert" all rod carriages. Inserting rod carriages is the same as inserting rods except when the "Fast Insert" is tripped by scram.

4.7. Automatic Control System

The automatic control system functions to maintain the steady state temperature of the primary coolant system at 508 F. Because of the long time constants involved in the reactor and heat transport system, it is necessary to provide more than a controller that has temperature error input only. The controller is designed to generate an output signal, ϵ , which is defined by the following equations:

$$\epsilon = \frac{N_i - N_c}{N_c}$$

where

N_i = actual reactor power

N_c = power demand

$$N_c = K_1 F_s + K_2 \left[\frac{T_h + T_c}{2} - 508 + \frac{1}{T} \int \left(\frac{T_h + T_c}{2} - 508 \right) dt \right]$$

where

K_1 = steam load gain factor

F_s = steam load

K_2 = temperature signal gain factor

T_h = reactor outlet temperature

T_c = boiler outlet temperature

τ = integration time constant

The plant controller compares N_i with N_c . If the output error signal reaches a predetermined percentage, control rod motion is initiated. The power demand signal is composed of the steam load indication with a correction for temperature error. During a steam load change N_i will be forced to match N_c . If the primary coolant average temperature changes, a correction is added to N_c . If the temperature error persists for an appreciable time, the integral term will add an additional power correction.

4.8. Nomenclature

A	Heat transfer area, ft ²
C_i	Concentration of the delayed neutrons precursors of Group I
C_p	Specific heat of primary coolant, Btu/lb-°F
C_s	Specific heat of secondary coolant, Btu/lb-°F
F_s	Secondary steam flow, lb/sec
δk	Amount by which the reactor multiplication factor differs from unity
K_1	Steam load gain factor
K_2	Temperature signal gain factor
ℓ^*	Mean effective neutron lifetime, sec ⁻¹
M	Mass, lb
N_c	Power demand (automatic controller)
N_i	Actual reactor power (automatic controller)

n	Neutron level (power level)
Q	Heat transfer rate, Btu/sec
S	Neutron source
T_c	Boiler outlet temperature, F
T_c'	Reactor 1st pass inlet temperature, F
T_d	Reactor 1st pass outlet (2nd pass inlet) temperature, F
T_e	Reactor 2nd pass outlet temperature, F
T_f	Reactor third pass inlet temperature, F
T_g	Reactor third pass outlet temperature, F
T_h	Reactor outlet temperature, F
T_{bi}	Boiler tube inlet temperature, F
T_{bo}	Boiler tube outlet temperature, F
T_{REF}	Normal average primary temperature, 508 F
U	Overall effective heat transfer coefficient, Btu/hr-ft ² -°F
W	Flow rate, lb/sec
β	Fraction of total delayed neutrons
β_i	Fraction of delayed neutrons in Group I
ϵ	Automatic control system output error signal
λ_i	Decay constant of Group I of delayed neutrons
τ	Time constant, sec ⁻¹

4.9. Parameter List*

<u>Parameter</u>	<u>Beginning of life</u>	<u>Middle of life</u>	<u>End of life</u>
<u>Power split</u>			
2nd pass	43.6%	37.8%	35.9%
3rd pass	56.4%	62.2%	64.1%

*This parameter list applies to the analog study. Slightly revised data became available toward the end of the study. For consistency, no changes to the analog input data were made.

<u>Parameter</u>	<u>Beginning of life</u>	<u>Middle of life</u>	<u>End of life</u>
<u>Moderator coefficient, nominal</u> ($\delta k/^\circ\text{F}$)			
2nd pass	$- 0.856 \times 10^{-4}$	$- 0.564 \times 10^{-4}$	$- 0.434 \times 10^{-4}$
3rd pass	$- 1.080 \times 10^{-4}$	$- 1.275 \times 10^{-4}$	$- 1.315 \times 10^{-4}$
Total (Isothermal)	$- 1.936 \times 10^{-4}$	$- 1.839 \times 10^{-4}$	$- 1.749 \times 10^{-4}$

<u>Doppler coefficient, nominal</u> ($\delta k/^\circ\text{F}$)			
2nd pass	$- 0.33 \times 10^{-5}$	$- 0.26 \times 10^{-5}$	$- 0.17 \times 10^{-5}$
3rd pass	$- 0.81 \times 10^{-5}$	$- 0.81 \times 10^{-5}$	$- 0.82 \times 10^{-5}$
Total	$- 1.14 \times 10^{-5}$	$- 1.07 \times 10^{-5}$	$- 0.99 \times 10^{-5}$

Neutron data:

λ_1	3.0890	3.0702	3.0555
λ_2	1.1566	1.1556	1.1540
λ_3	0.3057	0.3070	0.3081
λ_4	.1134	.1141	.1147
λ_5	.0306	.0306	.0306
λ_6	0.0125	0.0125	0.0125
β_1	0.000354	0.000343	0.000334
β_2	.001000	.000965	.000937
β_3	.002958	.002843	.002754
β_4	.001431	.001381	.001342
β_5	.001543	.001496	.001459
β_6	<u>.000223</u>	<u>.000215</u>	<u>.000208</u>
Total	0.007509	0.007243	0.007034
ℓ^* sec	2.7×10^{-5}	2.7×10^{-5}	2.7×10^{-5}

Fuel pin data:

<u>Gap conductivity</u> (Btu/hr-ft ² - $^\circ\text{F}$)			
2nd pass	0.15	0.131	0.091
3rd pass	0.15	0.113	0.037

<u>Parameter</u>	<u>Beginning of life</u>	<u>Middle of life</u>	<u>End of life</u>
Fuel pin data: (Cont'd)			
<u>Water film coefficient, 4 pumps</u> (Btu/hr-ft ² -°F)			
2nd pass	2838	2838	2838
3rd pass	2582	2582	2582

<u>Parameter</u>	<u>2nd pass</u>	<u>3rd pass</u>
<u>Heat capacitance</u> (Btu/°F)		
Fuel	540	540
Clad	297	297
Pin surface area (ft ²)	1889	1889

<u>Primary flow</u> <u>times sec</u>		<u>1 pump</u>	<u>2 pumps</u>	<u>4 pumps</u>
<u>From</u>	<u>To</u>			
T _c	T' _c	2.22	2.27	1.36
T' _c	T _d	13.52	6.92	4.15
T _d	T _e	2.02	1.03	0.62
T _e	T _f	11.01	5.63	3.38
T _f	T _g	2.51	1.28	0.77
T _g	T _h	23.85	12.20	7.32
T _h	T _{bi}	2.71	2.77	1.66
T _{bi}	T _{bo}	2.90	2.97	1.78
T _{bo}	T _c	2.54	2.60	1.56
Total		63.28	37.67	22.60

Pressurizer relief valve (spring loaded code valve)

Opens at 2000 psig

Capacity - 18,000 lb steam/hr

Secondary steam relief valve

Opens at 800 psig (520°F)

Capacity - 224,000 lb/hr

Steam by-pass valve

Opening time: 16 sec

Capacity at 460 psia: 190,000 lb/hr

Steam stop valves

1 sec closing time

<u>Parameter</u>	<u>1 pump</u>	<u>1 pump each loop</u>	<u>4 pumps</u>
<u>Primary flow</u> (millions lb/hr)			
Total flow	2.65	5.18	8.63
Core by-pass	0.32	0.63	1.03
2nd pass element	2.16	4.22	7.05
2nd pass leakage	0.17	0.33	0.55
3rd pass element	1.93	3.77	6.30
Control rod channel	0.23	0.45	0.75
<u>Overall boiler U</u> (Btu/hr-ft ² -°F)			
	452	445	650
<u>Boiler heat</u> <u>Transfer area</u> (ft ²)			
	3682	7364	7364
(Total area ÷ 1.1)			
<u>Primary boiler</u> <u>Water mass</u> (lb)			
(weighted for tube mass)	3338	6675	6675
<u>Secondary coolant mass</u> (lb)			
	9688	19,375	19,375
<u>Specific heat (Btu/°F)</u>			
Primary	1.18	1.18	1.18
Secondary	1.15	1.15	1.15

Turbine throttle valve

Normal positioning time: Ahead - 27 sec
Astern - 19 sec

Emergency positioning time: Ahead - 9 sec
Astern - 9 sec

Trip time: Ahead - 3 sec

Instrumentation time constants

T_h (safety)	2.0 sec
T_h and T_c (indication and control)	20 sec
Steam flow	3 sec

Controller

Dead band	Error = 1/63
Maximum rod speed	Error = 3/63
Jump height (at 1/63 error)	5 in. /min rod speed
Rod speed	5 in. to 15 in. /min
Scram delay	240 milliseconds
Steam signal gain	1.0
Average temperature gain	2000 Btu/sec/°F
Time constant for integrated temperature error	200 sec

Figure 1. Flow Diagram for Reactor and Heat Transport System.

5. MATHEMATICAL AND ANAOG MODEL

Refer to Figure 2 for the block diagram of this analog simulation. Refer to Section 4.8 for the nomenclature used in equations and descriptive statements.

5.1. Reactor Kinetics

5.1.1. Logarithmic Simulation

In considering a reactor startup accident the neutron level distribution covers approximately 10 decades from the source to the power range. Direct simulation of kinetics equations requires that the problem be stopped and started several times as the computer overloads. Moreover, the linear method is subject to accumulative error. For these reasons, the reactor kinetics equations are simulated in a logarithmic form. Begin with the standard equations:²

$$\frac{dn}{dt} = \frac{\delta kn}{\ell^*} - \frac{\beta n}{\ell^*} + \sum_{i=1}^5 \lambda_i C_i + S \quad (1)$$

$$\frac{dC_i}{dt} = \frac{\beta_i n}{\ell^*} - \lambda_i C_i \quad (2)$$

Divide both equations by n and make the following substitutions:

$$m = \frac{1}{n} \frac{dn}{dt}$$

$$V_i = \frac{C_i}{n}$$

$$W = \frac{S}{n}$$

$$m = \frac{\delta k}{\ell^*} - \frac{\beta}{\ell^*} + \sum_{i=1}^5 \lambda_i V_i + W, \quad (3)$$

$$\frac{dV_i}{dt} = \frac{\beta_i}{\ell^*} - mV_i - \lambda_i V_i, \quad (4)$$

$$\log n = \int m \, dt.$$

$$\frac{dW}{dt} = -\frac{S}{n} \times \frac{1}{n} \frac{dn}{dt} = -Wm.$$

Figure 3 shows the analog model for the logarithmic kinetics equations. The quantity m in equation 3 is summed up by amplifier 22 and is integrated by amplifier 25 to obtain $\ln n$. Equation 1 for each of the five delay groups (delay groups 5 and 6 are combined) is solved by the integrators 2, 5, 8, 11 and 14. The source term is solved by integrator 24. The cross-products $m V_1$, $m V_2$, $m V_3$, $m V_4$, and $m V_5$ for each delay group and Wm for the source indicate that a seven-cup servo-multiplier is appropriate with m as the driving function. In practice, a better simulation is obtained when a separate servo-multiplier is used for each product. In Figure 3 the outputs of the delay and the source integrators drive the servo motors, and the m signal is fed to the A cup in each case. For any negative value of m out of amplifier 22, the delay group integrators cannot vary from equilibrium values because the feedback through the individual multipliers is self-correcting and independent of the other delay groups. If m is zero, there is no feedback. If the output of amplifier 22 goes positive, the delay integrators are not self-correcting and will run away. This is not possible during a startup transient. The diode prevents the positive drift of amplifier 22 during a steady state. To simulate rod withdrawal, a δk ramp is introduced by amplifier 17.

The logarithmic model is used for the startup accident simulation until the power range is reached (1 MW-t or less). At this point, appreciable fuel and water heat-up begins, and a more extensive simulation is necessary. In the power range a linear reactor kinetics simulation is used.

5.1.2. Linear Simulation

For convenience of simulation, divide equation 2 by $\frac{\lambda_i^*}{\beta_i}$.

$$\frac{\lambda_i \ell^*}{\beta_i} \frac{dC_i}{dt} = \lambda_i n - \frac{\lambda_i \ell^*}{\beta_i} C_i \lambda_i \quad (5)$$

In the analog diagram, Figure 4, the integrators 35 through 39 solve this equation for each of the five delay groups. The output of amplifiers 28 and 41 is Pr or n. This is multiplied by λ_i , which is in the settings of pots 31, 35, 39, 43, and 47. The settings of pots 29, 33, 37, 41, and 45 in the feedback also include λ_i . Therefore the output of the five delay group integrators is:

$$\frac{\lambda_i \ell^*}{\beta} C_i$$

Since pots 30, 34, 38, 42, and 46 include the β terms; their outputs are:

$$\lambda_i \ell^* C_i \text{ for the five groups.}$$

The ℓ^* is not needed and must be divided out, so that equation 1 becomes:

$$\frac{dn}{dt} = \frac{\delta kn}{\ell^*} - \frac{\beta n}{\ell^*} + \frac{1}{\ell^*} \sum_{i=1}^5 \ell^* \lambda_i C_i + S$$

In the power range, the source term S is completely off-scale in the computer and can be dropped. The final form of equation 1 is:

$$\frac{dn}{dt} = \frac{\delta kn}{\ell^*} - \frac{\beta n}{\ell^*} + \frac{1}{\ell^*} \sum_{i=1}^5 \ell^* \lambda_i C_i \quad (6)$$

The product, βn , is obtained from the output of amplifier 30 attenuated by pot 27, whose setting includes β . The product, δk_n , is obtained from the servo multiplier, SM 1, whose inputs are Pr and δk_{net} . Amplifier 32 sums all the quantities on the right side of equation 6. The large gain factor, $1/\ell^*$, is obtained by the combination of scaling factors in pot settings, ten gain inputs to amplifiers 31 and 32, and the high gain hookup of amplifier 32 (0.1 megohm feedback resistor). Amplifier 31 performs the integration to provide an output Pr or n. Amplifier 42 sums the various δk contributions to provide the net δk input for SM 1. The model provides for δk effects from Doppler, moderator, automatically controlled rods, shim rods, constant rate of rod withdrawal, and rod insertions at various ramps. The output of pot 49 furnishes the shim rod δk .

The output of amplifier 45 provides δk for rod withdrawal accidents or rod insertion. Function switch 1 is used for initiating a rod withdrawal accident, and pots 50 and 51 are used to set the withdrawal rate. The scram trip signal is simulated by comparator 1, and the scram delay is simulated by amplifier 47. Comparator 2 actually trips the rod insertion and stops the rod withdrawal. The outputs of amplifier 43 and 44 represent the δk feedback from the Doppler and moderator effects, respectively. The origin of these signals is discussed in Section 5.2 and 5.3.

5.2. Fuel Pin

The fuel pin, including the surrounding coolant, is divided into five heat cells for simulation purposes. There are three concentric equal-volume fuel cells, one clad cell, and one coolant cell. Each fuel cell is considered as a point source of equal heat generation. Heat is assumed to transfer between each of the 5 cells in point-to-point fashion. For each combination of flow and time in core life, a fuel pin temperature profile is calculated apart from the analog model. The temperature profile is determined using variable fuel heat transfer factors, but when the five point temperature profile is simulated, the overall heat transfer factor between cell points is constant as the average cell temperature varies during transients.

The general equation for heat generation and transfer is derived from an energy balance consideration of the following block diagram.

Using cell 2 as an example:

$$\frac{d(M_2 C_p T_2)}{dt} = Q_{in} - Q_{out}$$

$$Q_{in} = Q_{generated} + U_1 A_1 (T_1 - T_2)$$

$$Q_{out} = U_2 A_2 (T_2 - T_3)$$

$$\frac{d(M_2 C_p T_2)}{dt} = Q_{generated} + U_1 A_1 (T_1 - T_2) - U_2 A_2 (T_2 - T_3)$$

Since M_2 is constant:

$$M_2 C_p \frac{dT_2}{dt} = Q_{generated} + U_1 A_1 (T_1 - T_2) - U_2 A_2 (T_2 - T_3)$$

$$\frac{dT_2}{dt} = \frac{Q_{generated}}{M_2 C_p} + \frac{U_1 A_1}{M_2 C_p} (T_1 - T_2) - \frac{U_2 A_2}{M_2 C_p} (T_2 - T_3) \quad (7)$$

The dimension of $UA/M_2 C_p$ is 1/sec, which is the reciprocal of the time constant.

A separate and identical five cell simulation is used for each of the two heat generation passes in the core. The fifth cell or water cell, which is actually two cells, is common to the fuel pin and the primary coolant loop. It will be discussed in more detail in Section 5.3.

The third-pass fuel-pin simulation is represented by amplifiers 1 through 15 in Figure 4. Amplifiers 64 through 69 represent the third-pass primary-coolant heat cells. Amplifiers 49 through 63 represent the second-pass fuel-pin simulation, and amplifiers 76 through 81 represent the second-pass primary-coolant heat cells.

For an example, consider amplifiers 54, 55, 56, and 57, which represent the center fuel cell of the second pass in solving general equation 7. Using the terminology of this equation, the output of the integrator is T_2 , since it sums and integrates all terms on the right side of the equation. The output of amplifier 49 is $Q_{gen}/M_2 C_p$, which is equal to the reactor power generated in one third of the fuel in the second pass divided by the heat capacitance, $M_2 C_p$. Since all three fuel cells have equal volume, the coefficient for each cell is provided by amplifier 29 and pot 25. The output of amplifier 53 is $U_1 A_1 / M_2 C_p (T_1 - T_2)$. The output of amplifier 56 is $(-) U_2 A_2 / M_2 C_p (T_2 - T_3)$. The output of amplifier 55 is $(T_2 - T_3)$. Pot 61 provides the constant coefficient, $U_2 A_2 / M_2 C_p$.

The temperatures of the three fuel heat cells are averaged by amplifier 48 and pot 57. The setting of pot 57 also includes the Doppler coefficient of reactivity for the second pass. The output of amplifier 61 is proportional to the ΔT between the clad and the second-pass average

water temperature. The setting of the B cup, or pot, of SM 4 is proportional to the heat transfer factor between the clad and cooling water. The hookup of SM 4 provides for a variable film drop during a primary flow coastdown. Comparator 3 provides for automatic switching when the clad reaches the saturation temperature of the primary coolant. When the relay switches, the heat resistance between the clad and coolant is cut out. In this manner an approximate local boiling condition is simulated.

5.3. Primary Flow

The primary heat transport system is divided into thirteen cells as dictated by the location of heat sources, heat sinks, safety and control temperature sensors, and the distribution of water mass. The mathematical equations are obtained from an energy balance as shown below for typical cells 5, 6, and 7. Assume that cell 5 is a water coolant cell in the 2nd or 3rd pass. This cell picks up heat from the typical fuel-pin clad cell that was discussed in Section 5.2 in the portion preceding equation 7.

$$\frac{d(M_5 C_p T_5)}{dt} = M_5 C_p \frac{dT_5}{dt} = Q_{in} - Q_{out}$$

$$Q_{in} = U_4 A_4 (T_4 - T_5) + W C_p T_6$$

$$Q_{out} = W C_p T_5$$

Mixing within each cell is assumed so that the outlet temperature is the same as the average

$$M_5 C_p \frac{dt_5}{dt} = U_4 A_4 (T_4 - T_5) + W C_p (T_6 - T_5)$$

$$\frac{dt_5}{dt} = \frac{U_4 A_4}{M_5 C_p} (T_4 - T_5) + \frac{W C_p}{M_5 C_p} (T_6 - T_5) \quad (8)$$

The dimensions of $U_4 A_4 / M_5 C_p$ and $W C_p / M_5 C_p$ are 1/sec, which is the reciprocal of the time constant. Primary cells that are not part of the boiler or the second or third reactor passes have only the second term on the right side of equation 8.

To illustrate the simulation, consider amplifiers 76 through 81, which are for the second-pass primary coolant cells. The first cell is represented by amplifiers 79, 80, and 81. It simulates the average cell temperature. Therefore its temperature (output of amplifier 79) is the source of the moderator δk feedback for the second pass. The temperature is multiplied by the second pass moderator coefficient (Pot 168) and summed with the third pass contribution by amplifier 44.

Equation 8 is solved in the following manner. The output of amplifier 103 is proportional to T_6 ; and the output of amplifier 80 is proportional to T_5 . The pot settings of P95 and P96 include the coefficient, $W C_p / M_5 C_p$. The output of the summer amplifier 81 is proportional to $W C_p / M_5 C_p (T_6 - T_5)$. The output of comparator 3 is proportional to $(T_4 - T_5)$, and pot 94 provides the coefficient $U_4 A_4 / M_5 C_p$. Amplifier 79 performs the integration of dT_5 / dt .

The E pot of SM 3 provides for variable flow, W , during a simulated coastdown accident. Figure 4 indicates that only the five primary cells that are in the core have provision for coastdown flow. This simulation is all that is necessary because the second-pass inlet temperature will not change during flow coastdown. The total integrated primary flow is not enough to displace a volume of water equal to that included in the inlet leg piping and thermal shields.

Amplifier 88 and pot 105 provide the time generation for flow coastdown. Diode Function Generator (DFG) 1 simulates the transient flow that is an input to this analog program. The flow coastdown curves are shown by Figure 5. DFG 1 drives SM 3 whose ganged pots, A, B, C, D, and E, provide transient flow simulation to the primary coolant cells. DFG 1 also

feeds DFG 2, which simulates the curve for flow versus 0.8 power of flow. DFG 2 then drives SM 4, which provides a film coefficient, varying as the 0.8 power of flow, for the simulation of heat transfer from clad to water in the second and third passes.

All primary coolant cells are identified in Figure 4. The temperature subscripts around the loop are the same as those shown by Figure 1. The actual primary average temperature is obtained from the points labeled A through M in Figure 4. Amplifiers 109, 110, and 111 sum these temperatures after weighting them according to volume. Amplifiers 85 and 108 with their associated pots provide a 20-second time constant to simulate the temperature sensors that provide signals for indication and automatic control. Amplifier 128 provides a 2-second time constant for the T_h input to the safety system.

5.4. Boiler

5.4.1. Primary Side Simulation

The primary side is divided into two cells as described in Section 5.3. For each of the boiler primary cells the following heat balance can be written.

$$\frac{d(M_p C_p T_p)}{dt} = Q_{in} - Q_{out}$$

Since mass is constant:

$$\frac{d(M_p C_p \bar{T}_p)}{dt} = M_p C_p \frac{d\bar{T}_p}{dt}$$

$$Q_{in} = W_p C_p T_{in}$$

$$Q_{out} = W_p C_p T_{out} + UA (\bar{T}_p - T_s)$$

Therefore:

$$\frac{d\bar{T}_p}{dt} = \frac{W_p C_p}{M_p C_p} (T_{in} - T_{out}) - \frac{UA}{M_p C_p} (\bar{T}_p - T_s) \quad (9)$$

Since there are two cells, the mass and heat transfer are divided in two equal parts. The two cells are identical in simulation except that the cell represented by amplifiers 93, 94, and 95 provides the average boiler primary temperature that, together with the secondary side temperature, determines the ΔT used for both primary cells and the secondary side.

As an example, amplifier 92 provides the T_{in} and amplifier 94 provides the T_{out} for equation 9. Pots 110 and 111 have a setting proportional to $W_p C_p / M_p C_p$. The output of amplifier 113 has a setting proportional to $UA / M_p C_p$.

5.4.2. Secondary Side Simulation

The following equation is written for an energy balance of the secondary side of the boiler:

$$\frac{d(M_s C_s T_s)}{dt} = Q_{in} - Q_{out}$$

$$Q_{in} = UA (\bar{T}_p - T_s)$$

$$Q_{out} = Q_{\text{steam load}} = Q_{SL}$$

$$\frac{d(M_s C_s T_s)}{dt} = UA (\bar{T}_p - T_s) - Q_{SL}$$

M_s is assumed constant. Between 20 and 80% power, the boiler level is programmed to make M_s a constant. For the upper and lower 20% ends of the power range, the mass change is not enough to warrant the consideration of variable mass in the model. During transients when the feedwater flow and steam flow do not exactly match, there is a deviation from constant mass. This factor was considered, and comparison checks were made on the analog. The effect of feed flow lag could not be detected. Therefore the secondary side heat balance can be written in the following form:

$$\frac{dT_s}{dt} = \frac{UA}{M_s C_s} (\bar{T}_p - T_s) - \frac{Q_{SL}}{M_s C_s} \quad (10)$$

The output of amplifier 117 is proportional to $(-) Q_{SL}$, and pot 152 provides the coefficient $1/M_s C_s$. The output of amplifier 113 is proportional to $(\bar{T}_p - T_s)$, and pot 149 provides the coefficient $UA/M_s C_s$.

5.5. Secondary System

5.5.1. Steam Throttle Valve

DFG 4 simulates the function, steam flow/valve position versus steam temperature, shown by Figure 6. By this means the

variation of steam flow with pressure is taken into account. The throttle valve position is provided by amplifier 121, which drives SM 6. The output of the A cup of SM 6 is then proportional to the steam load demand of the throttle valve.

5. 5. 2. Secondary Relief Valve

With the proper scaling factors DFG 4 can be used as part of the simulation for the relief valve. The relief valve is assumed to have only two positions, fully-open or closed. Pot 148 provides the relief-valve opening set point. Normally the comparator 5 switch is in the grounded position since the output of amplifier 112 is plus. When the set point is reached and amplifier 112 goes negative the comparator connects to DFG 4 output, and a step increase in steam flow is provided. While the valve is relieving, amplifier 116 and pot 153 furnish a small negative input to the comparator, which holds the valve open briefly after the steam temperature has dropped below the opening set point. Thus, a slight operating band is provided between the opening and closing set points.

5. 5. 3. Steam Dump Valve

The dump valve can be operated either manually or automatically. In automatic operation the valve action will maintain a given secondary pressure that is determined by a specified set point. With the valve in automatic operation the boiler load demand on the primary system can be held approximately constant. This advantageously eliminates the requirement of frequent reactor control movement during ship's maneuvers.

The simulation provides the following:

1. Pressure control deadband of approximately 10 psi.
2. Maximum driving force by the valve operator whenever the error signal exceeds the deadband.
3. Inertia and friction.
4. Valve position feedback for comparison with error signal.
5. Minimum full-stroke period of 16 seconds.

The output of amplifier 125 is the error signal. Comparators 6 and 7 simulate the deadband. The output of the comparators and amplifier 124 is proportional to the fixed driving force of the valve

operator. Pots 159 and 160 provide the inertia factor. Therefore their outputs represent the driving acceleration. The output of amplifier 119 represents the retarding acceleration due to friction. Amplifier 122 provides net acceleration. Double integration by amplifiers 120 and 118 provides the valve position. Amplifier 118 drives SM 5. The product from the A cup of SM 5 is proportional to steam flow demand. DFG 3 simulates the function, steam flow/valve position versus T_g , shown by Figure 7. This function is similar to the one generated by DFG 4.

The dump-valve position feedback is delayed by amplifier 127 and compared with the incoming error signal. As the valve moves toward a new position the incoming error signal variation is reduced to provide more stable operation. Pots 163 and 164 and the four diodes around amplifier 125 prevent a false error signal from the valve position signal when the steam pressure error is zero.

5.6. Safety System

The high-power trip input-signal to the safety system is taken from amplifier 30. The high T_h trip input is provided by amplifier 128, which simulates a 2-second time constant. The power and T_h signals are taken through diodes to comparator 1 so that either trip signal can cause a scram. The diodes prevent the addition of these two signals.

If primary-system makeup and pressurizer-heater input are neglected in transient analysis, there is a single primary-average-temperature that corresponds to the low-pressure scram trip-point. This temperature can be precalculated. Therefore the low-pressure trip signal can be taken from amplifier 110. Comparator 10 also is necessary because the low pressure trip comes from a decreasing signal, whereas reactor power and T_h trips come from increasing signals. The 240-millisecond scram delay is provided by amplifier 47 and comparator 2.

5.7. Automatic Control System

The automatic control system output is an error signal defined as follows:

$$\epsilon = \frac{N_i - N_c}{N_c}$$

$$\text{where } N_c = K_1 F_s + K_2 \left[\frac{T_h + T_c}{2} - 508 + \frac{1}{\tau} \int \left(\frac{T_h + T_c}{2} - 508 \right) dt \right]$$

The output of amplifier 17 is proportional to

$$\frac{T_h + T_c}{2} - 508.$$

Pots 15 and 16 provide the coefficient K_2/τ . The output of amplifier 18 is proportional to

$$\frac{K_2}{\tau} \int \left(\frac{T_h + T_c}{2} - 508 \right) dt.$$

Pot 17 furnishes the coefficient K_2 and provides an output

$$K_2 \left(\frac{T_h + T_c}{2} - 508 \right).$$

The output of amplifier 87 is proportional to F_s . Since $K_1 = 1$, no pot is required. The output of amplifier 21 is then proportional to $N_i - N_c$. Amplifier 22 and SM 2, used as a divider, generate the error signal

$$\frac{N_i - N_c}{N_c}.$$

Comparators 8 and 9 simulate the error deadband.

5.8 Control Rod Drive System

Figure 8 shows a simple block-diagram representation of the GE rod drive system. Either the automatic-control system error or the manual-control signal is translated to a velocity demand voltage. The analog model Figure 3 only indicates the velocity demand from the automatic control system (output of comparators 8 and 9). The actual drive system amplifies the velocity demand signal to drive the motor of the synchro transmitter, whose output is a rod position demand. Amplifier 23 simulates the synchro transmitter.

The synchro receiver also functions as a control transformer whose output is proportional to the difference between demanded and actual rod positions. The control transformer output is amplified and used to drive the rod motor, gears, and rod lead screw. The gear box furnishes a mechanical feedback of actual rod position to the control transformer.

Amplifier 24 compares demand rod position with actual rod position, and provides a short delay, thus simulating the synchro-receiver-control-transformer and the mag amp delay. The output of amplifier 24

represents a driving force, and pot 22 represents the reciprocal of inertia. Therefore the output of pot 22 is driving acceleration. Amplifier 25 provides rod velocity with a negative feedback representing friction as a retarding acceleration. The output of amplifier 26 is proportional to rod position and is also proportional to δk , assuming linear rod worth.

The inertia and friction factors for the analog model were verified by comparing the model performance with the results of the rod drive line tests³ and with the performance of the analog model developed by Battelle Memorial Institute.

Figure 2. Block Diagram of Analog Simulation

Figure 3. Analog Model No. 1
(Logarithmic Reactor Kinetics for Startup Accident)

Figure 4. Analog Model No. 2

Figure 5. Primary Flow Coastdown

Figure 6. DFG No.4 — (Steam Flow)/(Throttle Valve Position)
Vs Steam Temperature

Figure 7. DFG No. 3 — (Steam Flow)/(Dump Valve Position)
Vs Steam Temperature

Figure 8. Block Diagram of the GE Control Rod Drive

6. ANALYSIS, RESULTS, AND CONCLUSIONS

6.1. Startup Accident

6.1.1. Analysis

The startup accident is defined as the continuous, accidental control-rod-withdrawal starting with the reactor subcritical at the source level. The effective multiplication factor of the core is continuously increased to cause a change from a subcritical to a supercritical condition. The neutron level, initially at a source level of 10^{-10} of full power (2% subcritical), increases as δk is added. The accident is terminated when the reactor power reaches a level high enough for self-shutdown or scram action.

The rate at which excess reactivity can be added to the core depends on the withdrawal velocity and the net reactivity worth of the rods per unit of distance in the core. The maximum reactivity addition rate that could occur in the Savannah is $3.3 \times 10^{-4} \delta k/\text{sec.}$ For this analysis, however, much higher δk rates are studied in order to show that the reactor is safe for δk addition rates in excess of those caused by any rod-drive system malfunction.

Since the results of the analysis depend on computed parameters other than the δk rate, the effects of Doppler coefficients and scram times are studied. For the faster rates of reactivity addition, the Doppler coefficient provides an inherent negative reactivity feedback that causes a power turnover before scram can occur. The peak power and the heat transferred to the coolant are affected by the value of the Doppler coefficient. Doppler coefficient variations of a factor of two are used in this study. To a limited degree, the core life variation effect of delayed neutron-fractions, of power split between core passes, and of fuel-pin thermal characteristics was investigated.

Most of the analysis was performed without any moderator coefficient feedback. Experimental data⁴ have shown that the moderator

coefficient of reactivity at all temperatures is negative. Therefore the moderator coefficient is assumed equal to zero for the cold startup condition. Without the aid of negative moderator δk in the power range the accident is more severe at peak power. The effect of startup with a hot plant and moderator feedback is also considered in order to check for primary system overpressure.

In the sub-power range the logarithmic analog model is used as described in Section 5 and illustrated in Figure 3. The logarithmic method gives smooth, consistent results. As a check, the linear model is used for one withdrawal rate by constant rescaling after two decades of neutron level change. Figure 9 shows the close agreement between the stepped model and the log model for a withdrawal accident equivalent to the addition of $4.0 \times 10^{-3} \delta k/\text{sec}$.

In the neutron level range above 10^{-2} full power, the Doppler negative reactivity feedback is very important. A linear power model is derived to account for power production and temperature in the fuel cell (see Section 5 and Figure 4). The problem is scaled to 1/10 of the actual time. The problem is started from initial conditions with the correct δk on amplifier 42 feeding the A cup of SM-1. This establishes an initial dn/dt on amplifier 32 and provides the proper integrating rate on power amplifier 31. Steady-state zero-power temperatures are set into the fuel, clad, and water integrators in the initial condition mode. The reactor power is set at the desired value, and the delayed concentrations are set at zero in the initial condition mode. A rod withdrawal δk rate is fed to amplifier 45. The problem is started by switching to the operate mode. The power excursion is terminated by a scram (3% δk removal per second) initiated from the power level trip (90 MW-t). The reactor power is magnitude scaled up to 200 MW-t per volt for the large power excursions resulting from the highest δk rates.

6.1.2. Results

6.1.2.1. Effect of Reactivity Addition Rate

The startup accident is run for δk rates varying from 1×10^{-4} to $1 \times 10^{-2} \delta k$ per second. The total δk in the system at the time the reactor power reaches 10^{-2} full power depends on the δk rate. Figure 10 shows the variation of total δk with $\delta k/dt$. For the new core,

the total effective delayed neutron fraction is 0.0075. For rates exceeding about 5×10^{-4} δk per second, the reactor is above prompt critical at 1 MW-t. The total δk in the system at the time the power level reaches 1 MW-t is about 1% for the extreme rate of 1×10^{-2} δk per second. Figure 11 shows the peak neutron power level as a function of $\delta k/dt$. The neutron power reaches a very high peak value, but this high level does not indicate core damage. Figure 12 shows the peak values of the fuel, clad, and water temperatures as a function of $\delta k/dt$. At a rate of 1.0×10^{-2} $\delta k/sec$, a power excursion of 21,000 MW-t causes a fuel temperature rise of less than 600 F (average of third-pass fuel). Figure 13 shows a better measure of the core thermal performance in terms of the peak value of the average third-pass heat flux as a function of $\delta k/dt$. The highest value of $\bar{q}_3'$ is 135,000 Btu/hr-ft² resulting from a δk rate of 1×10^{-2} $\delta k/sec$. This value is below 190,000 Btu/hr-ft², at which point the hot channel burns out in a steady state.

The startup accidents produce extremely high, short-duration peak powers. Figure 14 shows the power-time characteristics for a δk addition rate of 4.0×10^{-3} $\delta k/sec$. The power reaches the peak value of 7500 MW-t in approximately 100 milliseconds, and is shut down by the inherent negative reactivity feedbacks from fuel temperature and the Doppler coefficient. At 200 milliseconds, the power is shut down to about 600 MW-t. Scram is initiated at about 280 milliseconds to complete the shutdown. The lower curve on Figure 14 shows the power transmitted to the water during the same transient. The peak value is less than 90 MW-t (the overpower safety trip point). The difference between the total power generated and the power transmitted to the water is absorbed by fuel and clad. Since, at about 600 milliseconds, the power generation and heat transfer rates are about equal the fuel temperature levels off. Figure 15 shows the time dependence of the fuel, clad, and water temperatures for the 4×10^{-3} $\delta k/sec$ addition accident. Figure 16 shows the time dependence of δk_{rods} , δk_{Dop} , and δk_{net} . The δk_{Dop} has reduced the δk_{net} to 0.0075 at the time of power turnover; that is, the Doppler effect has compensated for about 0.0020 δk . At the point of power turnover the δk_{net} is equal to β , the delayed neutrons are still negligibly small, and therefore dn/dt is equal to zero. The maximum δk_{net} occurs at time zero (power at 1 MW-t) prior to negative reactivity compensation.

6.1.2.2. Effect of Doppler Coefficient of Reactivity

Since the Doppler effect provides the inherent shutdown mechanism for this accident, results are expected to depend on the Doppler value chosen. A moderate uncertainty in the calculated Doppler value is due to the uncertainty in the change in the resonance integral with temperature change and to the approximations made in calculating the effect of fuel temperature distributions with respect to neutron importance.

Variations of a plus or minus two factor were made on the nominal value of the Doppler coefficient ($-1.14 \times 10^{-5} \delta k/^{\circ}F$) for a range of δk rates from 2×10^{-4} to $4 \times 10^{-3} \delta k$ per second. Figure 17 shows the variation in peak power as a function of α_{Dop} . The effectiveness of α_{Dop} is very pronounced for the higher δk rates. The peak power is doubled for a 0.5 α_{Dop} value and is halved for twice the α_{Dop} value. This same relationship is almost correct for heat flux. The peak $\bar{q}_3'$ for 0.5 α_{Dop} is 161,000 Btu/hr-ft² as compared to 82,500 Btu/hr-ft² for the nominal α_{Dop} in the $4.0 \times 10^{-3} \delta k/sec$ addition accident. Figures 18 and 19 show the variation of $\bar{q}_3'$ and fuel temperature with the α_{Dop} for various δk addition rates.

6.1.2.3. Effect of Scram Time

The scram time has no effect on peak power for reactivity addition rates above $4 \times 10^{-4} \delta k/sec$. Figure 14 shows the time behavior of the reactor power for the accident when adding $4 \times 10^{-3} \delta k/sec$. The neutron power passes through a peak before scram occurs, and begins to level off prior to the scram. Using the same δk addition rate ($4.0 \times 10^{-3} \delta k/sec$) the accident is run for scram times between 1 and 10 seconds. In each case, the 240-millisecond time delay is used with 3% δk available for scram. The effect of lengthening scram time is shown in Figure 20 in terms of integrated power versus scram time. A 1-second scram time results in total integrated power equal to 650 MW-t seconds. A 10-second scram time results in total integrated power equal to 1100 MW-t seconds. Figure 21 shows the variation in $\bar{q}_3'$ with scram time for the startup accident when $4 \times 10^{-3} \delta k/sec$ is added. Figure 22 shows the rate of heat transfer to the water as a function of time for the startup accident with no scram when $4 \times 10^{-3} \delta k/sec$ is added. With no scram, the peak heat flux

is 140,000 Btu/hr-ft², which is well below burnout, but is on the borderline of central fuel melting. If the plant is initially at 240 F, there is no immediate danger from overpressurization of the primary system. The pressurizer relief valve has steam volume relieving capacity to match primary expansion into the pressurizer if the reactor power is less than 150 MW-t, even without spray and secondary steam demand. If the high power level (approximately 140 MW-t) is sustained, the pressurizer fills in approximately one minute. Therefore, positive safety action must be initiated, but rod insertion within the first few seconds is not required.

A startup accident occurring with primary water initially at 500 F presents a more serious heat-up problem than a low temperature startup accident does. Any net heat input to the primary system in excess of 55 MW-t expands primary water faster than the relief valve can remove steam. The safety action must reduce the power below 55 MW-t within the first few seconds. Figure 23 shows primary temperature versus time for the startup accident equivalent to the addition of 4.0×10^{-3} δk /sec, with no moderator coefficient and with a reactivity removal rate of 3.0×10^{-3} δk /sec. Although the relief valve opens, the maximum primary heat up rate (0.6 F/sec) is below the relief valve capacity (0.7 F/sec).

Figure 24 is a cross plot of results indicating the reactivity removal rate required of the safety system to prevent overpressure, versus the rate of reactivity addition from the startup accident. Both curves of Figure 24 are for a plant initially at 508 F with nominal Doppler coefficient. The two curves compare the effects of a nominal moderator coefficient and a zero moderator coefficient.

6.1.3. Conclusions

The reactor period entering the power range is so short that there is a very high power peak, even though the safety trip is actuated at 90 MW-t. The Doppler effect limits the power excursion during the scram delay period, and the δk removal by rods does not start until after the high power peak has occurred. The Doppler effect is so powerful that if the overpower trip only stops the rod withdrawal, very high rates of δk addition can be sustained without fuel element burnout or central melting of the UO₂ fuel pin. For instance, with four primary pumps operating and with a nominal Doppler coefficient, a rod withdrawal

equivalent to the addition of $4.0 \times 10^{-3} \delta k/\text{sec}$ (with rod stop at 90 MW-t) produces an average third-pass heat flux of 140,000 Btu/hr-ft² and a central fuel temperature just below 5000 F in the hottest pin.

However, the limiting factor is excessive primary system heatup and overpressure. If the startup accident occurs with the system near 500 F (during preparations for recovery after a scram) the danger of overpressure is much greater. The primary code relief-valve capacity is exceeded for reactor power above 55 MW-t if there is no steam load, no pressurizer spray action, and no relief through the pilot-operated relief valve. With a hot plant the rod insertion rate required to prevent overpressure is approximately equal to the withdrawal rate that precipitates the excursion. This statement holds true for a Doppler coefficient numerically equal to or greater than $-1.14 \times 10^{-5} \delta k/^{\circ}\text{F}$. This analysis is extended to a Doppler coefficient equal to $-0.57 \times 10^{-5} \delta k/^{\circ}\text{F}$ (half nominal). For this value a full scram (removal of at least 3.0% $\delta k/\text{sec}$) is required to prevent overpressure for the maximum reactivity addition rate analyzed.

Changing the setpoint over a reasonable range has no appreciable effect because the reactor is on such a short period when the power range is reached. The upper limits for the trip point can be set near the reactor power that corresponds to steady state burnout or central melting. Since the lag in heat flux behind power is so large, there is ample margin of safety. The required rate of reactivity removal must be at least 3.0% $\delta k/\text{sec}$.

Automatic limiting action does not prevent safety action.

6.2. Rod Withdrawal in the Power Range

6.2.1. Analysis

The rod withdrawal accident is the accidental continuous withdrawal of control rods starting with the reactor generating power and with the primary system average temperature at 508 F. The philosophy of varying parameters is the same as that for the startup accident (Section 6.1.1.). The following parameters are varied as indicated.

Reactivity addition rate: up to $7.5 \times 10^{-3} \delta k/\text{sec}$

Doppler coefficient: -0.57×10^{-5} , -1.14×10^{-5} , -2.28×10^{-5}
 $\delta k/^{\circ}\text{F}$

Moderator coefficient:	-0.97×10^{-4} , -1.94×10^{-4} , -3.88×10^{-4} $\delta k/F$
Initial power level:	10.35, 17.25, 34.5, 69 MW-t
Scram time:	1 to 40 seconds (5% δk total reactivity removal)

Number of primary pumps: one pump and four pumps

The effect of the variation of core life is partially investigated.

For analog simulation the model shown by Figure 4 is used. The voltage ramp that simulates the addition of reactivity is introduced by function switch 1 and amplifier 45. The accident is terminated by a safety-system trip signal introduced by comparator 1 and amplifier 47. The safety trip can only stop the rod withdrawal or initiate rod insertion. The rate of reactivity removal is determined by the setting of potentiometer P 52. The problem is scaled to 1/10 real time. The reactor power is scaled so that 1 volt equals 10 MW-t power. This gives a useful range from 10 MW-t to 1000 MW-t.

6.2.2. Results

6.2.2.1. Reactivity Rate Effect

To show the effect of a wide variation in reactivity addition rates the range 3.0×10^{-4} to 7.5×10^{-3} $\delta k/sec$ is analyzed. Other parameters are held constant at the points indicated:

Nominal Doppler coefficient = -1.14×10^{-5} $\delta k/F$
 Nominal moderator coefficient = -1.94×10^{-4} $\delta k/F$
 10.35 MW-t initial power
 1 second scram time (5% total δk) at 90 MW-t
 Four pumps
 New core

Figure 25 shows the peak reactor power as a function of the δk rate from 3.3×10^{-4} to 7.5×10^{-3} $\delta k/sec$. As the δk rate is increased, the power overshoot increases. The overpower trip point is 90 MW-t. For the fastest δk rate (7.5×10^{-3} $\delta k/sec$) the power excursion peaks at 900 MW-t. The peak power greatly exceeds the steady-state burnout power (185 MW-t), but this transient power level does not cause damage. The true measure of core damage is the rate of heat transfer from the clad to the water in the third pass, $\bar{q}_3'$ (Figure 26). At

the faster δk rates, the power excursion duration is so short that only a low heat flux is established before the scram is initiated. However, as the δk rate is increased in the range toward the right end of the graph, the power overshoot becomes larger and the heat flux goes up.

As shown by the left end of the curve of Figure 26, the heat flux rises at a very steep slope as the δk rate is decreased. However, the peak heat flux does not exceed the peak reactor power shown by Figure 25. Although Figure 25 does not show data points below δk rates of 3.0×10^{-4} $\delta k/\text{sec}$, the shape of the curve indicates that the peak reactor power does not appreciably overshoot the overpower trip point (90 MW-t) as the δk is decreased from 3.0×10^{-4} $\delta k/\text{sec}$ to zero. In fact, as the reactivity addition rate approaches zero, the peak power (and heat flux) drops away from the 90 MW-t level; because heat input to the primary water causes a high reactor outlet temperature (T_h) scram before the overpower trip is reached. For very slow δk addition rates the power rises only slightly above the initial value (10.35 MW-t) because the negative δk effect of moderator heatup tends to hold power down.

Figure 27 shows the peak values of the average third pass fuel, clad, and water temperatures plotted against the reactivity addition rate. The temperature curves have the same general shape and the same explanation for the shape as the heat flux curve (Figure 26).

Figure 28 illustrates the effect of variable δk addition for rates below the range of Figures 25, 26, and 27. These runs in Figure 28 commence at 17.25 MW-t, a variation from the previous 10.35 MW-t initial power. The scram trip signal stops the rod withdrawal but does not remove any reactivity. A complete investigation of the effects of varying initial power and scram rate is described in Sections 6.2.2.4 and 6.2.2.5. For the two slowest rates indicated by Figure 28 the negative reactivity feedback from the moderator and Doppler coefficients reduces the rate of power increase so that high T_h scram occurs before the high power scram point is reached.

6.2.2.2. Doppler Coefficient Effect

Fuel coefficient effect on the severity of the rod withdrawal accident is determined by varying the α_{Dop} by a factor of plus or minus two about the nominal value of $-1.14 \times 10^{-5} \delta k/F$. Other parameters are the same as stated at the beginning of Section 6.2.2.1. The more powerful Doppler causes a faster shutdown with lower peak power, and the less effective low Doppler gives higher peak power values. Figure 29 shows the difference in peak power versus the δk rate for these values. The highest value of 1120 MW-t for the $0.5 \alpha_{\text{Dop}}$ compares to 900 MW-t for the nominal Doppler when both occur in the accident when $7.5 \times 10^{-3} \delta k/\text{sec}$ is added. Figure 30 shows the fuel, clad, and water temperatures (third pass average) versus the δk rate. The higher value of the α_{Dop} gives a higher negative-reactivity feedback, which slows down the power excursion. At low δk rates, this high Doppler allows a higher temperature rise because the power is maintained below the high power trip for a relatively long time, and the integrated heat input is high. At faster δk rates, the high Doppler restricts the magnitude of the power overshoot and causes less temperature variation.

Figure 31 shows the variation of peak power with the α_{Dop} as a family of curves for different δk rates. There is little effect on the peak power with δk rates of less than $5 \times 10^{-3} \delta k/\text{sec}$. For the fastest δk rate studied, varying the α_{Dop} by a factor of two does not cause burnout. Even though the peak power variation can be large, the heat flux into the water is insignificant (see Section 6.2.2.4 and Figure 34).

6.2.2.3. Moderator Coefficient Effect

Moderator coefficient changes have a small effect on the severity of the rod withdrawal accident. Varying this coefficient by a factor of plus or minus two about the nominal value causes negligible changes owing to the time delay in heat transfer to the water as shown in Figure 32. The peak power is essentially the same for $0.5 \alpha_{\text{mod}}$ and $2 \times \alpha_{\text{mod}}$.

The higher value of the moderator coefficient does change the power transient time duration for the case of $3.3 \times 10^{-4} \delta k/\text{sec}$ reactivity addition. Since the power ramp is turned over more

drastically by the negative reactivity feedback, the time interval needed to reach the scram point is lengthened. This delay causes a higher rise in water temperature. Figure 34 (discussed in Section 6.2.2.4) also shows the effect of varying the moderator coefficient.

6.2.2.4. Initial Power Level Effect

Most of the accidents are initiated from a steady-state power level of 10.35 MW-t corresponding to the normal hotel load of the ship. To determine the effect of initial power level on the severity of the accident, runs are made starting at 34.5 and 69 MW-t, with all other parameters the same as stated at the beginning of Section 6.2.2.1.

Figure 33 shows the peak power and the heat flux versus the initial power for two δk rates (1.0×10^{-3} and 4.0×10^{-3} $\delta k/\text{sec}$). The initial power level of 10.35 MW-t used for most of the runs gives the highest peak power. The highest initial power level gives the highest value of the heat flux. However, the peak heat flux for all cases starting at 10.35 MW-t is only slightly higher than the 69 MW-t steady-state value. Figure 34 shows that the heat flux is higher when the initial power level is 69 MW-t, and also shows the effect of variable moderator and Doppler coefficients for the rod withdrawal case equivalent to reactivity addition of 4×10^{-3} $\delta k/\text{sec}$. The heat flux is well below the burn-out level.

6.2.2.5. Scram Time Effect

A series of runs are made to determine if a longer scram time affects accident severity. Scram times are varied from 1 to 40 seconds (5% δk is removed each time) for three δk rates (3.3×10^{-4} , 1.0×10^{-3} , and 4.0×10^{-3} $\delta k/\text{sec}$). Other parameters are the same as indicated at the beginning of Section 6.2.2.1. For each of the three δk rates, a run is made in which the power level trip stops the rod withdrawal but does not insert rods for a scram. Figure 35 shows the peak power versus scram time for the three δk rates. For δk rates of 1.0×10^{-3} $\delta k/\text{sec}$ and less, there is no appreciable effect from longer scram times. For the δk rate of 4.0×10^{-3} $\delta k/\text{sec}$, the power increase between 1-second and an infinite time is from 215 to 250 MW-t peak power. For rates up to 4.0×10^{-3} $\delta k/\text{sec}$, the power overshoot for no scram is relatively small. The power decay with time is slower for the long scram

times or for no scram. This causes a higher rise in fuel, clad, and water temperatures and in heat flux. Figure 36 shows the peak values of the third-pass average heat flux, $\bar{q}_3'$, as a function of scram times for the three δk rates. For short scram times, the lowest heat flux occurs with the fastest δk rate. However, as scram times are increased, this is somewhat reversed. The peak values of $\bar{q}_3'$ for no scram and δk rates up to 4×10^{-3} $\delta k/\text{sec}$ do not exceed the steady-state value for 69 MW-t power. Figure 37 shows the fuel and clad temperature variations with scram time.

6.2.2.6. Effect of the Number of Coolant Pumps in Operation

Most of the rod withdrawal accidents were studied for the four-pump flow case. To determine the effect of flow rate on the accident severity, several one-pump runs were made. Various reactivity addition rates were used ranging from 0.33 to 7.5×10^{-3} $\delta k/\text{sec}$. Since one major effect of reduced flow rate is a much longer coolant transit time, the α_{mod} was varied by a factor of two.

Figure 38 shows the peak reactor power versus $\delta k/dt$. The peak power values for the three values of α_{mod} appear to fall on a single line. Figure 38 can be compared with Figure 25 to show the differences between one-pump and four-pump operation.

6.2.2.7. Effect of Core Life

As the reactor core ages the delayed neutron fractions and decay constants change, fission products build up in the fuel pin gap, a larger portion of the total power generation is shifted to the center or third pass, and there are changes in the weighted moderator and Doppler coefficients that are applied to each of the two passes. These parameters were calculated for three core ages; beginning of life (BOL, zero full-power days), middle of life (MOL, 300 full-power days), and end of life (EOL, 550 full-power days).

Figure 39 compares the reactor power versus time curves of a rod withdrawal accident for a BOL core and an EOL core. Other parameters are:

Four pumps

Nominal α_{Dop} , nominal α_{mod}

17.25 MW-t initial power
 3.1×10^{-4} δk /sec reactivity addition
Stop rods at 90 MW-t (no δk removal)

The EOL case has the slower power rise and the slower power decay. The decreased fuel-pin gap conductivity (due to fission gas buildup) requires a higher fuel temperature for any given power level, and therefore the Doppler effect is more pronounced (although the Doppler coefficient is not necessarily greater). Therefore, for the rod withdrawal accident, the EOL core power curve requires longer to reach the 90 MW-t trip point, and more δk is added. Since there is no δk removal by rods, the moderator coefficient provides the only means for shutdown; and since the EOL case has more δk added when the rods are stopped, more moderator heatup and integrated power input are required for shutdown.

6.2.2.8. Adverse Combination of Parameters

Of all rod withdrawal cases run, it was found that the most adverse combination of parameters is:

Two primary pumps running
17.25 MW-t initial power
 2×10^{-4} δk /sec reactivity addition
End of core life for power split, Doppler and moderator coefficients, delayed neutron constants, and fuel pin conductivity
Slow scram rate

Figures 40 and 41 indicate the results of this run. Figure 40 shows that high reactor power is not a problem because the high T_h trip is reached when the power is approximately 82 MW-t. Figure 41 shows that the pressurizer relief valve opens in 39 seconds, assuming no spray, and that the maximum primary heatup rate is 0.8 F/sec. The heatup rate is important because, if the rate exceeds 0.7 F/sec, the capacity of the relief valve is exceeded.

If the 540 F T_h safety trip initiates reactivity removal at 4.3×10^{-4} δk /sec (average for fast insertion on GE drives) Figure 41 shows that the primary average reaches 544 F. The maximum primary pressure reaches approximately 2800 psia (assuming no spray, no relief from the pilot-operated relief valve, and no net letdown). If

the safety trip initiates reactivity removal at 3%/sec, the primary temperature reaches approximately 537 F, and the primary pressure does not exceed 2200 psia.

When the pressurizer relief valve opens (assuming no spray or blowdown), the pressure is approximately 2000 psia, and the reactor outlet temperature is 534 F. If reactivity removal at 4.3×10^{-4} $\delta k/\text{sec}$ is initiated at this point (see Figures 40 and 41), the primary average temperature reaches 524.5 F, and the maximum heatup rate is less than 0.7 F/sec.

6.2.2.9. Automatic Limiting Action

The effect of stopping rods or inserting rods, at a rate equivalent to reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$, was investigated for reactivity addition rates between 3.3×10^{-4} and 7.5×10^{-3} $\delta k/\text{sec}$. All runs were made with four pumps operating, a new core, and nominal moderator and Doppler coefficients. Both the high power and the high T_h trip points were varied. Figures 42 and 43 indicate the most important results.

According to Figure 42 with reactivity addition at 3.3×10^{-4} $\delta k/\text{sec}$, stopping the rods produces the same power peak as inserting the rods to remove reactivity at 3.0×10^{-4} $\delta k/\text{sec}$. This coincidence of power peaks holds true for other rod rates as well. The trip point is 83 MW-t (120% of 69 MW-t) and the power overshoots are 2 MW-t for the 3.3×10^{-4} $\delta k/\text{sec}$ addition case and 20 MW-t for the 1.0×10^{-3} $\delta k/\text{sec}$ addition case. If the graded protective action is to be effective in precluding safety action at 90 MW-t the overshoot must be held to 7 MW-t. Protective action initiating reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$ when power reaches 83 MW-t limits power overshoot to 7 MW-t for reactivity addition rates up to 5.0×10^{-4} $\delta k/\text{sec}$.

As shown by Figure 43 stopping of rods also limits power overshoot to 7 MW-t, but the additional heat input to the primary coolant causes a T_h safety trip at 540 F. Figure 43 shows that the faster rod withdrawal produces less primary heatup than the slower rod withdrawal. This is because the fast case adds less total reactivity before the trip point is reached.

During the graded protective study the high power trip was always reached before the high T_h trip. However, some of the results of the safety system studies, including slower rod withdrawal rates and high T_h trip, are applicable to the whole protective system analysis. Results indicate that, for reactivity addition rates as low as 0.75×10^{-4} $\delta k/\text{sec}$, the T_h overshoot above a protective trip point at 530 F is more than 10 F, when protective action removes reactivity at 3.0×10^{-4} $\delta k/\text{sec}$. Therefore, protective action at 530 F does not preclude safety action at 540 F.

6.2.3. Conclusions

6.2.3.1. Safety Action Required with Existing Setpoints

For reactivity addition rates up to and including 4.0×10^{-3} $\delta k/\text{sec}$, an overpower trip that stops the rods can prevent burnout and central melting. Average third-pass heat flux is 99,000 Btu/hr-ft² when moderator and Doppler coefficients are nominal, -1.94×10^{-4} and -1.14×10^{-5} $\delta k/^\circ\text{F}$, respectively. When the coefficients are varied between half and twice nominal value, the variation in maximum heat flux is plus or minus 5%.

However, as with the startup accident, the limiting factor is overpressure. Stopping rods at the overpower trip allows primary pressure to rise to 2100 psia when reactivity addition is 4.0×10^{-3} $\delta k/\text{sec}$. Reversing rods at a speed equivalent to the removal of 3.0×10^{-4} $\delta k/\text{sec}$ will prevent overpressure. At a faster reactivity addition rate of 7.5×10^{-3} $\delta k/\text{sec}$, overpressure is prevented by rod insertion equivalent to reactivity removal at 3.0% $\delta k/\text{sec}$.

For slow rod withdrawal rates the primary coolant heatup becomes excessive. The Doppler and moderator coefficients tend to hold back the power excursion while the integrated energy transferred to the coolant continues to rise. Usually the highest primary heatup occurs when the high reactor outlet temperature (T_h) trip is reached at a power just under 90 MW-t. The most adverse combination of circumstances is with two pumps (one in each loop) operating, 17.25 MW-t initial power, end of life core, 2.0×10^{-4} $\delta k/\text{sec}$ reactivity addition rate, no spray and no relief through the pilot relief valve.

In this case the Doppler and moderator coefficients preclude an overpower trip, and the system heats up until T_h reaches 540 F. Nothing less than reactivity removal at 3% $\delta k/\text{sec}$ will prevent the primary pressure from rising above 2200 psia.

6.2.3.2. Safety Action Required with New Setpoints

The T_h trip point is determined by the thermal characteristics of the core. An increase in the reactor inlet temperature decreases the overpower to departure from nucleate boiling (D. N. B.) in the hottest channel; for example, a 30 F rise in reactor inlet temperature reduces the power by about 10% to D. N. B. with normal flow. The setpoint for the T_h trip must therefore be established to prevent high initial temperatures from invalidating other safety set points designed to prevent core damage. The accident analyses conducted in this scope of work have not investigated the effects of an increased reactor temperature at the beginning of the transient. Thus, retention of the present T_h scram setpoint of 540 F as a maximum is recommended to assure compatibility with the other recommended set points and rod rates.

With the existing safety system there is an advantage gained from lowering the T_h setpoint to 534 F. At this setting reactivity removal at the rate of 4.3×10^{-4} $\delta k/\text{sec}$ (equivalent to a fast insertion with GE drives) prevents overpressure, whereas a reactivity removal rate of 3% $\delta k/\text{sec}$ is required with the trip point at 540 F. However, if the high power trip setting is raised above 90 MW-t (130% of 69 MW-t) the adequacy of a lowered T_h trip point for preventing overpressure is uncertain. With a raised high power trip setting, more heat may be transferred to the primary system with a resulting higher pressure before the high power or T_h trip setting is reached.

Overpressure occurs when the capacity of the pressurizer relief valves (which open at 2000 psig) is exceeded. If the capacity of the present relief valves is approximately doubled or if a safety system trip at 2000 psi is provided, overpressure would be prevented, even with increased high power trip settings.

The limiting factor for raising the high power trip is steady state burnout or central melting. For the slower rates of reactivity addition (less than approximately 4.0×10^{-4} $\delta k/\text{sec}$) the pressure trip is reached before the high power trip. For the faster rates of

reactivity addition, the high power trip is reached first. Even though reactor power does overshoot the trip point, the heat flux to the water does not overshoot.

For the present system, the reactivity removal rate required with the overpressure trip is 4.3×10^{-4} $\delta k/sec$.

6.2.3.3. Automatic Limiting Action

If a trip is provided at 83 MW-t (120% of 69 MW-t) to initiate reactivity removal at 3.0×10^{-4} $\delta k/sec$, the safety trip point is not reached for rod withdrawal accidents that add reactivity at 5.0×10^{-4} $\delta k/sec$ or less. If the safety setpoints are raised 7 MW-t or more, for two, three, or four pump operation; the protective trip point can be set at 90 MW-t.

Stopping of rods without insertion, even at a setpoint as low as 69 MW-t, causes the opening of secondary relief valves, and therefore is not a desirable automatic limiting action.

Automatic limiting action is not effective in preventing T_h safety trip owing to the large transient overshoot of T_h . However, reactivity removal at 3.0×10^{-4} $\delta k/sec$, initiated when $T_h = 530$ F, delays safety action, including the opening of secondary relief valves, so that the operator has more time to act. This setting of 530 F permits ample power (more than recommended):

Four pumps	131 MW-t
Three pumps	106 MW-t
Two pumps	79 MW-t
One pump	40 MW-t

Stopping of rods without reactivity removal allows so much temperature overshoot that the probability of safety action is not reduced.

No automatic limiting action is necessary for the high pressure trip, since the pressurizer spray valve and pilot-operated relief valve perform this function.

6.3. Excessive Steam Demand

6.3.1. Analysis

The largest size steam pipe is the 10-inch header that carries the combined flow of the two steam generators. For the purpose of defining the maximum leak, it is assumed that this pipe line is completely severed so that steam discharges directly to atmospheric pressure through the 10-inch opening. It is calculated that this size break results in total steam flow equivalent to 380 MW-t, at normal steam pressure corresponding to 69 MW-t steady-state power. The steam pressure immediately drops as the excess demand causes flashing of steam and cooling of the secondary and primary masses. Therefore the rate of steam leakage decreases from its initial value during the transient. The accidental opening of the steam bypass valve corresponds to 50 MW-t steam demand and is, therefore, less severe than the maximum steam leak.

The following parameters are varied.

Leak size: maximum size (10-inch break) down to 1/3
maximum size

Primary pumps running: one, two, and four

Initial power (corresponding to pump combination): 17.25, 34.5,
69 MW-t

Doppler and moderator coefficients: half nominal, nominal,
twice nominal

High power trip point: 69 MW-t to 150 MW-t

Reactivity removal rate: zero or 3.0×10^{-4} $\delta k/sec$

6.3.2. Results

Figures 44 and 45 indicate the results of a leak 1/3 maximum size with four pumps running, nominal Doppler and moderator coefficients, and no rod motion. These results are typical for this

accident. At time zero, when the leak actually occurs, the total steam demand (Fig. 44) is highest. The heat transfer from primary to secondary across the boiler lags the steam demand, and therefore the secondary temperature drops (Fig. 45). As the secondary temperature and pressure drop, the steam flow through the leak decreases. Since the heat transfer to the primary water lags the heat transfer to the secondary, the primary temperature decreases (Fig. 45). This drop in primary temperature causes the addition of reactivity from the moderator coefficient effect. Equilibrium is reached at about 120 MW-t, which is below the steady-state power level that corresponds to burnout or central melting.

Figure 46 is a cross plot of maximum reactor power and minimum primary average temperature versus leak size with nominal coefficients, one pump running and 17.25 MW-t initial power. No control action and no safety action, except low pressure scram, is simulated. Because of the poor heat transfer from primary to secondary, the steam pressure drops drastically. With the maximum leak, the maximum reactor power is below the normal high-power trip point (90 MW-t). The lowest primary temperature is above 475 F, where low pressure scram occurs (without make-up or pressurizer heater operation).

Figure 47 shows maximum reactor power versus leak size for two pumps running (one in each loop), nominal coefficients, and 34.5 MW-t initial power. No control and no safety actions, except low pressure scram, are simulated. The entire curve lies below the steady-state power that corresponds to burnout at 1500 psia. The primary pressure (not plotted) does not drop below the 1500 psia scram point.

Figure 48 shows a family of curves (maximum reactor power versus leak size) for four-pump operation and 69 MW-t initial power. No control action and no safety action, except low pressure scram, are simulated. The effect of the various combinations of Doppler and moderator coefficients is shown. For the higher leak rates, either a high Doppler or a low moderator coefficient prevents the reactor power from increasing fast enough to match the steam demand before the primary temperature drops to the low-pressure scram point.

When the Doppler coefficient is low and the moderator coefficient is high, the maximum reactor power exceeds the level of steady-state burnout (at 1500 psia) when the leak size is near the maximum. These two curves and the curve of nominal coefficients exceed the steady state level of central fuel melting for leak sizes near 50% maximum. Figure 49 shows minimum primary temperature versus leak size for the same conditions that were described for Figure 48.

Figure 50 shows maximum reactor power versus high power trip point for four pumps running, maximum steam leak, half nominal Doppler coefficient, and twice nominal moderator coefficient. This combination of parameters represents the worst case; i. e., the rate of reactor power rise is greater and the equilibrium power level (without safety action) is higher than any other case analyzed. The purpose of the curve is to illustrate the amount of power overshoot following a high power trip that initiates reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$ or greater. The rate of reactivity removal has little effect on the peak power.

6.3.3. Conclusions

6.3.3.1. Safety Action Required with Existing Trip Points

For one-pump operation with nominal moderator and Doppler coefficients and an initial power of 17.25 MW-t, the maximum leak (10-inch break) does not cause either an overpower trip or a low pressure trip. Equilibrium is established at a low secondary pressure and a reasonable flow rate with a high ΔT between primary and secondary.

With two-pump operation (one in each loop), nominal moderator and Doppler coefficients, and initial power at 34.5 MW-t, the maximum steam leak causes neither burnout nor central melting, even without rod insertion at the overpower trip. Low pressure scram is not reached.

With four-pump operation, nominal moderator and Doppler coefficients, and initial power at 69 MW-t, low pressure trip is not reached; but central melting occurs if rod insertion is not initiated at the overpower trip point. A reactivity removal rate of 3.0×10^{-4} $\delta k/\text{sec}$ prevents central melting, but a low pressure scram occurs because the steam demand exceeds 90 MW-t and because the primary system continues to cool down.

With four-pump operation and either twice nominal Doppler or half nominal moderator coefficient, low pressure scram is tripped. If the high power trip does not insert rods, power is above 90 MW-t when the low pressure scram occurs. However, burnout and central melting are not reached. With either half nominal Doppler or twice nominal moderator coefficient, central melting and possible burnout occur if rods are not inserted at the 90 MW-t trip point. With reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$, power overshoots to a maximum of 4 MW-t. Low pressure scram follows.

6.3.3.2. Safety Action Required with New Setpoints

For the maximum secondary steam leak, potential damage is from central melting or burnout. Damage is prevented by reactivity removal initiated at 90 MW-t at a rate of 3.0×10^{-4} $\delta k/\text{sec}$. Reactor power overshoot is approximately 4 MW-t with heat flux lagging. Therefore peak heat flux is less than 4 MW-t above the trip point. On this basis the overpower trip point for multi-pump operation can be raised above the present setting of 90 MW-t to a value sufficiently below the steady-state burnout and central melting points to allow at least 4 MW-t overshoot of the trip point.

6.3.3.3. Automatic Limiting Action

A trip that initiates reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$ at 83 MW-t (or at 90 MW-t if the safety trip is raised) will prevent high power safety action, but a low pressure safety trip will follow unless operator control becomes effective.

6.4. Sudden Closing of Steam Stop Valves

6.4.1. Analysis

It is assumed that both steam stop valves are closed so that total steam flow is reduced to zero in one second. If this accident actually occurs in the ship, the auxiliary turbine generators drop in voltage until the automatic low voltage trip is reached in approximately 30 seconds. The diesel generators start automatically but, since approximately 10 seconds is required for the diesels to pick up the load, the primary-pump low-current trip initiates a reactor scram unless the operator starts the diesels prior to the power failure.

The analog model does not account for the action of all the auxiliary equipment. Continuous primary flow is assumed, and the following parameters are varied.

Doppler and moderator coefficients:	half nominal, nominal, and twice nominal
High T_h trip point	: 520 F - 560 F
Reactivity removal rate	: zero, 3.0×10^{-4} $\delta k/sec$, 3% $\delta k/sec$
Primary pump - Reactor initial power combination:	
1 pump	57 MW-t
2 pumps	69 MW-t
4 pumps	69 MW-t

The 57 MW-t for one-pump flow is chosen so that T_h is just under the normal 540 F trip point.

6.4.2. Results

Figure 51 shows the transient reactor power following the sudden closing of steam stops at time zero for the case of four pumps running, 69 MW-t initial power, new core, and nominal moderator and Doppler coefficients. The reactor shutdown is caused only by the negative effect of the moderator coefficient. The high reactor-outlet temperature trip is set at 540 F, but Figure 52 shows that the maximum T_h reached is approximately 530 F. Without spray or net letdown, the pressurizer relief valve opens when the primary average temperature reaches 520 F.

However, the maximum rate of primary heatup is 0.4 F/sec. This rate is lower than the effective reduction of the code-rated relief valve that counteracts primary heatup at a rate of 0.7 F/sec.

At 48 seconds the secondary relief valve opens and in approximately 5 additional seconds the pressure increase is sufficient to actuate the overpressure trip and isolate all systems within the containment vessel. The effect of containment isolation is not simulated.

Figure 53 shows the effect of varying the Doppler and moderator coefficients. This figure is a cross-plot of maximum average primary temperature versus T_h trip point with four pumps running, new core, and 69 MW-t initial power. The high T_h trip initiates reactivity removal at 3% δk /sec. The point at which each curve levels off at the right end represents a trip point setting above the maximum transient temperature. The pressurizer relief opens whenever the setpoint is above 526 F. However, the maximum primary heatup, 0.54 F/sec, does not exceed the relief valve capacity.

Figures 54, 55, and 56 show maximum average primary temperature versus T_h setpoint for flow cases of one pump, two pumps (one in each loop) and four pumps, respectively. These three figures indicate that the higher rate of reactivity removal always reduces the peak temperature. However, since the highest rate of primary expansion occurs ahead of the trip, the rod insertion rate has no effect on the maximum rate of primary relief. The one-pump condition produces the most primary heatup because the initial power to flow ratio is highest. However, other factors, such as slow primary fluid transit time and high primary film resistances to heat transfer, also affect the results.

The worst combination of parameters is with one pump running, twice nominal Doppler coefficient, and half nominal moderator coefficient. The results of this combination are shown by Figures 57 and 58. With the T_h trip point set at 560 F, the rod insertion occurs at 100 seconds. The primary average temperature reaches 550 F at approximately 110 seconds. By this time the overpressure trip has isolated all containment penetrations, because secondary relief occurs at 57 seconds. The maximum heatup rate of 0.58 F/sec limits primary pressure to 2000 psia.

Since four-pump operation is most normal, additional results for this combination are shown by Figure 59. In this case the

most adverse combination of moderator and Doppler coefficients is assumed with initial power at 69 MW-t. Without rod motion the primary heatup rate does not exceed the capacity of the pressurizer relief valves. If reactivity removal at 3.0×10^{-4} $\delta k/\text{sec}$ is initiated when T_h is 530 F, the safety trip when T_h is 540 F is avoided.

6.4.3. Conclusions

The analog results indicate that rod insertion is not necessary to avoid damage from overpressure or from filling the pressurizer.

However, other considerations dictate that the reactor must be shutdown by some means. The cooling water supply is interrupted by containment isolation. Primary pumps are not guaranteed to run longer than 15 minutes at full speed without cooling water. Letdown is stopped so that inseal leakage fills the pressurizer at the rate of 10-20 gpm. Primary heatup plus inseal leakage can fill the pressurizer in 15-20 minutes. Approximately 4000 pounds of secondary steam can be released into containment.

From a practical standpoint it is highly probable that a high T_h trip will never be reached, and that the rods will either be scrammed by low primary pump current or operator action. Adjusting the trip point or providing graded protective action is not an effective means for controlling the accident.

6.5. Primary Flow Coastdown

6.5.1. Analysis

When there is a failure of the power supply to the primary coolant pumps, the primary flow is reduced by motor losses and friction in the primary loop. The kinetic energy of the pump and primary water tends to keep the water flowing. Reference 5 describes a method for calculating coastdown flow and for predicting flow versus time curves. Figure 5 shows two Savannah coastdown curves that were determined by this method.

The basic analog model shown by Figure 4 is used to simulate the coastdown accident. However, since the integrated flow during the accident is low compared to the large primary volume, the reactor inlet temperature does not change. Therefore all of the primary flow model except the reactor is disconnected. The effect of flow coastdown

is simulated by varying the flow time constants by means of a multiple potentiometer servo-multiplier. The effect of the increased primary film heat resistance is simulated by another servo-multiplier whose position varies as the 0.8 power of flow.

When the water film surrounding the fuel pins reaches the saturation temperature during the transient, the effect of local boiling is simulated approximately by completely switching out the film resistance.

Steam voids are not simulated. Therefore, when the coolant reaches the bulk boiling point, the results do not include the added negative reactivity effect of moderator voids.

The safety trip is assumed to come from the undercurrent monitor and rod insertion, if used, is initiated 240 milliseconds after the start of flow coastdown.

The decay heat of the reactor is superimposed on the transient power generation from neutron fission. Figure 60 shows decay heat in megawatts of power vs time after shutdown for infinite irradiation. Even though some of the cases investigated were started from power levels below full power, the decay heat portion of the transient power is based on infinite operation at full power.

The possibility of core damage is evaluated by comparing the maximum heat flux in the core with the heat flux necessary to cause a departure from nucleate boiling (D. N. B.). Initially, only the flow resulting from the inertia of the primary water and pumps is considered. If a D. N. B. is indicated, the effect of natural circulation within the reactor is considered. The natural circulation path is upward through the second and third passes and downward through the control rod flow channels. The water flowing upward through the second pass reaches the reactor upper plenum by means of the orifices which are provided to allow a fraction of the flow to bypass the whole core during normal operation.

Figure 61 indicates the allowable flux in the average and hot channels versus flow per pin. Figure 61 shows that a flux of 50,000 Btu/hr-ft² is allowed in the average channel without D. N. B. At this point the total flow equivalent to 50 lb/hr per pin would be approximately 262,000 lb/hr which corresponds to 2.8% of full 4-pump flow. The allowable hot channel flux is approximately 94,000 Btu/hr-ft². Therefore these

flux limits have to be exceeded before D. N. B. actually exists even though the first phase of the analysis (without considering natural circulation) may indicate D. N. B.

The cases considered are:

3 pumps	68 MW -t	4-sec scram
2 pumps	69-MW -t	No scram
2 pumps	48-MW -t	4-sec scram
1 pump	24-MW -t	No scram

All power levels are equal to or greater than the recommended power level for each of the pump combinations. The 2-pump case considers 2 pumps in one loop, since this has a slightly more adverse power to flow ratio than 2 pumps, one in each loop.

6.5.2. Results

Figure 62 shows total transient reactor power (including decay heat) for the 3-pump-68 MW-t case and the 2-pump-69 MW-t case. Figure 63, 64, and 65 indicate the final results for the case of a complete coastdown from 2 pumps at full speed in one loop with power initially at 69 MW and without scram. Figure 63 shows that, without natural circulation, a D. N. B. may occur in the third pass at approximately 9 seconds. At this point the heat flux is 165,000 Btu/hr-ft², which exceeds the allowable heat flux even with natural circulation.

Since a D. N. B. is indicated, the heat transfer coefficient at the clad surface is assumed to decrease to 100 Btu/hr-ft²-°F, and the maximum clad temperature is determined. Figure 64 shows that the clad reaches 1100 F which is far below the melting temperature. Although burnout does not occur at this temperature, there may be some mechanical deformation near the hot spot.

Figure 65 shows that in the second pass the burnout to actual flux ratio is near one and therefore a D. N. B. is indicated. Further analysis shows that the maximum clad temperature is less than 1100 F.

Figure 66 and 67 indicate results for the case of three pumps at full speed coasting down with power initially at 68 MW-t and with full insertion of all rods in 4 seconds after scram initiation. Figure 66 shows that the ratio of burnout to actual flux in the third pass does not drop below 2 in the first 14 seconds. At this point the hot channel and average channel heat fluxes are 76,800 and 50,800 Btu/ft²-hr, respectively. Therefore even though the curve (Figure 66) is sloping toward an apparent D.N.B., natural circulation is adequate to prevent an actual D.N.B. Figure 67 shows that flow reversal (due to natural circulation within the 2nd pass elements) occurs at 6 seconds but that the burnout flux to actual flux ratio is high.

Figure 68 shows the total transient reactor power curve for the 2-pump, 48 MW-t, 4 sec scram case and the 1 pump, 24 MW-t, no scram case. Figure 69 indicates that for the case of 2 pumps at full speed coasting down with power initially at 48 MW-t and with full insertion of all rods in 4 sec, a D.N.B. may occur in the third pass at 12 seconds. However, the flux at this point is 65,300 and 41,878 Btu/hr-ft² in the hot and average channels respectively. These flux levels can be handled by natural circulation within the reactor without D.N.B. Figure 70 shows that for the same case the region of possible D.N.B. is not reached in the second pass even without the consideration of natural circulation in the whole pass.

Figure 71 (third pass) and Figure 72 (second pass) show that for the case of one pump at full speed coasting down with power initially at 24 MW-t, and without scram, a D.N.B. does not occur. The third pass is marginal but natural circulation is adequate since the hot channel and average channel fluxes are 49,500 and 26,800 Btu/hr-ft², respectively.

6.5.3. Conclusions

For the coastdown case of two pumps, 69 MW-t power, and no scram, D.N.B. is assumed but the clad temperature does not exceed 1100 F, and the reactor is considered safe. Under the same conditions with either 3 or 4 pumps operating the maximum clad temperature is less.

With 3 pump operation at 68 MW-t or 2 pump operation at 48 MW-t, a complete loss of pumping power with a 4-sec scram does not

cause D.N.B. With one pump operation at 25 MW-t, D.N.B. does not occur following a coastdown even if scram is not initiated.

It is therefore concluded that if a complete loss of pumping power occurs when reactor power is within recommended limits, a scram insertion of rods within 4 seconds after the normal 240 millisecond delay will prevent plant damage (D.N.B.). Based on 69-MW-t as 100% power, the recommended power levels for full speed pump operation are:

4 pumps	69 MW-t
3 pumps	59 MW-t
2 pumps (in one loop)	41 MW-t
2 pumps (one in each loop)	45 MW-t
1 pump	21 MW-t

6.6. Capsize Ship

6.6.1. Analysis

The Marvel-Schebler replacement rod-drive system is scrambled by releasing the drive line to allow control rod free fall into the core. The mechanism is capable of scram action within a ship attitude of 45 degrees from vertical. Beyond this attitude, the scram becomes less effective (slower) with increasing list until at some angle the rods will

not move inward when released. The loss of the ability to scram occurs in a capsized ship accident. The list of the ship also causes the progressive loss of secondary boiler water. The analog simulation postulates the instantaneous loss of secondary mass from the boiler occurring with the initial condition of the reactor at 69 MW-t with full primary coolant flow (four pumps at full speed). The core parameters of the delayed neutron fraction, the power split, and the fuel-pin thermal characteristics correspond to those of a new core. Since the inherent shutdown characteristics depend on the α_{mod} and α_{Dop} values chosen, variations of a factor of plus or minus two are used in these coefficients. Part of the analysis is made without safety action, in order to determine the inherent shutdown capability of the reactor.

Owing to the nature of the capsize accident, the pressurizer and the relief valve can not be assumed to function normally when the secondary mass is lost from the boiler. A series of runs are made to determine the safety action required to prevent damage if the relief valve is submerged. These runs are made for nominal and half-nominal moderator values. Primary coolant pump operation is assumed to continue throughout the course of the accident. If the electrical power fails and the primary coolant pumps consequently stop, the accident is similar to the flow-coast-down accident discussed in Section 6.5.

To initiate this accident, it is necessary to modify the basic linear-power model to allow steam generator bypass. This is done by a parallel circuit which includes the primary-water time delay in the steam generator, but does not have the normal heat removal. The accident is started from the initial condition of normal temperatures around the loop. The integrator in the alternate circuit is at the normal boiler-outlet temperature. When the operate mode is started, the alternate circuit is switched into the loop. Therefore, the boiler outlet temperature rises to the inlet temperature with the proper time delay.

6.6.2. Results

Figure 73 shows the inherent shutdown characteristics (no safety action) of the reactor for the expected moderator and Doppler values at the beginning of core life. The reactor power is reduced to about 12 MW-t within the first 60 seconds after the accident, and to about 3 MW-t within the first 2 minutes. As a result of this accident, the

primary system temperature increases from 508 to 544 F. When the primary-system pressure reaches 2000 psia (12 F temperature rise), the rate of change of the primary-system average temperature is 0.6 F/sec. Under normal pressurizer and relief-valve operating conditions, the primary system is not overpressurized since the pressurizer does not fill and since the rate of temperature rise is within the relief valve capacity (0.7 F/sec). Figure 74 shows the effect of the moderator and Doppler variations on the temperature and the temperature change rate for the capsized accident with no safety action taken.

The moderator coefficient has the greatest effect on the accident results (Fig. 74). The only case in which inherent shutdown is not sufficient is for half nominal moderator. In this instance, the maximum primary-system temperature, 564 F, is high enough to cause the volumetric expansion of the primary system to fill the pressurizer. Also, the primary-system temperature change rate when the pressure reaches 2000 psia is about 0.8 F/sec. This change rate exceeds the pressurizer relief-valve capacity for relieving steam. For moderator values above 0.75 times the nominal value and for all Doppler values the inherent shutdown is sufficient to prevent primary-system overpressurization if the relief valve is relieving steam, that is, if the relief valve is not submerged by water in the pressurizer.

Figure 75 shows the results of variable reactivity-removal rate and variable moderator coefficient for the case of a submerged pressurizer relief valve. The desired criterion for safety action limits the primary-system temperature rise to 12 F. A 12-degree temperature rise causes the primary-system pressure to rise to 2000 psia (the code relief-valve setting). If the temperature is held to a 12-degree rise, it does not matter if the relief valve is submerged. A rod insertion rate of about 5.5×10^{-4} δk /sec holds the temperature rise to 12 degrees for the nominal moderator case, and a rate of about 6×10^{-4} δk /sec is sufficient for the half-nominal moderator case (Fig. 75). For this study, safety action is initiated at the same instant that the boiler secondary mass is lost. This corresponds to the safety action actuated by the ship's list.

Inserting rods at the normal control speed of 13.5 inches per minute gives an average reactivity insertion rate of about 3×10^{-4} δk /sec. This rate is less than the rate required to prevent pressurization

above 2000 psia. The final pressure is calculated (assuming no flow through the relief valve) to be 2200 psia for the half-nominal moderator case with an insertion rate of 3×10^{-4} δk /sec. This is slightly above the 2000-psia relief valve setting, but it is well under the 3000-psia design pressure. The actual pressure is less since the relief valve has a 200-gpm water flow capacity. Approximately 30 seconds are required for the temperature to rise the additional 6 degrees beyond the initial 12 degrees. In this time interval, the relief valve is capable of passing about 13 ft³ of water (about twice the insurge caused by an additional 6 degree heat). A safety action of emergency insertion at a rate of 3×10^{-4} δk /sec is sufficient to prevent damage in an overpressurized primary system.

6.6.3. Conclusions

With no heat transfer to the secondary, with water relief at 200 gpm, and with a moderator coefficient of -0.97×10^{-4} δk /F (half nominal for new core), reactivity removal at the rate of 3.0×10^{-4} δk /sec prevents primary pressure above 2000 psig.

6.7. Ejection of One Control Rod

6.7.1. Analysis

The step δk accident simulates the instantaneous control-rod ejection following a complete mechanism top rupture of the Marvel-Schebler drives. If the mechanism ruptures, the control rod ejection occurs over a finite length of time. This analysis is to show that the reactor is safe for an instantaneous ejection of one control rod. If the reactor can take a step δk equal to one control-rod worth, there is no need to compute in detail how fast the rod is ejected. The accident is initiated from 0 to 100% power to determine the initial power-level effect.

The step δk accident is simulated for two physical cases: when the reactor is critical and cold, and when the reactor is generating full power during normal operating conditions. In the first case, the moderator coefficient is assumed to be zero. In the second case, the moderator coefficient is the nominal calculated value for 508 F primary temperature. In both cases, the nominal calculated value is used for α_{Dop} . Both cases have 10% δk /sec reactivity removal, initiated by overpower trip. The reactor is assumed to have a new core. The delayed neutron

fractions, power split, and fuel-pin thermal characteristics correspond to the non-irradiated core.

The analog computer model (Fig. 4) is not changed. The step δk is introduced by throwing a function switch to connect a voltage source to the δk summer. Because of the large magnitude of the power excursion, the power is scaled at 200 MW-t per volt. This gives a useful upper limit of about 20,000 MW-t. The simulated transients were run at 1/10 normal speed to improve accuracy. The range of the step δk inputs is 0.7 to 1.15% (limited to 1.15% by power scaling). Because of the high magnitude scaling (200 MW-t = 1 volt), it is difficult to start the problem at low power. The initial condition of the computer for zero power is actually set at 0.05 volts (approximately 10 MW-t). The initial conditions for the five delay group concentrations are set at zero. This combination is believed to give an initial power somewhere near zero. For the 100% power case the initial condition for power and delay concentration is 0.345 volts (corresponding to an initial power level of 69 MW-t).

6.7.2. Results

Figure 76 shows the variation in peak reactor power with δk step for both initial conditions. The peak powers resulting from a step δk of 1.15% are 23,400 and 23,800 MW-t for initial power levels of 0 and 100%. Whereas the peak power values for the two cases are nearly the same, the heat fluxes are quite different. Figure 77 shows the heat flux, $\bar{q}_3''$, versus the δk step. The initial heat-flux value is zero for zero power and 70,500 Btu/hr-ft² for 100% power. Since the heat flux is the true measure of damage, the accident initiated from 100% is much more severe. The peak value of $\bar{q}_3''$ is 102,000 Btu/hr-ft² for the 1.15% δk step. This is slightly over half the value of 190,000 Btu/hr-ft² required to produce a burnout heat-transfer rate. The reactor is safe beyond the largest δk used (1.15%). However, limitations in scaling make it extremely difficult to show the true limit of safety.

6.7.3. Conclusions

The results of the δk step accident indicate that the reactor is safe for δk steps exceeding 1%. Although the limit of safety has not been defined, results show a safety margin of 1.9 between burnout heat-flux and peak transient heat-flux for a δk step of 1.15%.

The worth of any single control rod depends on the overall control rod configuration and the moderator temperature. Calculations indicate that the highest worth assigned to any rod is 1.2% for the central rod with the moderator temperature at 508 F. This control rod is part of the last group withdrawn in the prescribed control-rod program. Extrapolation of the calculated results from a δk step of 1.15 to 1.2% indicates that the instantaneous ejection of the control rod of highest worth does not cause damage to the reactor core.

6.8. Control System Studies

6.8.1. Reactivity Rate Required for Maneuvering (With Automatic Control)

The maximum rate of steam load change that the emergency throttle speed allows is 20 to 100% or 100 to 20% in 9 seconds. Using these two transients with various rod speeds, the required reactivity rate is found. The objective is to keep primary-system average temperature fluctuations within plus or minus 6 F. The automatic control system, essentially as described in Section 4.7 is used except that the limit of maximum rod speed is varied.

The results of one transient, 20 to 100% in 9 seconds, are shown by Figure 78. Reactivity change of 1% per minute is available to the controller. This δk rate limits the average primary temperature fluctuation to 6 F. Using the same δk rate on a 100 to 20% transient in 9 seconds, the actual primary average changed 5.6 F, and the indicated average (T_h plus T_c divided by two) varied 6.5 F. The actual average temperature cannot be determined in the ship but is readily available on the computer. The indicated average, taken across the boiler, is delayed by the thermometers that have a time constant of approximately 20 seconds. The pressurizer level change (neglecting letdown and make-up) is proportional to the actual average temperature change.

The automatic control system is considered adequate if the maximum available rod speed provides reactivity changes at 1% δk per minute.

6.8.2. Reactivity Rate Required to Prevent Opening of the Secondary Steam Relief Valves (With Automatic Control)

The secondary relief valves open at approximately 800 psig, which corresponds to a saturation temperature of 520 F. It is postulated

that the main turbine trips in one second with the reactor operating at 69 MW-t and with the auxiliary turbine generators operating at the equivalent of 10 MW-t reactor power. Therefore the steam demand transient is 69 MW-t to 10 MW-t in 1 second. This is about the most severe transient that is imposed for analyzing the automatic control system because the plant is automatically switched to manual control at 6.9 MW-t. Figure 79 shows the transient results with no rod motion. The relief valves open at 65 seconds and intermittently thereafter. The reactor power is reduced by the negative reactivity from the moderator coefficient. The power levels out at 20 MW-t, which contains the 10 MW-t base load plus the average steam load of the relief valves.

Figure 80 shows the results of the same transient except that the automatic control system is operating with a maximum available reactivity of $3/4\%$ δk per minute. The steam temperature reaches a peak of 516 F. Therefore $3/4\%$ δk per minute is considered adequate to prevent the opening of the secondary relief valves.

6.8.3. Maximum Allowable Reactivity Rate for Stability

6.8.3.1. Analysis

A study is performed to determine if there is an upper limit in the rod speed allowable for manual control. It is postulated that at some relatively high rod speed the operator has difficulty in maintaining a steady state. That is, if some disturbance puts the power out of a desired deadband, the corrective action, however short, drives the power past the opposite side of this desired deadband. Since the human operator response can not be programmed, a simple on-off controller is used. Since the response and accuracy of this controller exceeds that of the human operator, any oscillations caused by the controller indicate the inability of a human operator to manually control the reactor.

The controller used for this study compares reactor power with a demand power made up of steam flow and temperature error. The system is disturbed by adding a small step of reactivity (usually 0.1%) that causes a power rise of about 8 to 10 MW-t. The controller responds to drive the power back into the deadband (varied between 1.1 and 3.3 MW-t). When the error exceeds these limits, the rod speed is a fixed value. Pots are provided to allow controller gain, deadband, and

rod speed variation. The δk generated by the rods is summed with the moderator and Doppler δk .

The analysis is performed to study one variable at a time to determine its effect on manual control. The variables affecting control stability are rod speed, deadband, Doppler coefficient, and control-rod-drive response time.

6.8.3.2. Results

Owing to the nature of this analysis, the results are not easily shown by measured variable crossplots. The results of each individual run can be summarized best by showing whether oscillations occur. The description and results of 18 computer runs are displayed in the following table. Except as noted, all runs are initiated by step δk addition of 0.1%, with reactor power initially at 69 MW-t.

Run	a_{Dop}	a_{mod}	Deadband (MW-t)	Rod-drive time constant (seconds)	Rod speed (inches per minute)	Maximum undershoot (MW-t)	No. of times deadband exceeded (Not counting initial step)
1	Nom.	Nom.	3.3	Zero	15	None	None
2	Nom.	Nom.	3.3	Zero	30	None	None
3	Nom.	Nom.	3.3	Zero	7.5	None	None
4	2 × Nom.	Nom.	3.3	Zero	15	None	None
5	2 × Nom.	Nom.	1.1	Zero	15	Slight	None
6	1/2 Nom.	Nom.	1.1	Zero	15	0.4	None
7	1/2 Nom.	Nom.	1.1	Zero	30	0.4	None
8	2 × Nom.	Nom.	1.1	Zero	30	None	None
9	3 × Nom.	Nom.	1.1	Zero	30	None	None
10	Nom.	Nom.	1.1	0.25	15	0.8	None
11	Same as run 10 except disturbance = +0.05% 6k					0.8	
12	Nom.	Nom.	1.1	0.50	15	1.2	1
13	Nom.	Nom.	1.1	1.0	15	1.7	1
14	Nom.	Nom.	1.1	2.0	15	2.5	2
15	Nom.	Nom.	1.1	3.0	15	2.7	3
16	Nom.	Nom.	1.1	3.0	30	3.8	7

Run 17 determines the feasibility of controlling with the temperature error only. A time delay of 0.250 seconds is used with 15 inches per minute rod speed and nominal reactivity coefficients. As expected, the system is definitely unstable. The combination of time required to heat the water plus the thermometer response is so great that the control rod motion starts late and makes large overshoots. This single run provides sufficient verification that temperature control alone is unstable.

Run 18 has a lower steady-state power level of 15% (10.35 MW-t). The deadband is 1.1 MW-t, and the rod speed is 15 inches per minute. The response is very good since the control rods return the power into the deadband without undershooting.

6.8.3.3. Conclusion

At rod speeds as high as 30 inches per minute, a simple on-off controller using a power error can control reactor power following a disturbance without causing oscillatory rod motion. Human operator control is not directly predictable owing to differences in reaction time, skill, and attention to duties. However, it is concluded that rod speeds up to 15 inches per minute can be used for manual control without difficulty.

6.8.4. Pulse Reactivity Addition

The Marvel-Schebler rod drive moves in 1/32-inch incremental steps. The computed linearized rod worth is $1.32 \times 10^{-3} \delta k$ per inch. Each pulse has a reactivity worth of $4.1 \times 10^{-5} \delta k$ per pulse. The introduction of a small reactivity pulse causes a small abrupt power rise followed by a more gradual rise until inherent reactivity feedback begins to take effect. The initial rise can be computed simply. For a step input of $4 \times 10^{-5} \delta k$, the initial rise is worth about 0.5% change in the neutron level. At a power level of 69 MW-t, this is about 0.4 MW-t. The behavior following the initial rise is not easily predicted owing to the negative feedback from Doppler and moderator coefficients. Several runs are made to demonstrate the effect of pulse reactivity.

With the reactor at a steady-state power level of 69 MW-t, a $4 \times 10^{-5} \delta k$ pulse is inserted into the reactor. The power makes a step rise of about 0.4 MW-t, and continues to climb. At 2 seconds, the power

is turned over at a peak rise of 0.6 MW-t. The reactor power then steadily declines to its steady-state value (69 MW-t) in 100 seconds.

To determine the effect of the δk pulse magnitude, a pulse worth $8.0 \times 10^{-5} \delta k$ is inserted. The power steps abruptly by about 1.0 MW-t, and then climbs to a peak rise of about 1.3 MW-t in 2 seconds before it decays to steady state in 100 seconds. The power rise is approximately proportional to the reactivity worth of the pulse.

To determine the effect of a_{Dop} on the pulse addition, runs are made for half nominal a_{Dop} and zero a_{Dop} . As the Doppler coefficient decreases, the peak power increases slightly (less than 0.1 MW-t). The power reduction is faster for the smaller Doppler coefficients.

It is concluded that the power pulse resulting from a reactivity pulse is proportional to the δk worth of the pulse, and the time required to return to steady state depends on the negative feedback or reactivity coefficient values for moderator and Doppler.

6. 9. Primary System Leak

It is not feasible to develop an analog model to simulate the steam voids generation in the core and the shutdown therefrom that occurs during a primary leak accident. Therefore no new data is available.

Page 54 of reference 6 indicates that, following a rupture equivalent to the cross section of the 12-9/16-inch pipe, pressure equilibrium is reached in 30 seconds. To provide power holddown at this point, approximately 5.75% δk removal by rod insertion from low pressure trip is required, assuming no benefit from moderator or void. This is equivalent to reactivity removal at a rate of $0.0575/30 = 1.92 \times 10^{-3} \delta k$ per second. Immediate shutdown (within the first second) requires overcoming the highest Doppler deficit, which is 1.5% δk at the end of core life. This requires a rod insertion equivalent to reactivity removal at a rate of $1.5 \times 10^{-2} \delta k/\text{sec}$, initiated by the low pressure trip.

The fastest possible shutdown does not prevent damage from a maximum leak, but with smaller leaks an immediate shutdown prevents possible damage if the leak is isolated before make-up ability is lost.

Graded protective action that initiates reactivity removal during low pressure increases the probability of low-pressure safety action because the reduction of power tends to cool the primary system and accelerate pressure decrease.

6.10. Reactivity Required to Maintain a 20-Second Period

This transient is started with the reactor at about 100 kw-t steady state. Four-pump flow and a new core with nominal moderator and Doppler coefficients are simulated. It is assumed that the period is equal to $\ell^*/\delta k$. This relation is strictly true only if all neutrons are prompt, but for relatively long periods the approximation is good.

Therefore, a constant net δk is fed into the reactor kinetics simulation. The Doppler and moderator reactivity (with normal feedback to the kinetics broken) are subtracted from the net δk in a summer amplifier, which produces the transient rod δk .

The results are shown by Figure 81, which is a plot of rod δk rate and reactor power versus time. As the coolant and fuel heat up with the power increase, the δk addition rate provided by control rods must be increased to cancel the negative δk from the moderator and Doppler coefficients. At 86 MW-t (125% of 69 MW-t) the required rate of δk addition by rods is 8.0×10^{-4} δk per second.

6.11. Comparison of Analog Results With Savannah Testing Data

While the Savannah was at Yorktown, Virginia, a series of dockside tests were performed under carefully controlled conditions to provide transient data for subsequent comparison to analog runs. During the transients, the boiler feedwater was hand controlled to provide constant boiler mass, and the primary make-up valve was placed in manual operation to keep the primary mass constant. The transients were performed between nominal reactor power levels of 45 and 13.0 MW-t. The steam load was established by use of the steam dump valve. Prior to each transient run, the plant was brought to a steady state condition and data was recorded. The visicorder was used to record important plant parameters during the transients. The visicorder traces were transcribed and reproduced. Two transients, runs 3D and 4D, were selected as typical runs for comparison with analog runs. They are a down transient from 45 MW-t to base load and an up transient from base load to 45 MW-t.

The analog model incorporated the latest available shipboard data as input; such as temperature coefficient, power coefficient, and boiler conductance. The analog model was not changed with respect to basic simulation of the reactor kinetics, primary loop, or steam generator, as shown by Figure 4.

Figures 82 and 83 compare the ship down transient (run 3D) with a similar analog run. Figure 82 shows that the reactor power curves compare remarkably well, but the steam flow curves do not compare as well. This discrepancy exists largely because of the uncertainty of the flow-meter accuracy in the low-flow range of the instrumentation. Figure 83 indicates a steam pressure discrepancy of 30 psi, which corresponds to a temperature discrepancy of 4 or 5 degrees. The poor comparison of the average of the cold and hot sections of the primary system cannot be readily explained. Even though there may be an error in simulating the estimated 20-second time response of the thermometers, the analog results and test data should settle out together. The change in the true weighted primary average temperature from the analog does, however, compare well with the calculated ship true average obtained from pressurizer level change data.

Figures 84 and 85 compare the ship up transient (Run 4D) with a similar analog run. Figure 84 shows that the steam flow simulated on the analog is a little higher than the ship steam flow. Since the steam flow is input data to the analog it can be made almost exactly equal to the ship steam flow by means of a carefully adjusted diode function generator. If this is done, the reactor power curves will no doubt fall closer together. Figure 85 shows fair agreement between actual and simulated steam pressure. The primary temperature curves shown by Figure 85 are very flat because the rods are manually moved at the very beginning of Run 4D for the purpose of holding the primary average nearly constant.

Further runs were made on the analog to determine the effect of changing some of the nominal parameter values. All of the analog studies from March, 1961, to the time of the ship testing in April, 1962, were made using information that was up-to-date, as of March, 1961. The simulation using this information is referred to as Model 1. Model 2 was developed in connection with the results analysis program during the Savannah testing phase at sea and at dockside. The basic simulation technique is not changed. The following coefficients that are constants in the model are changed. The third column indicates actual test results that were published⁷ after the analog work was complete.

	<u>Model 1</u>	<u>Model 2</u>	<u>NSS 110</u>
Moderator coefficient (cents per F)	-2.58	-3.3	-3.4
Power coefficient (cents per MW-t)	-1.00	-1.2	-1.3
Boiler over-all "U" (Btu/hr-F-ft ²)	650	721	---
Primary flow (lb/hr)	8.63×10^6	9.8×10^6	9.38×10^6

To compare the two models, two of the rod-withdrawal accidents that were analyzed earlier using Model 1 were rerun using Model 2. For both accidents, four pumps were running, the core was new, 15% power was the starting point, and the rods were stopped when the reactor power reached 130%. Rod withdrawal rates equivalent to reactivity addition at 3.3×10^{-4} δk /sec and 1.0×10^{-3} δk /sec were used.

Figure 86 indicates reactor power versus time for both models with δk addition at 3.3×10^{-4} δk /sec. With Model 2 the power peaked later and trailed off higher than with Model 1. Figure 87 indicates that the negative δk from Doppler is more powerful for Model 2. This delays the power rise and high power trip and allows more positive rod δk to be added.

Figure 88 indicates that with Model 2 the primary temperature rise is slower but higher, as would be expected by the shape of the reactor power curve. Steam relief occurs with either model, so that the final steam temperatures are the same.

Figure 89 compares reactor power and δk for the 1.0×10^{-3} δk /sec reactivity-addition accident. For this rod rate the time of power peaking and rod stopping is almost independent of the difference in power coefficients of the two models. However, after a few seconds the fuel and moderator heat are effective. Since Model 2 has stronger coefficients, the shutdown effect is greater than for Model 1. Therefore, the reactor power shuts down faster after the peak.

Figure 90 compares primary and secondary temperatures for the 1.0×10^{-3} δk /sec reactivity-addition rate. Since Model 2 shuts down the power faster, it is to be expected that the primary temperature rise is less than for Model 1.

The range of moderator and Doppler coefficients used with Model 1 in the Savannah Upgrading Studies includes coefficients used in Model 2. Therefore, the usefulness of the results of the past year's work using Model 1 has not been impaired by the deviation of test coefficients from purely theoretical coefficients.

It is further concluded that the basic Savannah analog model is adequate for the analytical study that is covered by this report.

Figure 9. Log n Vs Time, Startup Accident

Figure 10. Total δk at 1 MW Vs δk Rate, Startup Accident

Figure 11. Peak Power Vs δk Rate, Startup Accident

Figure 12. Peak Temperature Vs δk Rate, Startup Accident

Figure 13. Third Pass Average Heat Flux Vs Reactivity Addition Rate, Startup Accident

Figure 14. Power Vs Time — $4 \times 10^{-3} \delta k/\text{sec}$, Startup Accident

Figure 15. Temperature (Third Pass Average) Vs Time, Startup Accident,
 $\delta k/dt = 4 \times 10^{-3} \delta k/sec$

Figure 16. δk Vs Time, 4×10^{-3} $\delta k/\text{sec}$, Startup Accident

Figure 17. Peak Power Vs Doppler Coefficient For Various δk Rates, Startup Accident

Figure 18. Peak Value of Third Pass Average Heat Flux Vs Doppler Coefficient, Startup Accident

Figure 19. Fuel Temperature Vs α_{Dop} Coefficient, Startup Accident

Figure 20. Integrated Power Vs Scram Time, $4.0 \times 10^{-3} \delta k/\text{sec}$,
Startup Accident

Figure 21. Third Pass Average Heat Flux Vs Scram Time, $4.0 \times 10^{-3} \delta k/sec$, Startup Accident

Figure 22. Heat Transfer Rate to Water Vs Time, Startup Accident Without Scram, 4.0×10^{-3} $\delta k/\text{sec}$ Addition Rate

Figure 23. Average Primary System Temperature Vs Time, Startup Accident,
 4.0×10^{-3} $\delta k/\text{sec}$ Reactivity Addition

Figure 24. δk Removal Rate Vs δk Addition Rate to Prevent Overpressurization of Primary System, Startup Accident

Figure 25. Peak Power Vs δk Addition Rate, Rod Withdrawal Accident

Figure 26. Peak Average Third Pass Heat Flux Vs δk Rate, Rod Withdrawal Accident

Figure 27. Peak Average Third Pass Temperatures Vs δk Rate, Rod Withdrawal Rates

Figure 28. Reactor Power Vs Time, Variable δk Addition Rate, Rod Withdrawal Accident

Figure 29. Peak Power Vs δk Rate for Two Values of Doppler Coefficient, Rod Withdrawal Accident

Figure 30. Peak Values of Third Pass Average Temperatures Vs δk Rate For Two Values of Doppler Coefficient

Figure 31. Peak Power Vs Doppler Coefficient For Various δk Rates, Rod Withdrawal Accident

Figure 32. Peak Power Vs δk Rate For Two Values of Moderator Coefficient, Rod Withdrawal Accident

Figure 33. Variation of Peak Power and Heat Flux With Initial Power, Rod Withdrawal Accident

Figure 34. Third Pass Heat Flux Vs Moderator and Doppler Coefficient, Rod Withdrawal Accident

New Core
 4 Pumps
 4×10^{-3} $\delta k/\text{sec}$ Reactivity Addition
 Initial Power = 69 MW (70,500 Btu/ft²-hr)

Figure 35. Peak Power Vs Scram Time For Three δk Rates, Rod Withdrawal Accident

Figure 36. Peak Value of Third Pass Average Heat Flux Vs Scram Time, Three δk Rates, Rod Withdrawal Accident

Figure 37. Fuel and Clad Temperatures Vs Scram Time, Rod Withdrawal Accident

Figure 38. Peak Power Vs δk Rate For One Pump Operation, Rod Withdrawal Accident

Figure 39. Reactor Power Vs Time For Two Core Ages, Rod Withdrawal Accident

Figure 40. Reactor Power Vs Time, Worst Rod Withdrawal Case

Figure 41. Primary and Secondary Average Temperatures, Worst Rod Withdrawal Case

Figure 42. Reactor Power Vs Time, Graded Protective System, Rod Withdrawal Accident.

Figure 43. T_h Vs Time, Graded Protective System, Rod Withdrawal Accident

Figure 44. Steam Demand, Heat Transfer, and Reactor Power Vs Time

Figure 45. Primary and Secondary Temperature Vs Time

Figure 46. Maximum Reactor Power and Minimum Primary Temperature Vs Leak Rate

Figure 47. Maximum Reactor Power Vs Leak Rate

Figure 48. Maximum Reactor Power Vs Leak Rate

Figure 49. Minimum Primary Temperature Vs Leak Rate

Figure 50. Maximum Reactor Power Vs High Power Trip Point,
Maximum Steam Leak

Figure 51. Reactor Power Vs Time, Sudden Closing Steam Stops

Figure 52. T_h , Primary Average, and Secondary Temperatures Vs Time, Sudden Closing Steam Stops

Figure 53. Maximum Average Primary Temperature Vs T_h Trip Point, Sudden Closing Steam Stops

Figure 54. Maximum Average Primary Temperature Vs T_h Trip Point, Sudden Closing Steam Stops

Figure 55. Maximum Average Primary Temperature Vs T_h Trip Point, Sudden Closing Steam Stops

Figure 56. Maximum Average Primary Temperature Vs T_h Trip Point, Sudden Closing Steam Stops

Figure 57. Reactor Power Vs Time, Sudden Closing Steam Stops

Figure 58. T_h , Primary Average, and Secondary Steam Temperature Vs Time, Sudden Closing Steam Stops

Figure 59. Reactor Outlet Temperature Vs Time, Sudden Closing Steam Stops

Figure 60. Decay Heat Vs Time

Figure 61. Heat Flux Vs Pin Flow
(Natural Circulation)

Figure 62. Reactor Power Vs Time
(2 Pumps and 3 Pumps Coastdown)

Figure 63. Third Pass Heat Flux Ratio Vs Time
(2 Pumps, 69 MW, Coastdown)

Figure 64. Third Pass Clad Temperature Vs Time
(2 Pumps, 69 MW, Coastdown)

Figure 65. Second Pass Heat Flux Ratio Vs Time
(2 Pumps, 69 MW, Coastdown)

Figure 66. Third Pass Heat Flux Ratio Vs Time
(3 Pumps Coastdown)

Figure 67. Second Pass Heat Flux Ratio Vs Time
(3 Pumps Coastdown)

Figure 68. Reactor Power Vs Time
(2 Pumps and 1 Pump Coastdown)

Figure 69. Third Pass Heat Flux Ratio Vs Time
(2 Pumps, 48 MW, Coastdown)

Figure 70. Second Pass Heat Flux Ratio Vs Time
(2 Pumps, 48 MW, Coastdown)

Figure 71. Third Pass Heat Flux Ratio Vs Time
(1 Pump Coastdown)

Figure 72. Second Pass Heat Flux Ratio Vs Time
(1 Pump Coastdown)

Figure 73. Reactor Power Vs Time, Capsize Accident, No
Safety Action

Figure 74. Temperature and dT/dt Vs Reactivity Coefficient, Capsize Accident, No Safety Action

Figure 75. Primary System Temperature Change Vs Reactivity Removal Rate, Capsize Accident

Figure 76. Peak Power Vs δk Step

Figure 77. Peak of Third Pass Average Heat Flux Vs δk Step

Figure 78. Maneuvering Study — 20 to 100% in 9 sec, δk Required to Hold ± 6 degrees

Figure 79. Control Study — Prevention of Secondary Relief, No Rod Motion

Figure 80. Control Study — Prevention of Secondary Relief, Automatic Control

Figure 81. Reactor Power and δk Addition Rate Vs Time for Constant 20 sec Period

Figure 82. Power and Steam Flow Vs Time
(Analog Results and Test Data)

Figure 83. Steam Pressure and Primary Temperature Vs Time
(Analog Results and Test Data)

Figure 84. Power and Steam Flow Vs Time
(Analog Results and Test Data)

Figure 85. Steam Pressure and Primary Temperature Vs Time
(Analog Results and Test Data)

Figure 86. Reactor Power Vs Time, 3.3×10^{-4} $\delta k/\text{sec}$ Addition, Model 1 and 2 Comparison

Figure 87. δk Vs Time, 3.0×10^{-4} $\delta k/\text{sec}$ Addition, Model 1 and 2 Comparison

Figure 88. Primary Average and Steam Temperature Vs Time,
 3.3×10^{-4} $\delta k/\text{sec}$ Addition, Model 1 and 2 Comparison

Figure 89. Reactor Power and δk Vs Time, $1.0 \times 10^{-3} \delta k/\text{sec}$ Addition, Model 1 and 2 Comparison

Figure 90. Primary Average and Steam Temperature Vs Time,
 1.0×10^{-3} $\delta k/\text{sec}$ Addition, Model 1 and 2 Comparison

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7. DISCUSSION

Analyses and operational experiences have demonstrated that the NS Savannah power plant has a high degree of inherent capability to follow load demand and to protect itself from abnormal or accident conditions. The purpose of the control system and the safety system is to supplement this inherent self-regulating characteristic. To achieve the objective of uninterrupted operation without compromising safety, unnecessary power shutdowns must be reduced to a minimum. Three methods to achieve this objective are:

1. Elimination of all safety actions not required to protect the plant from damage by:
 - (a) Providing only those actions proven necessary.
 - (b) Reducing the number of signals initiating scram action by using a single parameter to monitor the proximity to more than one type of damage.
2. Adjusting the safety setpoints to the maximum deviation from the normal operating values to obtain maximum utilization of the inherent self-correction properties thereby reducing the probability of reaching a scram setpoint.
3. Employment of automatic power-limiting action to prevent parameters from reaching safety setpoints.

7.1. General Safety System Requirements

The NS Savannah requires positive, reliable safety action to prevent damage from: (1) rod withdrawal accidents, (2) excessive steam demand, (3) loss of primary system flow, and (4) loss of primary coolant. Failure monitoring circuits must also initiate safety action to ensure plant protection.

7.2. Overpressure

Overpressure will be reached during many rod withdrawal accidents unless positive protection is used. The following parameters should be considered as potential safety signals to prevent overpressure if they are measured with adequate sensitivity and reliability.

1. Neutron flux or UO_2 temperature can be employed to protect against startup accidents or rapid rod-withdrawal rates at power. The neutron flux can be used as a short-period trip or a flux-level trip to protect against startup accidents. Stopping of the rods is required at the beginning of this transient to limit the source of the accident; therefore, the safety signal initiating the safety action must come from a parameter that changes significantly at the beginning of the transient. The UO_2 temperature change initiates the inherent Doppler shutdown mechanism and, if it could be measured, would provide an adequate safety signal, but with present technology the neutron level is used to initiate mechanical rod action. After the rods are stopped, the required rod insertion time can be quite long (approximately one minute). Because of the inherent Doppler shutdown, neutron-flux trip at high power is effective, and a short reactor-period trip or low-power neutron-flux trip is not necessary. All three of these signals are used in the present safety system.

2. Pressure, rate-of-change of primary-coolant temperature, or rate-of-change of pressurizer level could be employed to protect against an excessive primary-coolant expansion rate. For slow rod-withdrawal rates during power operation, excessive primary-system pressure could be experienced without exceeding allowable reactor power or allowable coolant temperatures. Therefore, the rate of expansion of the primary coolant can lead to overpressure. A pressure signal can probably be measured with the greatest reliability and sensitivity. Also, the requirement of a safety signal to protect against this phenomenon could be eliminated by increasing the capacity of the ASME-code relief valve if primary-system relief action is preferred to a safety-system circuit. The existing T_h safety signal protects the plant from excessive coolant-expansion rate.

3. Primary-coolant temperature or pressurizer level can be used to protect against total primary-system expansion sufficient to fill the pressurizer. With proper spray action, complete filling of the pressurizer with water can be achieved during slow transients without exceeding power or pressure limitations. However, this condition must be avoided, since it could readily lead to an overpressure condition. Filling of the pressurizer is prevented with the present T_h information available to the safety system.

7.3. Underpressure

A reduction in primary-system pressure will result from the loss of primary coolant or excessive steam demand. The minimum acceptable pressure for normal operation is determined by the burnout characteristics of the core. Primary-system pressure or low primary-coolant temperature could be used to protect against low operating pressure. A low-pressure signal is presently provided but a low coolant temperature could be utilized if compatibility with the neutron-flux-level trip and reactor operating procedures is ensured. Either signal should suffice to keep the reactor subcritical following a primary-system rupture.

7.4. Excessive Fuel and Clad Temperature

Excessive fuel and clad temperatures can result from loss of coolant flow, excessive steam demand, and certain types of rod withdrawal accidents. Electric current (or power) to primary pump motors, primary-coolant flow rate, or reactor outlet temperature provides a potential safety signal to prevent film boiling and subsequent high clad temperatures following a complete loss of coolant flow. The condition that can lead to excessive clad temperature is an unfavorable balance of heat flux and coolant flow; therefore, the safety signal must anticipate this condition. Rapid detection or anticipation of flow reduction provides the required degree of protection, but reactor outlet temperature monitoring can also be adequate, if it can be measured with the required fast response. If a safety trip based on the actual flow is set high enough to provide early protection for four-pump operation, then the trip point must be reset whenever the number of operating pumps is reduced. Conversely, if the trip is set for one-pump operation, almost

two seconds is required for flow to reach the trip following the loss of four pumps unless the setpoint is adjusted. At present, flow reduction is detected by pump undercurrent monitoring and by measuring ΔP across each boiler. Only the undercurrent monitor has the desired degree of anticipation for all pump combinations.

Neutron flux level or UO_2 fuel temperature measurements can be used to prevent excessive UO_2 temperatures or clad temperatures with normal flow. With the present status of technology, neutron flux level is the preferred signal to prevent excessive heat fluxes that can lead to excessive temperatures.

7.5. Graded Protective System

Operating history and analysis show that frequent safety action (scram) caused when plant parameters reach the safety setpoints is not to be expected. Therefore extensive employment of automatic limiting action to reduce the frequency of scrams is not justified at this time. Graded protection provides a means of reducing the probability of primary and secondary system relief (on the basis of operational convenience — not safety), but the disadvantages of installing and maintaining additional equipment and the inadvertent power reductions due to failures outweigh the apparent advantage of reduced scram probability. This conclusion should be reevaluated in the future by considering additional operating experience and plant modifications. Although in some cases the setpoint and corrective action can be sufficient to prevent plant damage, automatic limiting action can be employed as a safety action without scram backup; if, and only if, the required degree of reliability is obtainable.

7.6. Failure Monitoring Circuits

7.6.1. Loss of Hydraulic-Supply-Manifold Pressure Scram

The scram accumulators are initially charged on the gas side to 1500 psig, and are designed to hold this charge approximately one year. After the hydraulic pumps are started and reach 3075 psig, the accumulators accordingly reach 3075 psig (the oil is separated from the gas by a piston). If the oil-manifold pressure drops, accumulator pressure will eventually drop to the same pressure through leakage paths. At some point insufficient pressure and oil are available for a full scram. Annunciators are provided for low pressure in the hydraulic oil header and for low accumulator-gas pressure for individual accumulators. However, if the operator chose to continue operation in spite of these alarms, a condition could develop where scram action was required but not available. Therefore the scram on loss of manifold pressure is necessary with the present control-rod-drive system.

7.6.2. Nuclear Instrumentation Failure

The relays that initiate scram are held in the energized position for normal plant operation. A safety-system scram signal or a failure of the scram amplifiers will deenergize the relays and a scram will result. If the design were such that an instrumentation failure did not cause scram, then a condition might arise after the instrumentation failure where a scram was required but could not be initiated. Therefore, the nuclear instrumentation failure scram is necessary.

7.6.3. Rod-Test-Switch Scram

The rod-test switches are provided to allow rod-carriage operation without rod movement when a scram condition prevents normal carriage movement. If the test switches should be actuated when the reactor is critical it would be theoretically possible for rods to stick after the carriages and rods had separated. False rod positions would be indicated and rods could later jump to meet the carriages. Therefore the rod-test-switch scram should be retained. The probability of these switches being inadvertently actuated during critical operation would be reduced by providing tight administrative

control. For example, enclose the switches in locked cabinets with key access limited to high-level supervisors.

7.6.4. Loss of Rod-Drive Voltage

Scram is initiated upon loss of two-phase power that supplies all rods not being driven through the GE group demand unit. All rod-position indication is also dependent upon this two-phase power supply. If this power failure occurs the rods would remain in position but could not be moved electrically. Scram capability would exist by hydraulic accumulator action. If power demand changes were limited of if the steam-bypass valve was functioning, the ship could continue operation by nuclear power. In any event, if an unsafe condition was approached owing to transient operation, the safety system is adequate to protect the plant from damage. Therefore, the scram from loss of rod-drive voltage is not necessary.

7.6.5. Cabinet Door Interlocks

There are numerous cabinet door interlocks that cause a scram if the doors are opened. These interlocks are provided for protection against personnel injury and tampering. However, tampering when the plant is not operating could cause unsafe operating conditions later. More adequate protection without the possibility of unnecessary scrams could be provided by installing cabinet locks with key access limited to high-level supervisors.

7.6.6. Velocity-Monitor-Trip Fast Insertion

This fast insertion (all rods driven into the core at maximum carriage speed) is initiated from the failure of the GE group demand units to follow velocity demands. This action is not necessary for safety protection. Even if a servo group of five rods is withdrawn accidentally at maximum carriage speed, the safety system provides adequate protection. Therefore the velocity-monitor fast insertion is not necessary.

7.7. Manual Safety Action

Although the safety system is designed to protect against all conceivable conditions of abnormal operation, the human operator should still retain the means for initiating safety action, especially in

circumstances where abnormal conditions can be anticipated. Therefore, the manual scram should be retained. The manual fast insertion has little advantage over a scram when less than scram action is necessary. If the rods as presently programmed are driven for more than a few seconds the reactor becomes subcritical, and immediate recovery is not feasible because the period is negative and the rods are out of the prescribed pattern. Therefore the manual fast insertion should either be eliminated or retained only for use with the Marvel-Schebler rod drive system, which has a programmed insertion that is just the reverse of the linearized withdrawal program.

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APPENDIX A

Attenuator Settings

Note: Potentiometers Q1 through Q26 appear on Figure 3. All other potentiometers appear on Figure 4

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
Q1	λ_1	0.1	0.3089
Q2	β_1/l^*	1/100	.131
Q3	$\beta_1/l^*\lambda_1$	1/100	.0424
Q4	λ_1	0.1	.3089
Q5	λ_2	0.1	.1157
Q6	β_2/l^*	1/100	.370
Q7	$\beta_2/l^*\lambda_2$	1/100	.320
Q8	λ_2	0.1	.1157
Q9	λ_3		.3057
Q10	β_3/l^*	1/1000	.1095
Q11	$\beta_3/l^*\lambda_3$	1/2500	.1432
Q12	λ_3	2.5	.7642
Q13	λ_4		.1134
Q14	β_4/l^*	1/100	.530
Q15	$\beta_4/l^*\lambda_4$	1/2500	.1870
Q16	λ_4	2.5	.2830
Q17	λ_5 (average of groups 5 and 6)		.0259
Q18	β_5/l^* ($\beta_5 = \beta_5 + \beta_6$)	1/120	0.545

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
Q19	$\beta_5 / l^* \lambda_5$ (groups 5 and 6 combined)	1/3000	0.854
Q20	λ_5 (average of groups 5 and 6)	3.0	.0777
Q21	Scaling		.400
Q22	Startup Accident, δk Addition Rate	4000/100	---
Q23	Subcritical δk Margin	4000/100	.80
Q24	$1/l^*$	$1/10 \times 1/4000$	.926
Q25	β/l^*	1/1000	.278
Q26	Source: (subcritical δk)/ l^*	1/1000	.7408
1	I. C.; 3rd pass fuel; 1st cell (917 F); 400 F Base	$\frac{1}{40} \times \frac{1}{100}$	.1292
2	UA (1st to 2nd cell)/MC (1st or 2nd cell); 3rd pass fuel		.706
3	I. C.; 3rd pass fuel; 2nd cell (820 F); 400 F Base	$\frac{1}{40} \times \frac{1}{100}$	.1049
4	UA (2nd to 3rd cell)/MC (2nd or 3rd cell); 3rd pass fuel		.152 $\times 10$
5	I. C.; 3rd pass fuel; 2nd cell (729 F); 400 Base	$\frac{1}{40} \times \frac{1}{100}$	.0823
6	UA (3rd fuel cell to clad)/MC (3rd fuel cell); 3rd pass		.117 $\times 10$
7	I. C.; 3rd pass clad (535 F); 400 F Base	$\frac{1}{10} \times \frac{1}{100}$	.153
8	UA (3rd pass clad to coolant)/MC (clad-3rd pass)	2	.754
9	Same as Pot 174		.3022
10	Clad temperature during local boiling; 400 F Base	$\frac{1}{10} \times \frac{1}{100}$	.223
11	3rd pass Doppler coefficient (.81 $\times 10^{-5}$ δk /F)	$\frac{1}{3} \times 40 \times 1000$	.108
12	Averaging for $(T_h + T_c)/2$		.500
13	Averaging for $(T_h + T_c)/2$		0.500

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
14	Reference temperature (508 F) for controller; 400 F Base	$\frac{1}{2} \times \frac{1}{100}$	0.54
15	Scaling	$\frac{1}{10}$	.100
16	$\tau \times K_2 = \frac{1}{200} \times 2.11 \text{ MW/F}$	2×10	.211
17	$K_2 (2.11 \text{ MW/F})$	2	.422 $\times 10$
18	Automatic Controller Deadband (1/63)	$\frac{100}{100}$	.0159
19	Same as Pot 18		.0159
20	$\delta k \text{ rate/controller error;}$ $3.0 \times 10^{-4} \delta k/\text{sec} \div 3/63$	10	.0630
21	Same as Pot 20		.0630
22	Reciprocal of rod drive effective inertia		0.318
23	Rod drive effective friction factor		1.00
24	(Btu/sec-MW) (F/Btu) fuel (3rd pass power fraction) (948) (1/540) (.564) = .992	$\frac{1}{40} \times 10$	0.2485
25	Btu/sec-MW) (F/Btu) fuel (2nd pass power fraction) (948) (1/540) (.436) = .766	$\frac{1}{40} \times 10$	.1915
26	I. C. Reactor power = 69 MW-t	$\frac{1}{100}$	.69
27	$\beta/1000 \ell^* = .00751/.027$		.278
28	$1/10000 \ell^* = 37$	$\frac{100}{10 \times 1000}$	.37
29	Average of λ_5 and λ_6		.0259
30	$(\beta_5 + \beta_6)/100 \ell^*$		.654
31	Average of λ_5 and λ_6		.0259
32	I. C. reactor power	1/100	.69
33	λ_4		.1134
34	$\beta_4/100 \ell^*$		0.530

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
35	λ_4		0.1134
36	I. C. reactor power	1/100	.69
37	λ_3		.3057
38	$\beta_3/100 \ell^*$		.1095 $\times 10$
39	λ_3		.3057
40	I. C. reactor power	1/100	.69
41	λ_2	1/10	.1157
42	$\beta_2/100 \ell^*$		.3705
43	λ_2	1/10	.1157
44	I. C. reactor power	1/100	.69
45	λ_1	1/10	.3089
46	$\beta_1/100 \ell^*$		.1311
47	1/10 λ_1		.3089
48	I. C. reactor power	1/100	.69
49	δk shim	---	---
50	Scaling	1/10	.1000
51	δk rate for rod withdrawal accident	100	---
52	δk rate for scram	100	---
53	Bias for scram delay trip		.10
54	(Setting of P53)/(scram delay in sec)	1/100	.0417
55	High T_h trip point (400 F Base)	$\frac{1}{100} \frac{1}{2}$	---
56	(Pot P55 setting)/(High power trip setting, MW)		---
57	2nd pass Doppler coefficient ($.33 \times 10^{-5} \delta k/F$)	$\frac{1}{3} \times 40 \times 1000$	0.0444

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
58	I. C. 2nd pass fuel; 1st cell; 797 F; 400 F Base	$\frac{1}{40} \times \frac{1}{100}$	0.0992
59	UA (1st to 2nd cell)/MC (1st or 2nd cell); 3rd pass fuel		.730
60	I. C. 2nd pass fuel; 2nd cell; 724 F; 400 F Base	$\frac{1}{40} \times \frac{1}{100}$	.0811
61	UA (2nd to 3rd cell) /MC (2nd or 3rd cell); 3rd pass fuel		.174 $\times 10$
62	I. C. 2nd pass fuel; 3rd cell; 664 F; 400 F Base		.0659
63	UA (3rd fuel cell to clad)/MC (3rd fuel cell); 3rd pass		.1178 $\times 10$
64	Scaling	10/40	.25
65	I. C. 3rd pass clad (529 F); 400 F Base	$\frac{1}{10} \times \frac{1}{100}$	.129
66	UA (3rd fuel cell to clad)/MC (clad) - 2nd pass	40/10	.2615 $\times 10$
67	UA (2nd pass fuel to coolant)/MC (clad - 2nd pass)	10	.333 $\times 10$
68	Same as P10		.223
69	Same as P66		.2615 $\times 10$
70	MC (3rd pass clad)/MC (3rd pass coolant)	10/2	.875
71	I. C., Tg (526.7 F); 400 F Base	$\frac{1}{100} \times \frac{1}{2}$	.6333
72	Coolant flow time constant 1/2 of 3rd pass		.260 $\times 10$
73	Same as P72		.260 $\times 10$
74	Same as P70		.875
75	I. C. 3rd pass average coolant (517.7 F) 400 F Base	$\frac{1}{100} \times \frac{1}{2}$	.5885
76	3rd pass moderator coefficient	2 $\times 1000$	.216
77	Same as P72		0.260 $\times 10$

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
78	Same as P72		0.260×10
79	I. C., T_f (508.76 F); 400 F Base	$\frac{1}{100} \times \frac{1}{2}$	.5438
80	Coolant flow time constant T_e to T_f		.296
81	Same as P89		.296
82	Scaling	1/10	.1000
83	(Leakage flow fraction) $\times$ (flow constant; T_g to T_h) .1715 $\times$.1365 = .0234	10	0.234
84	Adjustment for steady state flow		1.00
85	Coolant flow time constant for leakage channel		0.330
86	(Leakage fraction) $\times$ (time constant - leakage channel) .579 $\times$.330 = .196		.196
87	(Leakage fraction) $\times$ (time constant - leakage channel) .421 $\times$.330 = .139		.139
88	I. C., T_e (508.76 F); 400 F Base	$\frac{1}{100} \times \frac{1}{2}$	.5438
89	MC (2nd pass clad)/MC (2nd pass coolant)	10/2	.1129 $\times 10$
90	Coolant flow time constant - 1/2 second pass		.323 $\times 10$
91	Same as P90		.323 $\times 10$
92	UA (2nd pass clad to coolant)/MC (clad - 2nd pass)	2	.666
93	I. C. 2nd pass average coolant (502.62 F); 400 F Base	$1/100 \times 1/2$	.5131
94	Same as P89		.1129 $\times 10$
95	Same as P90		.323 $\times 10$
96	Same as P90		0.323 $\times 10$

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
97	(Flow fraction) $\times$ (flow constant; T_g to T_h) $.8290 \times .1365 = .1131$		0.1131
98	Coolant flow time constant T_g to T_h		0.1365
99	Adjustment for steady state flow		1.00
100	I. C., T_h (522.6 F); 400 F Base	$1/100 \times 1/2$	0.613
101	Flow fraction		.8815
102	T_h sensor time constant (automatic control)		.05
103	Same as P102		.05
104	Flow fraction		.1185
105	Time scale		---
106	Coolant flow time constant; T_h to T_{bi}		.602
107	Same as P106		0.602
108	Adjustment for steady state flow		1.00
109	I. C., T_{bi} (519.56); 400 F Base	$1/100 \times 1/2$	0.5978
110	Coolant flow time constant; 1/2 boiler tubes		.720
111	Same as P110		0.720
112	Adjustment for steady state flow		1.00
113	UA (boiler primary to secondary)/MC (boiler primary)		0.1690
114	I. C., Boiler primary average (508); 400 F Base	$1/100 \times 1/2$	.54
115	Same as P110		.720
116	Same as P110		0.720
117	Adjustment for steady state flow		1.00
118	Same as P113		0.169

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
119	I. C., T_{bo} (496.46 F), 400 F Base	$1/100 \times 1/2$	0.4823
120	Coolant flow time constant, T_{bo} to T_c		0.641
121	Same as P120		0.641
122	Adjustment for steady state flow		1.00
123	I. C., T_c (496.46 F); 400 F Base	$1/100 \times 1/2$	0.4823
124	Coolant flow time constant, ' T_c ' to ' T_d '		0.241
125	Adjustment for steady state flow		1.00
126	I. C., T_d (496.46 F); 400 F Base		0.4823
127	Same as P124		0.241
128	Coolant flow time constant, T_c to T_c'		0.735
129	Adjustment for steady state flow		1.00
130	I. C., T_c' (496.46 F), 400 F Base		0.4823
131	Same as P128		.735
132	T_c sensor time constant		.05
133	Same as P132		.05
134	Primary volume weighting for $\bar{T}_{primary}$	10	.6880
135	Primary volume weighting for $\bar{T}_{primary}$	10	.5880
136	Primary volume weighting for $\bar{T}_{primary}$	10	.1083 $\times 10$
137	Primary volume weighting for $\bar{T}_{primary}$	10	0.1273 $\times 10$

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
138	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	0.1885×10
139	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	.525
140	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	.3722
141	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	.8475
142	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	$.1314 \times 10$
143	Primary volume weighting for $\bar{T}_{\text{primary}}$	10	0.6955
144	I. C. , flow mode		1.00
145	I. C. , flow mode	10	0.3715
146	I. C. , flow mode	10	.2603
147	I. C. , flow mode	10	.2678
148	Secondary relief valve set point (520 F), 400 F Base	$1/100 \times 1/2$	.600
149	UA (primary to secondary)/MC (secondary)	10	.597
150	I. C. , T_s (458.8 F), 400 F Base	$1/100 \times 1/2$	.294
151	Scaling		.100
152	(Btu/MW-sec)/MC (secondary)	$1/2 \times 10$	.2128
153	Secondary relief valve operating band setting		.02
154	Secondary relief valve rate setting		.955
155	Scaling	10/40	.25
156	Steam dump valve friction factor		0.1825
157	I. C. , steam throttle valve position		1.00

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
158	Steam throttle valve maneuvering rate		---
159	Reciprocal of steam dump valve inertia		0.1080
160	Same as P159		.1080
161	Steam dump valve; controller deadband		.01
162	Same as P161		.01
163	Steam dump valve; controller error null		.5
164	Same as P163		.5
165	Steam dump valve; position signal time constant	1/10	.05
166	Same as P165 but without scaling		.5
167	Steam dump valve; controller temperature set point, 400 F Base	1/100 $\times$ 1/2	---
168	2nd pass moderator coefficient	2 $\times$ 1000	.171
169	T _h safety signal time constant		.5
170	Same as P169		.5
171	Secondary leak rate setting		---
172	Same as P54	1/100	.0417
173	Low pressure scram set point (475 F average Primary) 400 F Base	1/100 $\times$ 1/2	.375
174	UA (3rd fuel cell to clad)/MC (clad) - 3rd pass	40/10	.3022
175	UA (3rd pass fuel to coolant)/MC (clad - 3rd pass)	10	.377 $\times$ 10
176	I. C., leakage flow temperature (503.6 F) 400 F Base	1/2 $\times$ 1/100	.518
177	I. C., T _h , control (519.6), 400 F Base	1/2 $\times$ 1/100	0.598

<u>Pot no.</u>	<u>Definition</u>	<u>Scaling</u>	<u>Setting</u>
178	I. C., T_h , safety (519.6), 400 F Base	$1/2 \times 1/100$	0.598
179	I. C., T_c (496.46 F), 400 F Base	$1/2 \times 1/100$	0.4823

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