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ABSTRACT

The results of a study to determine the limiting temperature conditions in a large reactor system are presented. The study considers a sodium-cooled breeder reactor system having a loop-type primary system configuration. A temperature range of 930-1050°F in reactor outlet temperature is covered. Significant findings were that the use of the materials for the 930°F reference design, i.e., a core material of 20% cold-worked 316 stainless steel, a primary piping material of 316SS, and a steam generator material of unstabilized 2-1/4 Cr - 1 Mo resulted in limiting conditions in component performance at the higher temperatures. Means to circumvent these limits through the use of alternate materials, mitigation of thermal transients, and/or design changes are presented. The economic incentive to make some materials changes is also presented.

This document has been prepared to satisfy the requirements of the Primary System Temperature Limits Task (Milestone 12205-04) and the Transit Mitigation Task (Milestone 12305-03).

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1.0 INTRODUCTION AND SUMMARY

1.1 PURPOSE OF STUDY

By increasing the mixed-mean reactor outlet temperature of large LMFBR reactor systems, it is possible to improve plant efficiency through employing higher temperature and pressure steam conditions, as well as reduce the size of some of the major plant components with a resultant improvement in the overall economics of the plant. However, limits must be placed on the amount the temperatures can be raised because of the degradation in materials properties with increasing temperature.

The purpose of this study was to assess the economic benefits of increasing primary system temperatures and to determine if temperature limiting conditions are reached using materials which are already developed or are being developed. If limiting conditions were identified at or below 1050°F, it was the additional purpose of this study to determine what changes in the system design and materials can be made to remove these limitations. The plant configuration considered was a loop-type plant employing three primary loops. A plant life of 40 years was assumed. A detailed description of the reference plant design is given in Reference 1.

1.2 OVERALL APPROACH USED

Reactor systems were optimized to give minimum total power generating costs at 930°F, 990°F, and 1050°F reactor outlet temperatures using the System Optimization Code (SOC-II) (Reference 2). The selection of these temperatures is based on the specified temperature range for the study of 930°F to 1050°F. The results of the SOC-II

calculations yielded plant and plant arrangement parameters and component sizes, optimum operating conditions, and economic parameters. The base case SOC-II calculations were made using the same materials as described in Reference 1. From examination of the SOC-II results, as well as the results of the accompanying structural analysis effort, it was possible to identify some limiting conditions where changes in materials would be required. SOC-II calculations were then performed using these new materials to determine their impact on system design and economics.

In support of the designs produced by the SOC-II analysis, the entire Nuclear Steam Supply System (NSSS) was examined to identify the critical areas in the system components which were thought to be most sensitive to increases in temperature and to thermal transients. From this list of critical areas, five were selected as being most critical to temperature effects and these five areas were subjected to detailed structural evaluation. Within the scope of this study, it was not possible to examine the five critical component areas at all three reactor outlet temperatures for all the different groups of thermal transients predicted for the NSSS over its plant life. The system having a 1050⁰F reactor outlet temperature was selected because it was felt that this would represent the severest test for the components.

An existing system simulation model which was developed for analysis of the design described in Reference 1 was modified to provide the thermal transient data required for the structural evaluation of the component critical areas at the 1050⁰F outlet temperature. Because of the large number of transients which must be considered to obtain a realistic evaluation of a component's structural adequacy, umbrella transient sets were developed to reduce the large number of transients to a more manageable group.

Based upon the results of the structural analysis, design and materials changes were to be identified which would improve those critical areas which were found to have damage fractions in excess of those allowed by the ASME Code for the reference design materials. Also, means were to be identified whereby the thermal transients could be mitigated to reduce thier effects on the critical areas. A large number of transients

were analyzed, and it was possible to identify the transients which were most troublesome and for which mitigation schemes would prove beneficial in reducing their contribution to component stresses.

1.3 SUMMARY OF RESULTS

Figure 1a shows the overall economic tradeoffs with temperature employing the reference plant design materials. Although the temperature range considered in this study was only from 930°F to 1050°F, a saturated cycle for an 875°F reactor outlet temperature was added to the curve to provide the economic trend in the low temperature direction. The potential economic benefits of using alternate materials are shown in Figure 1b. Shown here is the base case using the materials employed in the 930°F reference design as well as the potential economic benefits available through the use of materials changes. The savings shown in the curves are additive, with the first curve representing only that saved by going to an advanced cladding and duct material, (Alloy D9) the second adds going to a new steam generator material (stabilized 9 Cr - 1 Mo), and the lowest curve adds going to a new primary/intermediate system structural material (stabilized 9 Cr - 1 Mo). Figure 1c shows these same benefits on capital costs. It is seen that there is a good incentive for going to advanced materials, however, there is little incentive to increase reactor outlet temperatures beyond about 975°F. This is primarily due to the increase in piping length required at higher temperatures and its effect on containment building costs. For a pool-type reactor, or for a loop plant using flexible joints (bellows) in the primary loop, economic benefits can be gained out to higher temperatures. This can be seen in Figures 1d and 1e, which take out the effect of the containment building diameter increase.

The magnitude of plant thermal transients increase with increasing temperature, and a number of critical component areas were found not to meet the creep fatigue damage limits at the higher temperatures. Some transient mitigation schemes were incorporated to reduce the effects of plant transients, however, with only the most basic mitigation schemes

FIGURE 1a. EFFECT OF REACTOR OUTLET TEMPERATURE
ON POWER COSTS

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FIGURE 16. ECONOMIC INCENTIVES FOR ALTERNATE MATERIALS VS. REACTOR OUTLET TEMPERATURE

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FIGURE 1c. ALTERNATE MATERIALS OFFER
CAPITAL COST SAVINGS

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FIGURE 1d. POWER COSTS VS. REACTOR OUTLET
TEMPERATURE - WITHOUT CONTAINMENT
BUILDING COST

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FIGURE 1e. CAPITAL COST SAVINGS USING
ALTERNATE MATERIALS - WITHOUT
CONTAINMENT BUILDING COST

considered, four of the critical components could still not meet the life fraction limits at the highest temperature considered (1050°F). These were the reactor vessel exit nozzle, the primary pump bearing support, the IHX upper tubesheet, and the steam generator upper tubesheet. It is felt that materials substitution, design configuration changes, or additional transient mitigation schemes would provide adequate margins in these areas, however, the scope of this study did not allow final resolution of these areas. These should be evaluated further in any follow-on effort.

This document has been prepared to satisfy the requirements of the Primary System Temperature Limits Task (Milestone 12205-04) and the Transient Mitigation Task (Milestone 12305-03).

2.0 EFFECTS OF TEMPERATURE ON PLANT DESIGN AND ECONOMICS

The effects of reactor outlet temperature were first evaluated for the materials associated with the 930°F reference design. These effects are covered here in detail. The effects of changes in these reference materials on plant economics is covered later in Section 4.0.

2.1 SYSTEM DESIGN POINTS

System design points and operating conditions were determined for systems having mixed-mean reactor outlet temperatures of 930, 990, and 1050°F. The system design points were determined using the SOC-II code, and the points selected are those which yield the minimum total power generating costs at that particular outlet temperature. The general plant configuration and the materials used are the same as those given in Reference 1.

The SOC-II code aschematic of which is shown in Figure 2, carries out a complete plant design and cost estimate for a given set of major plant parameters, i.e., reactor power, reactor outlet temperature, and steam cycle conditions. In doing this, it iterates on intermediate hot leg temperature, primary system ΔT , and intermediate system ΔT to obtain the preferred operating conditions for the most economical plant. For each iteration the code first thermal-hydraulically sizes the reactor core. Once the core has been sized, the code performs the core physics analysis and calculates the breeding ratio, doubling time, and fuel cycle costs. It then calculates the size of the vessel, required flow rate, pipe sizes and lengths, pump sizes and pumping power, and optimizes the IHX and steam generator designs. For the latter analysis, it optimizes on tube size, number of tubes, tube length, etc., at each point. The code then sizes the buildings and calculates the total plant capital costs based on unit cost information in the code. The turbine generator and BOP system design and capital costs are provided by the BOP data provided in the code for each steam cycle. The code also estimates system differential unavailability as it differs with heat exchanger number of tubes, turbine generator type, number of components, e.g., a SG recirculation system, etc. All of these costs, i.e., capital, unavailability, and fuel cycle are then included to get overall power costs.

SOC-II DESIGN POINT OPTIMIZATION CODE

FIGURE 2

During the iteration process, the plant parameters are corrected for pumping power to assure a constant net electrical power output basis for each plant.

In order to take advantage of the increased reactor outlet temperature, different steam conditions with increasing turbine efficiencies were used at the different outlet temperatures. In this study, all plants used the Benson superheated steam cycle with once-through, combined evaporator/superheater, hockey-stick steam generators. The system net electrical output was held constant at 950 MWe with the reactor thermal power being allowed to vary with the increased efficiencies at the higher temperatures. The number of fuel assemblies in the core was varied with reactor thermal power within the constraint of a maximum fuel pin linear power of 16 kW/ft. Fuel assembly residence times were adjusted to yield maximum fuel discharge burnups of 115 MWd/kg of heavy metal for the three outlet temperature reactors. Because of decreased cladding strength and the effects of corrosion and fuel-cladding interaction, to achieve this constant burnup in the fuel, it was necessary to vary the fuel pin fission gas plenum length so that a constant cumulative damage fraction (CDF) of 0.5 in the cladding was maintained at each outlet temperature. This means that fuel for each reactor would have the same end-of-life fuel pin rupture probability.

A summary of powers, temperatures, flow rates, pressures, and efficiencies for the three designs are given in Table 1.

The required reactor thermal power for a net electrical output of 950 MWe is shown in Figure 3 as a function of reactor outlet temperature. Also shown is the overall system efficiency. There is about a 5% decrease in reactor power in going from 930 to 1050°F outlet temperature and about two points improvement in net plant efficiency.

The optimum primary and secondary sodium temperatures are shown in Figure 4. It can be seen that as the reactor outlet temperature is raised, the optimum primary and secondary ΔT 's increase. The increase in ΔT results primarily from the trade off of core, pump and piping size,

TABLE 1
SYSTEM DESIGN POINTS VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Turbine Throttle Steam Pressure, psig	2200	2400	2400
Turbine Throttle Steam Temperature, °F	850	900	950
Plant Net Power Output, MWe	950	950	950
Net Plant Efficiency	0.366	0.377	0.385
Turbine Generator Gross Output, MWe	1027	1020	1017
Turbine Generator Gross Efficiency	0.391	0.401	0.408
Reactor Thermal Power, MWt	2597	2521	2471
Reactor Inlet Temperature, °F	640	650	670
Reactor ΔT , °F	290	340	380
Reactor ΔP (Plenum-to-Plenum), psi	100	100	100
Primary Flow, Total, 10^6 lb/hr	100.3	83.3	73.2
Primary Pump Discharge Flow, gpm	80,100	67,300	59,700
Primary Pumping Power, MWe	25.6	21.4	19.0
Primary Pump ΔP , psi	182	182	182
Thermal Power/IHX, MWt	872	845	828
IHX Primary Inlet Temperature, °F	932	992	1052
IHX Intermediate Outlet Temperature, °F	890	950	995
IHX Intermediate Inlet Temperature, °F	610	610	625
Intermediate Flow, Total, 10^6 lb/hr	104.3	83.5	75.4
Intermediate Pump Discharge Flow, gpm	79,500	63,600	57,500
Intermediate Pumping Power, MWe	13.8	11.3	10.0
Intermediate Pump ΔP , psi	100	100	100
SG Thermal Power/Loop, MWt	875	848	831
SG Steam Outlet Temperature, °F	855	905	955
SG Steam Outlet Pressure, psia	2290	2490	2490
SG Feedwater Temperature, °F	470	470	470
Steam Flow, Total, 10^6 lb/hr	9.85	9.26	8.73
Rejected Heat, MWt	1647	1571	1521

FIGURE 3 . EFFECT OF REACTOR OUTLET TEMPERATURE ON REACTOR POWER AND SYSTEM EFFICIENCY

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FIGURE 4. OPTIMUM PRIMARY AND SECONDARY ΔT 'S AND SECONDARY HOT LEG TEMPERATURE VS. REACTOR OUTLET TEMPERATURE

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and pumping power cost effects against the heat exchanger costs until an optimum pinch point is reached in the evaporator section of the steam generators. The optimum secondary hot leg temperature results primarily from the tradeoff between the intermediate heat exchanger and steam generator costs. Reducing the secondary hot leg temperature increases the steam generator costs, whereas, increasing it increases IHX's costs because of the tradeoff of thermal driving force between primary and secondary sodium and secondary sodium and steam.

The primary and secondary sodium flow rates and pumping power requirements as a function of outlet temperature are shown in Figure 5. The larger pumping power requirements of the primary pumps relative to the secondary pumps reflect the pressure loss through the reactor core. The reactor pressure drop was held constant at 100 psi for this study because numerous past trade studies have shown that a very broad and almost flat minimum in power costs as a function of reactor pressure drop exists near a value of 100 psi.

The main design characteristics of the reactor core which are influenced by reactor outlet temperature are given in Table 2. As the system efficiency improves, the number of fuel assemblies in the core may be reduced because of the lower reactor thermal power required. This is constrained by a limiting fuel pin linear power of 16 kW/ft. This limit is consistent with the requirement that there be no fuel centerline melting during reactor operation.

As the outlet temperature is raised, the required fuel assembly duct wall and interassembly gap thicknesses increase. This occurs because the neutron irradiation induced swelling and creep of the 20% cold-worked 316 stainless steel duct material is strongly temperature dependent. The correlations used to predict swelling and creep are periodically revised as more experimental data becomes available. The correlation used in this study for swelling is the Revision 5 equation of the Nuclear Systems Materials Handbook (NSMH). The correlation used for

FIGURE 5. EFFECT OF REACTOR OUTLET TEMPERATURE ON PRIMARY AND SECONDARY

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TABLE 2
CORE DESIGN CHARACTERISTICS* VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
Number of Fuel Assemblies	224	218	212
Number of Radial Blankets	198	198	198
Number of Control Assemblies	25	24	23
Lattice Pitch, in.	6.811	6.883	7.175
Interassembly Gap, in.	0.430	0.530	0.750
Duct ID Across Flats, in.	6.058	5.990	6.021
Duct Wall Thickness, in.	0.160	0.180	0.200
Total Assembly Length, in.	196.9	217.7	264.2
Number of Fuel Pins/Assembly	271	271	271
Fuel Pin OD, in.	0.302	0.302	0.302
Pin Pitch/Diameter	1.207	1.193	1.198
Clad Thickness, in.	0.017	0.020	0.023
Fuel Length, in.	46.3	46.3	46.3
Axial Blanket Length, in.	31.2	31.2	31.2
Upper	14.1	14.1	14.1
Lower	17.1	17.1	17.1
Gas Plenum Length, in.	30.4	51.2	97.7

* All dimensions are at operating temperatures.

irradiation creep is the Revision 3 equation of NSMH. Even though there is a good chance the correlations for swelling and creep will change, from past history, it is very improbable that the influence of temperature on these properties will drastically change. The required fuel assembly duct wall and interassembly gap thickness is shown in Figure 6. As outlet temperature is increased, the duct wall thickness must be increased because of the reduced strength of the material, and the interassembly gap must be increased because of the increased swelling and duct bulging due to irradiation creep. There is an economic and performance trade-off between duct wall and gap thickness because of the different effect of steel and sodium on the nuclear performance of the core. Steel has a stronger influence on breeding ratio and fissile mass requirements than sodium. The duct wall and interassembly gap trade-off also includes the small effect on reactor vessel diameter and building size (the change in gap has a larger effect on size than a given change in wall thickness). The constraints on gap and wall thickness are that there be no end-of-life (EOL) contact between adjacent assemblies and that the minimum gap will allow sufficient assembly load pad thickness for seismic considerations. The gaps and wall thicknesses shown in Figure 6 are those which yield the minimum power generation cost at the outlet temperatures considered.

The influence of reactor outlet temperature on fuel pin design is shown in Figure 7. The parameters shown are the cladding thickness, the fission gas plenum length, and the maximum cladding mid-wall temperature (including 2σ uncertainties). The cladding thickness and gas plenum length are set by the requirement that the cumulative damage fraction (CDF) in the worst pin be ≤ 0.5 at EOL. This is consistent with the guidelines given in Reference 3. In this instance, a trade-off exists between the amount of steel in the core (cladding thickness) and its effect on nuclear performance and the reactor vessel and building height (gas plenum length). The values shown represent the values which yield the minimum power generating cost at each outlet temperature studied. The clad thickness shown is the beginning-of-life (BOL) value. In the CDF calculation, the cladding thickness is reduced 0.002 in. for manufacturing defects

FIGURE 6. EFFECT OF REACTOR OUTLET TEMPERATURE ON FUEL ASSEMBLY DESIGN

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FIGURE 7. EFFECT OF REACTOR OUTLET TEMPERATURE ON FUEL PIN DESIGN

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and dimensional tolerance and the effects of sodium corrosion and fuel cladding chemical interaction on wall thickness are included. Figure 8 shows the amount the cladding thickness is reduced to account for corrosion and ID attack. The stress-rupture correlation used in the CDF calculations is the one developed by Brizes for unirradiated FFTF first core steel, Reference 4. This is consistent with the approach used in the Life III Fuel Rod Performance Code, Reference 5.

The main effects that the increased duct wall, cladding thickness, and interassembly gap of the higher temperature reactors have on the nuclear performance are a reduction in breeding ratio and an increase in compound system doubling time. These effects are illustrated in Figure 9. The reduced breeding ratio is reflected in an increase in fuel cycle costs which are discussed in Section 2.2. Doubling time itself was not used as a separate criteria in this study. Table 3 shows the main core performance characteristics at the three outlet temperatures studied.

A comparison of the main layout features of the primary systems are given in Table 4. Of particular importance are the dimensions of the reactor containment building which are illustrated in Figure 10. The main cause of the containment building height increase with increasing temperature is the increase in the fuel pin fission gas plenum length. The main cause of the containment diameter increase is the increase in the primary hot leg piping length required for thermal expansion effects. The length of the primary hot leg piping is shown in Figure 11 as a function of outlet temperature. Even though the piping diameter is decreasing with increasing temperature which would permit shorter piping, the length of the primary piping must be increased because of the rapidly decreasing allowable stresses, S_{mt} , in the 316 stainless steel pipe material. The strength of 316 stainless steel as a function of temperature is shown in Figure 12. The very pronounced effect of material strength on piping length can be better understood by referring to Figure 13 which shows the primary cold leg piping diameter and length versus reactor

FIGURE 8. EFFECT OF REACTOR OUTLET TEMPERATURE ON CHADDING WASTAGE

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FIGURE 9 . EFFECT OF REACTOR OUTLET TEMPERATURE ON BREEDING RATIO AND DOUBLING TIME

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TABLE 3
CORE PERFORMANCE CHARACTERISTICS* VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
Breeding Ratio	1.298	1.263	1.221
Doubling Time, yr	12.3	15.8	22.9
Discharge Burnup, MWd/kg			
Peak	115	115	115
Core Average	77.6	77.6	77.6
Fissile Inventory, kg	4400	4328	4451
Incore	3275	3182	3222
Excore	1125	1146	1229
Peak Linear Power, kW/ft	16.0	16.0	16.0
Clad 2 σ Hot Spot Temperature, °F	1127	1214	1297
Average Fuel Residence Time, yr	2.13	2.13	2.13

* At middle of equilibrium cycle.

TABLE 4
COMPARISON OF PRIMARY SYSTEMS DIMENSIONS VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
<u>RV Internals</u>			
ID of Innermost Thermal Shield, ft	14.1	14.3	14.7
Core Vessel ID, ft	15.9	16.1	16.5
UIS Large Diameter, ft	12.9	13.0	13.4
UIS Small Diameter, ft	9.2	9.3	9.6
UIS Height, ft	19.2	20.9	24.8
<u>Reactor Vessel</u>			
Lower RV ID, ft	23.0	23.1	23.6
Upper RV ID, ft	44.3	44.6	45.4
RV Height, ft	50.5	54.5	62.9
Small Rotating Plug Diameter, ft	10.8	10.9	11.2
Intermediate Rotating Plug Diameter, ft	22.3	22.5	23.1
Large Rotating Plug Diameter, ft	32.5	32.7	33.5
Top Plug Thickness, ft	8.0	8.5	9.1
TP Insulation Thickness, ft	3.0	3.5	4.1
<u>Primary Piping</u>			
Hot Leg ID, in.	34	32	30
Pump Suction Line Length, ft	115	130	176
Cross-Over Line Length, ft	70	79	107
Cold Leg ID, in.	27	25	23
Cold Leg Length, ft	186	158	146
<u>Containment</u>			
Diameter, ft	181	194	236
Height, ft	156	163	180

FIGURE 10. EFFECT OF REACTOR OUTLET TEMPERATURE ON CONTAINMENT BUILDING SIZE

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FIGURE 11. EFFECT OF REACTOR OUTLET TEMPERATURE ON PRIMARY HOT LEG PIPING DIMENSIONS

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STRENGTH (S_{MT}) vs TEMPERATURE FOR 316SS (FROM CODE CASE 1592-10)

FIGURE 12

FIGURE 13. EFFECT OF REACTOR OUTLET TEMPERATURE ON PRIMARY COLD LEG

outlet temperature. The cold leg temperatures are low and vary so little, 640-670°F, that the variation in allowable stress is insignificant, and the influence of piping diameter on length completely dominates. In addition to the piping effect on containment diameter, there is also a small increase with temperature caused by the larger core vessel diameter required to accommodate the increase in the fuel assembly lattice pitch from the thicker duct wall and larger interassembly gaps. This increase is partially offset by the decreasing number of fuel assemblies required at the higher temperatures.

The physical characteristics of the intermediate heat exchangers are compared vs outlet temperature in Table 5. Table 6 gives the characteristics of the hockey stick steam generator modules. The effects of the reactor outlet temperature on the required number of tubes in the IHX and SG units are shown in Figure 14. This illustrates the improvement in these components which results from the increased thermal drives available as outlet temperature is raised. As the number of tubes decrease, the number of welds required also decrease, and the potential for defective welds and tube leaks is reduced. This results in an improvement in the estimated plant availability because of the reduced downtime required to repair tube leaks. There is also the obvious benefits in fabrication and materials costs because of fewer tubes. However, in the SG modules, the advantage of the reduced number of tubes disappears at higher temperatures for 2-1/4 Cr-1 Mo alloy. With the IHX, the OD of the tubes and the tube wall thickness are essentially constant with temperature, and there is a subsequent reduction in heat transfer area as shown in Figure 15. With the SG module, such is not the case. In 2-1/4 Cr-1 Mo, there are several temperature dependent effects which require an increase in the tube wall thickness as the temperature is raised. One is the reduction in strength of the material with increasing temperature as shown in Figure 16. An additional reduction in strength results from decarburization of the material by the secondary sodium. The additional SG tube wall thickness required to compensate

TABLE 5
COMPARISON OF IHX PHYSICAL CHARACTERISTICS VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
Heat Transfer Area, 10 ³ ft ²	80.7	65.6	48.2
Number of Tubes	9695	7009	4686
Tube OD, in.	0.75	0.75	0.75
Tube Wall Thickness, in.	0.049	0.049	0.049
Total Tube Length, ft	46.8	51.7	56.1
Active Tube Length, ft	42.4	47.7	52.4
Tube Sheet Thickness, in.	16.4	14.2	11.9
Shroud OD, ft	11.4	9.7	7.9
Shell OD, ft	12.3	10.6	8.9
Primary ΔP	18.9	18.8	18.8
Secondary ΔP	16.3	18.2	25.4
Log Mean ΔT, °F	34.8	40.0	49.8

TABLE 6
COMPARISON OF STEAM GENERATOR PHYSICAL CHARACTERISTICS
VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
Heat Transfer Area/Module, 10 ³ ft ²	23.2	24.2	28.5
Number of Tubes/Module	2058	1761	1626
Tube OD, in.	9/16	11/16	7/8
Tube Wall Thickness, in.	0.081	0.129	0.232
Active Length, ft	76.5	76.2	76.4
Tube Sheet Thickness, in.	12.4	14.2	17.1
Shroud OD, in.	54.0	55.8	62.1
Shell OD, in.	62.8	63.3	68.7
Module Weight, tons	132	178	277
Steam Exit Velocity, fps	47.1	48.6	52.3
Steam Side Corrosion Allowance, in.	0.020	0.032	0.053

FIGURE 14 .EFFECT OF REACTOR OUTLET TEMPERATURE ON NUMBER OF TUBES IN IHX AND SG MODULE

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FIGURE 15 . EFFECT OF REACTOR OUTLET TEMPERATURE ON HEAT TRANSFER AREAS

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STRENGTH (S_{MT}) vs TEMPERATURE FOR 2¼ Cr-1 Mo (FROM CODE CASE 1592-10)

FIGURE 16

for decarburization is shown in Figure 17. The final effect is the tube wall thickness allowance required for steam-side corrosion and periodic cleanings of the steam-side tube surfaces. The corrosion rate predicted for 2-1/4 Cr-1 Mo SG tubes is strongly temperature dependent. The temperature dependence which was used was derived from the information in Reference 6 normalized to produce the 24-mil corrosion and cleaning allowance specified for CRBRP steam generators at the CRBRP steam temperature of 905⁰F and module life of 30 years. The resulting cleaning and corrosion allowance which is predicted is shown in Figure 18. The allowance for routine cleanings over the 40-year module is independent of temperature and was estimated as 4 mils. The resulting SG tube wall thickness as a function of reactor temperature for the three steam temperatures considered is shown in Figure 19. Also shown is the predicted tube OD. To obtain the tube OD, a minimum ID of 0.4 in. was specified. The tube ID for CRBRP SG's is 0.407 in. (0.625 OD x 0.109 w) and welding techniques have been developed for autogenous butt welds for this dimension. In the opinion of ESG weld engineers, specifying a tube ID much smaller than this would require considerable weld development and would be very risky. It is also their opinion that a wall thickness of 0.150 in. is about the limiting size using current welding methods and may well be a limit for any known method applicable to SG tube-to-tubesheet full penetration fusion welds. Based on these assumptions, a reactor outlet temperature limit exists at about 1000⁰F for systems using fusion-welded 2-1/4 Cr-1 Mo tubes and tubesheets.

FIGURE 17 . STEAM GENERATOR TUBE THICKNESS
ALLOWANCE FOR DECARBURIZATION BY
SECONDARY SODIUM

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FIGURE 18. STEAM GENERATOR TUBE STEAM-SIDE CORROSION ALLOWANCE VS. STEAM TEMPERATURE

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FIGURE 19. EFFECT OF REACTOR OUTLET TEMPERATURE ON STEAM GENERATOR TUBE DIMENSIONS

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2.2 ECONOMIC EVALUATION

The differential capital, fuel cycle, and total power generating costs obtained from the SOC-II results are shown in Figure 20 as a function of reactor outlet temperature. The costs are relative to those for a 930°F outlet temperature plant using the reference set of plant materials and are given in mills/kWh in 1978 dollars.

These results indicate a minimum in the total power costs at a reactor outlet temperature of about 950°F. The minimum in capital costs occurs at about 965°F. There is no minimum indicated in fuel cycle costs over the range of 930-1050°F.

A summary of the system differential capital costs is given in Table 7 relative to the 930°F system. These data are shown graphically in Figure 21. The capital costs were developed in 1978 dollars and are based on a fifth-of-a-kind plant in each case. The unit costs for the reactor vessel, internals, and sodium piping were based on CRBR costs modified to a fifth-of-a-kind plant. Costs for the pumps and steam generator modules were generated by ESG using the pricing methods employed in our ongoing CRBR and base program efforts in these areas. The IHX costs were based on ESG heat exchanger cost estimating methods modified and normalized to the large Foster Wheeler IHX cost estimate for a fifth-of-a-kind plant. The steam side piping unit costs, building costs, and turbine island costs were obtained from AE estimates. It is readily seen from this figure, which capital cost factors cause the power costs to turn upward above about 950°F. The cost of the reactor containment building is the main factor, with the reactor vessels and internals next in importance, followed by the steam generator modules.

The containment building cost shows an increase at all temperatures above the base because the required building size always increases with temperature. The diameter and height of the containment building as a

FIGURE 20. EFFECT OF REACTOR OUTLET TEMPERATURE ON POWER COSTS

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TABLE 7
CAPITAL COST SUMMARY (DIRECT AND INDIRECT)
(Millions \$ 1978)

Reactor Outlet Temperature, °F	930	990	1050
Reactor			
Core Vessel	Base	0.4	1.3
Reactor Vessel	Base	2.9	10.4
Guard Vessel	Base	0.6	2.9
Internals	Base	2.7	9.4
Control Rods and Drives	Base	-0.3	-1.7
Top Plugs and Deck	Base	1.0	3.7
Fuel Handling Equipment	Base	0.7	2.2
Primary and Secondary Systems			
IHX's	Base	-7.7	-15.7
Piping			
Primary	Base	-1.2	-0.6
Secondary	Base	-2.8	-2.9
Pumps	Base	-11.0	-16.7
Containment Building	Base	12.7	56.8
Steam Generator System (Modules and Buildings)	Base	1.2	16.9
Turbine Island	Base	-1.7	-1.9
Total	Base	-2.6	64.3

FIGURE 21. DIFFERENTIAL CAPITAL COSTS VS.
REACTOR OUTLET TEMPERATURE

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function of outlet temperature was shown earlier in Figure 10. In going from a 930 to a 1050⁰F outlet temperature, there is a 55-ft increase in containment diameter which would translate into a 70% increase in containment volume if the height were constant. However, there is also a 24-ft height increase which adds another 15% increase to the building volume for a total increase of about 96% over the temperature range.

The increase in the cost of the reactor system components with outlet temperature is mainly attributable to the properties of 20% CW 316 SS and their behavior with temperature. These properties and their effects on fuel assembly length and core diameter were discussed in Section 2.1. All of these effects lead to increases in core vessel, reactor vessel, and guard vessel size, and these are significant cost items. As the size of the fuel assembly and core increases, so does the size of the instrument tree, the fuel handling system, the lower core support structure, the thermal shields, and the core restraint mechanism. The sizes of the small, intermediate and large rotating plugs, and the top deck increase as the size of the reactor vessel increases. One effect which is solely a function of outlet temperature and is independent of the changes occurring in the core dimensions is the required thickness of thermal insulation required below the top deck. This is not a major cost item, but is included in the top deck cost.

The cost increase with temperature of the steam generator modules is due to the increase in tube OD and wall thickness discussed in Section 2.1. Even though the number of tubes per module is decreasing and the associated labor costs are decreasing along with them, the overall size of the module is increasing, and the labor costs associated with the shell, headers, etc., are increasing. At the 990⁰F outlet temperature conditions, there is a decrease in module labor costs because the tubing costs more than offset the rest of the module labor costs. At 1050⁰F, the nontubing costs dominate. The cost for materials increases at both the 990 and 1050⁰F outlet temperature conditions because the amount of material required for the modules increases. The overall effect is that there is an initial decrease in SG module costs and then it turns significantly upward.

The capital cost factors which are improving with outlet temperature are the costs of the IHX units, the pumps, the piping, and the turbine island.

The IHX costs are decreasing because the number of tubes and the size of the units decrease significantly because of the improvement in system efficiency and thermal driving force obtained at the higher temperatures. The costs of the primary and secondary pumps decrease because the sodium flow rates in the primary and intermediate loops decrease as the ΔT 's are increased. The piping costs decrease because the pipe diameter effect on costs is stronger than the length effect but, as may be seen from Table 7, the piping length effect has started to turn the primary piping costs upward above 990°F even though the overall differential piping cost relative to 930°F is still negative. The differential turbine island costs are seen to be small.

The components of the fuel cycle costs are given in Table 8. Here it can be seen that the fabrication costs increase as the reactor outlet temperature is increased. This is primarily caused by the increase in fuel pin fission gas plenum length discussed in Section 2.1 and reflects the additional cost of the increased length of fuel pin cladding required. The other components of the fuel cycle costs--reprocessing, Pu credit, Pu inventory, and Pu gain--show a slight decrease at the 990°F outlet temperature. This decrease in these fuel cycle cost components occurs because they are a function of the fissile mass in the core and the 990°F core has a smaller incore fissile inventory than the other two (see Table 3). This occurs because the fissile mass requirement of the core is, among other things, a function of the core size. The core height was held constant in this study, but the diameter varies because the number of fuel assemblies was varied according to the thermal power requirements and the assembly lattice pitch was varied according to the duct wall and inter-assembly gap requirements. The number of assemblies goes down with increasing temperature and the assembly lattice pitch goes up. Since

TABLE 8
FUEL CYCLE COSTS
(Mills/kWh \$ 1978)

Reactor Outlet Temperature, °F	930	990	1050
Fabrication	3.78	3.89	4.47
Reprocessing	0.80	0.78	0.89
Pu Credit	-1.20	-0.95	-0.97
Pu Inventory	2.64	2.61	2.70
Pu Gain	-0.98	-0.97	-1.01
Total	5.04	5.36	6.08

the core diameter is a function of the product of the number of assemblies and the lattice area, a minimum occurs because the two are going in opposite directions. The total fuel cycle costs are a sum of positive and negative components, and the slight decrease at 990⁰F disappears in the summation process.

The fuel cycle costs are computed in the SOC-II code using the method of Stauffer, et al., Reference 7; the economic guidelines of EPRI, Reference 8; and the fabrication cost method of HEDL, Reference 9.

2.3 OTHER TEMPERATURE DEPENDENT EFFECTS

2.3.1 Environmental Effects

The use of higher reactor outlet temperatures permits the use of more efficient steam cycle conditions so that for systems producing the same net electrical power output, there is a reduction in the amount of waste heat rejected to the environment (see Figure 22). In this study, it was assumed that the plants employ an evaporative cooling tower rather than circulating water heat rejection, and the economic effect of the differences in the amount of makeup water is not expected to be significant. There will be a difference in the tower plume and drift effects which may require additional effort and expense in developing the Environmental Report for the lower efficiency plant.

Another difference between high- and low-temperature plants which will impact the environment is the quantity of fission product wastes produced. With the higher efficiency systems, there is a reduction in the quantity of fissionable material consumed to produce the same net electrical power. As a consequence, the number of fission products produced is less (see Figure 22). The economic and environmental effects of the ultimate disposal of fission product wastes is one of the major problems facing the nuclear industry today. All attempts to reduce the quantity of fission products are surely to be beneficial to the solutions to the disposal problems.

2.3.2 Mass Transfer Effects

Radioactive corrosion products from fuel cladding are transported from the core and deposited throughout the primary loop, particularly in the IHX, but also in the hot leg pump. The amount of corrosion is very temperature-dependent. Fission products from failed fuel elements also go into solution with the sodium and deposit throughout the primary loop. Assuming one can design fuel assemblies to the same reliability as a function of temperature, this effect is not temperature dependent, but depends primarily on how many failed elements are allowed during operation.

FIGURE 22. EFFECT OF REACTOR OUTLET TEMPERATURE ON THERMAL POLLUTION AND FISSION PRODUCT WASTES

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This mass transfer effect presents two potential problems. One is a radiation source term in the primary vaults which could limit access for maintenance, and the other is contamination of components which then require decontamination prior to repair. This later effect is temperature-dependent because the radioactive deposits diffuse into the metal of the components at the higher temperatures requiring removal of metal for decontamination rather than just surface deposit removal.

2.3.2.1 Corrosion and Fission Product Transport

From a radiological standpoint, the principal isotopes released by 316 SS cladding to the sodium in the corrosion process are Co^{60} , Co^{58} , Mn^{54} and Fe^{59} . They are created by the following reactions:

The corrosion rate for this evaluation is based on an oxygen concentration of 0.7 ppm. The corrosion rate of the trace elements differ from the base corrosion rate of the alloy as some elements are preferentially leached or retained. Numerous on-going studies are attempting to better define the elemental corrosion rates which are generally typified by the relation:

$$\overline{\text{CR}}_i = \eta_i \times \text{CR}$$

At the above oxygen concentration, η_i is as follows for the major isotopes:

<u>Isotope</u>		<u>η_i</u>
Co^{60}	=	0.12
Co^{58}	=	0.12
Mn^{54}	=	2.19
Fe^{59}	=	0.28

Nonsoluble gaseous fission products such as Kr, Xe, Ar, Ne, and tritium contribute only to the cover gas activity and are not considered here. Only fission products which contribute to the primary heat transfer system activity are considered. This includes all nongaseous fission products.

The total inventory released from any fission product isotope is computed based on the escape fractions of the major isotopes of concern as follows:

<u>Element</u>	<u>Escape Fraction</u>
Cs ¹³⁶ , Cs ¹³⁷	$EF = \frac{(1 - e^{-x})}{x}$
Cs ¹³⁴	EF = 1
Sb, Te, I	$EF = \frac{0.2 (1 - e^{-10x})}{10x}$
Sr	EF = 0.005
Cs ¹³⁷ , Ba, La, Zr, Nb	EF = 0.003
All other fission products	EF = 0.001

* $x = 1.5 \times 10^5 \lambda$ where λ = decay constant of ith isotope in seconds.

These were obtained from the CRBR PSAR except for those of Cs¹³⁷, Ba, La, Zr, Nb, which were obtained from the recent US/USSR Seminar on Reactor Safety.

Based on the above, the dose rate from an IHX associated with corrosion product and fission product deposition is given in Figure 23. It was assumed that 100% of the corrosion products and nonsoluble non-gaseous fission products were deposited on the wetted surfaces of the IHX's. The dose rate was calculated at 10 days following reactor shutdown with the loop drained. The effect of any radioactive sodium film remaining after loop draining was calculated to be a negligible contributor to the cell dose rates.

FIGURE 23. IHX GUARD VESSEL SURFACE
RADIATION LEVEL (10d AFTER
REACTOR SHUTDOWN)

Included in Figure 23, are the CRBRP radiation zone classes for personnel access. The criteria defining the zone classes are given in Table 9. It may be seen from Figure 23 that the IHX area changes from a Class III to a Class IV zone at a reactor outlet temperature of 980°F. The significance of this zone class change on maintenance operations is not known at this time, but any increase in equipment radiation levels will increase the cost of maintenance operations.

2.3.2.2 Decontamination of LMFBF Components

Effective decontamination methods are available for components used in water-cooled nuclear reactors, but they are ineffective when used on LMFBF components. The important difference in the behavior of these deposits is that radioactive isotopes are deposited in elemental form from sodium and at hot leg temperatures they diffuse into the substrate metal. In consequence, some metal must be removed to achieve significant decontamination of these components. Since the diffusion behavior of these isotopes is not yet fully understood, a discussion of the known diffusional behavior of LMFBF deposits is helpful in understanding decontamination.

2.3.2.2.1 Diffusion of Radioactive Isotopes into Austenitic Steel from Sodium

Transgranular diffusion of metals can be described by the general equation,

$$D = D_0 \exp - \frac{Q}{RT} \quad \text{where,}$$

D is the diffusion coefficient, $\text{cm}^2\text{-sec}^{-1}$

D_0 is a constant

Q is the activation energy of diffusion cal mole^{-1}

R is the gas constant $\text{cal mole}^{-1} \text{K}^{-1}$

T is the temperature of diffusion, K deg.

TABLE 9
CRBRP RADIATION ZONING CRITERIA

Zone Class	Design Dose Rate Range (mrem/hr)	Zone Description	Design Occupancy Criteria
I	≤ 0.2	Restricted area, routinely occupied	Full Time
II	> 0.2 to ≤ 5	Restricted area, non-routinely occupied	10CFR20 and OPDD-10
III	> 5 to ≤ 100	Radiation area, limited access for routine task	10CFR20 and OPDD-10
IV	> 100 to ≤ 5000	High radiation area, access to perform specific nonroutine task	10CFR20 and OPDD-10
V	> 5000	Inaccessible	10CFR20

From the above equation, the depth of transgranular diffusion can be predicted if D_0 , the activation energy, and temperature of diffusion are known for an element. The elements of primary interest in decontamination are Mn^{54} and Co^{60} . Although other isotopes deposit, their behavior can generally be neglected based on their relative abundance or half-life. The numerical values needed to calculate diffusional penetration by a transgranular mechanism are available for both Mn^{54} and Co^{60} and early work on decontamination used these values to predict the amount of metal removal required for decontamination (Reference 10). For achieving a decontamination factor (D.F.) of 10 (90% isotope removal) for Mn^{54} , the removal of 0.045 mil (1.1×10^{-3} mm) of base metal was predicted for a component which had operated at 1050°F (566°C) for a substantial period of time. Since Mn^{54} is the isotope of interest having the highest diffusion rate in austenitic steel, this should be the maximum amount of metal removal required. Actual decontamination tests reported in Reference 10 and elsewhere have established the fact that Mn^{54} diffuses into 304 SS LMFBR components at an even higher rate than predicted by the transgranular diffusion equation. For example, the depth of Mn^{54} diffusion was ten times that calculated, based on the depth of metal removal required to give a D.F. of 10 in the above example. Considerable uncertainty still exists regarding the mechanism of radioactive isotope diffusion into LMFBR components and the behavior of different component alloys. Service time and operating temperature are also important variables. Using the results of incremental analysis from a test loop (Reference 10), the relative effect of operating temperature on the amount of metal removal required to achieve a D.F. of 10 on 304 SS hot leg components is shown in Table 10.

TABLE 10
METAL REMOVAL REQUIRED TO PRODUCE >10 D.F.
ON HOT LEG COMPONENTS

Temperature, °F (°C)	Depth, μm
875 (468)	4
930 (499)	5
990 (532)	7
1050 (566)	8.5

2.3.2.2.2 Decontamination Methods

As a result of an extensive screening program of potential decontaminating agents (Reference 11) and additional process studies carried out in GFY 1977, a decontamination process has been developed for hot leg LMFBF components made from 304 SS. The selected reagent is 2.5% glycolic (hydroxyacetic) acid - 2.5% citric acid at a temperature of 70°C. The process time required depends on the component service time in the reactor and the metallurgical characteristics of the various heats used to fabricate the component. A preliminary process specification describing the details of the process has been issued (Reference 12). The process time is determined by a preliminary laboratory test on "travelers," specimens which have been exposed to the same time-temperature conditions as the component in reactor sodium. A process time of two-three weeks for typical hot leg components made from 304 SS is expected. Decontamination of components containing 316 SS has not yet been evaluated.

The most critical process parameter to control is the oxygen content of the acid solution. An oxygen concentration of less than ten parts per billion (ppb) must be maintained at all times in the acid solution. This is done by sparging it with a suitable grade of inert gas. The process is terminated by sparging the acid solution with air which passivates the component and prevents post-decontamination corrosion. The effect of reactor operating temperature on the decontamination process required is related only to the time required to decontaminate the component to a given D.F. value. Since the rate of acid attack is constant, the relative time required to achieve an equivalent D.F. ratio is directly proportional to the depth of Mn⁵⁴ diffusion. The metal removal depths given in Table 10 indicate that the decontamination time required for a component operated at 1050°F (566°C) will be slightly over twice that required for an equivalent component which operated at 875°F (468°C). However, the metal damage resulting from the acid process also increases with the time of treatment. So far, requalification criteria for components to be returned to use in a reactor have not been established. In the event these requirements involve metal pitting or intergranular penetration values which have to be exceeded to suitably decontaminate components which have operated above a given temperature, this will be an additional incentive to prefer operation at temperatures below this value.

It should be remembered that diffusion and decontamination studies in the U.S. are based on 304 SS. The use of other alloys may significantly influence both the diffusion of isotopes and decontamination rates. Further, experimental work is underway to trap radionuclides before they deposit which would also change the decontamination requirements, if successful.

3.0 ASSESSMENT OF COMPONENT CRITICAL AREAS

3.1 SELECTION OF COMPONENT CRITICAL AREAS

In order to select component critical areas for detailed analysis which it was felt would be most sensitive to increases in the reactor outlet temperature, a comprehensive review of the entire system was made. From this review, a list of all areas which might be adversely affected by thermal transients and higher temperatures was compiled. This list was then reviewed by ESG personnel familiar with the design and analysis of the various components on the list. These personnel were requested to provide comments on the temperature sensitivity of the components in their area of expertise. The complete list along with abbreviated comments obtained are given in Table 11.

From reviews and discussions of the items in Table 11, a final selection of five component critical areas was made. Assistance was provided, during the final selection, by the results obtained from the steam cycle comparison study (Reference 13). In this study, five areas were subjected to a similar type analysis but with a less comprehensive set of thermal transients. The areas investigated were: (1) the primary IHX inlet nozzle, (2) the IHX upper tubesheet, (3) the steam generator upper tubesheet, (4) the primary pump bearing support, and (5) the primary piping integral pipe support.

With the guidance of the Reference 13 results and the expert comments from the system review, the five areas given in Table 12 were selected for detailed analysis. Three of the areas selected-reactor vessel outlet nozzle, primary pump bearing support, IHX upper tubesheet - thoroughly cover the primary system hot leg where temperatures are being increased the most. The most critical cold leg component was felt to be the secondary pump bearing support which is in the secondary cold leg. The secondary cold leg sees thermal up-shocks following reactor scram. The final area selected was the steam generator upper tubesheet which sees both secondary hot leg sodium transients and outlet steam temperature transients.

TABLE 11
POTENTIAL TEMPERATURE SENSITIVE CRITICAL AREAS

Critical Component	Limiting Criteria	Potential Relief	Considerations
Reactor Core and Internals			
Fuel Pin	Loss of ductility or life fraction	Change materials, clad thickness, gas plenum length	Maintain cladding integrity for retaining fission product gasses. Prevent brittle fractures. Limit thermal creep strain.
Fuel Assembly Duct	Refueling and core restraint	Change materials, increase gap	Distortion from irradiation induced swelling and creep. Difficulty in fuel assembly removal. Alignment of control rods. Duct distortion makes design of the core restraint mechanism more difficult.
Upper Internals	Thermal fatigue	Change materials	Surface cracking of thermal liner caused by temperature fluctuations and oscillating thermal stresses. Fluctuations result from variations in sodium outlet temperature from different fuel, control, and blanket assemblies.

TABLE 11 (continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Lower Core Support Structure	Creep fatigue	Add thermal liners	Massive structure. Sodium trapped within structural boundary. Startup and shutdown ramps, other transients.
Core Restraint	Creep fatigue	Change materials, design configuration	Maintain structural integrity to resist outward "flowering" of driver blanket, control, and reflector subassemblies.
Reactor Vessel			
Upper Wall	Creep fatigue	Add thermal shielding	Fluctuating sodium levels and temperatures. Startup and shutdown ramps, other transients.
Inlet Nozzle	Stress level	Add thermal shielding	Nozzle is thermally restrained by the vessel wall. Sees all return transients.
Outlet Nozzle	Creep fatigue	Add thermal shielding, change design configuration	Nozzle is surrounded by cold pool of sodium. Complex design for potential creep fatigue relief.

TABLE 11 (continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Intermediate Heat Exchanger			
Inlet Nozzle	Creep fatigue	Mitigate transients, add thermal shielding	Varying wall thicknesses cause temperature gradients through thickness.
Upper Tubesheet	Creep fatigue	Change design configuration, mitigate transients	Transients cause through thickness gradients.
Tubes	Buckling	Change design configuration (increase number of tube spacers)	Plugged tubes which run hotter would create the potential for interacting with active tubes.
Support Structure Ring	Seismic and temperature	Design configuration change	Not thermally sensitive due to relative isolation from transients. Primary stress limited. Design changes would not significantly increase thermal stresses.

TABLE 11 (continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Bellows	Creep fatigue	Change materials	304 SS significantly temperature sensitive. Governed by convolution stress and strain limits. Alloy 800 better at elevated temperatures.
Lower Tubesheet/Head	Interference	Design change, mitigate transients	Transients could cause tubesheet to expand radially more than the shell.
Sodium Pump (Primary & Secondary)			
Bearing Sleeve/Journal/Bearing Support	Binding/misalignment. Creep fatigue.	Mitigate transients	Differential heating of sleeve. Increased temperature/gradients may cause distortions in the support.
Suction Nozzle/Bowl Joint	Creep fatigue	Change operating procedures, add thermal shielding	Varying wall thickness causes temperature gradients through thickness. Lower allowable stresses and creep fatigue at higher temperatures.

TABLE 11 (continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Bearing Thermal Shield & Diffuser Support	Creep fatigue	Mitigate transients	Gap between the impeller and support must be maintained. Maintain a reasonable sealing by the rear labyrinth seal.
Piping			
Local Stresses at Clamps	Stress limits	Change design configuration	Clamp size increases with larger reactions. Thinner pipe wall heats up faster than clamp thus causing contact pressure.
Piping	Creep fatigue	Change design configuration	Stresses due to constraint of thermal expansion. Consider space restrictions and economy. Primary concern at nozzles and bends. Substantial thermal expansion loads to limit loop size.

TABLE 11 (Continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Steam Generator			
Tubes	Decarburization and loss of strength	Change materials	<p>Departure from nucleate boiling causes thermal cycling.</p> <p>Decarburization causes strength degradation.</p> <p>Higher temperature reduces material strength.</p>
Tubesheet	Creep fatigue	Change material, design configuration, mitigate transients	<p>Large thermal gradients between the rim and outer periphery holes induces significant residual stresses.</p> <p>Creep damage increases significantly at higher temperatures with 2-1/4 Cr - 1 Mo.</p>
Inlet Nozzle	Creep fatigue	Mitigate transients and thermal shielding	Varying wall thickness causes temperature gradients through thickness.
Dissimilar Materials at Inlet Nozzle	Differential strain	Eliminate dissimilar materials, intermediate transition material	Stainless steel to alloy steel.

TABLE 11 (continued)

Critical Component	Limiting Criteria	Potential Relief	Considerations
Support Structure Ring	Seismic and temperature	Design configuration change	<p>Not thermally sensitive due to relative isolation from transients.</p> <p>Primary stress limited.</p> <p>Design changes would not significantly increase thermal stresses.</p>

TABLE 12
SELECTED STRUCTURAL ANALYSIS AREAS

Critical Area	Considerations
Steam Generator Upper Tubesheet	Large thermal gradients between the rim and outer periphery holes induces significant residual stress.
Primary Sodium Pump Bearing Support	Increased temperature gradients could lead to creep-fatigue and distortions in the bearing support thus causing misalignment and binding.
Secondary Sodium Pump Bearing Support	Cold leg transients could cause distortion and binding.
Reactor Vessel Outlet Nozzle	Complex design at the reactor vessel wall for potential creep fatigue relief. Nozzle is surrounded by cold pool of sodium while being subjected to internal hot transients.
Intermediate Heat Exchanger Upper Tubesheet	Perforated area tracks outlet temperature while shell and junction lag.

3.2 SELECTION OF THERMAL TRANSIENTS

Reference 14 presents the results of an effort devoted to identifying all the system transients and their frequency for the reference loop design. These transients are based on a review of those developed for the CRBRP Program coupled with a fault tree analysis of the present plant design described in Reference 1.

The number of transients which were identified is very large. In order to reduce the number to a manageable size, a study was made to identify the design shaping umbrella transients for the component critical areas selected above. An umbrella transient is defined as a transient with a characteristic shape and magnitude such that other transients can be identified which would fall within the overall envelope of the umbrella transient. A total of 19 umbrella transients were identified and these are given in Table 13. The maximum number of umbrella transients which applies to any particular component is 11 for the steam generator. Table 13 gives the identification of all the transients included under each umbrella transient and the number of occurrences for each. It also shows which critical component area of the five selected is affected by each transient.

The time behavior of the umbrella transients were in most instances evaluated using the computerized system simulation model described in Reference 15. The dynamic model which was developed for a 930°F reactor outlet temperature was updated to the conditions of the 1050°F outlet temperature reactor. For a few of the transients, U-23, U-34, E-05, E-11, and E-16, the model lacked the capability to perform the analysis. These transients were estimated using an evaluation of the results of previous similar studies and engineering experience and judgement. In all cases the transients are based on the transient mitigation scheme presented in Section 4.3.1.1.

3.3 STRUCTURAL EVALUATION

Structural analyses of the five component critical areas listed in Table 12 were performed to evaluate the effects of the umbrella transients

TABLE 13
UMBRELLA TRANSIENTS

Umbrella Transient Ident.	Description	Total No. of Events	Effected Component									
			Reactor Vessel Outlet Nozzle		Primary Pump		HX		Secondary Pump		Steam Generator	
			Tran. 1	No. 2	Tran.	No.	Tran.	No.	Tran.	No.	Tran.	No.
N-06	Loading and unloading between 40 and 100% power	23,210	N-06 N-07 N-08	1,770 20,000 1,440	N-06 N-07 N-08	1,770 20,000 1,440	N-06 N-07 N-08	1,770 20,000 1,440	N-06 N-07 N-08	1,770 20,000 1,440	N-06 N-07 N-08	1,770 20,000 1,440
U-01	Reactor trip from full power with normal decay heat	446	U-01 U-03	407 20	U-01 U-03	365 20	U-01 U-03	385 20	U-01 U-03	363 20	U-01 U-03	381 20
U-02	Reactor trip from full power with minimum decay heat	20	U-02	20	U-02	20	U-02	20	U-02	20	U-02	20
U-05	Uncontrolled rod withdrawal from 100% power	8	U-05	8	U-05	8						
U-07	Uncontrolled rod withdrawal from startup with manual trip	11	U-07 U-06	1 10	U-07 U-06	1 10	U-07 U-06	1 10				
U-12	Partial loss of intermediate sodium pump	10									U-12 U-13	3 7
U-17	Primary pump speed increase	42	U-17 U-18	21 21	U-17 U-18	21 21						
U-23	Inadvertent water side dump of one steam generator loop	24					U-23 U-21 U-22 U-24 U-35 E-12 E-13	2 1 3 2 4 6 6	U-23 U-21 U-22 U-24 U-35 E-12 E-13	2 1 3 2 4 6 6	U-23 U-24	2 2

TABLE 13
UMBRELLA TRANSIENTS
(Continued)

Umbrella Transient Ident.	Description	Total No. of Events	Effected Component									
			Reactor Vessel Outlet Nozzle		Primary Pump		IHX		Secondary Pump		Steam Generator	
			Tran.1	No.2	Tran.	No.	Tran.	No.	Tran.	No.	Tran.	No.
U-32	Inadvertent opening of one turbine bypass valve	14					U-32	7	U-32	7		
							U-34	7	U-34	7		
U-33	One turbine bypass valve fails open following reactor trip	7					U-33	7	U-33	7	U-33	
U-34	Inadvertent opening of steam generator power relief valves	7									U-34	
U-37	Main feedwater control and isolation valves fail to close on reactor trip	1									U-37	
E-01	Primary pump mechanical failure	6					E-01	6				
E-02	Intermediate pump mechanical failure	6										
E-05	Main steam line rupture	1					E-05	1	E-05	1	E-05	
E-06	Loss of primary pump pony motor with check valve failure to close	1					E-06	1			E-06	
E-08	Design basis steam generator sodium/water reaction	36	E-08	6	E-08	6			E-08	6		
			E-11	6	E-11	6						
			E-12	6	E-12	6						
			E-13	6	E-13	6						
			E-14	6	E-14	6						
			E-16	6	E-16	6						

TABLE 13
UMBRELLA TRANSIENTS
(Continued)

Umbrella Transient Ident.	Description	Total No. of Events	Effected Component					
			Reactor Vessel Outlet Nozzle		Primary Pump		IHX	
			Tran. ¹	No. ²	Tran.	No.	Tran.	No.
E-11	Rupture disk failure in steam generator loop	12					E-11 E-16	6 6
E-16	Inadvertent dump of intermediate heat transfer loop sodium	6						E-16 6

1 Identification of transients under umbrella.

2 Number of occurrences

listed in Table 13 on the components' structural integrity. Of particular interest is the creep-fatigue damage fraction calculated in accordance with ASME Code Case 1592-10. Some of these components were earlier found in Reference 13 to have adequate structural design margins for the reference 930°F reactor outlet design. Because of the limited scope of this study, the analysis could only be performed for one additional temperature condition. The design having a reactor outlet temperature of 1050°F was selected to provide an upper bound to the range of interest. Only the reference set of materials were investigated.

The stress analysis was performed using the APSA finite element computer code (Reference 16). Finite element models were developed to represent the area of concern. In conjunction with this, thermal analysis models were developed whose boundary coordinates were the same as those of the stress models. The thermal models were used with the TAP Code (Reference 17) to provide time histories of the temperature fields for the thermal transients considered. From the time histories provided, the most critical temperature fields were determined. These were then used in the stress model to calculate stresses and strains. From this information, the maximum stress intensities, creep-ratchetting strain, fatigue damage fractions, and creep-fatigue damage fractions were determined.

A summary of the results of the structural analysis of the component critical areas is presented in Table 14. It can be seen that for the 1050°F reactor outlet temperature, with its higher ΔT 's, none of the components exposed to the hot leg temperature transients were found to have acceptable structural damage fraction. It should be noted that these analyses were only performed for the same materials of construction as used in the 930°F reactor outlet reference design. It should be further noted that although some of the damage fractions are much greater than 1, in most cases rather modest reductions in stress intensities would provide adequate design margins. It is felt that this could be accomplished, quite readily, by slight changes in the design configuration (in the case of the tubesheets and nozzles, a change to higher strength materials) or further mitigation of the system transients. A consideration of such changes is presented in Section 4.0.

TABLE 14

SUMMARY OF STRUCTURAL EVALUATION OF COMPONENT CRITICAL AREAS FOR 1050°F REACTOR OUTLET TEMPERATURE

	IHX Tubesheet	Reactor Vessel Outlet Nozzle	Primary Pump Brg Support	Intermediate Pump Brg Support	Steam Generator Tubesheet
Critical Transient	N-06	N-06	U-01/U-17	U-23	U-01
Number Cycles	23210	23210	38.5/42	24	427
Maximum Stress (ksi)	14.9	39.2	30.8/54.7	52.1	44.4
Accumulated Strain	21.58 (U-01)	.16	---	---	---
Creep Damage	2.24	1.28	.29	.01	4.21
Fatigue Damage	.33	1.17	1.0	.09	---
Creep-Fatigue Damage	2.57	2.45	1.29	.10	4.21

3.3.1 Steam Generator Upper Tubesheet

The type of steam generator which was used in this study is the ESG hockey-stick design illustrated in Figure 24. The area of the steam generator which has proven to be most sensitive to thermal transients is the upper tubesheet as indicated on Figure 24. The finite element model which was used to evaluate the thermal stresses is shown in Figure 25. The critical section of the tubesheet which is indicated on Figure 25 is between the outer row of holes and the solid rim.

Eleven of the 19 umbrella transients listed in Table 13 are applicable to the SG upper tubesheet. For each transient, the TAP code output of temperature vs time was used to determine the exact time when the maximum temperature differential (ΔT) occurs. Using the maximum ΔT 's APSA runs were made to obtain the thermal stresses in the tubesheet. Typical CRT plots of the APSA output are presented in Figures 26 and 27. Figure 26 shows a stress intensity plot. Figure 27 illustrates the structural deformation of the tubesheet as a result of the temperatures and stresses imposed.

A post-processing run for each transient found equivalent plate stresses and stress intensities for the tubesheet. These stress results were linearized to determine Q (maximum secondary) stresses in the tubesheet. The steam generator upper tubesheet was evaluated using a simplified inelastic analysis method which is described in Reference 19. Currently, Appendix T to Code Case 1592 recognizes two methods for providing assurance against failure. The first method is based on elastic analysis but is considered to be very conservative and restrictive. When the Code limits cannot be satisfied by elastic analysis, the adequacy of the design can be verified by inelastic analysis. This second approach may be generally characterized as being less conservative (but more realistic) than the elastic analysis but very time consuming and expensive. Therefore, an alternative method of design assessment of the tubesheet was employed which is less conservative than the elastic Code limits, and at the same time quicker, more economical, and more conservative than a detailed inelastic analysis.

STEAM GENERATOR UPPER TUBESHEET

Figure 24

STEAM GENERATOR UPPER TUBESHEET
STRESS MODEL

Figure 25

STEAM GENERATOR UPPER TUBESHEET
TYPICAL STRESS INTENSITY PLOT

Figure 26

STEAM GENERATOR UPPER TUBESHEET
TYPICAL UNDEFORMED/DEFORMED STRUCTURE PLOT

Figure 27

The results of the structural evaluation of the steam generator upper tubesheet are presented in the form of histograms in Figures 28 and 29. The peak stress intensity which is developed in the tubesheet for each thermal transient is shown in Figure 28. The total creep damage fraction of 4.21 which was calculated is shown in Figure 29. Assuming no design margin, a damage fraction of one is allowed. It can be seen that the most damage occurs from reactor trip transients U-01 and U-02. This is primarily due to the large number of reactor trips which are predicted. Since the damage fraction is greater than one for the 1050°F reactor outlet temperature conditions, some design or operating changes are required. This could be accomplished by a reduction in the number of reactor trips, further mitigation of the steam and secondary sodium temperature transients, and/or the use of alternate steam generator materials such as 9 Cr - 1 Mo or I800H which are more resistant to creep damage. The fatigue damage fraction was found to be negligible since 2-1/4 Cr - 1 Mo possesses very good cyclic properties at high temperatures.

3.3.2 Primary Pump Bearing Support

The primary pump bearing support structure is illustrated in Figure 30. The bearing support structure is bolted to the pump internal cylinder, and its purpose is to support the hydrostatic bearing for the pump shaft and impeller assembly. The bearing support was assumed to be made of 304 SS for this analysis. Since the primary pump is located in the hot leg, it is strongly influenced by increases in reactor outlet temperature and is subjected to those transients which affect the reactor outlet temperature. Of the 19 umbrella transients listed in Table 13, seven are applicable to the primary pump bearing support. The finite element model shown in Figure 31 was developed to evaluate the effects of these transients on the structural behavior of the bearing support using the APSA code. Figure 32 shows a stress intensity plot for the bearing support. Figure 33 shows the deformed and undeformed structure

FIGURE 28
PEAK STRESS INTENSITY
STEAM GENERATOR TUBESHEET

PUMP BEARING SUPPORT DESIGN

Figure 30

PRIMARY PUMP BEARING SUPPORT
STRESS MODEL

Figure 31

PRIMARY PUMP BEARING SUPPORT
TYPICAL STRESS INTENSITY PLOT

Figure 32

PRIMARY PUMP BEARING SUPPORT
TYPICAL UNDEFORMED/DEFORMED STRUCTURE PLOT

Figure 33

under typical transient conditions. The peak stress intensity for each of the transients considered is shown in Figure 34. Histograms of the creep, fatigue and creep-fatigue damage fractions are given in Figures 35-37. The results of the analysis indicate that the bearing support also fails to meet the structural criteria of Code Case 1592 (for the 1050°F reactor outlet temperature conditions) since it has a creep-fatigue damage fraction of 1.29.

Of this damage, fatigue is the predominate contributor with a value of 1.0. Since the fatigue life is very sensitive to stress levels, it is felt that a minor change in design or a change in materials would produce an acceptable fatigue life.

3.3.3 Intermediate Pump Bearing Support

Because of the similarities between the primary and secondary pump bearing support designs, the finite element model shown in Figure 31 for the primary pump was also used to evaluate the effects of secondary cold leg thermal transients on the intermediate pump. Nine of the 19 umbrella transients given in Table 13 are applicable to the intermediate pump. The histogram showing the results of the structural analysis is given in Figure 38. The creep-fatigue damage fraction for this component is only 0.1, which is well within the limit of 1.0 imposed by Code Case 1592. The major part of the damage fraction, 0.09, is due to fatigue caused primarily by the transients U-02, reactor trip from full power with minimum decay heat, and U-23, inadvertent water side dump of one steam generator loop. The creep damage fraction was calculated to be 0.01.

3.3.4 Reactor Vessel Outlet Nozzle

The configuration of the reactor vessel outlet nozzle is shown in Figure 39. The pipe penetration is connected to the reactor vessel wall by a flexible joint to accommodate the differential thermal expansion

FIGURE 34
PEAK STRESS INTENSITY
PRIMARY PUMP

FIGURE 35
CREEP DAMAGE FRACTION
PRIMARY PUMP

FIGURE 36
FATIGUE DAMAGE FRACTION
PRIMARY PUMP

FIGURE 37
CREEP - FATIGUE DAMAGE FRACTION
PRIMARY PUMP

FIGURE 38
CREEP - FATIGUE DAMAGE FRACTION
INTERMEDIATE PUMP

Figure 39

between the hot nozzle and the colder vessel wall. The exterior of the outlet nozzle is exposed to the relatively cold sodium which is used to cool the reactor vessel wall. The nozzle interior is exposed to the reactor outlet temperature. Of the 19 umbrella transients given in Table 13, seven are applicable to the reactor outlet nozzle. The finite element model which was developed to evaluate the structural adequacy of the nozzle at a 1050°F reactor outlet temperature is shown in Figure 40. The material was assumed to be 304 SS. A plot of the stress intensity for a typical transient condition is shown in Figure 41. The deformation of the nozzle under typical stress conditions is shown in Figure 42. The results of the analysis of the outlet nozzle for the seven applicable umbrella transients are summarized in Figures 43-46. The peak stress intensity for each of the transients is shown in Figure 43. Creep, fatigue, and creep-fatigue damage fractions are shown in Figures 44-46. The results show that the outlet nozzle does not meet the criteria of Code Case 1592 for creep or fatigue. The creep damage fraction is 1.28, and the fatigue damage fraction is 1.17. The transient which contributes the most to this damage is N-06, loading and unloading between 40 and 100% power. This is because of the large number of cycles, 23,210 each, loading and unloading, which are predicted over the plant life. Some reduction in damage fraction may be possible through the addition of a thermal shield around the nozzle to provide a separation zone between the nozzle and the cold pool. The use of 9 Cr - 1 Mo as the vessel and piping material may also be beneficial.

3.3.5 Intermediate Heat Exchanger Upper Tubesheet

The IHX unit is a straight-tube, vertical, counterflow design with a floating tubesheet. The upper tubesheet is rigidly fixed to an internal intermediate side head which floats within the IHX body. Differential thermal expansion between the tube bundle and shell is accommodated by a bellows joint between the floating head and the fixed shell. The IHX is made of 304 SS. A drawing of the upper tubesheet region of the IHX

REACTOR VESSEL OUTLET NOZZLE
STRESS MODEL

Figure 40

REACTOR VESSEL OUTLET NOZZLE
TYPICAL STRESS INTENSITY PLOT

Figure 41

REACTOR VESSEL OUTLET NOZZLE
TYPICAL UNDEFORMED/DEFORMED STRUCTURE PLOT

Figure 42

FIGURE 43
PEAK STRESS INTENSITY
OUTLET NOZZLE

FIGURE 44
CREEP DAMAGE FRACTION
OUTLET NOZZLE

FIGURE 45
FATIGUE DAMAGE FRACTION
OUTLET NOZZLE

FIGURE 46
CREEP-FATIGUE DAMAGE FRACTION
OUTLET NOZZLE

is shown in Figure 47. The finite element model which was developed to perform the structural evaluation of the tubesheet is shown in Figure 48. Ten of the 19 umbrella transients of Table 13 are applicable to the IHX upper tubesheet. Figure 49 shows a plot of the stress intensity in the tubesheet under typical transient conditions. Figure 50 shows the deformation which occurs in the structure under these conditions. The results obtained from the analysis of the ten umbrella transients is given in Figures 51-53. These results indicate that the design does not meet the criteria of Code Case 1592 at 1050⁰F reactor outlet temperature. A creep-fatigue damage fraction of 2.57 was obtained. The major part of the damage (2.24) is due to creep and results from transient N-06. A reduction in the large number of cycles for transient N-06 or a substitution of an alternate material with improved creep properties would be necessary to reduce the damage fraction to an acceptable value less than 1.0.

INTERMEDIATE HEAT EXCHANGER
UPPER TUBESHEET DESIGN

Reference Drawing
N222R000069

Figure 47

Figure 48

INTERMEDIATE HEAT EXCHANGER
UPPER TUBESHEET
TYPICAL STRESS INTENSITY PLOT

Figure 49

INTERMEDIATE HEAT EXCHANGER
UPPER TUBESHEET
TYPICAL UNDEFORMED/DEFORMED STRUCTURE PLOT

Figure 50

FIGURE 51
CREEP DAMAGE FRACTION
IHX TUBESHEET

FIGURE 52
FATIGUE DAMAGE FRACTION
LHX TUBESHEET

FIGURE 53
CREEP - FATIGUE DAMAGE FRACTION
14 X TUBESHEET

4.0 PLANT DESIGN CHANGES

4.1 MATERIALS CHANGES

4.1.1 Advanced Core Alloys

As discussed in Section 2, the properties of 20% CW 316SS, such as irradiation-induced swelling and creep and stress rupture strength, deteriorate considerably with increasing temperature. In order to improve on this situation, DOE has sponsored an advanced core alloy development program. The program initially considered 16 alloys but, in March 1978, the number was reduced to six. The alloys still being developed have the designations HT-9, D57, D9, D21, D66, and D68.

The properties of these alloys as they are currently known are given in Reference 20. The properties which are essential to fuel pin and fuel assembly performance predictions are:

- Time-to-Stress Rupture
- Sodium Corrosion Rate
- Fuel-Cladding Chemical Interaction
- Irradiation Induced Swelling and Creep
- Yield and Ultimate Tensile Strengths
- Young's Modulus and Poisson's Ratio

These properties, which are given in Reference 20, were recently updated, and these updated properties were incorporated into the SOC-II Code so that design and economic evaluations of these alloys could be performed.

In addition to the mechanical properties listed above, it is necessary to know the effects the different alloy compositions have on the nuclear performance of the reactor including such parameters as the breeding ratio and the fissile mass requirement. The sensitivity of the breeding

ratio and fissile mass to alloy composition was calculated using the nominal composition of the alloys given in Reference 20 and the neutron absorption cross sections from recent burnup studies done at the ESG. This approach does not treat any changes in neutron energy spectrum which the different alloys might cause but this effect is not believed to be large. These nuclear sensitivity coefficients have also been incorporated into the SOC-II code.

Calculations were made to evaluate the impact of all six advanced alloys on system design and economics at 930, 990, and 1050°F reactor outlet temperatures. The effect they have on total power generating costs are shown in Figure 54. The costs are given relative to the cost of a system using 20% CW 316SS as the core alloy and having a 930°F outlet temperature. In the analysis, only the core alloys were changed. The materials in the remainder of the system are the reference materials and were not varied. Also, the cladding and duct materials were changed together, i.e., no combinations of materials were considered. It may be seen from Figure 54 that all six advanced alloys show an economic advantage over 316SS. Only D68 shows an improvement over the 930°F/316SS core over the entire temperature range.

The effects the advanced alloys have on system capital costs are shown in Figure 55 relative to those of the 930°F/316SS system. Here again, all six alloys show an improvement over 316SS. This mainly results from the reduced vessel and building costs which are realized from the reductions in fission gas plenum lengths, cladding and duct wall thicknesses, and interassembly gaps with the advanced alloy cores. These reductions all translate into reductions in vessel and building sizes.

The fuel cycle costs which were obtained with the advanced alloys are shown in Figure 56. Here all alloys, except HT-9, show an improvement over the 930°F/316SS core at all outlet temperatures. These reduced fuel cycle costs are the result of the improved nuclear performance,

FIGURE 54. EFFECT OF ADVANCED CORE ALLOYS
ON POWER GENERATING COSTS

461510

10 X 10 TO THE CENTIMETER
KEUPFIL & ESSER CO. MADE IN U.S.A.

FIGURE 55. EFFECT OF ADVANCED CORE ALLOYS
ON CAPITAL COSTS

461510

K&E 10 X 10 TO THE CENTIMETER
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FIGURE 56. EFFECT OF ADVANCED CORE ALLOYS
ON FUEL CYCLE COSTS

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e.g., breeding ratio, doubling time, which is obtained with the advanced alloys either due to an improved composition or to the reduced amount of material required.

A summary of some of the main dimensional characteristics and nuclear performance parameters of the systems employing advanced alloys are given in Table 15. The same ground rules and restraints used for the 316SS cores were used here. Additional limits were placed on cladding and duct thicknesses because the strength properties of some alloys indicated thicknesses which, in the opinion of ESG Engineering personnel, have a poor history as cladding and would present handling problems as ducts. The limits used were a minimum cladding thickness of 0.012 in. and a minimum duct wall of 0.060 in.

A potential difficulty which may influence the selection of the high-nickel alloys, D21, D66, and D68, are their increased corrosion rate in sodium. This is an expected phenomenon because nickel is more soluble in sodium than iron or chromium. The impact of the increased corrosion rate is a required increase in fuel pin cladding wall thickness, especially at the higher temperature, and an increase in the mass transfer and deposition of radioactive corrosion products. This latter effect is made worse in the high-nickel alloys because cobalt, which is a natural impurity in nickel, is the source of Co^{60} which is one of the more hazardous radioisotopes produced. The amount of cobalt in the alloy would be a function of the nickel content unless the added expense of purifying the nickel, which would be associated with specifying a lesser amount, were to be absorbed.

The effects that the six advanced alloys would have on the IHX guard vessel surface radiation level were calculated using the same method used for 20% CW 316SS and described in Section 2.3.2. A graph of these results are shown in Figure 57. The contribution from fission products was assumed to be the same for all alloys, and the amount which

TABLE 15. CHARACTERISTICS OF ADVANCED ALLOY CORES

CORE MATERIAL	HT-9			D57			D9			D21			D66			D68		
OUTLET TEMP, °F	930	990	1050	930	990	1050	930	990	1050	930	990	1050	930	990	1050	930	990	1050
CLAD THK, mil	22	24	27	15	16	18	12	14	18	12	13	18	12	12	18	12	12	17
GAS PL. LEN, in.	30.2	52.7	99.1	31.8	51.0	87.3	25.9	41.9	61.7	14.5	24.6	40.2	15.7	37.6	51.1	10.3	17.9	22.9
FUEL ASSY LEN, in.	196.7	219.2	265.6	198.3	218.4	253.8	192.4	208.4	228.2	181.0	191.1	206.7	182.2	204.1	217.6	176.8	184.4	189.4
Na CORP., mil	.34	.99	2.47	.34	.98	2.43	.50	2.00	3.91	.99	3.24	6.75	.82	4.13	8.43	.93	2.16	4.45
ID ATTACK, mil	.21	.37	.60	.19	.33	.54	.99	1.77	2.99	.79	1.40	2.38	.16	.28	.48	1.18	2.06	3.52
DUCT WALL, in.	.14	.15	.19	.13	.14	.14	.17	.17	.18	.23	.22	.21	.10	.13	.16	.11	.11	.11
INTERASSY GAP, in.	.275	.255	.175	.167	.147	.159	.153	.228	.402	.468	.512	.543	.386	.271	.21	.177	.174	.167
LATTICE PITCH, in.	6.613	6.552	6.580	6.487	6.415	6.430	6.531	6.531	6.697	6.922	6.853	6.829	6.583	6.473	6.427	6.372	6.269	6.199
COR VEC. ID, ft	15.5	15.4	15.3	15.3	15.1	15.0	15.4	15.4	15.6	16.2	16.0	15.8	15.5	15.3	15.0	15.1	14.8	14.6
REFLECTOR VEC. HT, ft	50.3	54.6	62.9	50.5	54.4	60.8	49.6	52.7	56.7	47.9	50.0	53.2	47.9	52.0	54.8	46.9	48.6	50.0
CONTMT DIA, ft	180.3	193.1	234.0	179.9	192.6	233.4	180.0	193.0	234.3	181.5	194.2	234.8	180.2	192.8	233.3	179.4	192.0	232.5
CONTMT HT, ft	155.8	163.7	179.8	156.2	163.4	175.8	154.3	160.1	167.4	150.6	154.4	160.3	150.8	158.6	163.7	148.9	151.9	154.2
BREED. RATIO	1.321	1.309	1.277	1.324	1.317	1.294	1.383	1.365	1.311	1.339	1.340	1.299	1.348	1.345	1.259	1.338	1.353	1.289
DOUBLING TIME, yr	12.2	13.3	17.0	9.8	10.1	11.9	7.3	8.1	11.4	9.1	9.1	12.3	8.4	8.3	14.3	8.5	7.8	11.5
INORE MASS, kg	2974	2821	2768	3182	3039	2971	3192	3061	3041	3423	3259	3111	3237	3152	3050	3227	3062	2937
EXCORE MASS, kg	1100	1076	1113	1070	1033	1046	1018	1003	1070	1102	1059	1098	1055	1006	1080	1035	975	1018

FIGURE 57. EFFECTS OF CORE MATERIAL
AND REACTOR OUTLET TEMPERATURE
ON IHX GUARD VESSEL SURFACE
RADIATION LEVEL (10d AFTER
REACTOR SHUTDOWN)

10000

1000

100

10

DOSE RATE, mr/h

RADIATION
ZONE
CLASS

V

IV

III

II

D68
D21
D66
316SS
D9
HT-9
D57

REACTOR OUTLET TEMPERATURE, °F

900

950

1000

1050

was previously shown in Figure 23 is included in the totals shown here. For easy comparison, the dose rate for a 316SS core material is also shown. It may be seen that the three high-nickel alloys, D21, D66, and D68, do indeed contribute to higher dose rates in the primary system. The alloy, D9, shows the greatest improvement over 316SS with an increase in the outlet temperature at which the radiation zone class changes from Class III to Class IV of 40°F (from 980 to 1020°F). The two ferritic alloys, HT-9 and D57, are so similar in composition and corrosion rate that they yield the same dose rate. They show only a very small improvement over 316SS.

Another potential difficulty with the high-nickel content alloys, which should be mentioned but which was not evaluated in this study, is their tendency to lose ductility when exposed to neutron irradiation. The analysis of transient effects on fuel pin cladding was outside the scope of this study, but it is something that must eventually be considered in the development of the advanced alloys.

Considering all the effects, economics, mass transfer, and the potential for loss of ductility, it would appear at this time that Alloy D9 would be the preferred selection from among the six advanced alloys. It is only exceeded in power cost reduction by Alloy D68. It is superior to all six alloys in terms of primary system radioactivity and the preliminary indications are that it does not lose ductility as a function of neutron fluence as rapidly as Alloys D21, D66, and D68.

4.1.2 Steam Generator Alloys

There are three effects that limit the use of 2-1/4 Cr - 1 Mo as a steam generator material as the system temperatures are increased. These are 1) a loss of strength, 2) an increase in steam-side corrosion, and 3) an increase in decarburization by sodium resulting in an even

further decrease in strength. Because of these limits of 2-1/4 Cr - 1 Mo, there is a program underway in the U.S. to develop alternate structural alloys both for use as a SG material and as a potential replacement for 304SS and 316SS as piping, IHX, and major vessel materials. A report on the alloys being considered is given in Reference 21. A review of this and other literature resulted in two alloys being selected for evaluation in this study as alternate SG materials. The two are a stabilized 9 Cr - 1 Mo alloy being developed by Combustion Engineering (Reference 22) and a commercially available and ASME-coded (Code Case 1592-10) material, Alloy 800H. These two alloys were selected because they have the greatest potential for improvements in SG performance in terms of increased strength, reduced susceptibility to creep-fatigue damage, reduced corrosion rates in steam and sodium, resistance to chloride and caustic stress corrosion cracking, and resistance to decarburization by sodium. It was also felt desirable to include at least one alloy which has already been approved for high-temperature use by the ASME Boiler and Pressure Vessel Code. Alloy 800H was chosen for this reason and also because it was considered the backup material for CRBRP SG's and the French plan to use it in Super Phenix superheaters. Stabilized 9 Cr - 1 Mo was chosen because it represents a potentially economic replacement for both SG and primary system materials. The French are also investigating a stabilized 9 Cr - 2 Mo alloy (EM-12) for use in Super Phenix 2.

Some of the physical properties for stabilized 9 Cr - 1 Mo at its current stage of development have been reported in Reference 22. The time-dependent allowable stress, S_{mt} , is compared to other materials of interest in Figure 58. The S_{mt} values for these other materials were obtained from Code Case 1592-10. It may be seen that the allowable stress for 9 Cr - 1 Mo is much larger than that for 2-1/4 Cr - 1 Mo and it is larger than those for 304SS, 316SS, and Alloy 800H below about 1040°F.

Figure 58

Additional materials property data for 9 Cr - 1 Mo, which are needed to evaluate a SG design using the material, were researched at ESG and are contained in References 23-26. These include such properties as thermal conductivity, specific heat, thermal expansion coefficient, elastic modulus, and Poisson ratio as a function of temperature. Some information on the anticipated cost of the material is also given.

The behavior of 9 Cr - 1 Mo and Alloy 800H in sodium and steam was also researched at ESG, and the findings are given in References 27 and 28. The conclusions concerning sodium compatibility are that both alloys are resistant to sodium corrosion over the temperature range of interest and that decarburization by sodium is not a factor. With Alloy 800H as with other austenitic stainless steels, the tendency would be to carburize rather than decarburize if a carbon source were available. Because the carbon in 9 Cr - 1 Mo has been intentionally stabilized, it is not affected by decarburization.

The steam-side corrosion allowances on the SG tube wall thicknesses were calculated using the information from References 6, 27, and 28. The required allowance including 4 mils for periodic cleanings are shown in Figure 59 as a function of steam temperature. The allowance for 2-1/4 Cr - 1 Mo from Figure 18 is included here for comparison. It can be seen that there is about a factor of two reduction in corrosion allowance for 9 Cr - 1 Mo relative to 2-1/4 Cr - 1 Mo and that the corrosion allowance for Alloy 800H is mainly that included for cleaning. If the corrosion in Alloy 800H in sodium-heated SG's proved to be as small as indicated here, then the need for periodic cleaning would disappear and essentially no steam side allowance would be required.

This reduction in corrosion allowance coupled with the fact that no allowance for decarburization is required suggest that a substantial reduction in tube wall thickness is possible especially at the higher temperatures. The corrosion allowance along with the other properties

FIGURE 59. STEAM GENERATOR TUBE STEAM-SIDE CORROSION ALLOWANCE VS. STEAM TEMPERATURE AND TUBE MATERIAL

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of 9 Cr - 1 Mo and Alloy 800H were programmed into the SOC-II code so that the design features and economic aspects of systems using these materials for their SG's could be evaluated. The cost of 9 Cr - 1 Mo is not yet known because the material is not on the market. Discussions with Combustion Engineering, ORNL, and vendors of unstabilized 9 Cr - 1 Mo revealed a range of costs from 25% to 100% above the cost of 2-1/4 Cr - 1 Mo. It has been assumed that the labor costs would be the same as those for 2-1/4 Cr - 1 Mo because both require post-weld heat treatment (PWHT). The estimated cost of Alloy 800H is six times that of 2-1/4 Cr - 1 Mo, based on vendor data. No reduction in labor costs was taken although some might be possible because there would be no requirement for PWHT. Increases in weld inspection costs for Alloy 800H could offset any reduction in PWHT costs.

The total power costs obtained from the SOC-II analysis considering alternate SG materials are shown in Figure 60. The differential power costs are shown relative to those of a system with 2-1/4 Cr - 1 Mo SG's at a reactor outlet temperature of 930⁰F. The costs for the 9 Cr - 1 Mo SG's cover the 25 to 100% cost range previously mentioned. These curves represent only a change in SG material; all of the other materials in the system were held fixed. It can be seen that 9 Cr - 1 Mo can easily compete with 2-1/4 Cr - 1 Mo even at a 100% increase in material cost and that it becomes especially attractive at the 1050⁰F outlet temperature. It is apparent that an Alloy 800H SG is much too expensive. It does have the benefit of being a coded material but the cost of getting a material coded is probably less than the differences shown here for one power plant. The capital costs differential in millions of dollars is shown in Figure 61. These include the direct and indirect costs of the SG modulus and the satellite SG buildings. The 9 Cr - 1 Mo SG's show about a 40 million dollar reduction in capital investment over 2-1/4 Cr - 1 Mo SG's at a 1050⁰F reactor outlet temperature. The Alloy 800H SG's are about 35 million dollars more expensive than the 2-1/4 Cr - 1 Mo SG's at 1050⁰F.

FIGURE 60. TOTAL POWER COSTS VS. STEAM
GENERATOR MATERIALS

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FIGURE 61. DIFFERENTIAL STEAM GENERATOR
CAPITAL COSTS VS. STEAM GEN-
ERATOR MATERIAL

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DIFFERENTIAL CAPITAL COSTS (Direct and Indirect), 10⁶ \$ 1978

The same physical characteristics for 9 Cr - 1 Mo and Alloy 800H SG's which were given for 2-1/4 Cr - 1 Mo SG's in Table 6 are given in Table 16.

4.1.3 Primary System Alloys

The superior time dependent allowable stress, S_{mt} , for 9 Cr - 1 Mo relative to both 304 stainless steel and 316 stainless steel which was shown in Figure 58 suggested that its use in the primary system could offer some improvement over the stainless steels. In addition to better allowable stress characteristics, the 9 Cr - 1 Mo alloy has a smaller thermal expansion coefficient and a larger thermal conductivity than 304/316 stainless steel which would give it added advantages as a piping or heat exchanger material.

The effects of using 9 Cr - 1 Mo as the primary and intermediate piping material, as well as for the IHX's, were evaluated using SOC-II. The comparison was made for systems using 9 Cr - 1 Mo as the SG material. The core material was 20% CW 316 stainless steel. The differential power costs are shown in Figure 62 for a 9 Cr - 1 Mo primary system and for a 304/316 primary system. The differential costs are taken relative to the stainless steel primary system at a 930°F reactor outlet temperature. The material cost used for 9 Cr - 1 Mo was 10% less than the cost of 316 stainless steel and the labor cost was 25% more. These cost factors are equivalent to the upper limit values used in the SG material evaluation. It may be seen that there is a substantial cost incentive for a 9 Cr - 1 Mo primary system. The major reduction results from the reduced primary piping length in the hot leg and its effect on the reactor containment diameter. These two parameters are shown in Figure 63 as a function of reactor outlet temperature for both 316 stainless steel piping and 9 Cr - 1 Mo piping. The improved strength and reduced thermal expansion of 9 Cr - 1 Mo allows a 70-115 ft reduction in primary hot leg piping length. As a consequence, there is a 38-63 ft reduction in containment building diameter. The capital cost savings which may be realized from this are shown in Figure 64. Also shown is the reduction in IHX cost

TABLE 16
PHYSICAL CHARACTERISTICS OF STEAM GENERATORS USING ADVANCED MATERIALS

SG Material	9 Cr - 1 Mo			Alloy 800H		
Reactor Outlet Temperature, °F	930	990	1050	930	990	1050
Heat Transfer Area/Module, 10 ³ ft ²	20.1	17.7	14.6	24.2	21.5	17.0
Number of Tubes/Module	1,999	1,568	1,276	2,379	1,881	1,509
Tube OD, in.	1/2	9/16	9/16	1/2	9/16	9/16
Tube Wall Thickness, in.	0.046	0.061	0.075	0.047	0.057	0.059
Active Length, ft.	76.7	76.7	77.5	77.7	77.5	76.7
Tube Sheet Thickness, in.	10.1	9.8	9.2	12.0	11.7	10.5
Shroud OD, in.	50.1	47.1	42.5	54.7	51.6	46.2
Shell OD, in.	59.3	55.4	50.8	63.4	59.5	54.1
Module Weight, tons	87	84	76	110	101	81
Steam Exit Velocity, fps	46.6	47.0	66.6	39.8	38.0	48.2
Steam Side Corrosion Allowance, in.	0.011	0.016	0.025	0.0048	0.0054	0.0063

FIGURE 62. TOTAL POWER COSTS VS. PRIMARY
SYSTEM MATERIAL

FIGURE G3 . EFFECT OF PRIMARY SYSTEM
MATERIAL ON PRIMARY PIPING
LENGTH AND CONTAINMENT DIAMETER

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DIFFERENTIAL CAPITAL COSTS (Direct and Indirect), 10⁶ \$ 1978

which may be obtained from the improved heat transfer properties of 9 Cr - 1 Mo relative to 304 stainless steel. The same physical characteristics which were given for a 304 stainless steel IHX in Table 5 are given in Table 17 for a 9 Cr - 1 Mo IHX.

4.2 DESIGN CONFIGURATION CHANGES

4.2.1 Reactor System

In the reactor system, which includes the reactor core and internals, the core, reactor, and guard vessels and nozzles, the core support and restraint, and the top plugs and deck, there are many design configuration (dimensional) changes which are required when the reactor outlet temperature is increased. In many cases, the SOC-II code evaluates the required dimensional changes in the reactor system components. These include such changes as the fuel pin fission gas plenum length, the interassembly gap, the number of fuel assemblies, the height and diameter of the core, reactor and guard vessels, and the top plug and deck diameters and insulation thickness. These design changes and their impact on system economics are therefore included in the results presented in other sections of this report.

The results for the detailed structural analysis of the reactor vessel outlet nozzle at a 1050°F reactor outlet temperature showed that its damage fraction is greater than that allowed by the ASME High-Temperature Code Case 1592. Changes in the detailed design of a component such as the reactor vessel outlet nozzle is not handled by the SOC-II code. Each potential change must be subjected to the same type of detailed evaluation as described in Section 3. It was not possible to perform such an evaluation within the time allotted for this study.

4.2.2 Primary and Secondary System

The effects which increases in reactor outlet temperature have on component dimensions in the primary and secondary sodium loops are calculated in the SOC-II code, and the economic evaluation of these size changes are included in the results presented in this report.

TABLE 17
PHYSICAL CHARACTERISTICS OF 9 Cr - 1 Mo IHX VS REACTOR OUTLET TEMPERATURE

Reactor Outlet Temperature, °F	930	990	1050
Steam Cycle, psig/°F	2200/850	2400/900	2400/950
Heat Transfer Area, 10 ³ ft ²	65.2	53.4	42.1
Number of Tubes	7715	5634	4855
Tube OD, in.	0.75	0.75	0.75
Tube Wall Thickness, in.	0.049	0.049	0.049
Total Tube Length, ft	47.4	52.2	47.9
Active Tube Length, ft	43.0	48.2	44.1
Tube Sheet Thickness, in.	14.9	12.9	12.1
Shroud OD, ft	10.1	8.7	8.1
Shell OD, ft	11.2	9.7	9.1
Primary ΔP	18.9	18.9	18.7
Secondary ΔP	19.7	22.4	22.4
Log Mean ΔT, °F	34.8	40.0	49.8

Included are the changes in primary and secondary piping diameters and length. Pipe thickness was estimated to be constant over the range of interest in this study. The primary piping general arrangement and configuration within the reactor containment building employed in the SOC-II Code is that found to be optimum for the 930⁰F reference design of Reference 1. The general shape is fixed, however, the piping diameter is varied according to the flow requirements within the constraints of a maximum sodium velocity of 30 fps, and the length is varied according to the thermal expansion requirements within the constraints of the allowable stress for the piping material. The re-evaluation of different piping arrangements with variations in diameter and length was outside the scope of this study. However, because it has a strong influence on system economics, it is recommended that potential changes in piping configuration be evaluated in future efforts.

The size and number of tubes in the IHX are calculated by the SOC-II code for each reactor outlet temperature. This includes an evaluation of the tube OD and wall thickness, and tubesheet diameter and thickness within the constraints of the required heat transfer through the unit and the allowable stress for the material. A minimum tube wall thickness of 0.049 in. was specified because of handling considerations during manufacturing. The shell wall thickness was held constant. It is set by seismic considerations and the thickness variation over the temperature range of this study has been estimated to be only about 10%. This small variation would have an insignificant effect on the results. The structural evaluation of the IHX upper tubesheet at a reactor outlet temperature of 1050⁰F showed a damage fraction greater than that allowed by Code Case 1592. It is felt that a large part of this may be due to conservatism in the scoping finite element model used in that an insufficient number of points were used in the critical area. It is not sure that removing this conservatism alone would make the tubesheet acceptable. The critical section of the tubesheet is near the center of the tube region and most of the damage is due to creep. Optimization of the tube spacing with respect to the ligament stresses could offer some reduction in the creep damage fraction.

The size and head/flow requirements of the primary and secondary sodium pumps are calculated in the SOC-II code for the different reactor outlet temperature systems. The details of the design such as the configuration of the pump bearing support are not included. The structural evaluation described in Section 3.3 showed that the damage fraction for the primary pump bearing support exceeded the Code Case 1592 allowable at a 1050°F outlet temperature. Time did not permit an examination of alternate configurations but it is felt that the use of an alternate material such as 316SS or 9 Cr - 1 Mo would offer the preferable solution. The results of Section 3.3 showed that the intermediate pump bearing support is acceptable.

4.2.3 Steam Generator

The structural evaluation of the steam generator upper tubesheet for a 1050°F reactor outlet temperature system revealed a damage fraction much in excess of that allowed in Code Case 1592. This is due to the large thermal gradients between the rim and the outer rows of holes which arise during transients from the rapid cooling in the hole area and the delayed cooling of the solid rim. These thermal gradients induce significant residual stresses in the tubesheet during severe upset and emergency events. In the CRBRP program, a large number of alternate steam generator tubesheet design configurations were analyzed in an attempt to find a solution to the ligament stress problem. The results of this analysis, which are reported in Reference 29, showed that none of the alternate configurations provided a solution. It was necessary to employ transient mitigation for a solution. It is felt that transient mitigation and/or the use of 9 Cr - 1 Mo in the large system steam generators will provide the reduction in damage fraction necessary for the upper tubesheet to meet Code Case 1592 requirements.

4.3 TRANSIENT MITIGATION

The effects of thermal transients can be mitigated by either reducing the number of event cycles and/or the severity of the transient (rate, magnitude). This study addressed methods to reduce transient severity.

A dynamic plant model was developed for this purpose. The scope of the model includes the primary, intermediate, and steam/feedwater heat transfer circuits. All major components are incorporated in the model. Emphasis was placed on providing sufficient detail of the dominant plant characteristics to provide realistic transient results. For example, natural circulation, fluid inertia, pump dynamics and characteristics, valve characteristics, control and protection system logic and response times, etc., are simulated.

There are two basic areas to be considered in limiting transient severity. The first area involves selecting plant control system design features which limit plant transients to tractable levels. The second area involves inherent design features which significantly affect the plant transient response (e.g., flow decay to match fuel stored energy, loop elevations).

4.3.1 Control System Methods

Because of the large number of reactor trips and its significance for most plant components, schemes which mitigate this transient were concentrated on for this investigation.

During the first few minutes following reactor trip, the transient is governed solely by the match between the flow and fuel thermal energy decay characteristics. Thereafter, the plant shutdown control system determines the transient. One mitigation scheme uses feedwater flow control in conjunction with fixed pony motor speed. The other scheme uses sodium flow control.

4.3.1.1 Feedwater Flow Control

Early attempts to mitigate the hot leg by trimming feedwater flow with a steam temperature signal proved not feasible because of the excessive process delay. The next transient mitigation approach evaluated was to trim feedwater flow on steam generator outlet temperature control.

This scheme along with reference plant shutdown control scheme is shown in Figure 65. For this concept, single speed pony motors drive the sodium pumps. The cold leg temperature is held constant by controlling feedwater flow and maintaining steam pressure and feedwater temperature at essentially their pretrip values. For this shutdown control approach, the severity of the hot leg transient is dependent on the pony motor speed (see Figure 66). Reducing the pony motor speed "softens" the hot leg transients but impacts pump low speed design requirements, as discussed in Section 4.3.3.

Another method of softening the hot leg transients and increasing the IHX and steam generator design margins without a substantial reduction of pony motor speed involves only a minor modification to the above scheme. In this mitigation approach, the control scheme causes a programmed initial rise in steam generator sodium outlet temperature and then a slow decrease to saturation temperature. The resulting rise in steam generator sodium outlet temperature reduces the negative temperature rate of change at the steam generator sodium inlet. This, coupled with feedwater flow control to maintain the feedwater-to-sodium-flow fraction ratio less than unity, assures that the steam temperature will be tied to the sodium inlet temperature (superheat pinch point operation) and that a sudden decrease in steam temperature to saturation temperature does not occur as the sodium inlet temperature slowly approaches saturation temperature. The only changes to the earlier control system required to soften the transient besides controller, gain, and reset settings were:

- 1) The sodium outlet temperature trim controller set point is programmed as shown instead of being held constant.
- 2) The main feedwater set point index is 60% of intermediate sodium flow fraction instead of 100% (matched sodium-feedwater flow fractions).

BENSON FEEDWATER SHUTDOWN CONTROL SYSTEM

FIGURE 65

FIGURE 66

EFFECT OF PONY MOTOR SPEED ON PLANT HOT LEG THERMAL TRANSIENTS

The results for 7% and 4% pony motor speed using the programmed cold leg temperature set point are shown in Figure 67. Although the steam generator upper tubesheet (hot leg) transient is milder for the 4% case, low-speed hydrostatic pump bearing design problems make the 7% pony motor speed a prudent selection as the reference approach. This reference transient mitigation scheme selected for the 930°F reactor outlet condition was also used as the basis for all the transients at the 1050°F reactor outlet design evaluated in Section 3.2. For the purpose of comparison, the steam generator upper tubesheet transients for the constant cold leg temperature control scheme and the CRBRP steam generator with and without Protected Air-Cooled Condenser System (PACCS) are also presented in Figure 67.

Softening the hot leg trip transient in this way results in a more severe reactor inlet temperature transient because of the constant pony motor speed constraint. The increase in the severity of the cold leg transient was evaluated for the cold leg components (intermediate pump bearings, reactor inlet nozzle, and core support structure), and it appears that design margins are not effectively reduced.

4.3.1.2 Sodium Flow Control

In the event that additional transient mitigation schemes must be employed at the higher outlet temperatures, sodium flow control can be added to reduce transient rates even further. Two variable sodium flow control methods were investigated. One uses constant speed pony motors plus control valves (Figure 68), and the other employs variable speed pony motor pump drives (Figure 69) to modulate the flow. Both of the variable sodium flow control methods are used in addition with the reference feedwater (cold leg temperature) control scheme. This permits independent control of the hot and cold leg temperatures.

EFFECT OF CONTROL SCHEMES IN MITIGATING SG UPPER TUBESHEET TRIP TRANSIENT

FIGURE 67

SIMPLIFIED SHUTDOWN CONTROL SYSTEM DIAGRAM THROTTLE VALVE SCHEME

FIGURE 68

SIMPLIFIED SHUTDOWN CONTROL SYSTEM DIAGRAM VARIABLE SPEED PONY MOTOR SCHEME

FIGURE 69

The control system approach for both of these methods is basically the same. That is, the reactor and heat transfer system are initially maintained at the preshutdown levels and then the system temperature differences are slowly collapsed at a controlled rate of about $-50^{\circ}\text{F}/\text{hour}$ to approximately the saturation temperature. To achieve this end, the primary coolant flow is matched to the decay power after the transition from operating to shutdown flow. The primary flow set point is calculated (by the master control unit) to match the decay power and is trimmed by reactor fuel assembly outlet temperature. Direct temperature control is not used because of the large process delay time which is accentuated at low flow. The intermediate flow is matched to the primary flow to prevent redistribution of the IHX temperature gradients following shutdown. The steam generator pressure (controlled by the turbine bypass valve to the steam dump system) is maintained at its preshutdown value which, in conjunction with the feedwater flow control, minimizes the cold leg temperature change. The feedwater flow is modulated to prevent rapid collapse of the steam generator thermal gradients. The feedwater flow setpoint is programmed (by the master control computer) to match the intermediate flow decay. Steam generator sodium outlet temperature trim is provided for long-term temperature control and is programmed to provide a gradual increase (50°F in 4.5 hours) to saturation temperature. The plant shutdown control method for the sodium heat transfer system is represented schematically by Figure 70.

Models of both sodium flow control methods were developed and incorporated into the plant dynamics code in order to evaluate their performance. The model includes sensor, actuator, and controller dynamics. Figure 71 shows the results obtained with the constant pony motor plus control valve scheme. The computer results indicate excellent control performance. For this evaluation, the pony motor speed was fixed at 10%, and valve actuation rate was limited to no less than 100 sec for full stroke. This valve speed was not optimized, and it is anticipated that even slower-acting valves, and if desirable, even

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KE 20 X 20 TO THE INCH • 7 X 10 INCHES
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higher pony motor speed would also yield satisfactory performance. It is noted that a similar controls method, using throttle valves in conjunction with natural convection was used at the Hallam Plant. The control system worked well and resulted in mild trip transients.

Difficulty was experienced in achieving good stable control using variable pony motor speed to control sodium flow. To ensure the sodium flow is controlled by the pump for low speed pony motor operation, a plant hydraulic profile was chosen to limit natural convection head. Even so, the controller at times requires near zero or even zero pump speed in an effort to attain the desired sodium flow. In addition to the controls difficulties, the low speed operating conditions imposes lower pump bearing design problems (see Section 4.3.3). As a result, the variable pony motor speed scheme was dropped and the valve scheme was selected as the preferred method for achieving further transient mitigation if required. In addition, the valve method could provide temperature control if only natural convection were used for decay heat removal (the pony motors used as backup).

4.3.2 Inherent Design Features

In addition to these major transient mitigation schemes, several other inherent design feature effects were also evaluated for their potential for improving transient performance.

4.3.2.1 Upper Plenum Mixing

The upper plenum mixing is a function of the plenum geometry, coolant velocity, and the relative temperatures of the incoming and plenum coolant. A simple, one-dimensional, two-region model, was assumed for this study. The model contains a variable 100% mixing zone, the size of which is a function of the coolant velocity and temperatures, and a transport region between the exit nozzle and 100% mixing region.

The results obtained with this simulation appear reasonable, but they have not been verified by a validated three-dimensional code or test results. Evaluation of reactor upper plenum designs to minimize stratification, promote mixing, and generally utilize its heat capacity to ameliorate temperature perturbations are best accomplished with more rigorous, three-dimensional codes and test programs. However, it is presently felt that modifications to the reference reactor plenum design would not significantly mitigate the plant transients.

4.3.2.2 Plant Operating Schedule

For full- and part-load operation, the plant is designed to operate at constant power-flow ratio and, thus, essentially constant loop ΔT 's. This permits changes in load (even rapid) without temperature variations which might have an impact on component design margins. The part-load schedule requirements (steam conditions, reactor outlet temperature limits, etc.) are met by varying primary and intermediate sodium flow and feedwater flow in proportion to power. This establishes the requirement for the pump speed control range to match the part-load range, which is the reference system.

4.3.2.3 Pump Coastdown Speed and Loop Elevations

In the earlier analysis of the effects of pony motor speed on mitigating the reactor trip transient, only the average power driver and blanket core assemblies at MOEC were considered. Although these results (Figure 66) show milder upper plenum temperatures with decreasing pony motor speed, the effect on the cladding temperature of peak power fuel pins must also be considered. To investigate this, the parametric effects of pony motor speed, primary pump coastdown, and loop elevation effects on natural circulation were analyzed for peak power pins in both the driver and blanket core regions. Based on the bulls-eye core described in Reference 1, the peak power driver pin at BOL occurs in

Flow Zone 7 of the outer driver, and the peak power blanket pin at EOL occurs in Flow Zone 2 of the central blanket. Core hydraulics accounted for the proper flow splits between driver and blanket elements and included the buoyance effects.

The computer runs made are given below. All runs were made with the reference shutdown control scheme (steam generator sodium outlet temperature controlled to a constant trim value).

<u>Run No.</u>	<u>Description</u>
1-07	Constant 7% Pony Motor Speed
2-07	Natural Circulation Primary Sodium Pump Inertia Varied 1 0.25 x nominal* 2 0.5 x nominal 3 0.75 x nominal 4 1.0 x nominal 5 1.5 x nominal
3-07	Natural Circulation Nominal Primary Sodium Pump Inertia Thermal Center IHX to Core Changed 1 -15.0 feet 5.242 feet 2 0.0 feet nominal 20.242 feet 3 +15.0 feed 35.242 feet

* Nominal pump inertia - 225,000 lb-ft²

The results obtained from these runs are listed in Table 18:

TABLE 18
PEAK 2-SIGMA MID-WALL CLADDING TEMPERATURES
(°F)

Run No.	Description	Central Blanket Temp., °F (Time, sec)	Outer Drive Temp., °F (Time, sec)
1-07	7% Pony Motor	1638 (22)	1009 (36)
2-07	Variable Inertia		
	0.25 x nominal	1897 (18)	1231 (5)
	0.5 x nominal	1638 (22)	1087 (54)
	0.75 x nominal	1469 (17)	1066 (70)
	1.0 x nominal	1370 (13)	1054 (86)
	1.5 x nominal	1271 (9)	1045 (120)
3-07	Variable Elevation		
	-15.0 feet	1648 (25)	1124 (55)
	0 feet	1638 (22)	1087 (54)
	+15.0 feet	1629 (20)	1057 (50)

Based on the results presented in Table 18, it can be noted that the peak mid-wall cladding temperatures are moderate for the peak power driver elements and, except for the rapid pump coastdown case (0.25 x nominal), were below the pre-reactor trip value of 1190°F. However, relatively high peak cladding temperatures were experienced for the blanket pins for the nominal (7%) pony motor speed and pump coastdown time. The peak temperature was about 400°F above the pre-trip value of 1237°F.

To reduce peak cladding temperatures in the blanket pins, the flow decay must be matched to, or decrease slower than, the rate of stored energy released from the blanket pin (directly related to pin size). The stored energy in the .300-in. diameter driver pin is significantly less than in the 0.617-in. diameter pins used in the blanket assembly. Therefore, flow decay characteristics which generally cause the driver pin clad temperature to dip below its initial value still decay too rapidly with respect to the blanket pin stored energy to prevent blanket pin cladding temperatures from increasing (see Figure 72). Employing the reference LSBR primary pump kinetic energy results in an acceptable peak clad temperature of 1440°F following a reactor trip. Changing pony motor speed would not have an effect on reducing the peak blanket cladding temperature. Changes in loop elevation was seen to have only a minimal effect on peak cladding temperature.

4.3.3 System Effects on Pump Design

A number of plant parameters, such as pony motor speed, loop elevations, and pump coastdown time, were investigated to determine their effect on softening thermal transients. These studies, coupled with a realistic plant model, have brought to light system effects which impact on the primary and secondary pump requirements.

DRIVER AND BLANKET HOT CHANNEL TEMPERATURE
TRANSIENT FOLLOWING A REACTOR TRIP WITH NATURAL CONVECTION

*071780501
050578 0005

RUN 2-07 REACTOR TRIP WITH NATURAL CIRCULATION
ZERO PCT. PONY MOTOR SPEED - PARAM. PRI. PUMP INERTIA
THCLD-TEMP. (F) DRIVER HOT CHANNEL CLAD (CONST FACTOR)
THCLB-TEMP. (F) BLANKET HOT CHANNEL CLAD (CONST FACTOR)

FIGURE 72

There are two system effects which tend to cause hydrostatic bearing flow starvation. These are excessive natural convection and excessive coolant momentum, which both tend to reduce the pump pressure differential which induced bearing flow. The coolant momentum effect on pump head is transitory, occurring only during coastdown to pony motor pump speed. The effect of buoyant head occurs at pony motor pump speed.

4.3.3.1 Coolant Momentum Effect

If the fluid inertia of the loop is not negligible, the coolant flow will decay slower than the pump speed, and the pump will provide a drag which reduced the developed head.

The primary and intermediate pump heads for the transition period are shown in Figures 73 and 74 for various fluid-to-pump half-time ratios (T_F/T_R) or equivalent ratios of loop fluid to pump rotor kinetic energies. For these results, the fluid half time was held constant and the pump half time varied to yield ratios of 0.25, 0, 5, 1, 1.5, and 2.0. The pony motor speed was 7%. Figures 73 and 74 clearly show that the pump head which induces hydrostatic pump bearing flow is substantially reduced and even becomes negative for large T_F/T_R ratios. For the plant considered, the primary T_F/T_R is 0.11 and the secondary T_F/T_R is 0.25. From Figures 73 and 74 it can be seen that for both of these values the reduction in pump head during transition to pony motor speed is minimal. Furthermore, as indicated in the following table, the T_F/T_R ratios used during this study are consistent with pump design parameters which in turn are within the ANL design requirements.

PRIMARY PUMP HEAD DURING TRANSITION TO PONY MOTOR SPEED

REF. $T_F/T_R = 0.11$
PONY MOTOR SPEED 7%

*0717532-76
042678 0001

FIGURE 73

INTERMEDIATE PUMP HEAD DURING TRANSITION TO PONY MOTOR SPEED

REF. $T_F/M_R = 0.25$
PONY MOTOR SPEED = 7%

RUN 6-01.RT.7PCT PNY.CASE 1THRU 5-HYD 1AU/PUMP 1AU=25.5.1.1.5.2
HISLPP-PUMP HEAD(FRACT).NA INTRMD LOOP-REF 81.16 PSI

*0717532-76
042878 0031

PUMP HEAD FRACTION (REF. 81.16 PSI)

TIME AFTER TRIP (SECONDS)

FIGURE 74

PUMP REQUIREMENTS FROM TRANSIENT MITIGATION RESULTS

	LSI Study	ANL Design Requirements
Primary Pump		
• Gpm/head, ft	83000/476	85000/500
• Kinetic Energy Requirement ft-lb x 10 ⁷	1.6	1 ≤ KE ≤ 2
• Pony Motor Speed	>7%	4% - 7%
Intermediate Pump		
• Gpm/head, ft	74000/212	85000/300
• Kinetic Energy Requirement ft-lb x 10 ⁷	>0.80	0.7 ≤ KE ≤ 1.4

*Based on Fluid KE of $.20 \times 10^7$ ft-lb for the Intermediate Loop.

4.3.3.2 Buoyancy Effect

The buoyant head has an insignificant effect on loop flow at pump speeds corresponding to the plant operating range. However, for low-speed (pony motor) operation, the buoyant head can be dominant and acts as a natural convection "pump." This is illustrated by the data shown in Figure 75. The pump head and buoyant head given are prior to and 400 seconds after a reactor trip. Figures 76 and 77 show the effects of natural convection on pump developed head for the primary and intermediate pumps for various pony motor speeds. The effects of relative changes between the IHX and steam generator elevation is also shown in Figure 77. It is seen from these figures that it is possible to have practically no pump head (or even that it be negative) available for inducing bearing flows at low pony motor speeds and plant configurations having relatively large buoyant drives. It is apparent from Figures 76 and 77 that three factors determine the sodium bearing flow for low-speed operation: (1) pony motor speed, (2) plant hydraulic profile

FIGURE 75

THE "NATURAL CONVECTION PUMP" DEVELOPS
SIGNIFICANT HEAD AT PONY MOTOR CONDITIONS

	Pump Head (psi)	Buoyant Head (psi)
<u>Primary Pump</u>		
100% Speed and Flow	173.	.227
4% Speed*	.277	-0-
4% Speed and 5.22% Flow	.100	.245**
<u>Intermediate Pump</u>		
100% Speed and Flow	81.0	.321
4% Speed*	.130	-0-
4% Speed and 5.32% Flow	.222	.113**

*Isothermal Pump Operation
**400 seconds After Plant Trip

NATURAL CONVECTION "PUMP"

FIGURE 76

EFFECT OF BUOYANCY ON LOW SPEED INTERMEDIATE PUMP PERFORMANCE

FIGURE 77

(buoyant head), and (3) pump characteristics.

At slightly more than 7% pony motor speed (for reference pump and plant configuration), the buoyant head decreases to zero and this effect becomes negligible. Although a 7% pony motor speed and thus flow, results in more severe plant hot leg thermal transients (see Figure 66), than a lower (~4%) speed, these transients can be reduced to reasonable values by the present reference scheme for a 930°F reactor outlet, or by the sodium control valve approach for the 1050°F reactor outlet condition.

4.3.4 Transient Mitigation Summary

Because of the large temperature differences which occur in LMFBR's, large temperature transients can occur under off-normal accident conditions. However, the effects of these transients on plant hardware can be minimized by the application of transient mitigation schemes. Some of these can be made inherent in the system, such as loop elevations and pump coastdown time. Others require active control schemes such as feedwater or sodium flow control. Based on this evaluation, it was concluded that a minimum primary pump inertia (coastdown speed) is required to avoid excessive temperatures in the blanket assemblies under reactor trip condition.

It was further concluded that natural convection, following reactor trip, using pony motors only as a backup is also a feasible post-trip cooling scheme. If pony motors are used as the primary shutdown sodium flow mechanism, it was concluded that although a 4% pony motor speed provides slightly better transient conditions, a pony motor speed of at least 7% should be employed to avoid potential primary pump bearing flow starvation due to bouyancy effects following a reactor trip.

For the 930°F reactor outlet design, it was found that only feedwater flow control was required to mitigate the transient effects on the

steam generator to within acceptable levels. For the 1050°F reactor outlet design, it was found that even with feedwater control, some additional transient mitigation is probably required (if materials substitution or simple design changes prove inadequate). If this is required, it was found in this study that sodium flow control could more than adequately mitigate the transients. In this case, the pony motors would be run at constant speed, and the sodium flow would be controlled with butterfly control valves (as was done in the HNPF reactor).

4.4 CHANGES IN OPERATING CONDITIONS

It was shown in Section 2 that, as the reactor outlet temperature is increased, the primary and secondary sodium ΔT 's also increase in economic optimum systems (see Figure 4). These increased ΔT 's can give rise to increased thermal gradients in sodium components during steady-state operation as well as increase the severity of thermal stresses during thermal transient conditions. The excellent heat transfer properties of the sodium coolant tend to make the response of components to system transients even more severe than comparable transients in a LWR.

One potential method for reducing the magnitude of the steady-state and transient thermal stresses is to reduce or limit the primary and secondary ΔT 's. Since the ΔT 's shown in Figure 4 are the ones which give the minimum power generating costs without regard to thermal transient effects, there is an economic penalty associated with using ΔT 's which differ from these. The magnitude of the penalty has been assessed in the following way.

System designs were determined, using the SOC-II code at 990 and 1050⁰F outlet temperatures, which have the same primary and secondary ΔT 's as the optimum system at a 930⁰F outlet temperature. The primary ΔT was 290⁰F and the secondary was 280⁰F. These systems at 990 and 1050⁰F thus have the optimum configurations within the constraints of these ΔT 's. The code determined the optimum pipe diameters, pump requirements, fuel pin pitch and fuel assembly size, numbers of tubes and tube diameters in the IHX and SG units, etc. The economic parameters for these systems are also determined by the code.

A comparison of the total power costs, capital costs, and fuel cycle costs between the optimum systems and the systems in which ΔT 's were held constant is shown in Figure 78. It can be seen that the economic penalty associated with the fixed ΔT 's is substantial and, as expected, it becomes worse with increasing outlet temperature as the difference between the optimum ΔT 's and the fixed ΔT 's becomes larger.

FIGURE 78 . ECONOMIC PENALTY FOR HOLDING
PRIMARY AND SECONDARY ΔT 'S FIXED

The capital cost increase may be attributed to the changes which occur in primary and secondary piping length, pumping power, and containment diameter. If the primary and secondary ΔT 's are held constant, the sodium flow rates only change by the amount of change in reactor thermal power which occurs because of the more efficient steam cycles which are used at the higher reactor outlet temperatures. Since the flow rate change with the constant ΔT 's is less, the pipe diameter and pumping power changes are less than those in the optimum systems. Since the pipe diameters in the constant ΔT systems change only a small amount, their piping length increase is greater than in the optimum systems because the length is proportional to the diameter to the 7/3 power. The cold leg piping also increases in length because the cold leg temperatures are increasing the same amount as the reactor outlet temperature if ΔT 's are fixed. This temperature increase results in a reduction in allowable stress so that longer expansion loops must be used. These changes in the primary piping diameters are shown in Figure 79 and the changes in length are shown in Figure 80. Even though the piping cost increases with length and diameter, the main capital cost increase occurs in the reactor containment building because its required diameter must increase to accommodate the longer piping. The containment building diameter and differential capital cost as a function of reactor outlet temperature are shown in Figure 81 for the optimum ΔT and constant ΔT systems. Since the sodium flow rates and pressure drops remain almost constant for the constant ΔT systems, the pumping power and pump capital cost also remain essentially constant. The pumping power and differential capital cost are compared to those for the optimum ΔT systems in Figure 82.

FIGURE 79. PRIMARY PIPING DIAMETERS
FOR OPTIMUM ΔT 'S AND CONSTANT ΔT 'S

461510

10 X 10 TO THE CENTIMETER 18 X 25 C
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FIGURE 80. PRIMARY PIPING LENGTHS FOR
OPTIMUM ΔT 'S AND CONSTANT ΔT 'S

461510

10 X 10 TO THE CENTIMETER 18 X 25 C
KEUFFEL & ESSER CO. MADE IN U.S.A.

FIGURE 81. REACTOR CONTAINMENT BUILDING
DIAMETER AND COST FOR OPTIMUM ΔT 's AND
CONSTANT ΔT 's

461510

10 X 10 TO THE CENTIMETER 18 X 25 C
KEUFFEL & ESSER CO. MADE IN U.S.A.

FIGURE 82. PRIMARY AND SECONDARY SODIUM
PUMP POWER AND COST FOR OPTIMUM ΔT 'S
AND CONSTANT ΔT 'S

461510

K^oE 10 X 10 TO THE CENTIMETER 18 X 25 CM KEUFFEL & ESSER CO. MADE IN U.S.A.

5.0 RECOMMENDED ENGINEERING AND DEVELOPMENT AREAS

5.1 ADVANCED CORE ALLOYS

The evaluation of the economic aspects of the advanced core alloys as fuel pin cladding and fuel assembly duct material showed that substantial reductions in power costs are possible through their use. It must be recognized that the behavior of these alloys as cladding and duct material is still in a state of development. There is, therefore, a need for further evaluation of these alloys as additional experimental data becomes available. It is recommended that SOC-II analysis be performed whenever significant updates in the properties of the advanced alloys are made. This will provide valuable guidance toward the proper selection of those alloys which merit further development for advanced reactor cores.

During the course of this evaluation of the advanced core alloys, there were certain materials properties which significantly influence fuel pin design which it is believed require increased emphasis toward the acquisition of experimental data.

The corrosion rates in sodium of the precipitation-hardened Alloys D21, D66, and D68 appear to be overly conservative. In fact, the corrosion rates of all six alloys appear conservative relative to that of 20% CW 316 SS (See Figure 83). It is understood that this conservatism is due to the small amount of experimental data available. An accelerated effort in this area would be very beneficial.

The fuel-cladding chemical interaction for which allowance must be made in setting the fuel pin cladding thickness is another property which needs added attention. The amount of clad ID attack which was calculated using the current correlations in Reference 20 is shown in Figure 84 for the six advanced alloys and for 20% CW 316 SS as a function of reactor outlet temperature. Alloys D9, D21, and D68 show a much more pronounced temperature dependence than the other alloys. This

FIGURE 83. SODIUM CORROSION RATE OF
ADVANCED CORE ALLOYS VS REACTOR
OUTLET TEMPERATURE

461510

K&E 10 X 10 TO THE CENTIMETER 18 X 25 CM
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FIGURE 84. FUEL-CLADDING CHEMICAL
INTERACTION OF ADVANCED CORE ALLOYS
VS. REACTOR OUTLET TEMPERATURE

461510

K&E 10 X 10 TO THE CENTIMETER 18 X 25
KEUFFEL & ESSER CO. MADE IN U.S.A.

is difficult to understand because the composition of D9 is similar to that of 20% CW 316 SS and that of D68 is similar to D66. In addition to the different temperature dependence between D68 and D66, there is a large difference in the amount of attack. The acquisition of data on fuel-cladding chemical interaction requires rather long-term experiments because of its burnup dependence. Thus, early initiation of fuel pin irradiations is very important to the understanding of ID attack in the advanced core alloys.

It was necessary in this study to treat the effects of the advanced alloy composition on reactor nuclear performance in a way which may not properly differentiate between the alloys which are relatively similar in composition. To obtain a better measure of the influence of the advanced alloys on nuclear performance, it is recommended that nuclear analysis of cores using these alloys be made. This effort would be directed toward the development of alloy-dependent sensitive coefficients for breeding ratio and core fissile mass which are essential to the evaluation of fuel cycle costs.

The design limit, which was used in this study for fuel pins, was a CDF of 0.5. This is consistent with the guidelines of Reference 3 for steady-state irradiation, but it does not account for the effects of reactor transients on fuel pin cladding damage. A value of 0.5 rather than 1.0 allows some margin for transient effects, but it is not known whether this is a sufficient margin for some of the alloys considered. The high-nickel alloys, D21, D66, and D68, have shown some evidence for more rapid loss of ductility upon irradiation than the other alloys. It is recommended that transient analysis of fuel pins using the advanced alloys be made using the LIFE 4T code or other suitable methods. In establishing the umbrella thermal transient sets for the system, a large number of reactor transients were predicted. The analysis of these transients on fuel pins using advanced alloys would provide very valuable information to the core alloy development program.

The information, which was obtained from HEDL on fuel fabrication costs (Reference 9), did not become available until fairly late in this study. As a consequence, it was not possible to investigate the effect of fuel pin diameter on system economics. Since the HEDL-derived fuel fabrication cost method causes fuel cycle costs to have a stronger influence on total power costs than the previously used ESG method, it is possible that the optimum fuel pin diameter is larger than the 0.300-in. OD used in the present analysis. It is recommended that an optimization study on fuel pin diameter be performed for both the reference 20% CW 316 SS cladding material and the advanced core alloys.

5.2 PRIMARY SYSTEM

5.2.1 Primary Piping System

The main effect of reactor outlet temperature on system economics, which was determined during this study, was the strong influence of the primary hot-leg piping on reactor containment building size and cost. There are several areas where further engineering studies and materials development could be employed to improve on this situation.

The first study that is recommended is a reevaluation of the primary piping system as a function of reactor outlet system with the intent of optimizing the piping configuration for minimum containment size within the constraints of the ASME B&PV Code. The methods used by the SOC-II code to scale the piping length vs temperature are appropriate for a given piping configuration, but it obviously is beyond the scope of any system optimization code to determine other optimum piping layouts as the system changes. It would be appropriate to perform this piping study for both 316SS and 9 Cr - 1 Mo piping for the different temperature conditions to check the approach used in the computer.

An additional investigation of the primary piping system that would be beneficial is a study of the use of bellows to accommodate the piping thermal expansion rather than expansion loops. Work is being performed to develop an ASME Code Case for the use of bellows in sodium piping.

The results of a study on the effects of bellows on system economics as a function of reactor outlet temperature would certainly be of value to this effort.

5.2.2 Containment Building

In conjunction with the primary piping analysis recommended above, a detailed design layout and cost evaluation of the reactor containment building should be made as a function of outlet temperature. This would provide an important verification of the trends which are reported here.

A reactor containment building was used as the reference design in this study. Because of the strong influence of containment on system economics, a containment/confinement economic trade study should be performed, and the sensitivity of this choice to the economic trade-off should be evaluated. Admittedly, the confinement vs containment questions involves much more than economics, but an economic evaluation vs outlet temperature would show the incentives for solving the safety-related problems.

This effect would also not be so pronounced for a pool system. Therefore, a reevaluation of the economics vs reactor outlet temperature should be carried out with the pool concept as well.

5.2.3 Primary System Materials

It is recommended that the effort to qualify 9 Cr - 1 Mo as a high-temperature material for ASME Section III, Class 1 components be increased. This would include an increased accumulation of materials properties data. In addition to the normal engineering properties, such as yield strength, time to rupture, etc., the effects of irradiation and long-term immersion in sodium on the material properties need to be evaluated.

The results of this study indicated that further evaluation of the reactor vessel outlet nozzle configuration at elevated temperatures is required. This evaluation should consider both the use of 9 Cr - 1 Mo as a replacement for 316 SS as well as changes in the nozzle configuration.

5.3 STEAM GENERATOR SYSTEM

The analysis of the effects of increased sodium and steam temperatures which was performed during this study revealed several areas where further analysis and material development are required.

The presently indicated behavior of unstabilized 2-1/4 Cr - 1 Mo as both steam generator and tubesheet material shows that it would have limited use for systems with reactor outlet temperatures above 1000°F. This limitation could be real or it could be due to the limited quantity of data available for the material at the higher temperatures. The alloy may well be satisfactory at a 950°F steam temperature if its properties were better known. There is an ongoing effort to better understand 2-1/4 Cr - 1 Mo and continued monitoring of this effort to determine the effects of new data on system temperature limits should be done.

The effort which is underway to develop a stabilized 9 Cr - 1 Mo alloy for reactor system use should be expanded. Experiments should be initiated to evaluate the corrosion rate of the material in steam as a function of steam temperature. The creep and fatigue properties of the material need to be determined. The fabricability of the alloy into tubes, piping and vessels, and the costs associated with this, need to be determined. The weldability of the alloy, although part of the current development program, needs to be demonstrated relative to its use as a steam generator material, e.g., the tube-to-tubesheet weld. Although it would be a major effort, a demonstration SG unit made from 9 Cr - 1 Mo would make an enormous contribution toward the future development of more efficient, higher temperature systems.

The results of this study showed that further evaluation of the transient effects on the SG upper tubesheet is required at the higher temperature conditions. This effort should include analysis of a tubesheet made of 9 Cr - 1 Mo. The effects on the tubesheet of employing secondary hot-leg sodium temperature control during reactor scram should be evaluated.

A multiple loop system simulation model should be developed to allow calculation of single loop events for which conservative estimates of the transient effects had to be made.

A study should be made to evaluate the economic incentives for the use of separate evaporators and superheaters made from different materials. The properties of 2-1/4 Cr - 1 Mo at the lower temperatures and its reduced cost make it very attractive as an evaporator material. This coupled with a 9 Cr - 1 Mo, 316 SS, or Alloy 800H superheater could offer an economic advantage over the once-through units which were part of this study. The capability for performing such a study exists in the SOC-II code.

5.4 THERMAL TRANSIENT MITIGATION

The thermal transient mitigation study centered on evaluating methods and inherent design features to reduce the severity of the reactor trip transient. Several alternate approaches which offer potential for softening thermal transients which were not addressed during this study as well as approaches to reduce the number of cycles should be investigated.

In this study, the steam generator transients were mitigated using a constant feedwater temperature and varying feedwater flow. An alternate scheme which should be investigated for comparison is holding the feedwater flow constant and varying feedwater temperature.

Transient mitigation efforts in this study were limited to minimizing the effects of the reactor trip transient. An evaluation of additional schemes to mitigate other transients if required should be investigated.

Also, in this study it was assumed that transients employing backup decay heat removal systems looked the same as a normal reactor trip.

The validity of this assumption should be checked out.

This study investigated the use of sodium valves to control flow for the higher temperature cases. An alternate approach to investigate is providing excess natural convection flow capability (to satisfy safety margins) and to control the sodium flows to those required to mitigate transients using sodium flow control valves (per HNPF).

Additional effort should be directed toward reducing the number of cycles (particularly trip transients) through fast power runback capability and additional redundancy in some cases.

In general, the second iteration on transient mitigation for the higher temperature reactor outlet temperature cases could not be carried out. This, integrated with potential design and materials changes, should be carried out to complete the structural adequacy of the design at higher temperatures.

REFERENCES

1. "Liquid Metal Fast Breeder Reactor Conceptual Plant Design - 1000 MWe," FBR-77-3, Volume II Plant Description, May 1977
2. N. G. Galluzzo, "SOC-II System Optimization Code - Description and User's Manual," N222TI123001, October 21, 1975
3. RDT Design Guideline/Criteria for LMFBR Core Components (draft version for trial application), June 1976, Fast Breeder Reactor Department, General Electric Company, Sunnyvale, California
4. W. F. Brizes, "Review of the Fuel Cladding Cumulative Damage Functions for Incorporation into the Fuel Rod Performance Code," FMT-FI-WFB-103, September 29, 1977
5. "Life-III Fuel Element Performance Code," ERDA 77-56, July 15, 1977
6. M. J. Manning and E. Metcalfe, "Oxidation of Ferritic Steels in Steam," Presented at the International Conference on Ferritic Steels for Fast Reactor Steam Generators, London, May 30-June 2, 1977
7. T. R. Stauffer, et al., "Breeder Reactor Economics," Breeder Reactor Corporation Publication, July 1, 1975
8. L. E. Minnick, "Economic Guidelines," Letter to Project Managers, PLBR Design Projects, February 23, 1976
9. R. P. Omberg, "A Revised Fabrication Cost Equation for Use in FBR Core Design," Letter to Director, FFTF Project Office, Number 7861845, June 27, 1978
10. "Joint HEDL/AI Program Plan for LMFBR Decontamination Process Development," TC-380, December 1975
11. E. F. Hill, "Decontamination Studies - Results of Screening Program on Candidate Metal Removal Agents," N707TR840010, June 20, 1976
12. "Hot Leg Decontamination Specification," N707TI830017, April 12, 1977
13. F. A. Newburn, et al., "Steam Cycle Comparison Study," N222TI123009, November 17, 1977
14. G. S. Drucker, et al., "Transient Identification/Large System Improvement Study," N254TI122003, February 3, 1978

15. G. S. Drucker, et al., "Plant Control (Post-Shutdown) Transient Analysis," N222TI123010, October 3, 1977
16. J. F. Newell and S. F. Persselin, "Finite Element Axisymmetric and Planar Structural Analysis," IL 4112-5025 Rocketdyne Division, Rockwell International, April 18, 1974
17. E. Moody, "Thermal Analyzer Program Revision (TAP-4F)," TI-099-411-015, Revision F, June 8, 1978
18. W. R. Castle, "Primary System Temperature Limits Critical Components Stress Analysis, N254TI122009
19. W. Veljovich, "Recommended Ratcheting and Creep Fatigue Assessment Rules," N036TI630023, May 4, 1977
20. J. J. Laidler, "Alloy Properties Databook," HEDL Report TC-293, Revision 3, June 1, 1978
21. R. T. King, et al., "Report of Task Force on Alternate Structural Materials for Liquid Metal Fast Breeder Reactors," ORNL-5076, May 1976
22. G. C. Bodine, Jr., et al., "A Program for the Development of Advanced Ferritic Alloys for LMFBR Structural Application," TR-MCD-015, September 1977
- 23.* IL, S. Tsang to D. S. Bost, "Design Data of Stabilized 9% Cr - 1% Mo Ferritic Steel," November 18, 1977
- 24.* IL, S. Tsang to D. S. Bost, "Additional Information of Stabilized 9 Cr Ferritic Steel," November 23, 1977
- 25.* IL, S. Tsang to D. S. Bost, "Additional Property Data of Stabilized 9 Cr - 1 Mo Steel," January 27, 1978
- 26.* IL, S. Tsang to D. S. Bost, "Cost Information and Thermophysical Properties of Stabilized 9 Cr - 1 Mo Steel," April 19, 1978
- 27.* IL, S. Tsang to D. S. Bost, "Behavior of 9 Cr - 1 Mo Ferritic Steel in Sodium and Steam," January 20, 1978
- 28.* IL, S. Tsang to D. S. Bost, "Physical Properties and Corrosion Behavior of Incoloy 800 and Incoloy 800H," March 21, 1978
29. T. L. Anderson, et al., "Solution to Ligament Stress Problem in the Superheater Upper Tubesheet," N036TI630022, April 28, 1977

* See also: S. Tsang, "Material Property Data for Advanced Steam Generator Materials", N254TI122010, Sept. 22, 1978