✓
NUREG/CR-1617
BNL-NUREG-51254

DO NOT MICROFILM
COVER

ADVANCED REACTOR SAFETY RESEARCH DIVISION

QUARTERLY PROGRESS REPORT
APRIL 1 - JUNE 30, 1980

MASTER

Date Published - August 1980

DEPARTMENT OF NUCLEAR ENERGY, BROOKHAVEN NATIONAL LABORATORY
UPTON, NEW YORK 11973

Prepared for the U.S. Nuclear Regulatory Commission
Office of Nuclear Regulatory Research
Contract No. DE-AC02-76CH00016

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

The following pages are an exact
representation of what is in the original
document folder.

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

NUREG/CR-1617
BNL-NUREG-51254
AN, R-1,7,8

ADVANCED REACTOR SAFETY RESEARCH DIVISION

QUARTERLY PROGRESS REPORT
APRIL 1 - JUNE 30, 1980

NUREG/CR--1617
TI86 002780

HERBERT J.C. KOUTS, Department Chairman
WALTER Y. KATO, Deputy Chairman

Principal Investigators:

A.K. Agrawal
R.J. Cerbone
C. Sastre

Compiled by: Anthony J. Romano
Manuscript Completed: July 1980

DEPARTMENT OF NUCLEAR ENERGY
BROOKHAVEN NATIONAL LABORATORY, ASSOCIATED UNIVERSITIES, INC.
UPTON, NEW YORK 11973

Prepared for the
REACTOR SAFETY RESEARCH DIVISION
OFFICE OF NUCLEAR REGULATORY RESEARCH
U.S. NUCLEAR REGULATORY COMMISSION
CONTRACT NO. DE-AC02-76CH00016

FIN Nos.:

A-3015 A-3024
A-3016 A-3041

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

*EB
Jaw*

NOTICE

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, or any of their employees, makes any warranty, expressed or implied, or assumes any legal liability or responsibility for any third party's use, or the results of such use, of any information, apparatus, product or process disclosed in this report, or represents that its use by such third party would not infringe privately owned rights.

The views expressed in this report are not necessarily those of the U.S. Nuclear Regulatory Commission.

Available from
GPO Sales Program
Division of Technical Information and Document Control
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555
and
National Technical Information Service
Springfield, Virginia 22161

THIS PAGE
WAS INTENTIONALLY
LEFT BLANK

FOREWORD

The Advanced Reactor Safety Research Programs Quarterly Progress Report describes current activities and technical progress in the programs at Brookhaven National Laboratory sponsored by the USNRC Division of Reactor Safety Research. The projects reported each quarter are the following: HTGR Safety Evaluation, SSC Code Development, LMFBR Safety Experiments, and Fast Reactor Safety Code Validation.

The previous reports, BNL-NUREG-50624, BNL-NUREG-50661, BNL-NUREG-50683, BNL-NUREG-50747, BNL-NUREG-50785, BNL-NUREG-50820, BNL-NUREG-50883, BNL-NUREG-50931, BNL-NUREG-50978, BNL-NUREG-51014, BNL-NUREG-51082, BNL-NUREG-51130, BNL-NUREG-51177, and BNL-NUREG-51217 have covered the periods October 1, 1976 through March 31, 1980.

ADVANCED REACTOR SAFETY RESEARCH

TABLE OF CONTENTS

	<u>Page</u>
FOREWORD	iii
I. HTGR SAFETY EVALUATION	1
Summary	1
1. Graphite and Ceramics	3
1.1 Oxidation-Induced Strength Loss in HTGR Graphites	3
1.2 Effect of Stress and Different Atmospheres on the Strength and Oxidation Behavior of Nuclear Graphite	12
1.3 Elemental Analysis of PGX Graphite	20
References	22
2. Materials and Chemistry	27
2.1 Fatigue of Structural Materials	27
2.2 Creep Rupture Properties of Primary Circuit Structural Metals	34
2.3 Helium Impurities Loop	42
References	44
3. Fuel and Fission Products	45
3.1 Fission Product Migration in H451 Graphite	45
3.2 Fission Product Migration Studies	47
References	48
4. Analytical	49
4.1 HTGR Safety Code Library	49
4.2 Application of Available HTGR Safety Codes	55

	<u>Page</u>
4.3 HTGR - Gas Turbine Plant Licensing Considerations	55
II. LMFBR SAFETY EVALUATION	56
Summary	56
1. Thermal Hydraulic LMFBR Safety Experiments	58
1.1 Hydrodynamic Characteristics of Two-Phase Dispersed Systems	58
1.2 Heat Transfer in Internally Heated Pools	59
1.3 Assessment of the Thermal Hydraulic Technology of the Transition Phase of a Core Disruptive Accident in a LMFBR	61
References	64
2. SSC Code Development	68
2.1 SSC-L Code	68
2.2 SSC-P Code	72
2.3 SSC-W Code	76
2.4 SSC-S Code	78
Publications	78
References	80
3. SSC Code Validation	81
3.1 Inter-Code Comparisons of SSC-L/FFTF to IANUS	81
3.2 Inter-Code Comparison of SSC-L to DEMO/CRBRP	87
Publications	87
References	87

I. HTGR SAFETY EVALUATION

SUMMARY

Density profiles of PGX graphite specimens presumably oxidized in the diffusion limited temperature regime have been analyzed. In some cases a diffusion/reaction model incorporating one average diffusion length corresponding to a single pore network fits the derived burnoff profiles satisfactorily. In other cases a model incorporating two average diffusion lengths corresponding to surface and bulk pore networks fits the data better. The bimodal pore network model appears to give a better fit when the total burnoffs in the two networks are similar. Volumetric rate coefficients, K_v , calculated with the bimodal model indicate that the ratio of the surface to bulk rate coefficient, K_{vs}/K_{vb} , is relatively independent of burnoff and temperature and is approximately the same as the ratio of active surface areas expected in the two networks.

Analyses of major impurities in PGX graphite are almost complete. Sixty-two specimens, taken from three locations in two logs, were oxidized and strength tested. Concentrations of 10 impurities in these specimens have been determined and are currently being compared with the observed reaction kinetics.

It has been confirmed that the amount of compressive strength lost by PGX graphite after oxidation in O_2/N_2 mixtures is greater than that lost after oxidation in H_2O/He or CO_2/He mixtures. The reaction temperature does not seem to affect the strength loss over the temperature ranges 550 to 750°C for 2% H_2O/He , 450 to 550°C for air, 400 to 600°C for 2% O_2/N_2 , and 600 to 850°C for 2% CO_2/He .

Loss of compressive strength of H451 graphite does not depend on the kind of oxidizing atmosphere, i.e., air or H_2O/He , nor is it affected by prestress before oxidation. An equation of the form $S/S_0 = (\rho/\rho_0)^\alpha$ does not fit these data well; here S , S_0 , ρ , ρ_0 and α are the compressive strength after oxidation, mean compressive strength, density after and before oxidation, and a constant, respectively.

No effect of stress (up to about 40% of the mean strength) during oxidation on the reaction rate is observed for PGX graphite in a 2% H_2O/He mixture at 660°C. Tensile tests of oxidized PGX graphite have been initiated, giving a similar trend with burnoff in H_2O/He as that observed for compressive strength.

In kinetics experiments, the temperature dependence of PGX graphite oxidation in O_2 was measured to determine the effect of O_2 concentration. The activation energy for oxidation in 2% O_2/N_2 was 165 ± 7 (90% confidence limits) kJ/mol, which is ~ 20 kJ/mol lower than the activation energy measured for oxidation in air of specimens from a different log.

The exposure of 2-1/4 Cr - 1 Mo steel to wet helium environments at 530°C (1000°F) caused severe surface decarburization after times of 1000-3000 h. Severe losses in high cycle fatigue strength occur at 538°C as a

result of this carbon depletion. An analysis of the surface corrosion behavior shows that, initially, an iron oxide layer is formed beneath which is a chromium oxide layer. However, under the action of an applied stress the iron oxide is removed by spallation and chromium oxide becomes the stable oxide.

Preliminary data on the high cycle fatigue strength of Incoloy 800H and Hastelloy X show that at elevated temperatures reduction of the water content in the helium from 2500 to 5-10 μ atm. causes an increase in strength. The strength change is most marked in Incoloy 800H.

Some preliminary data on the creep rupture properties of Incoloy 800H and Hastelloy X in the new drier helium test gas indicate that the rupture life is increased with loss of rupture ductility as the helium environments become drier. Oxidation is still the major metal-gas interaction in the new drier environments. Qualitative analysis shows that oxidation characteristics are similar to those observed in air or in the old wet helium environment.

Continuing the study of fission product migration in H451 graphite, several molybdenum migration experiments were run and analyzed this past quarter. Additionally, experiments to study the migration of a Uranium-Thorium mixture have started.

Simulated fission product migration experiments have been continued with an improved source geometry designed to permit more accurate temperature measurement. The majority of released Sr associated with airborne particles seems to occur at temperatures between 1400°C and 1600°C. This result is in good agreement with a previous experiment conducted with a different source geometry but approximately the same initial Sr concentration.

A sensitivity evaluation of the ORECA code was completed, showing for instance very strong core temperature dependence on coolant velocity head, and core material properties. A memorandum summarizing the results has been drafted.

The exploratory program for HTGR gas turbine plant dynamics is in the debug state.

1. Graphite and Ceramics

This past quarter the focus of the investigation of strength loss mechanisms in HTGR graphites continued to be thermal oxidation of PGX graphite. Previously-obtained density profiles of PGX graphite were analyzed to learn more about the effect of surface burnoff on the experimentally obtained burnoff profiles. The effects of oxidizing atmosphere, temperature and prestress on oxidation kinetics and compressive strengths of HTGR graphites continued to be studied. Similar studies were initiated to determine the effects of these variables on the tensile strengths of HTGR graphites. Studies at SUNY at Buffalo were directed at chemisorption and surface mobility of oxidants, particularly oxygen, on graphite and the measurement of in-pore diffusion of oxidants into oxidizing graphite. Finally, elemental analysis of oxidized PGX graphite neared completion; these results should be useful in focusing attention on impurities as sources for promotion or inhibition of the PGX oxidation rate.

1.1 Oxidation-Induced Strength Loss in HTGR Graphites (F. B. Growcock, M. Eto, J. H. Heiser, III, C. A. Sastre, J. J. Barry)

Burnoff profiles of axial cylindrical PGX graphite specimens 3.81 cm ϕ x 7.62 cm oxidized at 700°C and 800°C in 0.17% H₂O + 1.7% H₂ (or 4.4% H₂) were obtained several months ago and reported in the last quarterly progress report (Growcock, 1980b). The data were fitted by an expression for simultaneous diffusion and reaction which incorporates a bimodal independent pore network model. The main features of this model are a surface network, N_s, of micropores and a bulk network, N_b, consisting of the entire array of pores. Reasonably good fits of the data were obtained using this model. This past quarter those data were further analyzed; the results of that analysis are discussed in what follows.

Diffusion lengths in N_s and N_b were calculated by fitting the data to equation (1); in which it is assumed that the graphite is a homogeneous medium:

$$B_i = Z_s \frac{I_1(r_i/L_s)}{r_i} + Z_b \frac{I_1(r_i/L_b)}{r_i}, \quad (1)$$

where B_i is the burnoff of the residual core, r_i is the distance from the center of the specimen, L_s and L_b are the diffusion lengths in N_s and N_b, respectively, I₁ is the modified Bessel function of the 1st kind of order 1 and Z_s and Z_b are fitting coefficients. The fitting coefficients have the values

$$Z_s = \frac{2B_{os}L_s}{I_0(r_o/L_s)} \quad \text{and} \quad Z_b = \frac{2B_{ob}L_b}{I_0(r_o/L_b)}, \quad (2)$$

where B_{os} and B_{ob} are the burnoffs at the surface resulting from oxidation in N_s and N_b, respectively, I₀ is the modified Bessel function of the 1st kind of order 0, and r_o is the original radius of the specimen. Using

equations (1) and (2), an expression similar to the Dodson equation (Dodson, 1960 and Burnette, 1977) may be obtained for the local burnoff:

$$B_k = B_{os} \frac{I_o(r_i/L_s)}{I_o(r_o/L_s)} + B_{ob} \frac{I_o(r_i/L_b)}{I_o(r_o/L_b)} \quad (3)$$

The iterative non-linear least squares routine used previously to fit equation (1) to experimental burnoff profiles was refined slightly to give faster convergence. The variation in uncertainty of the B_i vs. r_i data was small enough that weighting of the data was deemed unnecessary at this stage of the analysis. In most cases treatment of the data with the bimodal pore network model was found to give better fits than treatment with the standard single pore network model. Examples of these results are given in Figures 1.1 and 1.2. Figure 1.1 shows the fitted curves obtained from the application of both models to data from specimen #60, which was oxidized at 700°C to 1.90% burnoff; Figure 1.2 shows the results for specimen #41, oxidized at 800°C to 1.80% burnoff. In some cases, however, the fits obtained with the two models were almost identical, as exemplified by the results for specimens #7 and #66. The results for specimen #7, oxidized at 700°C to 2.14% burnoff, are shown in Figure 1.3, while those for specimen #66, oxidized at 800°C to 4.37% burnoff, are shown in Figure 1.4. Generally, the one and two pore network models gave almost identical results at depths greater than 1 mm. In most cases, the results were quite similar at even smaller depths. Only in the region very near the surface (less than 0.1 to 0.6 mm, depending on the specimen), did a discrepancy usually arise. In those cases where no significant difference between the models was observed, such as specimen #7 in Figure 1.3, high burnoff data near the surface were usually observed which could not be fitted satisfactorily. The use of Bessel functions to treat the observed burnoff behavior is being questioned, and the fitting routine itself is being reexamined to ascertain if either of these is a source of the problem.

Although the evidence supporting the bimodal pore network model is still rather meager, it is instructive to analyze the expressions used (equations (1) and (3)) to treat the burnoff profiles. Total burnoffs in each pore network may be obtained from equation (1):

$$B_{Ts} = \frac{Z_s}{r_o} I_1(r_o/L_s) \quad \text{and} \quad B_{Tb} = \frac{Z_b}{r_o} I_1(r_o/L_b) \quad (4)$$

Diffusion lengths and network burnoffs for the PGX specimens are presented in Table 1.1, along with total burnoffs and diffusion lengths obtained from the single pore network model (N_{single}).

Assuming constant reactivity and diffusivity we can calculate volumetric rate coefficients, K_v , and effective diffusion coefficients, D_{eff} , using equations (4) and (5):

$$\frac{B_T}{t} = S/V L \eta \frac{K_v \cdot 12P_{ox}}{\rho RT} \quad \text{for 1st order reaction} \quad (5)$$

Figure 1.1. Burnoff profiles of specimen #60, oxidized in 0.1% H_2O /1.7% H_2 /He at 700°C to 1.90% burnoff.

Figure 1.2. Burnoff profiles of specimen #41, oxidized in 0.17% H_2O /1.7% H_2 /He at 800°C to 1.80% burnoff.

Figure 1.3. Burnoff profiles of specimen #7, oxidized in 0.17% H₂O/1.7% H₂/He at 700°C to 2.14% burnoff.

Figure 1.4. Burnoff profiles of specimen #66, oxidized in 0.17% H₂O/1.7% H₂/He at 800°C to 4.37% burnoff.

Table 1.1

Average Diffusion Lengths
PGX Graphite: H₂O/H₂/He

Temp, °C	Specimen	N _{single}		N _s		N _b	
		B _T ,%	L,cm	B _{Ts} ,%	L _s ,cm	B _{Tb} ,%	L _b ,cm
700	7	2.11	0.131	1.91	0.163	0.16	0.163
700	60	1.90	0.124	0.55	0.034	1.28	0.60
700	29	0.79	0.12	0.71	0.11	0.17	0.30
700	11	0.49	0.23	0.18	0.011	0.37	0.30
700	96	0.42	0.17	0.19	0.012	0.28	0.33
800	66	4.37	0.068	4.07	0.063	0.51	0.155
800	41	1.80	0.080	1.08	0.028	0.80	0.20
800	109	0.57	0.13	0.21	0.025	0.23	0.35

where B_T is the total burnoff, t the reaction time, S/V the surface/volume ratio, $L = \sqrt{D_{eff}/K_v}$, P_{ox} the partial pressure of oxidant, ρ the density of the material, R the gas constant and the T absolute temperature. The reactivities obtained from each term of the burnoff expression (and by inference the reactivities in N_s and N_b) exhibit the interesting property that their ratio is relatively constant (see Table 1.2). If the pore size distribution in PGX graphite is similar to that of H451 graphite (see Growcock, 1980b), the average radius of the micropores is ~20 Å, the intermediate pores ~10³ Å and the macropores ~10⁵ Å. For a typical void volume distribution in these pores of 0.04, 0.10 and 0.03, respectively, the total area of the micropores is ~20x the total area of the intermediate pores and macropores. It may be expected that this would also be the ratio of the total active surface areas. The relative reactivities of the graphite in the two networks is seen from Table 1.2 to be 10-20. The outstanding agreement between these values, although possibly fortuitous, lends support to the hypothesis that there is an independent surface reaction and that reaction in this region occurs in the micropores.

Effective diffusion coefficients derived from equations (4) and (5) are plotted in Figure 1.5 as functions of the network burnoff along with values derived from GA data (GA, 1978a) using the single pore network model. The N_{single} results (D_{eff}^{single}) from the two laboratories appear to be at variance, but both show unmistakably that D_{eff} increases with total burnoff. The trends in these sets of data indicate that extrapolation to zero burnoff would yield an effective diffusion coefficient of 0.2% to 1% of the free binary gas diffusion coefficient, which is the expected value for as-received PGX graphite (GA, 1978b and Growcock, 1980a). The effective diffusion coefficients in N_s (D_{eff}^s) and N_b (D_{eff}^b) increase with

Table 1.2
Relative Reactivity in Pore Networks
OF PGX Graphite

<u>Specimen</u>	<u>Temperature, °C</u>	<u>B_{T_s}/B_{T_b}</u>	<u>K_{v_s}/K_{v_b}</u>
7	700	11.94	11.9
29	700	4.18	11.6
96	700	0.68	18.9
11	700	0.49	13.2
60	700	0.43	10.0
66	800	8.14	19.7
41	800	1.35	9.55
109	800	0.91	12.7

network burnoff just as D_{eff}^{single} increases with total burnoff. D_{eff}^S , however, appears to extrapolate to very low values at zero burnoff. In fact, it is rather easy to fit an expression that gives the Knudsen diffusion coefficient in micropores of 20 Å average radius at zero burnoff and the free binary gas diffusion coefficient at 100% burnoff; the line drawn through the D_{eff}^S data is one possible fit.

More direct evidence of the existence of the surface burnoff phenomenon comes from a "cocoon" experiment similar to that performed several months ago and reported in the last quarterly progress report (Growcock, 1980). In this experiment, however, the entire cocoon was 25 mm longer, so that after oxidation 12.5 mm were machined from each end of both the inner and outer specimens. The cocoon, made entirely from H451 graphite, was oxidized in dried, filtered air at 500°C. The inner specimen showed 3.00% burnoff at the end of one week. It was profiled as before, using, however, a different design of specimen holder to eliminate loss of material around the chuck during start-up of the lathe. This time the outer specimen was also profiled, beginning at the inner surface. The results (not shown) indicate some excess burnoff (ca. 3x the bulk burnoff) present at a depth less than 0.1 mm into each surface.

L_s and L_b , as well as Z_s and Z_b , are implicitly assumed to be independent of burnoff. This simplifying assumption has the effect that L_s , L_b , B_{O_s} and B_{O_b} are gross averages over a region of varying reactivity and diffusivity. The reactivity of PGX graphite can be quite unpredictable; however, in the majority of cases of oxidation in H_2O with an excess of H_2 , the reactivity of PGX graphite varies with burnoff in a manner similar to that of purer, more homogeneous graphites, i.e., the intrinsic reactivity increases monotonically with burnoff. A large increase occurs up to 1% burnoff, after which it changes by less than a factor of x2 up to 10% burnoff. The effect of burnoff on diffusivity has not yet been quantified in

Figure 1.5. Effect of network burnoff on the effective diffusion coefficients of $\text{H}_2\text{O}/\text{He}$ in PCX graphite.

our laboratory, but the trend is unmistakable: it increases with burnoff. The local reaction rate in the diffusion-limited temperature regime is proportional to the effective diffusion coefficient, D_{eff} , and the rate coefficient, K : $R_T \propto (D_{\text{eff}}K)^{1/2}$. Thus, the reaction rate inevitably increases with burnoff. This leads to a burnoff profile which becomes skewed toward higher burnoffs near the surface.

The skewedness of the burnoff profile may be expected to be most pronounced at low total burnoff under conditions leading to a relatively large diffusion length, assuming that D_{eff} is reasonably well behaved. Even under these conditions, however, it may be expected that the burnoff will decrease monotonically with distance into the specimen. No data is available to verify this presumption. Diffusivity and reactivity measurements at burnoffs ranging up to $\sim 60\%$ will be made this next quarter to clarify this point.

1.2 Effect of Stress and Different Atmospheres on the Strength and Oxidation Behavior of Nuclear Graphite (M. Eto, F. B. Growcock, J. H. Heiser, III, J. J. Barry, C. C. Finfrock)

1.2.1 Loss of Compressive Strength of PGX and H451 Graphites Oxidized in Different Atmospheres

Several data points were added to the results obtained previously on the strength loss of PGX graphite oxidized in $\text{H}_2\text{O}/\text{He}$, CO_2/He and O_2/N_2 mixtures (Eto, 1980a, 1980b). Figure 1.6 shows the strength loss due to oxidation in a 2% $\text{H}_2\text{O}/\text{He}$ mixture or in air for PGX graphite as a function of total burnoff. Here two data points which were obtained for the specimens oxidized in a 2% $\text{H}_2\text{O}/\text{He}$ mixture without H_2 at 550 to 750°C are combined with the previous data. New data on the relationship between compressive strength and total burnoff were also obtained for mixtures of 2% CO_2/He at 600°C and 700–850°C, and 2% O_2/N_2 at 600°C and 400–550°C; these are shown in Figure 1.7 where in addition to these data strength loss of the specimens cut out of the same log as for O_2/N_2 and CO_2/He is shown for a 2% $\text{H}_2\text{O}/\text{He}$ mixture at 600°C.

The data added in this quarter are in good agreement with previous data, confirming that the strength loss due to oxidation at a given total burnoff is larger for the atmosphere containing O_2 , i.e., 2% O_2/N_2 mixture or air, than for $\text{H}_2\text{O}/\text{He}$ or CO_2/He mixture. The result also indicates that the reaction temperature does not affect the amount of strength loss, at least for the temperature ranges examined in the present experiments, i.e., 600–850°C for CO_2/He , 550–750°C for $\text{H}_2\text{O}/\text{He}$, 400–600°C for O_2/N_2 and 450–550°C for air. The trend observed here is in good agreement with the results obtained by Thrower (1977) on 2020 and H440 graphites oxidized in air. These graphites showed no significant difference in the amount of compressive strength loss over the temperature range of 500 to 800°C (H440 graphite) and 550 to 700°C (2020 graphite). However, it has been reported (Velasquez, 1977) that the tensile strength of H451 graphite oxidized in a $\text{H}_2\text{O}/\text{He}$ mixture depends on the reaction temperature, i.e., a lower temperature gives rise to a greater loss of strength (5.2% loss per % burnoff at

Figure 1.6. Effect of bulk burnoff on compressive strength of PGX graphite oxidized in air or in a 2% H₂O/He (with or without H₂) mixture.

Figure 1.7. Compressive strength of PGX graphite oxidized in a mixture of 2% CO₂/He, 2% H₂O/He or 2% O₂/N₂ at various temperatures.

800°C) compared with that at a higher temperature (3.6% loss per % burnoff at 1000°C).

The lack of agreement among the investigators referred to above might be attributable to differences in the kinds of graphite, stress mode (tensile or compressive) or the temperature itself. Figure 1.8 shows the compressive strength of H451 graphite oxidized in 2% H₂O/He or in air as a function of total burnoff. It should also be noted here that no effect of reaction temperature nor of atmosphere is apparent. Although the temperature range in the experiments utilizing H₂O/He was about 100°C lower than that in the case of Velasquez et al., the mode of stress is the most likely candidate for the variation of the results.

Microscopic examination of H451 and PGX graphites oxidized in various atmospheres has been initiated using a scanning electron microscope (SEM) to investigate the mechanism by which these materials lose their strength. More holes or pits were found on the surface of PGX graphite specimens oxidized in H₂O/He than in O₂/N₂, which suggests that localized oxidation caused the observed smaller strength loss in H₂O/He. However, no clear difference has been found thus far between O₂/N₂ and CO₂/He, which requires us to carry out more detailed microscopic examination of the oxidized specimens.

Understanding of the above results on strength loss might be enhanced considerably if the SEM studies were supplemented with density profiles of larger specimens oxidized under the same conditions.

As was attempted for PGX graphite, values of $\log (S/S_0)$ for H451 were plotted against $\log (\rho/\rho_0)$ for specimens oxidized in a 2% H₂O/He mixture or in air to examine whether or not an equation of the form of $S/S_0 = (\rho/\rho_0)^\alpha$ could be fitted to the data points; here S , S_0 , ρ , ρ_0 and α are the compressive strength of the oxidized specimen, mean compressive strength, bulk density of oxidized specimen, bulk density before oxidation and a constant, respectively. As is seen in Figure 1.9, the strength loss of H451 graphite cannot be expressed very well with the simple equation given above; it will be recalled that this was not the case for PGX graphite, especially for the experiments involving oxidation in O₂/N₂.

1.2.2 Loss of Tensile Strength of PGX Graphite Oxidized in Different Atmospheres

Experiments on the tensile strength of PGX graphite oxidized in different atmospheres have been initiated during this quarter. The results obtained so far are shown in Figure 1.10. Oxidation was carried out in a manner similar to that for compressive specimens, except that specimens 12.7 mm in diameter and 76.2 mm in length were used for the tensile tests. Both ends of each specimen were glued (using epoxy resin) into 12.7 mm diameter x 12.7 mm deep holes of a pair of aluminum chucks. Tensile tests were performed at a strain rate of 5×10^{-4} /sec in the same way as was reported elsewhere (Romano, 1977). Figure 1.11 shows the appearance of several specimens after they were tested. Since the data points in Figure 1.10 are few in number, it is too early to draw any conclusions regarding

Figure 1.8. Effect of bulk burnoff on compressive strength of H451 graphite oxidized in air or a 2% H₂O/He mixture.

Figure 1.9. Plot of $\log (S/S_0)$ versus $\log (\rho/\rho_0)$ for H451 graphite oxidized in air or a 2% H₂O/He mixture. Here, S , S_0 , ρ , ρ_0 are the compressive strength after oxidation, mean compressive strength, bulk density of oxidized specimen and bulk density before oxidation.

Figure 1.10. Effect of bulk burnoff on tensile strength of PGX graphite oxidized in different atmospheres.

Figure 1.11. Tensile specimens 12.7 mm in diameter and 76.2 mm in length after they were tested.

the strength loss suffered after oxidation in O_2/N_2 compared to that obtained in the other mixtures. It is probable, however, that oxidation in 2% H_2O/He to 10% burnoff leads to ca. 45% loss of strength, as was the case for the compressive strength of this graphite. More data will be gathered in the next quarter on tensile, as well as compressive, strength loss in various atmospheres.

1.2.3 Effect of Stress on Oxidation Rate and Strength

As was planned in the previous quarter (Eto, 1980b), the stress rig experiments were carried out at a higher temperature than before, i.e., $660^\circ C$ in a 2% H_2O/He mixture at a flow rate of 300 sccm. The stress during oxidation ranged up to ca. 38% of the mean compressive strength for compressive specimens and ca. 43% of the mean tensile strength for tensile specimens. No positive effect of stress during oxidation on the reaction rate has been observed so far up to the indicated stress levels.

The data plotted in Figure 1.8 indicate that, for H451 graphite, compressive prestress also does not affect the amount of strength loss, which is the same conclusion drawn previously for PGX graphite.

1.2.4 Activation Energy for Oxidation in a Mixture of 2% O_2/N_2

The activation energy for oxidation of PGX graphite in a 2% O_2/N_2 mixture was estimated from Arrhenius plots obtained over the temperature range of $450-550^\circ C$. A value of 165 ± 7 kJ/mol (90% confidence limits) was obtained, which is about 20 kJ/mol smaller than that for oxidation in air (Eto, 1979); this difference may not be significant, since the specimens were obtained from different logs.

1.3 Elemental Analysis of PGX Graphite (F. B. Growcock, J. H. Heiser, III, C. C. Finfrock)

The high reactivity of PGX graphite toward H_2O/H_2 mixtures has generally been associated with its iron impurity. However, quantitative correlation between the reactivity and the iron concentration has proven to be elusive. Other properties of PGX graphite appear to be important. Since PGX graphite contains significant amounts of other impurities which may play a role in oxidation, it is prudent to examine these first before any other studies are contemplated. Because of their availability, the specimens examined were those used in a study on the effect of burnoff on compressive strength. They were oxidized in 0.17% $H_2O/1.7\% H_2/He$ at $800^\circ C$ and $900^\circ C$ in the "Uniform Flow Apparatus". The specimens (all axial), which were taken from logs 2V4-1 (EL-1) and 29N6-2 (EL-2 and ML-2) and measured 3.8 cm ϕ x 7.6 cm, are mapped in Figure 1.12.

Sixty-two specimens have been analyzed for twelve elements. The elements were chosen on the bases of abundances determined in prior work (Engle, 1978) and expected catalytic activity. However, the procedures used to analyze for aluminum and silicon were found to be faulty; these are currently being redetermined. Atomic absorption spectrometry was used to analyze for all elements except sulfur.

PGX LOG #2
ENDPIECE

PGX LOG #2
MIDSECTION

PGX LOG #1
ENDPIECE

Figure 1.12 Maps showing locations of PGX graphite specimens subjected to impurity analyses.

A 20-gram sample was taken from each specimen, which weighed ca. 150 g, and was ashed in air at 650°C; the ash was then fused with seven times its weight of $K_2S_2O_7$ or Na_2CO_3 , dissolved in acid and analyzed on a Perkin-Elmer Model 403 AA instrument. New 20-gram samples had to be taken for the re-analysis of Al and Si. These are currently being ashed. Sulfur was determined by oxidizing 1-gram samples from the specimens and analyzing the SO_2 so produced by ion chromatography.

The results are listed, in order of increasing total burnoff, in Tables 1.3, 1.4 and 1.5. Also listed are values of the parameter R_N , which is proportional to the observed experimental rate at a fixed temperature, assuming the efficiency factor, η , to be relatively constant. It is defined by

$$R_N = \frac{\rho RT}{12P_{ox} S/V\eta} R_T$$

and is equivalent to $\langle (D_{eff} K_v)^{1/2} \rangle$; the symbols are defined in Section 1.1. No obvious correlations between R_N and any of the elements listed are apparent, other than that of Fe in the EL-2a and ML-2 sets. The small variation in R_N , which is caused by oxidation occurring in the diffusion-limited regime and the sampling of only one-eighth of each specimen, leads to much scatter.

There are some general trends, however, that serve to separate specimens according to their general location. For example, EL-1 specimens generally have higher concentrations of Ca, Sr, Mg, Cu and S than ML-2 specimens. Likewise, EL-2a specimens generally have higher concentrations of Ca, Sr, Mg and Mn than ML-2 specimens, though the latter have more Ni and S than the EL-2a specimens.

Detailed examination of the data will be made after the re-analyses for Al and Si are complete.

REFERENCES

- BURNETTE, R. D., VELASQUEZ, C., HIGHTOWER, G. and KOYAMA, K., 13th Biennial Carbon Conference, Irvine, California, American Carbon Society (1977).
- DODSON, M. H., UKAEA Development and Engineering Report 148 (CA), 1960.
- ENGLE, G. B. and BEAVAN, L.A., "Properties of Unirradiated Graphites PGX, HLM and 2020 for Support and Permanent Side Reflector LHTGR Components", General Atomic Company, GA-A14646, June 1978.
- ETO, M. and GROWCOCK, F. B., "Strength of Oxidized Nuclear Graphite Subjected to Stress and Prestress", in Reactor Safety Research Programs Quarterly Progress Report, April 1 - June 30, 1979, Brookhaven National Laboratory, BNL-NUREG-51082, 1979.

Table 1.3

Elemental Impurity Concentrations in Oxidized PGX Graphite
 Axial EL-1 Specimens: 0.17% H₂O/1.7% H₂/He at 900°C

Specimen	% Burnoff	R _N , cm/sec	Fe	V	Ti	Ca	Ba	Sr	Mg	Mn	Ni	Cu	S
43	0.33	0.17	437	26.2	57.0	27.3	3.6	61.4	0.00	6.8	4.4	1.60	334
39	1.22	1.06	1044	22.6	51.0	22.6	4.6	55.7	0.00	9.6	5.7	1.70	198
27	1.94	0.98	272	24.6	56.2	38.8	3.2	77.7	1.10	3.6	3.2	0.80	142
47	2.48	1.51	416	28.5	62.6	32.3	3.9	72.6	0.15	6.9	4.7	1.60	238
33	2.67	0.91	92	15.1	33.1	66.2	2.8	155.6	0.00	3.6	2.1	1.00	148
22	2.72	1.06	207	26.5	57.1	37.9	2.9	83.0	0.68	2.6	2.8	0.78	150
29	2.98	1.18	257	29.9	66.1	38.9	5.2	81.9	0.38	3.5	3.1	1.06	97
25	3.20	1.01	17	11.7	26.0	23.9	1.5	18.7	0.54	0.4	1.1	0.89	139
6	3.42	1.27	143	14.9	34.6	56.9	4.0	131.0	0.00	2.3	2.1	0.59	243
23	3.48	1.27	993	28.2	60.8	17.8	2.8	45.9	0.00	6.7	5.4	0.80	157
34	3.67	1.09	501	25.5	56.0	20.1	4.4	53.1	0.38	8.0	5.5	1.24	150
20	3.83	1.26	992	28.5	57.9	17.7	4.6	40.7	2.73	7.1	6.1	1.19	159
37	4.02	1.37	1130	23.2	51.0	16.1	2.1	45.8	0.15	10.1	6.1	1.40	177
7	4.14	1.40	175	20.9	45.8	40.3	3.5	96.9	0.15	2.4	2.5	0.60	109
36	4.40	1.48	1062	25.5	56.2	22.6	3.8	55.7	0.00	8.4	5.4	1.20	189
46A	5.15	1.55	1181	26.5	59.5	22.6	4.1	53.2	0.15	11.5	7.2	1.70	226
42	5.24	1.30	1059	25.5	56.0	21.0	3.0	55.5	0.00	8.9	5.4	1.59	216
40	5.26	1.63	530	9.6	20.7	23.4	20.8	60.4	3.23	8.6	32.3	3.16	213
45	6.30	1.41	240	15.7	34.6	50.4	3.4	111.6	0.15	5.2	2.8	0.97	220
49	7.47	1.55	975	20.0	43.5	28.9	2.8	67.5	0.40	10.5	5.0	1.99	282
46B	8.92	1.70	995	24.5	55.8	22.5	3.2	62.7	0.15	11.5	5.4	1.99	220
28	11.69	1.95	414	26.5	57.9	24.1	2.4	62.5	0.69	6.0	3.9	1.19	148
2	11.92	1.93	1041	27.1	59.6	14.3	4.0	40.2	0.15	7.5	6.0	0.98	99

Table 1.4

Elemental Impurity Concentrations in Oxidized PGX Graphite
Axial EL-2a Specimens; 0.17% H₂O/1.7% H₂/He at 800°C^a

Specimen ^b	% Burnoff	R _N , cm/sec ^c	Fe	V	Ti	Ca	Ba	Sr	Mg	Mn	Ni	Cu	S
86*	0.07	0.17	7	31.3	65.6	32.0	3.1	89.1	0.15	0.2	0.0	0.10	80
83*	0.09	0.27	466	48.6	96.7	17.8	2.8	48.3	1.80	18.2	3.6	1.10	558
85*	0.12	0.22	69	40.1	86.0	173.5	4.3	1242.	6.00	48.0	1.4	1.90	1350
96*	0.12	0.32	77	41.1	84.0	195.5	4.6	1696.	6.13	50.2	1.8	1.99	3910
91*	0.19	0.40	316	55.6	128.0	30.7	3.1	92.3	0.00	7.7	3.2	0.90	296
87*	0.26	0.28	246	44.8	122.0	16.1	2.8	53.2	0.40	5.3	1.8	0.60	94
92*	0.43	0.43	402	22.8	52.5	30.7	4.6	75.2	0.85	9.8	3.6	1.20	398
66	0.50	0.46	127	42.5	98.7	48.5	3.2	116.8	0.30	3.3	0.7	0.40	334
97*	0.55	0.41	235	52.4	105.0	182.3	3.9	1592.	3.13	44.1	2.9	1.19	1280
64	1.1	0.68	434	51.4	103.0	16.1	2.4	63.0	0.40	13.4	3.2	1.30	213
28	1.3	0.54	323	39.2	78.9	31.4	4.5	84.9	0.29	14.4	3.2	0.97	273
10	1.5	NA	454	37.1	66.4	17.7	2.4	40.7	0.00	14.3	2.1	1.09	215
29	1.60	0.82	401	23.2	54.2	25.3	2.1	65.3	0.00	12.5	1.1	1.10	166
31	1.60	0.82	431	20.5	49.0	12.7	3.1	57.1	0.15	14.4	3.9	1.18	142
71	2.10	0.69	399	18.1	39.3	30.7	2.4	101.9	0.15	13.7	1.4	1.00	269
4	2.10	0.94	465	36.7	68.9	19.3	2.1	45.8	0.00	12.7	3.9	0.90	139
52	2.20	0.96	427	25.2	53.7	30.7	2.4	75.2	0.30	12.9	1.8	1.00	154
16	2.90	0.81	449	21.6	47.5	17.8	2.1	45.8	0.15	12.9	3.6	1.00	175
9	3.20	NA	430	30.0	65.6	16.0	2.8	43.1	0.15	14.8	2.1	1.00	139
15	5.0	NA	229	29.1	64.2	46.7	2.4	109.0	0.30	8.1	0.4	0.70	272

^aConcentrations are expressed as ppm, uncorrected for burnoff. All concentrations were determined via atomic absorption spectrometry on ashed 20-gram samples, except sulfur, which as SO₂ was measured via ion chromatography on 1-gram samples.

^bSpecimens marked "*" were obtained from the outer edge of the graphite disk.

^cNA = not available.

Table 1.5

Elemental Impurity Concentrations in Oxidized PGX Graphite
 Axial ML-2 Specimens: 0.17% H₂O/1.7% H₂/He at 800°C†

Specimen	% Burnoff	R _N , cm/sec	Fe	V	Ti	Ca	Ba	Sr	Mg	Mn	Ni	Cu	S
14	0.13	0.19	19	81.7	182.0	11.3	2.1	33.6	0.00	0.7	1.8	0.40	64
38	0.27	0.47	30	62.0	136.0	14.5	2.8	45.8	0.00	0.4	0.7	0.30	46
37	0.42	0.45	132	32.5	78.4	16.1	3.4	45.8	0.00	1.9	3.9	0.60	51
12	1.42	0.81	127	43.6	92.5	19.3	3.8	48.2	0.40	1.6	1.8	0.50	89
46	1.83	1.46	213	52.6	111.0	17.7	4.0	53.0	0.00	2.8	2.9	0.60	56
45	2.05	1.55	433	25.5	55.0	11.3	2.8	31.1	0.00	6.7	4.3	0.80	95
39	2.82	1.50	433	59.3	135.0	9.7	3.8	23.8	0.00	7.2	6.1	0.80	71
44	3.50	2.03	434	26.4	62.5	14.5	2.3	36.1	0.00	6.2	4.7	0.70	98
4	3.86	1.27	417	39.8	83.0	0.0	1.8	11.6	0.00	7.7	10.4	0.70	66
10	4.66	1.49	386	32.5	77.2	9.7	1.8	26.2	0.00	6.0	3.9	0.90	61
32	4.95	1.42	262	51.2	109.0	11.3	3.8	36.0	0.00	4.3	12.2	0.90	68
5	5.0	1.21	432	40.0	83.3	4.8	3.2	9.1	0.00	6.9	5.4	0.80	86
35	5.38	1.23	387	29.8	65.5	9.7	2.4	26.3	0.40	6.7	4.7	1.00	60
34	6.53	2.29	417	28.7	65.5	14.5	2.1	41.0	0.00	6.7	4.7	0.90	90
29	6.66	1.67	433	36.7	78.5	6.4	2.3	14.0	0.30	7.2	5.4	0.90	76
28	6.73	1.79	433	40.0	78.5	4.8	3.5	9.1	0.00	7.2	4.7	0.70	57
2	8.14	1.94	416	36.7	79.8	9.7	2.4	26.2	0.00	7.4	5.4	0.90	51
3	8.47	1.98	412	43.6	84.5	4.5	1.9	19.8	0.00	7.3	5.3	1.02	54

Concentrations are expressed as ppm, uncorrected for burnoff. All concentrations were determined via atomic absorption spectrometry on ashed 20-gram samples, except sulfur, which as SO₂ was measured via ion chromatography on 1-gram samples.

ETO, M., GROWCOCK, F.B. and HEISER, J. H., III, "Stress Induced Strength Loss in Nuclear Graphites", in Reactor Safety Research Programs Quarterly Progress Report, October 1 - December 31, 1979, Brookhaven National Laboratory, BNL-NUREG-51177, 1980a.

ETO, M., GROWCOCK, F. B., HEISER, J. H., III, BARRY, J. J. and FINFROCK, C. C., "Effect of Stress and Different Atmospheres on the Strength and Oxidation Behavior of Nuclear Graphite", Reactor Safety Research Programs Quarterly Progress Report, January 1 - March 31, 1980, Brookhaven National Laboratory, BNL-NUREG-51217, 1980b.

GA, HTGR Fuels and Core Development Quarterly Progress Report Ending February 28, 1978, General Atomic Company, GA-A14863, 1978a.

GA, HTGR Generic Technology Program Fuels and Core Design and Development Quarterly Progress Report for the Period Ending May 31, 1978, General Atomic Company, GA-A14953 (Addendum), 1978b.

GROWCOCK, F. B. and HEISER, J. H., III, "High Temperature Mass Transport Experimental Program", Brookhaven National Laboratory Informal Report, BNL-NUREG-27502, 1980a.

GROWCOCK, F. B., et al., "Oxidation-Induced Strength Loss in HTGR Graphites", in Reactor Safety Research Quarterly Progress Report, January 1 - March 31, 1980, Brookhaven National Laboratory, BNL-NUREG-51217, 1980b.

ROMANO, A. J. and CHOW, J. G. Y., "Proceedings of the Topical Meeting on Thermal Reactor Safety, Sun Valley, Idaho, American Nuclear Society, Conf.-770708, 1977.

THROWER, P. A., "Studies of Mechanical Properties and Irradiation Damage Nucleation of HTGR Graphite", Pennsylvania State University, Progress Report, February 1, 1976 -January 31, 1977, COO-2712-2, 1977.

VELASQUEZ, C., JOHNSON, W., HIGHTOWER, G. and BURNETTE, R., "The Effect of Steam Oxidation on the Strength and Elastic Modulus of Graphite H451", General Atomic Company, GA-A14657, 1977.

2. Materials and Chemistry

This program covers the evaluation of the mechanical properties of metallic materials. The objectives of the work are to critically review available materials data pertinent to HTGR safety, identify areas where information is sparse or unavailable, and to design and initiate experiments to yield data which will permit accurate assessments to be made of materials related safety problems.

2.1 Fatigue of Structural Materials (P. Soo, R. Sabatini, L. Gerlach)

The high cycle fatigue effort on HTGR structural alloys is continuing to investigate a major uncertainty in the fatigue of alloys at high temperature. Currently, little fatigue work is available for in-helium testing and that what is being generated is focussed on low cycle fatigue when the maximum accumulated cycles is only about 10^5 . Little information is being generated in the $10^5 - 10^8$ cycle range, hence high cycle fatigue evaluation is very difficult in many HTGR components.

Chemistries of alloys being evaluated in this program are given below:

	<u>C</u>	<u>P</u>	<u>S</u>	<u>Mn</u>	<u>Si</u>	<u>Cr</u>	<u>Ni</u>	<u>Mo</u>	<u>Fe</u>	<u>Others</u>
Incoloy 800H (Ht. HH7427A)	0.05	NA	0.003	0.67	0.46	19.83	32.17	NA	Bal.	0.65 Cu 0.43 Ti 0.39 Al
Hastelloy X (Ht. 4-2809)	0.11	0.021	0.008	0.50	0.41	20.67	Bal.	8.86	18.66	2.10 Co 0.55 W
2 1/4 Cr-1 Mo	0.14	0.012	0.016	0.57	0.37	2.2	0.16	0.92	Bal.	

NOTE: NA = not analyzed. All concentrations in weight percent.

2.1.1 2-1/4 Cr-1 Mo Steel

Annealed 2-1/4 Cr-1 Mo steel is used in the HTGR stream generator tubing system at temperatures less than about 539°C (1000°F). This restriction is necessary because of the relatively low resistance to corrosion at high temperature. Previous quarterly reports have shown that there is a strong environmental effect on the fatigue strength of 2-1/4 Cr-1 Mo steel (Soo, 1979); Figure 2.1 summarizes the high cycle fatigue data obtained to date. For unaged material the fatigue strength in air is about 20% lower than that in the wet helium environment. However, if the alloy is preaged in the test helium for several thousands of hours, decarburization occurs and a large decrease in fatigue strength results. This is clearly observable in optical metallographic sections of failed specimens (Figure 2.2). In an air test for an unaged specimen (MCF-6) the as-heat-treated structure shows longitudinal alignment of carbides. Since this test was a very short term test (15 min) there is little time for thermally induced structural change. In the case of an unaged specimen (MCF-24) tested in the wet helium environment used in the early stages of this work (which contained

Figure 2.1. Effect of a helium environment on the high-cycle fatigue of annealed 2-1/4 Cr-1 Mo steel at 538°C (1000°F).

Figure 2.2. Sectioned 2-1/4 Cr-1 Mo steel high cycle fatigue specimens showing the effects of preaging and testing in wet HTGR helium on decarburization. Magnifications 30X.

2500 μ atm. H_2O , 200 μ atm. H_2 , 40 μ atm. CO , 20 μ atm. CH_4 , and 10 μ atm. CO_2) structural changes in the steel have clearly occurred. Alignment of the carbides along the stress axis has been changed to a more random distribution. In this case the specimen was tested for 6.3 days prior to fracture, hence, thermal aging and, possibly, cyclic stressing was responsible for the redistribution of carbides. It should be noted that after 6.3 days in the test helium, no evidence for carbide concentrated changes at the specimen surface are noticeable.

In Figure 2.2 metallographic sections are given of two specimens which were preaged, in a stress free condition, in the helium test gas prior to fatigue cycling. For a preaging time of 3000 h there is a considerable amount of decarburization near the specimen surface (MCF-10). The depth of decarburization is approximately 0.6 mm ($\sim$ 25 mil). For the specimen preaged for 6000 h (MCF-16) this depth is about 0.8 mm ($\sim$ 35 mil).

An interesting feature observed in the two preaged specimens is that they both show a strong alignment of carbides in the longitudinal direction. This is in contrast to the unaged specimen (MCF-24) tested in the same helium environment. A plausible explanation is based on the dual effects of stress and thermally-induced structural change. It may be postulated that for unaged material tested for several days the carbide sizes and distributions are relatively small and can easily be changed by the conjoint action of thermal aging and cyclic stressing. This results in a change from the original longitudinal carbide alignment to a more random situation as seen in specimen MCF-24. For preaged samples, however, the aging in the absence of a cyclic stress serves to coarsen the carbide colonies with little change in their longitudinal alignment. Subsequent fatigue cycling is, therefore, ineffective in redistributing the large carbide distributions and the longitudinal alignment is retained.

An attempt was made to determine the nature of the oxidation process of annealed 2-1/4 Cr-1 Mo in wet HTGR helium. In Figure 2.3 sectioned fatigue samples were examined with the scanning electron microscope/microprobe. X-ray line scans were conducted across sections of the samples which were close to the minimum diameter region of the hourglass-shaped specimens. Note that two specimens (MCF-6, MCF-10) were chromium plated to maintain scale integrity during metallographic polishing. In addition, for this element, the detection sensitivity had to be varied from specimen to specimen so that the chromium peaks did not extend beyond the required scale. Conclusions from Figure 2.3 are:

- For short term tests in air (MCF-6) and in wet helium (MCF-24) an iron oxide surface scale is formed. There appears to be little or no concentration of chromium in this scale. There was significant cracking in the iron oxide scale of specimen MCF-24 which could have resulted in the initiation of fatigue crack nuclei.
- For the sample preaged for 3000 h in the wet helium prior to test, two distinct types of scale have formed. The outermost scale is thick and porous and is an iron oxide. Beneath this scale there is a thin, more intact chromium oxide.

Figure 2.3. Scanning electron microscope/microprobe line scans through the surface of annealed 2-1/4 Cr-1 Mo steel high cycle fatigue specimens tested in air and ETGR helium at 538°C (1000°F). Magnifications 500X.

- For the 6000 h aged specimen, the porous iron oxide rapidly spalled during fatigue and only the chromium oxide scale remains. It does not appear to be very protective for the conditions shown, since severe intergranular corrosion is seen.
- There appears to be a tendency for molybdenum to concentrate near the surface for the preaged specimens.
- For the 6000 h aged samples a chromium-depleted area below the chromium oxide scale is formed.
- Carbon concentration gradients cannot be detected with the sensitivity of the scanning electron microscope/microprobe.

The carbide depleted area significantly alters the nature of crack propagation as shown in Figure 2.4. For unaged material there is a uniform rumpling of the fracture surface. In the case of preaged specimens the decarburized regions are much softer and the fracture surface in these regions is quite smooth and featureless. Crack initiation in these soft surface regions should be much easier than in material containing large amounts of carbide.

The environmental effect on fatigue (Figure 2.1) may be generally explained as follows:

- For short term tests at 538°C (1000°F) in the 10^4 cycle range, the high stress levels cause failure to occur so quickly that the nature of corrosion in the two environments becomes unimportant, i.e., failure is predominantly stress dependent.
- For longer term tests at 538°C (1000°F) corrosion becomes more important and the higher oxidation rate in air gives rise to a lower strength compared to the wet helium. This could be caused by the thicker iron oxide layers in air tested material which, because of their high tendency to crack, could accelerate failure or it could be a result of the generally accepted rule that increased oxygen accelerates crack propagation rates.
- For long term preaging in the wet helium there is a serious decarburization problem and the loss in fatigue strength may be simply explained on this basis. For aging times of 6000 h intergranular corrosion is noticed and, for higher stress levels, fatigue cracks are likely to initiate at these regions.

2.1.2 Incoloy 800H Tested in Dry Helium

High cycle testing of Incoloy 800H is proceeding in the "new" drier helium which is similar to that described in Section 2.1, except the water level is maintained between 5-10 μ atm. Testing continues to be under load control conditions using a sinusoidal waveform with a cycling rate of 40 Hz. Stresses are controlled to about $\pm 2\%$ and temperatures to about $\pm 2^\circ\text{C}$.

Figure 2.4. Effect of decarburization on the crack propagation behavior of annealed 2-1/4 Cr-1 Mo steel. Arrows show crack propagation direction. Magnifications 120X.

Figure 2.5 shows the data obtained at this time, and comparisons are made with tests carried out in the old wet helium and in air. Clearly, reduced water/oxygen leads to enhanced fatigue strength. This is in accord with much previous data on a range of metals and alloys.

A disturbing factor in all three curves is the pronounced tendency to show a sharp fall in the fatigue strength at the longer test times (5×10^7 cycles, 15 days). This seems to indicate that metallurgical changes are such that Incoloy 800H may have a fatigue strength that becomes quite low for high accumulated cycles. Additional long term testing is being carried out to examine this phenomenon.

2.1.3 Hastelloy X Tested in Dry Helium

In the Hastelloy X effort the drier helium environment appears to exert only a small effect on strength when compared to the wet helium (Figure 2.6). However, the dry helium does give enhanced fatigue life. Future work will be centered on metallographic evaluation of failed specimens to determine why the environmental effect in wet and dry helium is small when compared to that for Incoloy 800H described above.

2.2 Creep Rupture Properties of Primary Circuit Structural Metals (K. S. Lee, L. Gerlach)

The safety of an HTGR relies on the structural integrity of the primary circuit metals throughout the design life of the reaction. Interactions of primary circuit metals with impurities in the helium coolant may cause the degradation of mechanical properties of these metals. Therefore, the study of these interactions is an important safety consideration. The objectives of our current program are to investigate the long term creep behavior of the primary circuit structural metals in an HTGR helium environment and to evaluate the possible changes in relation to a standard air testing.

The compositions of impurities in helium gas supplied from the Materials Test Loop (MTL) and the chemistries of test alloys are given in Section 2.1.1. The creep testing has been concentrated on Incoloy 800H and Hastelloy X which are used in the steam generator and in the thermal barrier cover plate, respectively.

At the present time all 20 creep testing units are in operation. Eleven tests in the helium environment started during the last quarter when the water level was changed from 2500 to 5-10 μ atm. All nine air tests have been running for over 10,000 hours. The longest test, on Hastelloy X loaded at the 10.0 ksi level at 760°C, has been running for over 21,500 hours and is still in the steady state creep region.

2.2.1 Incoloy 800H

During this quarter, one of the long term creep specimens, which was stressed at the 6.0 ksi level in the air at 760°C, ruptured after 14,809 hours. A specimen tested for the same condition in the old wet helium

Figure 2.5. Effect of test environment on the high cycle fatigue of Incoloy 800H at 760°C (1400°F).

Figure 2.6. Effect of test environment on the high cycle fatigue of Hastelloy-X at 871°C (1600°F).

environment showed a 12,614 hour rupture life. This result confirms earlier conclusions that there is no major effect of wet helium environment on the rupture life of Incoloy 800H at 760°C. Figure 2.7 shows the microstructures near the surfaces of both specimens. The surface oxide scales are adherent. The thickness of these scales and the depth of intergranular oxide penetration are almost the same. The air tested specimen did not show any evidence of internal oxidation of Al on the grain boundaries when examined by SEM microprobe analysis. This result is similar to that obtained for the old wet helium test environment.

The testing conditions in the new drier helium environment have been selected based on the data obtained from the wet environment in order to make a direct comparison between two water levels. Since most of the new tests are in the early stages of creep, information on rupture life and minimum creep rate are not yet available.

Preliminary results which compare the creep behavior in the air, wet and dry helium environments is illustrated in Figure 2.8. These data are from short term tests; i.e., 25.0 ksi at 649°C (1200°F). The dry helium tested specimen has not ruptured, but its creep curve indicates that it is in the tertiary creep range. Compared to the air and the wet helium environment, the improvement of creep properties in the dry helium is clearly apparent from these data. Detailed analysis of the improvement of creep properties in the dry helium environment will be conducted when additional results become available.

2.2.2 Hastelloy X

To date, two Hastelloy X specimens have ruptured in the dry helium environment. The results of the first specimen tested at 761°C (1400°F) at 16.0 ksi have been described in the previous progress report. No changes in rupture life and minimum creep rate have been observed for this test condition. The second specimen loaded at 8.0 ksi at 871°C (1600°F) has ruptured after 1578 hours. The complete creep curve is shown in Figure 2.9 along with tests for air and the wet helium environment. The rupture life increases by a factor of about three and the minimum creep rate improves by an order of magnitude in the dry helium environment. However, the rupture ductility 12.6% is significantly lower than the 40% and 47% for the air and wet helium tested specimens.

Figure 2.10 compares the longitudinal cross section of fractured specimens for the air, wet and dry helium environments. All specimens show that grain boundary cavity formation is the major mechanism of failure. Although it appears that the number of cavities is less in the dry helium tested specimen, no large difference could be observed. Microstructures near the surface are shown in Figure 2.11. Only the air tested specimen shows the dissolution of small precipitates underneath the oxide scale.

It seems that the fracture of the oxide scale is less in the wet helium environment. This is probably due to the fact that the rupture time was long enough to replace the spalled scale.

Figure 2.7. Microstructure of Incoloy 800H stress rupture tested at 6 ksi at 760°C. (a) in air, tr = 14,809 hr, (b) in wet helium, tr = 12,614 hr. Magnifications 500X.

Figure 2.8

Figure 2.9

Figure 2.10. Longitudinal cross sections of Hastelloy X stress rupture tested at 8 ksi at 871°C. (a) in air, tr = 637 hr, (b) in wet helium tr = 627 hr, (c) in dry helium, tr = 1578 hr. Magnifications 100X.

Figure 2.11. Microstructure of Hastelloy X rupture tested at 8 ksi at 871°C. (a) in air, tr = 637 hr, (b) in wet helium, tr = 627 hr, (c) in dry helium, tr = 1578 hr. Magnifications 500X.

SEM microprobe analyses were carried out on oxide scales and comparisons are made in Figure 2.12. A Cr rich Cr/Mn oxide scale is formed in all three test environments. The oxygen potential of the new drier environment has been changed significantly by changing the ratio of p_{H_2}/p_{H_2O} . However, oxide formation for Cr, Si, Mn and Ti are still thermodynamically possible. Only Fe has been changed to a reducing condition in this new environment. The depletion of Cr underneath the oxide scale was not observed in the dry environment, probably because of diffusion of Cr from matrix to this zone.

The increase in rupture life and the loss of rupture ductility in the dry environment suggests that the material has been strengthened during test in this environment. The possible strengthening mechanism is thought to be carburization. However, carburization was not detected by the techniques used so far. Bulk carbon analysis and interference micrograph techniques will be used when more specimens from the new environment become available.

2.3 Helium Impurities Loop (C. A. Sastre)

The modifications and testing of the loop and its instrumentation have continued during this quarter.

Mixtures of He, CO, CO₂, CH₄ and H₂ have been held in the HIL at room temperature for a period of 400 hours without detectable losses, which confirm the results of leak testing.

A steady water ingress which was suspected from previous experiments has been confirmed. Operating the loop at room temperature, the H₂O concentration was observed to increase steadily at a rate of 0.7 ppm/hour, which corresponds to about 10 micromoles per hour. This source of water appears to be due to the Beckman I.R. gas analyzers. It is not expected that it will interface with experiments, but it should be included in the interpretation of results.

The behavior of the molecular sieve purification system was tested. With the HIL at room temperature, and the molecular sieve isolated and filled with pure helium, mixtures of H₂, CO, CO₂, CH₄ and H₂O in the range of 100 to 1000 ppm each in helium were injected in the loop. The molecular sieve was connected and the gas composition changes followed. Hydrogen was diluted according to the ratio of loop volume to loop plus sieve volume as determined by He pressure changes determination. Therefore, hydrogen is not absorbed in the sieve. Water was reduced below the level of detectability in 2 hours, and CO₂ in 10 min.

The absorption of CO and CH₄ was found to be, reproducible, strictly linear with the pressure, independent of each other, and to reach steady values within a few minutes. For the range of concentrations of interest and within the capability of the measuring equipment, the loop connected to the molecular sieve behaves as if it has a perfect sink for H₂O and CO₂, a volume of 39 lts for He and H₂, a volume of 210 lts for CO and a volume of 87 lts for CH₄. The loss of H₂ from the loop at temperature was noted on the previous quarterly report.

Figure 2.12. SEM microprobe line scan through the oxide scale of Hastelloy X stress rupture tested at 8 ksi at 871°C. (a) in air, tr = 637 hr, (b) in wet helium, tr = 627 hr, (c) in dry helium, tr = 1578 hr. Magnifications 2000X.

REFERENCES

SOO, P., CHOW, J., SABATINI, R. and HARE, J., "Fatigue of Structural Materials", in HTGR Quarterly Progress Report for July 1 - September 30, 1979, BNL-NUREG-51130.

3. Fuel and Fission Products

3.1 Fission Product Migration in H451 Graphite (G. Uneberg, C. A. Sastre, D. G. Schweitzer)

Molybdenum

In continuing our study of the migration of fission products in H451 graphite, several additional molybdenum (Mo_2C) migration experiments were run and analyzed this past quarter. The experimental procedures, apparatus and analyses are the same as described in previous progress reports.

In the last progress report it was stated that the family of curves generated by these experiments extrapolate back to a "virtual source" concentration of about 30 ppm. This is not strictly true and requires some clarification. Some of the data extrapolate back to higher concentrations (as high as 50 ppm) while other data extrapolate to lower concentrations (as low as 11 ppm). This apparent spread in the values of "virtual source" concentrations implies some functional dependence upon the physical properties of the graphite (e.g., grain size, porosity, etc.), since all other parameters are accounted for. Given the range of properties of H451 graphite, this is not surprising. In any case, the "apparent" diffusion coefficients, D , have been calculated by normalizing the data of each individual experiment to its particular "virtual source" concentration, as obtained by extrapolation (see Table 3.1). The results are consistent with an activation energy of 139 kcal/mole (see Figure 3.1).

Table 3.1

<u>D (cm²/sec)</u>	<u>Temp. (°C)</u>	<u>Time at Temp.</u>	<u>Est. Error</u>
5.6 x 10 ⁻⁴	3200	1 hour	± 50%
4.0 x 10 ⁻⁴	3100	33 minutes	± 50%
2.4 x 10 ⁻⁴	3000	8 hours	± 50%
2.8 x 10 ⁻⁴	3000	4 hours	± 50%
1.3 x 10 ⁻⁴	2950	2 hours	± 25%
8.3 x 10 ⁻⁵	2900	1 hour	± 50%
6.9 x 10 ⁻⁵	2850	4 hours	± 30%

The main source of error in the diffusion coefficient arises in the visual match of the normalized experimental profiles to the profiles generated by TAC-2D. Included in this is an error introduced during the normalization process, because the normalization constant is obtained by extrapolation. These errors are estimated and are included with the diffusion coefficients in Table 3.1. It should be noted that systematic errors introduced during the material assay do not have an effect upon the diffusion coefficient, because of the normalization process.

ARRHENIUS PLOT OF MOLYBDENUM DIFFUSION COEFFICIENTS
FREQUENCY PARAMETER..... = $3.879\text{E}+005$
ACTIVATION ENERGY.. = 139.070 kcal/mol
90% CONFIDENCE INTERVAL... 100.708 to 177.432

46

Figure 3.1

Uranium-Thorium

Studies of the migration of a Uranium-Thorium mixture have begun this quarter. An H451 graphite susceptor containing Thorium and Uranium powders mixed stoichiometrically with spectroscopic grade graphite powder (to form UC_2 and ThC_2) has been inductively heated to $3000^\circ C$ for 3 hours. An assay has not yet been performed.

3.2 Fission Product Migration Studies (I. N. Tang and H. R. Munkelwitz)

Experimental work continued during the report period to study the migration of Sr from a heated source to the ambient environment by adsorption on airborne condensation nuclei evolved when an H451 graphite source is heated to high temperatures.

A new release source was designed to better approximate the black-body condition required for accurate temperature measurements. In addition, the new source geometry provides a more tortuous release path for the evolved material. Essentially, the $Sr(NO_3)_2$ -impregnated 2 mm (thick) x 2 cm (dia) discs of H451 graphite are now enclosed in an unimpregnated H451 graphite crucible with a 3 mm diameter sight hole on the cover for temperature monitoring. The temperature-measurement optical path (sight glass and prism) and pyrometer were calibrated using a similar crucible and foils of gold, silver, palladium, nickel and platinum whose melting points are well known.

Two experiments were conducted with the new source geometry to check reproducibility and determine at what temperature most of the material was released. The results of these experiments are shown in Table 3.2. Both runs were made with approximately the same initial amounts of $Sr(NO_3)_2$ and under nearly identical time and temperature conditions. The filters for collecting the particulate materials were exposed to the flowing gas stream during the entire temperature isotherm. At the conclusion of each time period the exposed filter was removed and a new filter placed before raising the temperature.

Table 3.2

<u>Duration</u>	<u>Corrected Temperature ($^\circ C$)</u>	<u>Filter Sr (μg)</u>	<u>Percent Transported</u>
72 min	1150 $^\circ$	< 1	
83 min	1300 $^\circ$	17.4	0.25
122 min	1560 $^\circ$	172	2.48
120 min	1750 $^\circ$	68.9	0.97
85 min	1150 $^\circ$	< 5	0.06
97 min	1300 $^\circ$	< 5	0.06
120 min	1565 $^\circ$	166	1.94
106 min	1750 $^\circ$	87.4	1.02

The total fraction of Sr captured on the filters is remarkably reproducible (2.3-3.7%) for the two experiments performed during this report period and a previous experiment with different geometry but approximately the same initial amount of $\text{Sr}(\text{NO}_3)_2$. It is clear that very little Sr, if any, is released at the temperature (1100°C) that $\text{Sr}(\text{NO}_3)_2$ decomposes into SrO and NO_2 . The majority of Sr release seems to occur at temperatures (1400° to 1600°C) at which SrO reacts with graphite to form substantial amounts of metallic Sr, which then vaporizes. Since the walls of the apparatus (above the heated section) are at room temperature and the gas flow rate is reasonably slow (400 cm³/min), these results indicate that portions of the vaporized Sr have reached the filters in the form of airborne particles. More work is in progress to investigate the transport mechanism and extend the work to other fission products.

REFERENCES

- PETERSON, J. F., "TAC2D. A General Purpose Two-Dimensional Heat Transfer Computer Code", GA-8868, September 1969.
- UNEBERG, G., SASTRE, C. A. and SCHWEITZER, D. G., "Molybdenum Migration in H451 Graphite", in Reactor Safety Research Quarterly Progress Report, April 1 - June 30, 1979, Brookhaven National Laboratory, BNL-NUREG-51082.
- UNEBERG, G., SASTRE, C. A. and SCHWEITZER, D. G., "Molybdenum Migration in H451 Graphite", in Reactor Safety Research Quarterly Progress Report, January 1 - March 31, 1980, Brookhaven National Laboratory, BNL-NUREG-51217.

4. Analytical

4.1 HTGR Safety Code Library (J. Colman)

Table 4.1 is a listing of our HTGR Safety Code Library to date.

Table 4.1

HTGR Safety Code Library

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
CRECT	OP	NP	Corrects data on ENDF/B tape.
LISTFC	OP	NP	Lists data from ENDF/B tape.
RIGEL	OP	NP	Manipulates data on ENDF/B tape.
ENDFB2	OP	NP	Converts data on ENDF/B tape to binary.
FLANGE	OP	NP	Prepares thermal cross section transfer arrays.
GAND2	OP	NP	Prepares fine group fast, resonance, and thermal cross sections from ENDFB2 binary tapes.
GFE2	OP	NP	Prepares fine group fast cross sections from ENDFB2 binary tapes.
MAKE	OP	NP	Prepares fine group fast cross section tape from GFE2 for spectrum codes.
WTFG	OP	NP	Prepares fine group thermal cross section tape from GAND2 or FLANGE for spectrum codes.
PRINT	OP	NP	Reads the fast cross section tape produced by MAKE.

C = In the process of being converted to the CDC 7600.

OP = Operational on the BNL CDC 7600 system under Scope 2.

R = Recently received from General Atomic Co. or Argonne Code Center.

P = General Atomic Co. proprietary code.

NP = Not considered to be a proprietary code.

B = In the process of development at Brookhaven National Laboratory.

L = Recently received from Los Alamos Scientific Laboratory.

I = Inactive.

O = Recently received from Oak Ridge National Laboratory.

Table 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
SPRINT	OP	NP	Reads the thermal cross section tape produced by WTFG.
GGC4	OP	NP	Prepares broad group cross sections from MAKE and WTFG tapes.
INTERP	OP	NP	Prepares broad group cross sections from MICROX output data tapes.
1-DX	OP	NP	Performs one-dimensional, diffusion theory, steady state calculations.
FEVER-7	OP	NP	Performs one-dimensional, diffusion theory, burnup and reload calculations.
TEMCO-7	OP	NP	Computes reactor temperature coefficients from input cross section data.
BLOOST-7	OP	NP	Performs zero-dimensional reactor kinetics calculations.
GAKIT	OP	NP	Performs one-dimensional reactor kinetics calculations.
TWIGL	OP	NP	Performs two-dimensional light water reactor kinetics calculations.
TAC-2D	OP	NP	Performs two-dimensional, transient conduction analyses.
FLAC	OP	NP	Calculates steady state flow distributions in arbitrary networks with heat addition.
POKE	OP	P	Calculates steady state flow distributions and fuel and coolant temperatures in a gas cooled reactor.
PREPRO	OP	P	Prepares input data and source code revisions for RECA code.
RECA	I	P	Calculates time dependent flow distributions and fuel and coolant temperatures in the primary system.

Table 4.1 Cont'd.

Program	Status	Proprietary	Function
CORCON	OP	P	Computes the temperature history and fission product redistribution following a loss of all convective cooling of the core.
SORSD	OP	P	Computes the release of volatile fission products from an HTGR core during thermal transients.
GOPTWO	OP	NP	Analyzes the steady state graphite burn-off and the primary circuit levels of impurities.
OXIDE-3	OP	P	Analyzes the transient response of the HTGR fuel and moderator to an oxidizing environment.
SAMPLE	OP	NP	Propagates uncertainties in probability distributions by Monte Carlo technique.
TAP	OP	P	Calculates the transient behavior of the integrated HTGR power plant.
SORSG	OP	P	Computes the release of non-volatile gaseous fission products from an HTGR core during thermal transients.
DIFFTA	OP	NP	Solves the steady state, two-dimensional diffusion equation by finite element method.
DIFFT1	OP	NP	Solves the time dependent, 2-D diffusion equation by the finite element method.
PDQ-7	OP	NP	Analyzes reactors in up to three space dimensions via diffusion theory.
SOSNCC	OP	NP	Calculates steady state natural convection flow rates during long term loss of power.
OSCIL	OP	NP	Simulates one-dimensional spring mass system for seismic analysis of HTGR core.

Table 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
H-CON1	OP	NP	Calculates one-dimensional heat conduction for an HTGR fuel pin by finite difference method.
HYDRA-1	OP	NP	Computes thermal/hydraulic parameters for single phase liquid due to external heat source.
CONTEMPT-G	OP	P	Simulates temperature-pressure response of an HTGR containment atmosphere to postulated coolant circuit depressurization.
SOSSME	OP	NP	Solves the steady state one-dimensional momentum equation.
HAZARD	OP	NP	Analyzes gas layering and flammability in an HTGR containment vessel following a depressurization accident.
CIRC (formerly JETS)	OP	NP	Calculates fluid dynamics in an HTGR containment vessel following a depressurization accident.
ORIGEN	OP	NP	Solves the equation of radioactive growth and decay for large numbers of isotopes with arbitrary coupling.
MOSBDA	OP	NP	Analyzes experimental data from Mossbauer spectrometer.
INREM	OP	NP	Calculates internal radiation doses.
EXREM	OP	NP	Calculates external radiation doses.
SODEMME	OP	NP	Calculates transient thermal hydraulic aspects of circulating gas systems.
KACHINA	OP	NP	Calculates fully interpenetrating fluid flows using Implicit Multifield (IMF) method, for handling different material fields.
SWIRL-S	OP	NP	Calculates fluid dynamics for two-dimensional steady state recirculating flows.

Table 4.1 Cont'd.

Program	Status	Proprietary	Function
SWIRL-T	B-I	NP	Calculates fluid dynamics for two-dimensional time-dependent recirculating flows.
CHAP-1 (Jan. 1978)	OP	NP	Simulates the overall HTGR plant with both steady state and transient solution capabilities.
EVAP	OP	NP	Calculates evaporation and redistribution of fission products during a temperature transient.
BAAL	OP	NP	Calculates three-dimensional fluid flows at all speeds with an Eulerian-Lagrangian computing mesh.
LEAF	I	NP	Calculates fission product release from a reactor containment building.
SUVIUS	I	NP	Solves the behavior of fission gases in the primary coolant of a gas-cooled reactor.
FYSMOD	I	NP	Calculates the two-dimensional solution of HTGR core blocks subjected to external motion.
NONSAP-C	I	NP	Calculates static and dynamic response of three-dimensional reinforced concrete structures, in addition to creep behavior.
LARC-1	I	NP	Calculates fission product release from BISO and TRISO fuel particles of an HTGR during the LOFC accident for single isotopes.
LARC-2	I	NP	Similar to LARC-1; in addition, handles release from isotope chains.
QUIL/QUIC	OP	NP	Solves complex equilibrium distribution in chemical environments.

Table 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
FENG, RATE and SURF	OP	NP	Three codes which create or add to the reactions data library for QUIL and QUIC codes. Reactions added are of types Free Energy, Rate and Surface, respectively.
RICE	OP	NP	Solves transient Navier-Stokes equations in chemically reactive flows.
THGRAF	OP	NP	Calculates position and velocity of the thermo-chromatograph as a function of time for various models.
FPPROD	OP	NP	Performs simplified fission product production analysis.
LASAN-LASL	I	NP	A general systems analysis code consisting of a model independent systems analysis framework with steady state, transient and frequency response solution capabilities. There are two versions of the code available - the original LASL version and the converted BNL version.
LASAN-BNL	OP	NP	
CHAP-2 (dec. 1978)	I	NP	Simulates the overall HTGR plant with both steady state and transient solution capabilities.
ORECA1	OP	NP	Simulates the dynamics of HTGR cores for emergency cooling analyses.
CORTAP	OP	NP	A coupled neutron kinetics - heat transfer program for the dynamics simulation of the HTGR core.
BLAST	OP	NP	A dynamic simulation of the HTGR reheater - steam generator module.
ORTAP	O/C	NP	A nuclear steam supply system simulation for the dynamic analysis of HTGR transients.

4.2 Application of Available HTGR Safety Codes (P. G. Kroeger)

Our efforts of trial runs with existing HTGR safety codes, to enhance confidence in these codes and to establish their limitations was continued.

The Oreca code was exercised with several variations of input parameters and model assumptions. It was found, for instance, that the gas densities in the pressure drop terms and the temperature dependence of the specific heat in the core energy equation can have a very significant effect on such process parameters as peak core and coolant temperatures. Thus, good data of such properties are essential for reliable code results. Furthermore, simplifications in the modelling of such parameters, for instance to increase speed of program execution, may not be possible in transients where these effects are very essential. A memorandum summarizing the most important results of this investigation has been drafted and will be distributed shortly, with the appropriate suggestions for code improvement.

4.3 HTGR - Gas Turbine Plant Licensing Considerations (P. G. Kroeger)

Development of the exploratory program for HTGR gas turbine plants has continued. First order models of the various components have been entered into a small systems program, having steady state and transient capabilities. Debugging and data selection are currently in progress.

II. LMFBR SAFETY EVALUATION

SUMMARY

Two sets of hydrodynamic dispersion experiments were performed in order to determine the effects of porous gas injector tubes in the test column upon the two-phase flow regime and local void distribution. The experiments were conducted in a pool with an initial liquid depth of 30 cm and over a range of superficial gas velocity (j_g) from 0.01 to 3.6 m/s. The measurements included lateral traverses at various axial coordinates and a vertical traverse along the test section centerline. Preliminary comparison of a sample of the data with and without the dummy injector tubes indicated that the lateral void fraction distribution appeared to be independent of axial location, and there appear to be no significant differences in the void profiles with or without the injector tubes.

The two-dimensional conservation equations for natural convection driven by a constant volumetric heat source were converted to ordinary differential equations by introduction of a stretched coordinate, η , and dimensionless stream function, f . A shooting technique was developed to solve the series of first order equations resulting from the decomposition of the field equations. This technique involved minimizing the RMS error in the difference between the computed and known asymptotic boundary conditions. This resulted in solving fifteen simultaneous ordinary differential equations. The results of the solution were in excellent agreement with the experimental correlations previously reported (Greene, 1980). The values for the local heat transfer agreed within $\pm 6\%$ while the values for the average heat transfer agreed within $\pm 2\%$. These results are preliminary and subject to change.

An NRC-DOE review group meeting was held at Silver Spring, Maryland to discuss the report "Assessment of the Thermal Hydraulic Technology of the Transition Phase of a Core Disruptive Accident in a LMFBR" by G. A. Greene, T. Ginsberg, and M. S. Kazimi (BNL-NUREG-27366). The accident analysis led to the conclusion that fuel dispersal due to volumetric boiling may not be able to prevent recriticality of the fuel due to solid blockages above and below the core. Major uncertainties in the thermal hydraulic phenomena were identified in the broad areas of fuel relocation and freezing, hydrodynamics of volume-boiling pools, and boundary heat transfer phenomena.

The Super System Code (SSC) development program encompasses a series of four computer codes: (1) SSC-L for system transients in loop-type LMFBRs; (2) SSC-P for system transients in pool-type LMFBRs; (3) SSC-W for system transients in LWRs and (4) SSC-S for long term shutdown transients in reactor systems.

During this quarter, under SSC-L code development, investigations of natural circulation cases in damaged LMFBR cores were done. The damage imposed is measured in terms of instantaneous increases to the fuel assembly nominal pressure drop (P). A report describing the plant protection and plant control systems was completed. Modeling of low-heat flux sodium boiling was initiated. Work in LMFBR accident progression analysis is continuing.

Under the SSC-P development effort, natural circulation analysis using the Phenix reactor design is continuing. A report documenting the steady-state and transient modeling for SSC-P is being prepared.

The SSC-W code was applied to analyze the TMI-2 accident during the early stage (single phase in primary) of that transient. It was also applied to analyze natural circulation in B&W-type plants.

Work on the SSC-S code continued in the area of intra- and inter-assembly effects.

During this quarter, work under the SSC code validation effort was performed on the prediction of the FFTF acceptance tests, with SSC-L. A report covering these analyses has been published. Additionally, inter-code comparisons for specific transients are being accomplished using SSC-L/DEMO for CRBRP transients and SSC-L/IANUS for FFTF transients.

1. Thermal Hydraulic LMFBR Safety Experiments

1.1 Hydrodynamic Characteristics of Two-Phase Dispersed Systems (T. Ginsberg, J. Klages, G.A. Zimmer, Y. Sanborn; and J.C. Chen, Lehigh University)

The purpose of this task is to study the hydrodynamic dispersive behavior of two-phase pools under conditions prototypic of transition phase accident circumstances. Adiabatic gas injection simulation experiments are carried out with fluid-pair systems which bracket the hydrodynamic parameters of fuel-steel boiling systems.

1.1.1 Experimental Investigations.

The initial series of hydrodynamic dispersion experiments was designed to aid in the evaluation of the concept of simulation of volume-boiling using distributed gas injection tubes (Ginsberg, 1977). The concern raised was that the injector tubes would alter the two-phase flow and so make interpretation of the results difficult. It was, therefore, decided to evaluate the influence of flow obstructions such as horizontal injection tubes on the two-phase flow patterns in a water-air pool system. The experimental system described previously (Ginsberg, 1978) was used to perform the experiments. The two-phase flow in a pool created by injection of gas through a porous plate was studied using a gamma densitometer and a pressure measurement system.

Figure 1.1 is a schematic representation of the system. Two series of measurements were made. The first was conducted with the test column devoid of flow disturbances. The second series was performed after insertion of an array of 1/4-inch brass rods, situated horizontally, perpendicular to the flow direction. The array configuration is shown schematically in Figure 1.2.

For both series of measurements, i.e., with and without the rod disturbances, gamma-void and pressure profile measurements were made. For a given set of flow conditions, the void fraction measurements included (i) lateral traverses at various axial locations, and (ii) a vertical traverse along the centerline of the front face of the test section. A comparison of the void and pressure profile measurements taken without and with the flow obstructions will enable us to evaluate the influence of the disturbances on the two-phase flow field characteristics.

The first series of measurements was presented in a previous Quarterly Report (Ginsberg, 1980). In that report, void data are presented for a single flow conditions. Data for a centerline, two-phase vertical traverse are presented. Full, empty and two-phase count data were presented along with the computed void fraction profile.

During the present quarter the two series of experiments described above were completed. Pressure and void profile data have been obtained both without and with the flow disturbances. Data were obtained for a pool with a collapsed liquid level (pool height with no gas flow) of 0.3 m. Superficial air velocities (j_g) ranged from approximately 0.01 - 3.6 m/s, corresponding to dimensionless vapor fluxes, j_g/U_∞ , from 0.05 - 14, where U_∞ is the air bubble rise

velocity in an infinite medium.

The bulk of the acquired data has not yet been analyzed. Conclusions as to the influence of the flow obstruction rods on the two-phase flow field cannot, therefore, be made with any degree of certainty. At present, therefore, only a sample of the experimental results will be presented.

Figures 1.3 and 1.4 present lateral void profiles measured at four axial measurement locations. They represent the variation of the time-average of the chordal-average void fraction with lateral position across the test column. The circles represent the average of 100 individual void fractions, computed on the basis of 100 sets of count data of 0.2s intervals for each count. The "X"-symbols represent the void fraction computed on the basis of the total recorded counts within the time span of 20s. The dimensionless vapor flux was $j_g / U_\infty \approx 10$ and the collapsed liquid level was approximately $H_0 = 0.3$ m. The error bars shown on the plots represent the standard deviation of the population of 100 individual void fractions. [It is noted that the standard deviation of the average void fraction would be a factor of 10 (i.e., $\sqrt{100}$) smaller than those indicated in Figures 1.3 and 1.4].

Figure 1.3 shows data taken with the test section free of disturbances, whereas Figure 1.4 represents data taken with the flow obstructions in place. Preliminary inspection of the above data indicates the following:

- (i) The lateral variation in void fraction appears to be independent of axial location.
- (ii) Local void fractions, near the center of the column of up to nearly 0.80 are observed in the churn-turbulent (visually-determined) flow regime.
- (iii) There appear to be no significant differences in the void profiles of Figures 1.3 and 1.4, i.e., with or without the presence of injection tubes under the conditions of this experiment.

A more detailed presentation and analysis of the data will be presented in the following Quarterly Report.

1.2 Heat Transfer in Internally Heated Pools (G.A. Greene)

The purpose of this task is to study and characterize local boundary heat transfer and void distributions in volume-heated pools. Experiments have been performed for boiling and non-boiling pools with vertical and inclined boundaries. Models have been developed for local and average, boiling and non-boiling heat transfer correlations, and local void fraction distribution.

This quarterly report presents the comparison between the boundary layer heat transfer model solution and the empirical correlation of the non-boiling pool boundary heat transfer data which was presented in the January-March 1980 quarterly report.

1.2.1 Similarity Solution.

The problem of natural convection along a vertical or inclined flat plate with a uniformly distributed volumetric heat source in the fluid was considered. By performing an order of magnitude analysis and neglecting small terms, the conservation equations for mass, momentum, and energy were derived (Advanced Reactor Safety Quarterly Progress Report, October 1 - December 31, 1979, Equations 1.3 - 1.5).

By the introduction of the stretched boundary layer coordinate and stream function as shown below

$$\eta = y Ra^{1/4} / x \quad (1a)$$

$$u = \partial \psi / \partial y \quad (1b)$$

$$v = -\partial \psi / \partial x \quad (1c)$$

into the field equations, and the introduction of the dimensionless stream function, f ,

$$f = \psi Ra^{1/4} \quad (2)$$

the boundary layer equations were transformed as follows:

$$\text{momentum} \quad f''' + \frac{5}{4} f f'' - \frac{3}{2} (f')^2 + \theta = 0 \quad (3a)$$

$$\text{energy} \quad \theta'' + \frac{5}{4} Pr f \theta' = - Pr / Ra^{1/2} \quad (3b)$$

These equations are subject to the following boundary conditions:

$$f = f' = 0, \quad \theta = \theta_w ; \quad \eta = 0 \quad (4a)$$

$$f' = \theta \rightarrow 0 ; \quad \eta \rightarrow \infty \quad (4b)$$

It can be easily shown that solution to Equations 3a and 3b subject to the boundary conditions 4a and 4b constitutes the solution to the boundary heat transfer as follows:

$$\frac{Nu}{Ra^{1/4}} = \frac{\theta'(0)}{\theta(0)} \quad (5)$$

1.2.2. Solution and Comparison to Experimental Data.

A technique to solve Equations 3a and 3b was developed. Reduction of the equations to a system of five first order equations required two additional boundary conditions at $\eta = 0$. Replacement of the asymptotic boundary conditions (at $\eta \rightarrow \infty$) by guessed at initial conditions, coupled to an iterative scheme to revise the initial conditions by minimizing the mean square error in the asymptotic conditions, reduced the problem to a linear system of fifteen first order,

initial value differential equations.

Appropriate experimental parameters, such as the Prandtl number and θ_w , were input to the model. The solution to the boundary layer equations, as indicated in Equation 5, is presented below:

The heat transfer correlation coefficient indicated represents $Nu/Ra^{1/4}$.

	MEASURED	CALCULATED
AVERAGE CORRELATION COEFFICIENT	.158 $\pm$.013	.161
LOCAL CORRELATION COEFFICIENT	.188 $\pm$.026	.201

The values for the average heat transfer agree within $\pm 2\%$ while the values for the local heat transfer agree within $\pm 6\%$.

These calculated values are based upon a "sample calculation" and are to be treated as preliminary. As a result, these values are subject to change. Attempts are underway to extend this analysis to the case of boiling pools.

1.3 Assessment of the Thermal Hydraulic Technology of the Transition Phase of a Core Disruptive Accident in a LMFBR (G.A. Greene, T. Ginsberg)

On April 22, 1980, an NRC-DOE review group meeting was held at Silver Spring, Maryland to review BNL-NUREG-27366. Extensive presentations were made by G. Greene and T. Ginsberg on the transition phase technology assessment and scoping calculations, respectively. The major conclusions resulting from this technology assessment review are presented below: section 1.3.1 deals with the accident analysis and section 1.3.2 deals with the transition phase phenomenology.

1.3.1. Accident Analysis.

The likelihood that a LOF accident would develop along a transition phase accident path is design-dependent. Previous analyses of the LOF accident in the case of FFTF suggested the likelihood of a transition phase path. The likelihood of a transition phase path for the homogeneous-core CRBR is an unresolved issue.

Transition phase recriticality events cannot be ruled out on the basis of fuel dispersal immediately following fuel disruption. The mechanisms of fuel-pin disruption are poorly understood. The period of transition from intact geometry to a molten fuel configuration cannot be characterized at this time. Fuel collapse during this period, therefore, cannot be ruled out.

Early termination of the transition phase accident progression by fuel blowdown to the sodium plena is unlikely. Material freezing in the subassembly blanket structure is the major factor in preventing blowdown.

Steel relocation and subsequent blockage formation is likely in an FFTF-type system, where sufficient time is available. Fuel dispersal would be impeded until the steel blockages melt out. Subsequently, molten fuel and steel would be ejected into the blankets. In a CRBR-type system, fuel and steel relocation would occur and, nearly simultaneously, molten fuel and steel would be ejected into the blankets. In both systems, blockages would likely occur and would impede further fuel relocation. Available evidence suggests that the molten material would not penetrate the entire flow path to the sodium plena. Further relocation would have to await blockage meltout.

Entrapped molten pool configurations are likely to exist during portions of the accident sequence. The issue of molten pool compaction during these portions of the progression cannot be avoided. Blockages are likely to confine molten pools of fuel and steel to the core region for some period of time during the accident. These pools would experience several stages:

- (i) Single-subassembly scale, characterized by large heat losses to yet-intact subassembly walls.
- (ii) Steel melt-in stage, with potentially large heat transfer to cold molten duct wall steel.
- (iii) Large core-wide pools, characterized by low surface to volume ratio and, relatively low heat losses.

The transition phase accident path is likely to progress from a stage of spatially incoherent fuel motion, while duct walls are still intact, to that of coherent core-wide fuel motion. Power transients, moreover, can provide a source of core-wide coherency to fuel motion whether or not duct walls are present.

Potentially, large quantities of molten fuel would be available within the core region. The potential for coherent core-wide fuel motion implies that re-criticality energetics must be carefully considered during this phase of the accident. Estimation of such energetics was beyond the scope of the work.

1.3.2 Transition Phase Phenomenology.

1.3.2.1. Fuel Relocation and Freezing.

Experimental and analytical investigations of the past few years suggest that the mode of molten fluid freezing in pin bundle geometry is dependent upon wall melting conditions. If the walls are non-melting, then the evidence suggests that the rate of molten fluid freezing is limited by conduction heat transfer through the frozen solid layer. It has also been noted that entrained voids and particulates can influence the freezing rate under non-melting wall conditions. Under conditions of melting walls expected during the flow of mol-

ten fuel through subassembly pin structure, the freezing rate depends upon whether the frozen solid layer, or crust, is stable or not. If it is stable and effectively insulates the flowing fluid from the steel heat sink, then the freezing rate is conduction limited, as described above. Under these conditions molten fuel would be expected to penetrate distances exceeding the length of the pin structure.

Simulation experiments using thermite mixtures suggest that molten fuel would penetrate $\sim .40$ m into pin structure flow channels before freezing terminates the flow. These experiments, moreover, indicate that the driving pressure and mixture void content have little influence on the extent of penetration. The mechanisms of freezing are not yet understood. A number of mathematical models have been proposed, however. These models which combine assumptions of unstable fuel crusts and heat transfer from molten fuel directly to molten steel appear to characterize the thermite data most closely. Fundamental uncertainties remain:

- (i) The basic mechanisms of molten fuel freezing in blanket structure.
- (ii) Criteria for fuel crust stability.
- (iii) The adequacy of thermite as a molten fuel-steel simulant.
- (iv) The effects of suspended particulate and entrained voids.
- (v) The effect of driving pressure.
- (vi) The influence of geometry.
- (vii) The effect of mass of molten fluid available for ejection.

1.3.2.2. Hydrodynamics of Volume-Boiling Pools.

The steady-state behavior of single-component volume-boiling pools has been studied in a number of simulation experiments. The available evidence suggests:

- (i) The pool-average void behavior of volume-boiling systems can be adequately characterized by one-dimensional drift-flux models. Available evidence for void distribution do not agree with the same models.
- (ii) The bubbly flow regime is stable to greater vapor velocities than predicted by available transition criteria.
- (iii) The churn flow regime is the dominant regime at vapor fluxes corresponding to decay heating power levels. Dispersed flow is not to be expected under these conditions.

Little information is available on the dynamics of volume-boiling multi-component systems under steady-state or transient conditions. Unresolved issues include:

- (i) Distribution of, and possible separation of, the vapor-producing steel from the heat generating fuel.
- (ii) Interaction of boiling fluid with the entraining cold steel from duct wall melt-in.
- (iii) Void dynamics models and flow regime transition criteria.

1.3.2.3. Boundary Heat Transfer Phenomena.

Correlations for local heat transfer to bounding vertical walls of boiling systems have been developed. The mechanism of heat transfer is flow regime dependent. In bubbly-flow, the mechanism has been identified as boundary layer flow driven by bubble-rise induced fluid circulation. The correlations, originally developed for post-accident heat-removal conditions, must be extrapolated to transition phase conditions. Only limited data for churn-turbulent pools are available, and the heat transfer mechanism is as yet not understood.

REFERENCES

- GINSBERG, T., et al., "Reactor Safety Research Programs Quarterly Progress Report for April 1 - June 30, 1977," BNL-NUREG-50683 (August 1977).
- GINSBERG, T., et al., "Reactor Safety Research Programs Quarterly Progress Report for October 1 - December 31, 1977," BNL-NUREG-50785 (February 1978).
- GINSBERG, T., et al., "Reactor Safety Research Programs Quarterly Progress Report for October 1 - December 31, 1979," (in press).
- GREENE, G. A., GINSBERG, T., and KAZIMI, M. S., "Assessment of the Thermal Hydraulic Technology of the Transition Phase of a Core-Disruptive Accident in an LMFBR," BNL-NUREG-27366 (February 1980).
- GREENE, G. A., et al., "Reactor Safety Research Programs Quarterly Progress Report for January 1 - March 31, 1980," (in press).

Figure 1.1. Schematic of Test Vessel (Front View) Showing Inserted Brass Rods (Typical).

Figure 1.2. Schematic of Test Vessel (Side View) Showing Inserted Brass Rods. Axial Rod Spacing = 0.50 in. Lateral Rod Spacing = 0.62 in.

Figure 1.3. Lateral Void Profiles. No Flow Obstructions. $H_0 = 0.3$ m. $j_g / U_\infty = 10$.

Figure 1.4. Lateral Void Profiles. With Flow Obstructions. $H_0 = 0.3$ m. $j_g / U_\infty = 10$.

2. SSC Code Development (J. G. Guppy)

The Super System Code (SSC) Development program deals with the development of an advanced thermohydraulic code to simulate transients in LMFBRs and LWRs. During this reporting period, work on four codes in the SSC series was performed. These codes are: (1) SSC-L for simulating short-term transients in loop-type LMFBRs; (2) SSC-P which is analogous to SSC-L except that it is applicable to pool-type designs; (3) SSC-W for simulating short-term transients in LWRs and (4) SSC-S for long-term (shutdown) transients occurring in either loop- or pool-type LMFBRs or LWRs. Reference is made to the previous quarterly progress report (Guppy, 1980) for a summary of accomplishments prior to the start of the current period.

2.1 SSC-L Code (J. G. Guppy)

2.1.1 Natural Convection in Damaged LMFBR Cores (J. G. Guppy)

Damage to fuel assemblies can be assumed to occur in various ways: (1) blockages at the assembly inlet orifice zone; (2) blockages through the heated zone via fuel swelling, bowing, etc.; or (3) blockages at the assembly outlet. Although the detailed responses of the flow rates and temperatures of the affected assemblies to these blockages depend on the actual blockage location, these influences of blockage location on the attained peak core assembly temperatures have been seen to be minor and can be considered as second-order effects.

For the purposes here, the postulated blockages are assumed to occur within the heated zone of the fuel assemblies. Both the extent of the blockage within an individual assembly and the number of fueled assemblies damaged within the core are investigated. To provide a normalized method of comparison, the extent of damage to an assembly as simulated in the SSC-L code is quantified by the amount of damage required to instantaneously raise the total assembly pressure drop (ΔP_A) by a certain factor. To determine an upper bound on the extent of damage required to significantly affect the core integrity, the amount of damage necessary to cause the sodium coolant to reach saturation temperature ($\sim 1200\text{K}$) in the affected fuel assembly was investigated.

The postulated damage to the various groupings of average fuel assemblies is assumed to occur under loss-of-electric-power (LOEP) and resultant natural convection conditions. A four channel representation of the CRBRP core is utilized. The four channels simulated here represent: (1) one channel for the various numbers of average fuel assemblies to which damage occurs; (2) one channel for the remaining undamaged average fuel assemblies; (3) average blanket channel (150 assemblies) and; (4) average control channel (19 assemblies). The use of both channels 1 and 2 to represent average fuel assemblies is done to provide a means of comparison for identical assemblies for damaged and undamaged conditions. For the purpose of this analysis, the CRBRP Reference Fuel Management Scheme (RFMS) core design (CRBRP, 1979) is utilized.

Increases to the corresponding average fuel assembly ΔP_A by factors of 1.0 (i.e., no damage), 1.88, 3.0 and 4.0 are considered. The number of assemblies assumed to be damaged is also varied, with groups of one assembly, seven assemblies (one CRBRP cluster) and forty-two assemblies (six CRBRP clusters) considered.

The peak maximum coolant temperatures reached in the various damaged average fuel assemblies are indicated in Table 2.1 for the cases studied. Typical of these results are the transient responses of the maximum coolant temperatures in the damaged channel as shown in Figure 2.1 for the seven assemblies.

As evidenced in Figure 2.1, an increase in the assumed damage in the fueled region leads to correspondingly higher peak coolant temperatures due to lower assembly flow rates. Typical of this type of natural convection transient, the lower flow rate produces not only higher temperatures, but also delays the time at which the second peak occurs (from 160 seconds for no damage case, out to 300 seconds for $\Delta P_A \times 4.0$ case).

No. of Assemblies Damaged	Peak Temperature (K)			
	$\Delta P_A \times 1.0$	$\Delta P_A \times 1.88$	$\Delta P_A \times 3.0$	$\Delta P_A \times 4.0$
1	912	1010	1115	1184
7	912	1009	1113	1181
42	912	1002	1103	1167

Table 2.1 Peak Coolant Temperatures Attained in Damaged Fuel Assemblies During LOEP Event

The transient system response for the other damage cases studied (groups of 1 damaged assembly and 42 damaged assemblies) are quite similar. The peak maximum temperatures attained are summarized in Table 2.1. As noted, even for cases where the assumed damage caused the initial ΔP_A to be increased by a factor of 4.0, the sodium in the affected average fuel channel did not reach saturation temperature.

Table 2.1 also indicates another interesting result. For cases where the assumed damage to the individual assemblies is identical, the peak temperature attained decreases as more assemblies are involved. This would not be entirely obvious at first glance since competing factors influence this response, namely: (1) increasing the number of assemblies assumed damaged leads to a higher overall resistance and consequently lower available loop and total vessel flow available; (2) as more assemblies are damaged, a higher percentage of the core will be at elevated temperatures, which means that more flow will redistribute in a relative sense to the damaged assemblies due to buoyancy driven flow redistribution effects; and (3) since all assemblies are forced by

Figure 2.1 Response of Maximum Coolant Temperature In Average Fuel Channel (Cluster of Seven Assemblies) To An LOEP Event With Assumed Damage

design to have identical pressure drops, as a consequence of momentum and continuity relationships, inclusion of more damaged assemblies provides a higher percentage of the available total flow to these assemblies. The results of this study show for the cases analyzed, that the combined effect of the second and third influences dominate over the first, such that the peak attained temperature decreases when the number of identical assemblies assumed damaged is increased. These results also emphasize the need for an analytical tool such as SSC-L, which can simultaneously account for the complex inter-coupling of the many competing physical phenomena modeled to properly calculate transients of this nature.

2.1.2 Plant Protection and Control Systems (M. Khatib-Rahbar, S-C. Dang)

Plant Protection and Control System (PPS-PCS) modeling and coding efforts were completed during this quarter. Testing of the steam generator controllers is just starting. A report describing PPS-PCS models, along with a user's guide was completed and is now in print. Extensions and modifications of the present approach to light water applications (SSC-W) will be accommodated as the need arises.

2.1.3 Low-Heat Flux Sodium Boiling Model (M. Khatib-Rahbar)

Work has just begun on the development of a fast running multi-channel sodium boiling model to be incorporated into the SSC-L and SSC-P codes for study of low-heat flux (post-scrum) natural circulation transients. The model will account for the frictional and acceleration pressure losses due to the two-phase flow using an empirical slip flow model representation, i.e., Lockart-Martinelli. The flow diversion model due to the vapor thermal expansion effect will most probably be neglected in the initial version, while the impact of this effect will be investigated for future modification.

2.1.4 Steam Generator Modeling (S. F. Carter, W. C. Horak, R. J. Kennett, G. J. Van Tuyle)

Much of the effort in this period was concentrated on simulating the TMI event, and otherwise exercising the SSC-W version. Several of the recent changes made in the generic steam generator were used in these runs. Corrections have been incorporated into the recently generated Cycle 33 of the SSC-L program library.

The steam generator development has reached a point where a clean-up effort is underway before any further major modeling modifications are undertaken. The anticipated result is a final version (highly modular, structured, readable, and documented) of the few pressure, homogeneous equilibrium model. Improvements anticipated over the longer time-frame include a single pressure model, which would be faster, simpler, and desirable for SSC-S and a drift flux model, which would improve representation of the steam generator level at low flows.

2.1.5 LMFBR Accident Progression Analysis (K. Jamali)

A Monte Carlo approach has been adapted for the probabilistic determination of dominant minimal cut sets in the UPICO (Uncertainty Propagation In Cut Set Ordering) computer program. Presently, UPICO is capable of performing 1000 Monte Carlo trials of up to 2000 minimal cut sets generated by the WAM-CUT code (see Figure 2.2 and Table 2.2). The limit of 1000 Monte Carlo trials will be substantially raised in the near future. The accuracy of the Monte Carlo technique will be tested by comparison with of the theoretically predictable means and possibly some other parameters as well. The functions of the UPICO code were described in the previous Quarterly Progress Report (Guppy, 1980).

The CRBRP-SHRS fault tree in both detailed and lumped forms is now ready for input into the WAM-CUT, LUMP (Guppy, 1980), and UPICO computer programs. The electrical system fault tree had to be separately analyzed for its interaction with the SHRS in order to remove the (significant) sources of dependency among the different bus-faults (e.g., loss of vital 480 VAC bus 12N1E042A).

This analysis can be carried out in two ways. The first approach is to include the entire electrical system fault tree in the analysis. This process is more concise but time consuming. An alternative approach is to accept a greater degree of approximation. This can be done by removing the significant dependencies (such as loss of offsite power or diesel generators) and approximating the remaining portion of the failures by pseudo-component faults as shown in Figure 2.3. If the total bus availability is known (through experience or by prior calculations), the pseudo-component unavailabilities can be readily estimated if they are assumed independent.

A more detailed analysis showed that the pseudo-components are often (weakly) dependent failures. The dependence among failures can, however, be approximated using bounding techniques similar to WASH-1400.

The event-lumping technique (Guppy 1980), will be tested by comparing the separate analyses of the detailed and lumped fault trees results.

2.2 SSC-P Code (I. K. Madni)

2.2.1 Steady-State Natural Convection at Low Power (I. K. Madni and E. G. Cazzoli)

The code is being used to simulate the long term, low power thermal response of the Phenix reactor core under natural convection cooling conditions. These conditions are reached by scramming the reactor from full power, and allowing the decay heat to remain constant at a given fraction of full reactor power.

2.2.2 Model Documentation (I. K. Madni and E. G. Cazzoli)

A report documenting the steady-state and dynamic modeling for SSC-P is being prepared.

Figure 2.2
Primary Event Names and 90% BOUNDS
Uncertainties

Table 2.2 Input/Output Description of LUMP, WAMCUT & UPICO Codes

Code	Input	Output
LUMP	<ol style="list-style-type: none"> 1 Unlumped Primary Events (Directly from Fault Tree) 2 90% BOUNDS of the unlumped events 3 Names of the lumped events (under the OR Gates) 	Names and probability arrays of the lumped events
WAM-CUT	<ol style="list-style-type: none"> 1 Partially lumped Fault Tree (Lumped Primary Events) 2 Names and Failure Probabilities 	Minimal Cut-Sets
UPICO	<ol style="list-style-type: none"> 1 Lumped event arrays (from LUMP output) 2 Minimal cut-sets (from WAM-CUT output) 3 Primary event names, and 90% BOUNDS 	<ol style="list-style-type: none"> 1 Most probable ordering 2 Top event sensitivity to the uncertainty of MCS's 3 Orders the significant primary event
	<p>NOTE:</p> <p>UPICO</p> <ol style="list-style-type: none"> 1. Generates log-normal deviates for unlumped primary events 2. Samples minimal Cut-set probabilities and orders them 3. Constructs a (n,2) array for each cut-set and its histogram (of the first M arrays) 	

Figure 2.3 Pseudo-Component Faults = PCFS

2.3 SSC-W Code (J. G. Guppy)

2.3.1 TMI-2 Applications (S. F. Carter, J. G. Guppy, W. C. Horak, R. J. Kennett, T. C. Nepsee, G. J. Van Tuyle)

The initial version of SSC-W was used to simulate the first 18 seconds of the Three Mile Island transient of March 28, 1979. Results from this analysis were presented at a progress report meeting at USNRC in late May. Because the initial version of SSC-W is limited to single phase in the primary loop, it is restricted to analyzing only the very early portion of this transient. During this period, many significant events occurred, making this a very severe and, therefore, a cumbersome transient to analyze.

Figure 2.4 compares the calculated and observed pressurizer pressure versus time. Significant contributions to this behavior come from a loss of feedwater (0-5 seconds), a turbine trip (1-2 seconds), the pressurizer relief valve opening (3 seconds) and sticking open, the turbine bypass opening (6-6.5 seconds), the reactor scramming (8 seconds), and the securing of the letdown flow (13 seconds). As may be readily observed, the results generated using SSC-W are quite good, especially given the preliminary nature of the version used. We have decided to defer further analysis of this transient until a somewhat cleaner and more convenient program library is available.

2.3.2 Natural Circulation Applications (W. C. Horak, J. G. Guppy, M. Khatib-Rahbar, G. J. Van Tuyle)

SSC-W has been applied to analyze natural circulation events for B&W reactor systems. Results of SSC-W predictions of natural circulation events for the B&W TMI-2 plant are discussed. The assumptions for the particular transient presented here were chosen so as to emphasize the effect of system nodalization. The system nodalization used is shown in Table 2.3. The transient analyzed assumed:

1. Loss of all electrical power at 0 sec.
2. Reactor scram at 2.5 sec.
3. No auxiliary feedwater until 40 sec.
4. Rapid primary pump coastdown (inertia at 50%).

Table 2.3 System Nodalization for Natural Circulation Cases

CASE	CORE	PRIMARY PIPING	STEAM GEN. PRI. AND SEC.	TOTAL NODES
1	3	5	32	40
2	16	15	32	63
3	16	75	32	123
4	16	150	32	198

PZR PRESSURE VS. TIME

Figure 2.4 Pressurizer Pressure During Early TMI Event

During natural circulation transients, the flow rates become very small (5% or less) and the frictional pressure losses are drastically reduced (by factors of 100-200). Since the total driving head is now very small (on the order of 1 psi), small differences in the locations of the thermal centers are important. To adequately track the thermal centers, a detailed accounting of the density distribution throughout the system is needed. In tracking the temperature distribution, the proper accounting of transport delays and heat capacity effects is also very important.

Figure 2.5 shows the response of the maximum core coolant temperature to the above described natural circulation transient for the four system nodalization cases. The differences in the temperature traces are a direct consequence of inaccurately defined density profiles throughout the system, particularly for Case 1. As seen, for this transient, Case 1 underpredicted the peak temperature (and the approach to the saturation temperature) by almost 10 K (18°F). The response is seen to be asymptotic as the system nodalization is increased. For transients out to 360 seconds of simulation time, cases 1, 2 and 3 required less than real time for these SSC-W runs on a CDC 7600 machine.

2.4 SSC-S Code (J. G. Guppy)

2.4.1 Inter- and Intra-assembly Effects (B. Chan)

Work with SPAC (Steady-state Porous-body Assembly Code) has been resumed for intra- and inter-assembly thermal/hydraulic analysis. A single blanket assembly with different powers and different flow rates (power 100%-3%, flow 100%-1%) has been run using SPAC. A temperature peaking factor map has been generated from these results. By performing the above analysis for all neighboring assemblies, information on the temperature peaking factor maps for each assembly can be generated. This information will be used in constructing an inter-assembly heat transfer model for SSC. Due to the unavailability of the detailed neutronic peaking factor data for each assembly, which are required input for SPAC, some code modification should be made to SPAC before making any further runs.

PUBLICATIONS:

AGRAWAL, A. K. and KHATIB-RAHBAR, M., "Dynamic Simulation of LMFBR Systems", Journal of Atomic Energy Review, (In Press).

AGRAWAL, A. K., et. al., "An Analysis of the Steady-Power Natural Circulation Experiments in LMFBR Plants", to be presented at ASME 1980 Winter Annual Meeting.

KHATIB-RAHBAR, M., "Modeling of Plant Protection and Control Systems for SSC", Brookhaven National Laboratory Topical Report, NUREG/CR-BNL-NUREG-R-7, June, 1980.

MADNI, I. K., "Transient Analysis of Coolant Flow and Heat Transfer in LMFBR Piping System", Brookhaven National Laboratory Topical Report, 1980, (In Print).

Figure 2.5 Response of Peak Coolant Temperature to Assumed Natural Circulation Event

REFERENCES:

- CRBRP, (1979) Preliminary Safety Analysis Report, With Amendments Through 51, Project Management Corporation (1975-1979).
- GUPPY, J. G., et. al., (1980), "SSC Code Development", in Reactor Safety Research Programs Quarterly Progress Report, April 1 - June 30, 1980, Brookhaven National Laboratory, (to be published).

3. SSC Validation (J. G. Guppy)

3.1 Inter-Code Comparisons of SSC-L/FFTF to IANUS (L. G. Epel)

Four classes of transients that have been run previously at BNL (Perkins, 1979) utilizing the IANUS code have been run successfully with the FFTF version of SSC-L. The four categories are:

- Normal Scram,
- Loss of Electric Power,
- Pipe Rupture, and
- Tornado

and the salient features of each of these will be described in the following four sub-sections.

Normal Scram

The normal scram is initiated by a loss of off-site power, resulting in reactor scram, tripping off all primary and secondary pump motors, and tripping of the DHX fan motors. When the pumps have slowed to pony motor speed, the pony motors maintain constant speed by means of the auxiliary power system. The fans, however, are not reactivated and the inlet vanes and dampers are closed.

There are numerous computed variables that can be compared between the two codes. Among the most interesting, since it depends on many thermal and hydraulic parameters, is the temperature of the sodium leaving the fueled region of the core. This variable depends strongly on post scram reactor power and core flow as well as on the heat transfer modeling in the core. The SSC-L/FFTF results compared very well with the IANUS results; as can be seen in Figure 3.1, the difference in calculated peak temperature is only 5K.

Loss of Electric Power

This scenario is identical to the normal scram for the first minute, until the pumps slow to pony motor speed. Then, since there is no auxiliary power available, the pumps continue to slow and eventually stop. Sodium circulation in the primary and secondary circuits is thus curtailed and maintained solely by thermal buoyancy heads.

This transient has been studied extensively with SSC-L/FFTF and numerous comparisons to IANUS results have been made and reported (Epel, 1980). In general, the core exit temperatures compared well with IANUS results. Any difference was traceable to differences in input preparation or plant design assumptions. For example, the SSC-L/FFTF modeling of the DHX utilized test data from the Liquid Metal Engineering Center which indicated a poorer heat rejection capability than had originally been expected or has been coded into IANUS. This would tend to make the SSC-L/FFTF results somewhat higher than those from IANUS, as was indeed the case.

Figure 3.1 Core Exit Temperature

Pipe Rupture

Four different pipe rupture transients were simulated indicating differences in modeling between SSC-L/FFTF and IANUS. The main difference is in the modeling of flow out of the break itself. SSC-L/FFTF incorporates a model due to (Quan, 1977) while IANUS has a simpler model that generally yields larger break flows and thus leads to higher core temperatures. Another modeling difference relates to the check valve. The SSC-L/FFTF model ignores the inertia of the valve whereas the version of IANUS that was used for this study incorporates consideration of the full dynamics of the valve. The dynamics affect the backflow in the case in which the rupture occurs just upstream of the check valve as seen in Figure 3.2. The larger backflow in the IANUS calculation results in a smaller core flow for the first second or so and hence causes a 22 K higher core exit temperature.

When the rupture occurs downstream of the check valve, the previous effect is circumvented. For the case where the break occurs just after the check valve (double ended), the calculated peak core exit temperatures differ by 67 K because of the difference in break flow modeling. When the backflows predicted by the two codes came into closer agreement (beyond 10-15 seconds or so) the calculated core flows and thus the core exit temperatures were also in better agreement.

The effect of break flow modeling is shown dramatically when pipe ruptures at the reactor vessel inlet are simulated. In one such transient, the rupture was assumed to be double ended, as before. In the other transient the rupture area was assumed to be 0.048 m^2 . This is the largest area permissible that still avoids boiling in the hot channel of the core. The flow through the break is shown in Figure 3.3 for both of these scenarios as predicted by SSC-L/FFTF and IANUS. It is interesting to note that there is good agreement between the two models for the double ended break, at least beyond a fraction of a second. For the small break, however, there is a large discrepancy in break flow for at least a half minute or so. This naturally leads to significant differences in core exit temperature, especially immediately after the pipe rupture occurs.

Tornado

The tornado is similar to the normal scram except that two of the three secondary loops are assumed to be inoperative. Thus, for the first few minutes the afterheat must be removed by pony motor action on three primary loops but only one secondary loop and this was found to be easily accomplished. At 230 seconds, when hot sodium returns to the bottom plenum from the two inactive IHX's, power to all four pony motors is assumed to be lost. The existing negative thermal head results in near-stagnation of the core flow and an ensuing steep temperature rise of the sodium leaving the core until a positive thermal head is re-established and the core flow is enhanced once more. The history of this transient, as computed by SSC-L/FFTF and IANUS is shown in Figure 3.4. The agreement during the first four minutes reflects the behavior discussed in the normal scram scenario plus the fact that both codes yield the same thermal response when the pony motors are running. When power

Figure 3.2 Normalized Backflow Through Checkvalve

Figure 3.3 Normalized Flow Through Pipe Rupture

Figure 3.4 Core Exit Temperature

is turned off at 230 seconds, both codes yield the aforementioned steep temperature rise. The peaks reached differ by about 36 K. This is because the core flow as computed by both codes is quite close to zero at 300 seconds and any difference in the predicted flows leads to large differences in core exit temperature. In other words, at very low flows, the core exit temperature is very sensitive to changes in the flow rate. In spite of the 36 K difference, the predictions of thermal response made by the two codes are reasonably consistent with one another.

3.2 Inter-Code Comparison of SSC-L to DEMO/CRBRP (R. Pyare)

A detailed comparison of SSC-L and DEMO output for pump coastdown to pony motor speed was completed and the pertinent results will be presented in the final report. Here, forced circulation in the primary and intermediate loops was maintained in the later part of the transient by the pony motor.

A natural circulation case for which DEMO results are available was also modeled on SSC-L. The primary, intermediate and tertiary loops were simulated. The reactor was represented by 5 channels: average fuel, hot fuel, average blanket, hot blanket and bypass. In the DEMO code, hot channels are simulated in such a way that average channels are not affected. For the hot channels, only steady state power and flow were matched. Detailed comparisons will be made for the average channels.

In the natural circulation case, electric power was lost to all the pumps at time zero. Both primary and secondary control rods were dropped manually. Main feedwater was lost at time zero and auxiliary feedwater was available 43 seconds later.

Short runs were made and discrepancies at the beginning of the transient were resolved. Longer runs encountered some problems in the steam generator module, especially with the time step calculations. After modifying the time step calculation, a run up to 319 seconds was successfully obtained. Comparisons for the reactor power, evaporator and superheater flow rates were found to be good. However, there were discrepancies in the primary and intermediate flow rates in the natural circulation mode and these are being investigated.

PUBLICATIONS:

EPEL, L. G. and GUPPY, J. G., "Verification of the SSC-L Code for FFTF Transients," ANS Transactions, 34, June 1980.

REFERENCES:

EPEL, L. G., GUPPY, J. G. and KENNETT, R. J., "Preliminary Pre-Test Analysis of FFTF Loss of Power Acceptance Tests", BNL-NUREG-27116, April, 1980.

PERKINS, K. R., et al "Analysis of FFTF System Transients", BNL-NUREG-25560, January 1979.

QUAN, V. and AGRAWAL, A. K., "A Pipe Break Model for LMFBR Safety Analysis", BNL-NUREG-50688, August 1977.