REACTOR SAFETY RESEARCH PROGRAMS

QUARTERLY PROGRESS REPORT

OCTOBER 1 - DECEMBER 31, 1977

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OCTOBER 1 - DECEMBER 31, 1977

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DEPARTMENT OF NUCLEAR ENERGY

BROOKHAVEN NATIONAL LABORATORY, ASSOCIATED UNIVERSITIES, INC.

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FOREWORD

The Reactor Safety Research Programs Quarterly Progress Report describes current activities and technical progress in the programs at Brookhaven National Laboratory sponsored by the USNRC Reactor Safety Research Division. The projects reported each quarter are the following: Gas Reactor Safety Evaluation, THOR Code Development, SSC Code Development, LMFBF and LWR Safety Experiments, Fast Reactor Safety Code Validation, Stress Corrosion Cracking of PWR Steam Generator Tubing, and Technical Coordination of Structural Integrity.

This report is the fifth of a series of quarterly reports which combines the reports of the individual projects mentioned above. The previous reports, BNL-NUREG-50624, BNL-NUREG-50661, BNL-NUREG-50683 and BNL-NUREG-50747 have covered the periods October 1, 1976 through September 30, 1977.

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I. HTGR SAFETY EVALUATION

SUMMARY

The thermochromatograph has been modified to do fission product release experiments from irradiated and thermally failed HTGR fuel kernels.

Code THGRAF has been written to calculate the predictions of three simple theoretical models for the thermochromatograph. The theoretical position of the band formed by a substance in the thermochromatograph depends sensitively on the vapor pressure coefficients A and B and the diffusion exponent α .

The reaction between nonstoichiometric UO_2 and CsI at 850°C results in the evolution of some CsI and I_2 . The iodine yield appeared to be a function of UO_2 stoichiometry.

It has been experimentally determined that the interaction of CsI in air or oxygen at $\sim 600^\circ\text{C}$ with Incoloy 800, Hastelloy X and, probably, stainless steel is related to the oxidation of chromium in these alloys to the VI oxidation state.

The apparatus for studying the interaction of cesium with fused silica has been modified. Measurements of cesium absorption by fused silica at temperatures of 600°C – 1100°C are under way.

CsNO_3 , which is a cesium source in simulated fission product release experiments, has been found to be relatively stable to decomposition up to temperatures of 750°C . In the presence of graphite, significant decomposition occurs at 570°C .

A high temperature mass spectrometer is operational and is being used to study the thermal decomposition of Cs_2O in vacuum.

A continuous flow apparatus was constructed to study aerosol formation during the oxidation of graphite by water vapor at high temperatures.

A new program has been initiated this quarter to experimentally evaluate the behavior of fuels, fission products, control rod materials, and graphite during simulated non-mechanistic core heatup events. An induction furnace has been designed and constructed, and temperatures up to the graphite sublimation range ($>3600^\circ\text{C}$) have been achieved. Some preliminary experiments have been carried out on UO_2 , ThO_2 , and Incoloy 800H (control rod cladding). For the fuel materials, rapid interaction is found to occur with graphite, and reduction to the carbides occurs. It is shown that rapid migration of liquid UC_2 and ThC_2 occurs in graphite even though peak temperatures of $\sim 3300^\circ\text{C}$ are maintained for only a very short time. Metallic constituents of the Incoloy 800H are also found to migrate rapidly through graphite at $\sim 3200^\circ\text{C}$.

High cycle fatigue testing continued on Type 422 stainless steel helium circulator material. Test temperatures were extended to 1000°F to evaluate hot spot conditions. At this temperature it was found that mean tensile stress enhances fatigue life, which is in contradiction to data obtained at 600 and 900°F . Additional work is being carried out to investigate this anomalous behavior. In-helium high cycle tests are being run on Incoloy 800H at 1400°F .

(760°C) and Hastelloy X at 1600°F (871°C). Compared to air tests on the same heats of material it appears that, for shorter duration tests, the helium environment (200 μ atm H₂, 40 μ atm H₂O, 40 μ atm CO, 20 μ atm CH₄, and 10 μ atm CO₂) enhances the fatigue strength. However, at longer test times there appears to be a tendency for the air and helium trends to converge.

Creep rupture testing of Incoloy 800H at 1200°F (649°C) and 1400°F (760°C), and Hastelloy X at 1400°F (760°C) in air and in helium environments obtained from the MTL loop is continuing. Data obtained to date has not demonstrated a strong difference between the minimum creep rates in the helium and air environments. However, for tests run at the same temperature and stresses, both the Incoloy 800H and Hastelloy X specimens failed at an appreciably shorter time. Furthermore, metallographic examination indicates that more voids are formed in both of the specimens tested in helium.

The Materials Test Loop operated continuously and routinely during the quarter. The Ti-Zr hydrogen removal system was added to the purification system and is presently being automated using a new gas chromatograph received during this reporting period. Automatic control of the other impurities will be attempted utilizing a multipoint recorder that has builtin high and low alarms on several terminals.

The effective binary gas diffusion coefficients for H451 and PGX graphites were measured and found to be quite similar, whereas the gas permeability of H451 was found to be 4% to 25% that of PGX.

Scanning electron micrographs of oxidized PGX graphite surfaces reveal that surface development is quite heterogeneous when large amounts of iron are present.

Three sets of carbon monoxide disproportionation experiments were conducted in the Helium Impurities Loop. The first set involved a comparison of disproportionation rates with similar tests done during the previous quarter. In the second set, both CO and CO₂ were injected in such proportions as to simulate the end point of one of the original disproportionation runs, and the process was allowed to continue. The continuation was found not to be a simple extrapolation of the original run. In the third set of tests, however, very little time was allowed to elapse between the interruption of a disproportionation run and its consequent "continuation" by CO and CO₂ injection. In such cases the "continuations" appeared to progress as though they were simple extrapolations of the first part of the runs.

Code EVAP has been used to calculate release curves for several elements and to investigate the effect of various parameters on the flow rate.

A series of flow visualization experiments to test the assumptions and predictions of the codes HAZARD and CIRC in the analyses of gas layering in the CV following a depressurization accident have been performed. To date, most experimental runs visually indicate intense mixing, albeit all scaling parameters have not been adequately simulated. Methods to improve numerical procedures in CIRC are continuing. To date, this specifically includes completion of coding with a variable mesh size.

The turbulent reactive mixing code, CHEMX, has been extended to include in addition to finite rate and frozen chemistry the added option of equilibrium kinetics. Also, the effects of buoyancy on the turbulent exchange mechanisms within jets are described. Results from numerical experiments depicting these two added options to the code are presented.

A subcontract with the University of Illinois dealing with "A Study of Flammability Under the Influence of Large Ignition Sources," has been initiated in this quarter.

Work is continuing on the CHAP code. Problems have been encountered due to inconsistencies between the code and the users manual in the areas of data input and default values. These problems appear reconcilable as were those problems associated with restart options.

Selected results relating the elastic response of the PCRV thermal insulation cover plate to turbulent flow generated noise are summarized.

Work on the project "Permeability and Retentivity of Stressed Concrete Relative to Fission Product Iodine" for the quarter has principally involved: The completion of high temperature sorption experiment No. 5 started in September 1977 (refer to Quarterly Report July through September, 1977); medium temperature sorption experiment No. 6; and source preparation for room temperature sorption experiment No. 7 scheduled to start the first week of January 1978. Concentration profile "fit" parameters and other data from previous sorption work have been included in this current report (Table 1.6) for purposes of comparison.

A comparison between analytical results using OSCVERT and the test results for an eight-block stack in a single column subjected to horizontal displacement input was obtained. Studies on the effect of earthquake duration on the dynamic response of a single-degree-of-freedom system with cubic nonlinearity continued. The design of a three-dimensional test rig is almost completed. The existing OSCVERT computer code for the core block dynamic response studies had been modified to improve the accuracy as well as to reduce the computer running time.

An inelastic time-independent analysis of a bimetallic weldment in the superheater section of the steam generator was completed. Feasibility studies on the development of models to include the parameters of the welding process in the calculation of residual stresses at the welds was initiated.

1. Fission Product Release and Transport

1.1 Thermochromatographic Studies of HTGR Fission Products (F. B. Growcock, A. Hosseini, R. D. Taylor, J. Skalyo, Jr., J. M. Dickey and J. Colman)

The thermochromatograph was modified for the fuel kernel heatup experiments as shown in Figure 1.1. A coaxial open end Ge(Li) detector with a resolution of ≤ 1.85 keV was obtained from Ortec for monitoring the gamma ray emissions of the fission products. Shielding for the detector has been ordered and a precision track is being constructed to permit scanning of the thermochromatographic tube. The detector is interfaced to a TN-11 computer, which is currently being programmed for isotope analysis.

The fuel kernels are handled in a graphite crucible which, after irradiation, is placed in a graphite susceptor tube inside a 100 mm O.D. quartz tube. Thermax (a commercial carbon black) is packed between the tubes for insulation. Temperatures of $>3000^{\circ}\text{C}$ have been obtained with a 12.5 kW Lepel induction heater, which should be sufficient to cause 100% failure of the fuel kernel coatings. Extension of the thermochromatograph from its current limit of 1000°C to 2000°C is planned in order to examine the chemical states of nonvolatile fission products released from the fuel kernels; a platinum resistance furnace is being ordered for this purpose. Meanwhile, a linear temperature gradient in the 1000°C - 2000°C region is being sought by trial and error design modifications without the use of an external heat source.

Exploratory work on unfailed, irradiated fuel kernels was continued. Gamma spectra were obtained on fuel kernels, irradiated in the HFBR at $\sim 10^{14}$ neutrons/cm²-sec for 15 minutes and 1 hour, after decay times ranging from 2 days to 3 months. A typical spectrum is that shown in Figure 1.2 for a one hour irradiation of 2 TRISO fuel kernels which were allowed to decay for 45 days.

Analysis of the spectrum can be found in Table 1.1. Several isotopes consistently appeared in all the spectra, although in varying amounts: ^{89}Sr , ^{91}Y , ^{95}Zr , ^{95}Nb , ^{103}Rn , ^{103}Rh , ^{131}I , ^{133}Xe , ^{137}Cs , ^{140}Ba , ^{140}La , ^{141}Ce , ^{143}Pr , ^{144}Ce , ^{144}Pr and ^{147}Nd . A computer code incorporating both primary and secondary fission product chains has been developed. It gives good qualitative agreement with the experimentally observed fission product yields. The code is currently being modified to provide a direct spectral comparison with experiment.

Well characterized, irradiated and unirradiated, BISO and TRISO fuel kernels have been ordered from General Atomic Corporation. They are scheduled to be delivered before the end of February.

Thermochromatographic work on simulated fission products is continuing. The CsI and I studies are almost completed. A calculation (with program THGRAF) of deposition temperature of FeI_2 produced from iodine interaction with steels indicates that deposition should occur at $350\text{--}400^{\circ}\text{C}$. Experimentally (Growcock, 1977) an iodine deposit was found in a band at this temperature but characterization of the full deposition profile is still not complete.

Figure 1.1. Thermochromatographic apparatus used for fuel kernel heatup experiments.

Figure 1.2. Fe(Li) detector γ -spectrum of TRISO fuel kernels after an irradiation for one hour and a 45 day decay period.

Table 1.1

Gamma Spectrum of TRISO Fuel Kernels After One Hour
Irradiation and 45 Day Decay Period

<u>Energy (MeV)</u>	<u>Relative Activity</u>
0.329	65
0.365	44
0.424	21
0.433	21
0.487	85
0.497	100
0.538	41
0.611	13
0.724	54
0.757	74
0.766	75
0.815	24
0.867	7
0.925	7
1.596	44

1.2 Simple Theoretical Models for the Process of Thermochemical Separation (J. M. Dickey, I. Tang, J. Colman)

In the thermochromatograph, He flows slowly down a long tube, along which is maintained a temperature gradient. A small quantity of a substance is vaporized at the hot end of the tube. After about half an hour the substance has deposited in a narrow band on the wall of the tube. Thereafter the band moves very slowly. The temperature of the wall at which the band is located is characteristic of the substance. The impurity atoms travel down the tube because they are carried along by the helium stream and also because they diffuse down the concentration gradient created by the temperature gradient. The vapor pressure determines the concentrations of the impurity in the gas in close proximity to the surface of the tube. The two physical properties of a substance which will determine the position of the band are the vapor pressure and the diffusion coefficient. The experimental data for these two properties often have a wide spread or are inadequate. Alternatively, theoretical expressions must be used if there is no experimental data.

We have investigated the effect of possible errors in the parameters used to calculate the diffusion coefficient and vapor pressure on the predictions of the simple theoretical models (Dickey, 1977) of the thermochromatograph. The calculations were based on the experimental data for CsI. The code THGRAF was used.

1.2.1 Diffusion Coefficient

For binary gas mixtures, over a small range of temperature and pressure (Bird, 1960), the mass diffusivity varies inversely as the pressure and as T

where T is the temperature in $^{\circ}\text{K}$,

$$D = D_0 T^{\alpha} / P.$$

The parameter α usually lies between 1.5 and 2. For a hard sphere gas, $\alpha = 1.5$ and D_0 can be calculated quite accurately using the Chapman-Enskog theory. For real gases, the equivalent hard sphere diameter depends on the temperature. D_0 and α also vary with temperature. The time that the band takes to move down the tube to a stated temperature was calculated using the diffusion model (Dickey, 1977) and is plotted in Figures 1.3 and 1.4. Figure 1.3 shows that the exponent α has a considerable effect on the location of the band. The results are much less sensitive to variations in the coefficient D_0 (Figure 1.4).

1.2.2 Vapor Pressure

The vapor pressure of a substance usually follows an Arrhenius law, $P = 10^{(A-B/T)}$. Figures 1.5 and 1.6 show the effect of variations in A and B on the time that the band takes to reach a temperature T . An error of 5% in the value of either A or B would shift the theoretical position of the band by about 50°C according to the convection model.

1.3 Interaction of CsI with HTGR Materials

1.3.1 The Interaction of CsI with Uranium Oxide (S. Nicolosi)

A series of experiments have been performed to investigate volatile products of the reaction between UO_x and CsI. Varying amounts of powdered UO_x and CsI were mixed and heated in vacuum in sealed quartz tubes. One end of each tube, containing the sample, was heated for about 20 hours to 850°C with the remainder of the tube at cooler temperatures. The coolest portion of the tube was at room temperature. The UO_x stoichiometry was varied from $\text{UO}_{2.0}$ to $\text{UO}_{2.67}$ and the weight percent of CsI mixed with UO_x was varied from 5% to 70%. The materials which deposited in the various temperature regions of the tube were analyzed to be CsI and iodine. In these experiments the yield of iodine from CsI varied from less than 1 weight percent to about 3 weight percent. Results indicate that the yield is effected by the UO_x stoichiometry. In the range of stoichiometries studied, a greater O/U ratio results in a greater yield of iodine per given amount of CsI.

1.3.2 Interaction of CsI with Stainless Steel, Incoloy 800 and Hastelloy X (S. Aronson, M. Friedlander - Brooklyn College)

The thermochromatographic experiments on the volatilization and deposition of CsI have demonstrated that CsI deposited on stainless steel at 550°C is decomposed in the presence of oxygen whereas it is not decomposed on quartz. An experimental program was undertaken to elucidate the reasons for the different behavior of CsI on quartz and stainless steel and to obtain information on the interaction of CsI with the HTGR alloys Incoloy 800 and Hastelloy X.

Mixtures of CsI with stainless steel, Incoloy 800 and Hastelloy X turnings as well as with Fe, Ni and Cr powders were heated in air, oxygen and helium for specific periods of time at temperatures of 550° to 650°C . In helium, no corrosion of the metals occurred either in the presence or absence of CsI. The data

Figure 1.3. Time, calculated using the diffusion model, that a band of CsI takes to travel down the tube. The values of the exponent α in the diffusion coefficient were 1.5, 1.65, 2.0.

Figure 1.4. Time, calculated using the diffusion model, that a band of CsI takes to travel down the tube. The diffusion constant, D_0 , was varied by $\pm 25\%$.

Figure 1.5. Time, calculated using the convection model, that a band of CsI takes to travel down the tube. The vapor pressure coefficient, A, was varied by $\pm 5\%$.

Figure 1.6. Time, calculated using the convection model, that a band of CsI takes to travel down the tube. The vapor pressure coefficient, B, was varied by $\pm 2\%$.

obtained in air and oxygen, to be discussed below, were similar indicating that nitrogen does not play a significant role in the interaction of metals with CsI. The most extensive data were obtained on samples exposed to air at 620°C for 4 hours. The results are shown in Table 1.2.

We observe in Table 1.2 that exposure of Hastelloy X and Incoloy 800 to air at 620°C resulted in the formation of a blue surface film. The metal surface remained shiny. The stainless steel surface appeared to be more tarnished. The presence of CsI resulted in a dull grayish-brown tarnish film on all 3 alloys. Rinsing the turnings in distilled water imparted a yellow color to the water. A positive qualitative test for Cr^{+6} was obtained (nitric acid, ethyl ether, hydrogen peroxide, blue color in ether layer) on the rinsings from the Incoloy 800 and Hastelloy X turnings. Negative results were obtained on tests for nickel and iron ions.

The data on the alloys indicate that CsI interacts with the chromium in the presence of an oxidizing atmosphere. This observation was confirmed by exposing mixtures of CsI and either iron, nickel or nickel powders to air at 620°C for 4 hours. Only chromium interacted with CsI with the formation of some Cr^{+6} ion.

The investigation is continuing with the objective of obtaining information on the interaction of CsI with HTGR alloys in the presence of preformed surface oxide films and mild oxidizing environments (e.g. water vapor).

1.4 Interaction of Cesium with Fused Silica (S. Aronson, M. Friedlander and J. Mittelman - Brooklyn College)

The apparatus for studying the interaction of elemental cesium with fused silica which was previously described (Aronson, 1977) was modified to make the preliminary experimentation more convenient. The modified apparatus is shown in Figure 1.7. A 0.3 gram sample of cesium metal was irradiated in a sealed quartz tube at BNL to a 0.3 mC level. The sample tube was opened and was inserted in the left leg of the quartz U-tube apparatus in an argon atmosphere glovebox. The other leg of the U-tube was filled with 22, 1 1/2 inch lengths of quartz tubing, 2 mm I.D. and 3 mm O.D. The apparatus was attached to an ion pump vacuum station, was evacuated to 10^{-7} torr and was sealed off. The legs of the U-tube were inserted into an oil bath and a tube furnace. Measurements of cesium absorption by quartz at temperatures of 600-1100°C are currently under way.

Other experiments on the interaction of cesium with quartz will require work in a flowing mixture of helium and water vapor. It will be expedient to use a salt such as CsNO_3 as the source of cesium in this type of experiment. Work was, therefore, begun on the thermal decomposition of CsNO_3 . Several CsNO_3 composition experiments were performed in a system containing a quartz spring balance in which sample weight changes could be observed. The preliminary results indicate that CsNO_3 does not significantly decompose at temperatures up to 750°C. In contact with powdered graphite, however, rapid decomposition occurs at 570°C. This latter observation is in accord with previous observations made on graphite soaked with an aqueous CsNO_3 solution and heated in vacuum.

Table 1.2

Data Obtained on Metal-CsI Samples Exposed to Air at 620°C

<u>Material</u>	<u>CsI</u>	<u>Appearance</u>	<u>Aqueous Solution</u>	<u>Positive Test For</u>
304 Stainless Steel	absent	slightly blue	colorless	-
304 Stainless Steel	present	tarnish	yellow	-
Incoloy 800	absent	blue film	colorless	-
Incoloy 800	present	tarnish	yellow	Cr ⁶⁺
Hastelloy X	absent	blue film	colorless	-
Hastelloy X	present	tarnish	yellow	Cr ⁶⁺
Iron Powder	absent	--	colorless	-
Iron Powder	present	--	colorless	-
Nickel Powder	absent	--	colorless	-
Nickel Powder	present	--	colorless	-
Chromium Powder	absent	green (Cr ₂ O ₃)	colorless	-
Chromium Powder	present	green (Cr ₂ O ₃) plus yellow (Cr ⁶⁺)	yellow	Cr ⁶⁺

Figure 1.7. Apparatus for studying the absorption of cesium by silica in vacuo.

1.5 Mass Spectrometric Study of Volatile HTGR Fission Products (S. Nicolosi, I. Tang and H. Munkelwitz)

A high temperature mass spectrometer (see Figure 1.8) has been put into operation to obtain information on the identity and stability of volatile HTGR fission products. The mass spectrometer utilizes a quadrupole mass filter in conjunction with an inductively heated Knudsen-type cell. Since the molecular beam is not modulated, the instrument is equipped with a manually operated shutter to enable the operator to distinguish beam intensity from background intensity.

The facility is currently being utilized to study the thermal decomposition of Cs_2O in vacuum at temperatures of 300–600°C. Cs_2O is a potential HTGR fission product. It is also being studied to verify the performance of the mass spectrometer, since some similar work has been done on this compound by other investigators. Above 600°C, resolution is lost, presumably because of interference from the induction heater.

Strong Cs signals have been observed with reasonably well fitted, linear IT vs. $1/T$ behavior. CsO , O_2 and O have been observed but the temperature dependence is not well resolved. This poor resolution results from scatter which may be caused by high background interferences. A strong Cs_2O signal has never been observed. A weak Cs_2O signal cannot currently be identified because operation of the manual shutter introduces major background interference. Modifications such as beam modulation and resistive heating are being made to improve the quality of the data. An ion pump is being installed in the Knudsen cell chamber to reduce background interference by allowing the mass spectrometer to be kept under continuous vacuum.

1.6 Aerosol Formation in HTGR Accidents (I. Tang and H. Munkelwitz)

Aerosol formation in an HTGR accident is an important safety problem to be considered since airborne particulates readily serve as condensation or adsorption centers for fission products which are released simultaneously with the aerosol. A program was initiated to investigate the various aerosol formation mechanisms pertaining to HTGR safety. Subsequent studies of the interaction of fission products with particulates will soon follow.

During the report period, a continuous flow apparatus was constructed to study the rate and extent of aerosol formation during the oxidation of graphite by water vapor at elevated temperatures. A schematic diagram of the apparatus is shown in Figure 1.9. The apparatus consists mainly of a test section where a graphite specimen is heated by induction, a humidifier for introducing measured amounts of water vapor in the helium carrier gas, and a separate section where the particles formed may be counted or collected for further examination.

In a typical experiment, a graphite specimen is first heated to a high temperature in a dry helium flow for outgassing. A large amount of particulate is always formed during initial specimen outgassing, and may last for days before a substantial reduction of the particle level is reached. The usual practice is to outgas at a temperature higher than the intended oxidation temperatures. The particles produced from specimen outgassing are generally below 0.05 μm in diameter, and their concentration is continuously monitored with a

Figure 1.8. High temperature mass spectrometer system.

HTGR GRAPHITE AEROSOL EXPERIMENT

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Figure 1.9. System for studying aerosol formation during graphite oxidation.

G.E. condensation nuclei counter. After proper outgassing, the specimen is cooled and weighed. It is heated again to a predetermined temperature before water vapor is introduced into the carrier gas to begin an oxidation experiment. Usually, a high level of particle concentration is established soon after the introduction of the water vapor, indicating the formation of an aerosol as a result of graphite oxidation.

Several runs were made using a 3 kW induction furnace for heating an H451 graphite sample. However, it was not possible to attain a sufficiently high temperature to properly outgas the sample due to the limited power available from the 3 kW heater. Only qualitative results were obtained. A larger heater is being acquired so that the sample can be heated to at least 1600°C. Among the variables to be studied in these experiments are specimen geometry, oxidation temperature, carrier gas flow rate, and water vapor concentration. The aerosol production rate will be correlated with the oxidation rate of graphite. It is expected that the results will provide a realistic assessment of the importance of aerosol formation from graphite oxidation.

1.7 Experimental Studies of Core Heatup Phenomena (P. Soo, C. A. Sastre and D. G. Schweitzer)

This is the first quarterly report for the experimental HTGR Core Heatup Program, which was formally initiated this fiscal year. The program was devised to obtain experimental data on the behavior of fuel, fission products, control rod materials, and graphite during hypothetical, non-mechanistic accidents in HTGR systems where temperatures are assumed to range up to the graphite sublimation point ($>3600^{\circ}\text{C}$). Since there are very few experimental data dealing with the physical and chemical interactions of these core materials, the current work will provide quantitative information for the analysis of core heatup scenarios.

An induction furnace has been constructed which is capable of attaining graphite sublimation temperatures in relatively large volumes ($\sim 50\text{ cm}^3$) and in relatively short time (~ 2 hours). A detailed description of this unit is given by Sastre (1977); hence, only a brief outline will be given here.

The test piece (susceptor) is a graphite cylinder measuring 6.25 inches long x 3.28 inches in diameter (Figure 1.10). A hole is machined into the center and pyrometric temperature readings are made via a graphite sight tube. The susceptor is imbedded in a commercial carbon black (trade name is "Super Thermax") which is enclosed within an alundum tube measuring about 7 inches in diameter. Power is supplied to the induction core with a 30 KVA motor generator at a frequency of 10 KHz. Heat losses were found to be relatively small using this configuration. Graphite sublimation temperatures were readily attainable at power outputs of only 30%.

To test the system, several preliminary runs were carried out as outlined in Table 1.3. Detailed results have been previously described by Sastre (1977) but some of the more prominent will be outlined here.

1.7.1 Graphite Interactions with Fuel and Control Rod Materials

In Run 3, three 1/2-inch concentric holes were machined into an electrolytic

SCHEMATIC OF INDUCTION FURNACE FOR CORE HEAT-UP EXPERIMENTS

Figure 1.10

Table 1.3

List of Runs Conducted

<u>Run</u>	<u>Furnace Contents</u>	<u>Power (%)</u>	<u>Time at Power (min.)</u>	<u>Max. Meas. Temp. (°C) (1)</u>	<u>Purpose of Run</u>
1.	No specimens.	18	27	2150	To check operation of furnace.
2.	Sample of rhenium.	20	59	3250	To verify temperature measurements.
3.	Cruc. I, 0.93 g I-800H filings + 1.1 g B ₄ C pellet. Cruc. II, 2.5 g UO ₂ powder. Cruc. III, 3.0 g UO ₂ pellet.	30	58	3200	Small-scale tests of fuel and control rod material interactions.
4.	Cruc. I, 2.8 g UO ₂ powder. Cruc. II, 0.73 g B ₄ C powder. Cruc. III, 0.93 g I-800H filings + 1.5 g B ₄ C pellet.	30	46	2900	As for Run 3.
5.	27.5 g UO ₂ pellet (0.5" O.D. x 0.82" long)	30	22	2300	Larger scale experiment to determine macroscopic fuel/graphite interactions.
6.	27.5 g UO ₂ pellet (0.5" O.D. x 0.82" long)	30	39	2500	Repeat of Run 5 with extended heatup time to determine effect on porosity of reaction products.
7.	4 x 7.5 g ThO ₂ pellets (each 0.42" O.D. x 0.42" long)	30	60	3300	To determine nature of fuel/graphite interaction.
8.	27.5 g UO ₂ pellet (0.5" O.D. x 0.82" long)	30	39	Not measured	Repeat of Run 6 for motion picture.
9.	Susceptor only.	30-50		~3600	Graphite sublimation experiment.

Note: (1) Actual temperature probably higher since plugging of sight tube frequently occurred and prevented temperature measurement.

graphite susceptor and small capped crucibles inserted into each. Crucible I contained a mixture of control rod materials (Incoloy 800H cladding and B_4C), Crucible II contained UO_2 powder, and Crucible III a lightly compacted UO_2 pellet. Operating at 30% power, a measured temperature of $3200^\circ C$ was attained after 58 minutes -- at which time the power was switched off and cooling allowed to occur naturally. Usually, the furnace takes approximately 20 hours to come to a temperature at which it can be disassembled.

Samples of graphite powder were taken from various locations within the susceptor by drilling along an axial direction and taking powder from sites in the same horizontal plane as the materials in the crucibles. Nine locations were probed as shown in Figure 1.11. By using X-ray fluorescence procedures it was found possible to estimate the distances of migration of the various fuel and control rod materials (Figure 1.11). X-ray emission peaks attributable to the U (L_α) critical emission energy of 13.5 keV were observed in specimens 1, 2, 3, 4, 5, and 7 after scanning and counting for a total of 50 minutes. Although the specimens were made in as standard a form as possible they were not sufficiently reproducible to use the peak heights as accurate measurements of uranium levels. However, as expected, specimen 5, which was closest to the crucible containing UO_2 powder, showed an extremely large uranium indication. Specimen 9 which was about 3 cm away from any source of uranium showed no evidence of this metal.

Several attempts were made to roughly estimate the contents of uranium in the graphite powders by weighing specimens prior to fluorescing and comparing peak heights with those of available standards. Reproducibility was poor, as described previously, with two separate specimens taken from powder 5 giving uranium concentrations of 50 and 250 ppm. For specimen 3 a concentration of 160 ppm was obtained. It does, however, seem that despite poor reproducibility uranium concentrations in the range of 100-300 ppm may easily be obtained in locations close to the UO_2 crucibles under the thermal conditions imposed during Run 3. Uranium appears to be extremely mobile and can readily migrate through distances of ~ 3 cm during short-term exposures at high temperature ($\sim 3200^\circ C$). As discussed below, the uranium probably migrates in the form of a liquid carbide.

Metallic elements such as iron, nickel and chromium from the Incoloy 800H are similarly observed in samples 2, 3, 4 and 9. It is not clear why sample 9 does not show the most prominent peaks because of its closer proximity to the Incoloy 800H source; possibly, this could be due to non-standard X-ray specimen preparation. Nevertheless, the detection of iron and nickel in specimen 5 again shows that metals under the conditions of Run 3 may diffuse through graphite up to distances of at least 3 cm.

Extraneous peaks due to bromine, zinc and copper are commonly observed. The bromine source is the adhesive "Magictape" used in specimen preparation and the zinc probably originates from the Thermax powder insulation which contains this element as a major impurity.

1.7.2 Graphite Interactions with UO_2 and ThO_2

Run 5 was conducted to determine the nature of the reaction between large UO_2 pellets and graphite. The susceptors in this case housed a commercial

DISTRIBUTION OF METALLIC ELEMENTS IN RUN 3

Figure 1.11

0.5 inch O.D. x 0.82 inch long fuel pellet. During heating to about 2000°C the temperature-time data obtained were essentially the same as that obtained in blank runs using empty susceptors. At about 2300°C rapid gas evolution was observed by the convection patterns above the furnace. After a few seconds, a small blue flame was observed at the top of the funnel (Table 1.4). This

Table 1.4

Visual Observations on Selected Runs

Run	Accumulated Time To:			
	First Smoke	To First Flame	End of Flame	End of Run
5. (UO ₂)	-	18 min/2300°C ⁽¹⁾	-	22 min
6. (UO ₂)	-	17 min/2300°C	21 min/2200°C	39 min
7. (ThO ₂)	22 min/2300°C	23 min/2500°C	28 min/1800°C ⁽²⁾	60 min

Notes: 1. All temperatures measured by pyrometer.
2. Low estimates due to obscuring of melt by reaction products.

burned for several seconds and then was not observable. A few seconds after the flame disappeared a noise similar to a car backfiring was heard along with the appearance of a large (about 4 inches long) intense flame, which burned for about 3 minutes with sporadic evolutions of sparks.

The experiment was repeated 2 times with UO₂ and once with ThO₂ (Table 1.3). The results appear to be very reproducible. In the case of ThO₂ the flame appeared at a temperature which was about 200°C higher than the flame in the UO₂ runs (Table 1.4).

Although the analyses of all the runs are not complete, we believe the results are consistent with the following explanation. The rate of reaction of UO₂ and ThO₂ with graphite becomes appreciable at 800°C to 1000°C and is very rapid above 2000°C. In the case of a relatively large solid piece of oxide the carbide initially forms only on the surface portions that are in contact with the graphite. For large furnace heating rates the diffusion of carbon through the UO₂ is not rapid enough to consume much of the UO₂ before the melting point of the carbide is reached.

The unreacted core of UO₂ or ThO₂ is mechanically prevented from contacting graphite by the solid carbide on the surface. When the respective carbides melt, (~2350°C for UC₂ and 2650°C for ThC₂ - Table 1.5), the unreacted oxides make effective and continuous contact with the graphite. This occurs at temperatures where the rate of the oxide-carbon reaction is extremely rapid resulting in a relatively violent release of the reaction product CO. The hot CO is released through the sight tube as a jet. When the jet passes through the room temperature air above the furnace it entrains enough air to form an explosive CO-O₂ mixture. (Such a mixture has a lower ignition limit of about 700°C).

Upon sectioning the Run 5 susceptor it was seen that the UO₂ pellet had shrunk slightly along the axial direction but had developed a convex surface. The pellet was gray-black in color and, over much of the surface, had a smooth metallic lustre; this was especially true for the top of the pellet. A small

Table 1.5

Melting Points of Various Carbides

<u>Carbide</u>	<u>Melting Point ($^{\circ}$C)</u>
Fe ₃ C	1837
Cr ₃ C ₂	1890
MoC	2692
Mo ₂ C	2687
TiC	3140
Al ₄ C ₃	Stable to 1400
B ₄ C	2350
UC ₂	2350
ThC ₂	2650

piece of this metallic-type material was examined by the Debye-Scherrer X-ray technique for identification. During the powdering process to prepare the specimen the latter was observed to spark. This pyrophoric behavior indicated that the sample was mainly uranium carbide (UC₂) formed by the reaction below and this was subsequently confirmed by the X-ray analysis.

The pellet was now broken in 2 pieces and found to have a gray-black porous texture. Although no detailed analysis was performed the breakdown of the original dense UO₂ structure indicated that reduction of the oxide was nearly complete, and that the pellet was mainly UC₂. Owing to the fast rate of cooling it seems that the low density is attributable to pockets of gas (CO) which are entrapped during the solidification process.

To check the probability that the porosity in the Run 5 pellet was caused by carbon monoxide evolution during the oxide reduction process, a duplicate run (No. 6) was carried out. In this case, however, the run duration and temperature were higher (Table 1.3) to ensure that the reaction above was complete. Figure 1.12 shows the contents of the sectioned susceptor. At the bottom of the machined hole a bulbous cavity has formed which is significantly larger than the original 0.5 inch diameter, flat-bottomed hole. It is clear that this cavity bounded the original UO₂ pellet and was formed by rapid interaction with the graphite. As the pellet reacted it gradually contracted and settled as a molten pool of carbide. From Figure 1.12 it is seen that a concave surface was formed on which is seen a lustrous layer similar to that found in Run 5. The carbide pellet in this case is much more dense than the one in Run 5, indicating that oxide reduction and CO evolution were carried to completion. From the shape of the bottom of the machined hole it may be seen that during carbide formation the UO₂ pellet had corroded the graphite to a depth of nearly 0.5 inch.

In the upper sections of the machined hole (Figure 1.12) deep pits are observed and are due to the ejection of UO₂ particles by the pressure of the carbon monoxide, and their deposition on the sight tube walls. The ejection process is extremely vigorous since it may be shown from the above reaction that a UO₂

Figure 1.12. Appearance of reaction products in Run 6 (UC_2). Magnification 1.1X.

pellet weighing 27.5 grams will, when fully reduced, produce about 100 liters of carbon monoxide at a reaction temperature of $\sim 2500^{\circ}\text{C}$.

In Run 7, 4 small ThO_2 pellets (0.42 inch O.D. x 0.42 inch long) were stacked inside a susceptor similar to those in Runs 5 and 6. The run lasted for 1 hour and a maximum measured temperature of 3300°C was achieved. Figure 1.13 shows that a dense plug has been formed whose surface shows areas with a bright metallic coloration. As with the UO_2 runs, there is a large degree of graphite corrosion especially in the downward direction. No X-ray analysis of the pellet was attempted due to the hygroscopic nature of the carbide which caused the rapid formation of a brown surface film.

Corrosion pits at the top of the susceptor hole are again observed but they are less numerous and more regularly shaped than those in Run 6. In fact, compared to the UO_2 the metallography suggests that the thorium run was much more quiescent even though it was maintained at a much higher temperature.

The bottom of the sight tube was also plugged by the ejected reaction products (Figure 1.13) and a small solidified "droplet" has formed. Unlike the melt below, however, several days after the completion of the run no oxidation of this droplet was seen, and it remained bright and metallic in appearance. It was speculated that this could be metallic thorium. Subsequent X-ray analysis proved this was not so. At this time we have been unable to identify the compound(s) present.

The 2 upper sections of the sight tube were sectioned (Figure 1.14) and revealed that significant corrosion and deposition had occurred. On the central section of the tube a white deposit is seen. It is not certain whether this was the original color or whether it had been formed after the run from reactions with air and moisture. No attempt has been made at this time to chemically analyze this deposit. In the uppermost section of the sight tube the amount of deposition is far less and localized corrosion is present.

Figure 1.13. Appearance of reaction products in Run 7 (ThO_2). Magnification 1.0X.

Figure 1.14. Deposition and corrosion on sight tube from Run 7 (ThO_2). Magnification 1.0X.

1.8 The Permeability and Retentivity of Stressed Concrete Relative to Fission Product Iodine (L. R. Zumwalt, J. F. Mirza - North Carolina State Univ.)

1.8.1 Sorption Experiments Numbers 5 and 6

Two sorption experiments were completed in this quarter. In the high temperature sorption experiment (henceforth referred to as Experiment No. 5) 3 concrete samples were held at a temperature of approximately 100°C and exposed to ^{131}I tagged iodine vapor at a partial pressure of 3.95×10^{-5} atm for periods of 9, 3 and 26 days, respectively. These samples were subsequently sectioned to obtain iodine concentration profiles. For sorption experiment procedures refer to the Quarterly Report for the period April through June, 1977.

The medium temperature sorption experiment (henceforth referred to as Experiment No. 6) was carried out in the same manner as Experiment No. 5 except for the fact that the 3 concrete samples in this case were held at approximately 60°C compared to 100°C for Experiment No. 5.

The profiles obtained were subsequently analyzed using a program for a non-weighted least squares error fit with respect to each parameter. (For theory and program details refer to the Quarterly Report for April through June, 1977). See Figures 1.15 to 1.26 for profile data points and fit.

The results of the "fit" of parameters are given in Table 1.6. The symbols used are defined as:

C_f^0 = Concentration of fast component at the surface of the concrete due to in-pore gas and surface diffusion and sorption into the bulk.

D_f = Diffusion coefficient of the fast process.

K_f = Rate constant for transfer of atoms in fast path to a more or less immobile state in the bulk of the concrete which is taken as corresponding to the state of the slow component.

D_s = Diffusion coefficient for the slow process in the bulk.

C_s^0 = The slow component concentration of ^{131}I tagged iodine at the surface.

K = Partition coefficient defined as C_T^0/C_g where C_T^0 is the total surface concentration due to contributions of the fast and slow paths. C_g is the concentration of ^{131}I tagged iodine in the gas phase. The experimentally determined average concentrations of ^{131}I tagged iodine in the concrete samples at the end of exposure periods are listed in Table 1.7.

A bulk calibration procedure was used to determine the average concentration. For details of the calibration procedure refer to the Quarterly Report for the period July through September, 1977.

Comparing the data compiled for the 3 sorption experiments, it can be seen that:

Table 1.7

Average Concentration of Iodine in Concrete Samples

Experiment No.	Sample No.	P_{I_2} (atm)	Exposure Time (sec)	$\bar{C}$ Average Concentration mg I/g Concrete
5 (100°C)	6	3.95×10^{-5}	8.5104×10^5	0.926
	7	3.95×10^{-5}	2.643×10^5	0.1707
	8	3.95×10^{-5}	2.15058×10^6	1.313
6 (60°C)	9	3.95×10^{-5}	8.9976×10^5	1.038
	10	3.95×10^{-5}	2.706×10^5	0.367
	11	3.95×10^{-5}	2.32518×10^6	1.845

1. D_s increases with temperature. The average D_s value as a function of temperature is given in Table 1.8.

Table 1.8

Average D_s vs. Temperature

Temp. (°C)	D_s (cm ² /sec)
20	6.9×10^{-9}
60	2.2×10^{-8}
100	4.5×10^{-8}

2. K_f/D_f decreases with increase in temperature. Average values of K_f/D_f are given in Table 1.9.

Table 1.9

Average K_f/D_f as a Function of Temperature

Temp. (°C)	K_f/D_f (Avg.)	Data Used From Sample Nos.
20	14.6	3, 4, 5
60	2.5	6
100	0.37	9(B), 11(A)
		11(A), 11(C)

3. C_s^0 , C_f^0 , C_T^0 , and K do not seem to be strong functions of either exposure time or temperature. Tables listing the variation of K are as follows:

Table 1.10A

Overall Average K as a Function of Temperature

<u>Temp. ($^{\circ}$C)</u>	<u>$\bar{K}$ ($\times 10^{-4}$)</u>	<u>Sample No.</u>
20	3.4	5
60	3.8	9, 10, 11
100	3.1	6, 7, 8

Table 1.10B

Average K - Variation with Exposure Time and Temperature

<u>Temp. ($^{\circ}$C)</u>	<u>Exposure Time (sec)</u>	<u>$\bar{K}$ ($\times 10^{-4}$)</u>	<u>Sample No.</u>
20	1.377 $\times 10^6$	3.4*	5
60	2.706 $\times 10^5$	1.8	10
60	8.9976 $\times 10^5$	3.7	9
60	2.32518 $\times 10^6$	4.4	11
100	2.643 $\times 10^5$	2.7	7
100	8.5104 $\times 10^5$	4.5	6
100	2.15058 $\times 10^6$	2.2	8

*One profile only in this case.

The values of $\bar{K}$ at 60°C seem to increase with exposure time (Table 1.10B) but this is not so in the 100°C case.

4. The average concentrations at the end of exposures appear to be functions of exposure time and temperature (Table 1.7). The average concentration increases with exposure and decreases with increasing temperature.

There is an element of doubt about the accuracy of the results obtained from the samples Nos. 6 and 8 for the following reasons:

a. The drill bits used for sectioning were worn from usage and generated an excessive amount of heat during the sectioning operation.

b. The sections were not very clean due to the fact that the drill bits were worn.

c. The worn tool bits also caused extensive vibration of the drill press frame which led to poor sectioning control.

The situation was compounded by the fact that drying of the concrete due to high temperature exposure had rendered it somewhat brittle.

In subsequent work (in the 60°C runs) care was taken to use sharpened drill bits in carrying out sectioning.

Figure 1.15. Theoretical model fit of Sample 6.1 experimental data (see Table 1.6).

Figure 1.16. Theoretical model fit of Sample 7 experimental data (see Table 1.6).

Figure 1.17. Hand calculated fit of Sample 8(A) experimental data (see Table 1.6).

Figure 1.18. Hand calculated fit of Sample 8(B) experimental data (see Table 1.6).

Figure 1.19. Theoretical model fit of Sample 8(C) experimental data (see Table 1.6).

Figure 1.20. Theoretical model fit of Sample 9(A) experimental data (see Table 1.6).

Figure 1.21. Theoretical model fit of Sample 9(E) experimental data (see Table 1.6).

Figure 1.22. Theoretical model fit of Sample 9(C) experimental data (see Table 1.6).

Figure 1.23. Theoretical model fit of Sample 10 experimental data (see Table 1.6).

Figure 1.24. Theoretical model fit of Sample 11(A) experimental data (see Table 1.6).

Figure 1.25. Theoretical model fit of Sample 11(B) experimental data (see Table 1.6).

Figure 1.26. Theoretical model fit of Sample 11(C) experimental data (see Table 1.6).

Table 1.6

Diffusion Parameters and Boundary Concentrations Obtained from Concentration Profiles

EXP. #	SAMPLE #	TEMP. °C	P _{I₂} (atm)	EXPOSURE TIME (SEC)	C _s ^o (mgI/gm conc.)	C _f ^o (mgI/gm conc.)	C _T ^o (mgI/gm conc.)	D _s (cm ² /sec)	K _f /D _f	K = $\frac{C_T}{C_f}$ (x 10 ⁻⁴)	POINTS EXCLUDED from left on plot	FIGURE NUMBER
4	3	20	1.76 x 10 ⁻⁵	1.15 x 10 ⁶	1.37	0.095	1.46	8.5 x 10 ⁻⁹	6.3	1.824		
	4	20	1.76 x 10 ⁻⁵	1.211 x 10 ⁶	1.53	0.106	1.64	4.1 x 10 ⁻⁹	14.9	2.049		
	5	20	3.95 x 10 ⁻⁵	1.377 x 10 ⁶	2.16	0.55	2.71	8.1 x 10 ⁻⁹	22.5	3.385		
5	6.1	100	3.95 x 10 ⁻⁵	8.5104 x 10 ⁵	6.47	0.227	6.70	1.76 x 10 ⁻⁸	0.37	4.748	---	1
	6.2	100	3.95 x 10 ⁻⁵	8.5104 x 10 ⁵	5.77	0.30	6.07	1.95 x 10 ⁻⁸	(1.0)	4.301	2, 12	---
	7	100	3.95 x 10 ⁻⁵	2.643 x 10 ⁵	3.85	0.003	3.85	4.13 x 10 ⁻⁸	(0.1)	2.728	---	2
	8(A)	100	3.95 x 10 ⁻⁵	2.15058 x 10 ⁶	---	---	3.4	5.0 x 10 ^{-8*}	---	2.409	---	3
	8(B)	100	3.95 x 10 ⁻⁵	2.15058 x 10 ⁶	---	---	3.7	6.5 x 10 ^{-8*}	---	2.622	---	4
6	8(C)	100	3.95 x 10 ⁻⁵	2.15058 x 10 ⁶	2.00	0.19	2.19	7.43 x 10 ⁻⁸	(3.0)	1.552	---	5
	9(A)	60	3.95 x 10 ⁻⁵	8.9976 x 10 ⁵	6.69	0.07	6.77	1.73 x 10 ⁻⁸	(1.0)	4.283	---	6
	9(A)	60	3.95 x 10 ⁻⁵	8.9976 x 10 ⁵	6.69	0.095	6.79	1.695 x 10 ⁻⁸	(1.0)	4.296	9	---
	9(B)	60	3.95 x 10 ⁻⁵	8.9976 x 10 ⁵	4.5	0.068	4.57	2.85 x 10 ⁻⁸	1.16	2.891	---	7
	9(C)	60	3.95 x 10 ⁻⁵	8.9976 x 10 ⁵	5.19	0.095	5.29	1.89 x 10 ⁻⁸	(1.0)	3.347	---	8
	10	60	3.95 x 10 ⁻⁵	2.706 x 10 ⁵	2.52	0.095	2.62	2.74 x 10 ⁻⁸	(1.0)	1.657	---	9
	10	60	3.95 x 10 ⁻⁵	2.706 x 10 ⁵	2.67	0.25	2.92	1.87 x 10 ⁻⁸	(1.0)	1.847	2	---
	11(A)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	7.04	0.025	7.06	1.58 x 10 ⁻⁸	(1.5)	4.466	---	10
	11(A)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	6.55	0.31	6.86	1.61 x 10 ⁻⁸	2.91	4.340	1	---
	11(B)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	6.34	0.025	6.36	3.35 x 10 ⁻⁸	(1.5)	4.023	---	11
	11(B)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	6.56	0.05	6.61	3.14 x 10 ⁻⁸	(1.5)	4.182	1	---
	11(B)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	6.18	0.405	6.58	3.09 x 10 ⁻⁸	3.5	4.163	1	---
	11(C)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	7.39	0.035	7.43	1.8 x 10 ⁻⁸	(1.5)	4.701	---	12
	11(C)	60	3.95 x 10 ⁻⁵	2.32518 x 10 ⁶	7.11	0.318	7.43	1.72 x 10 ⁻⁸	2.6	4.701	---	---

* Hand Fitted/Calculated Data

() Input Values - Program did not track.

The profiles for Sample No. 8 (A and B) could not be analyzed using the physical model (refer to the Quarterly Report for the period April through June, 1977 for details of model). They were analyzed using a simpler Fick's law diffusion model (using an error function fit) to obtain approximate values of D_s .

1.8.2 Sorption Experiment Number 7

Sorption Experiment No. 7 sample exposure is scheduled to start January 12, 1978. In this experiment, 3 samples will be exposed to ^{131}I tagged iodine vapors at a partial pressure of 3.95×10^{-5} atm for 9, 3 and 27 days, respectively. The experimental set up and procedures will be the same as those for Experiments Nos. 5 and 6 except for the temperature at which the samples will be maintained. For Experiment No. 7 the sample temperature will be 20°C .

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2. Materials, Chemistry, and Instrumentation

This program covers the evaluation of four main categories of materials: (1) metallic materials, (2) graphites, (3) PCRV, and (4) other materials. Included in (4) are control rod materials and thermal barrier insulation. At this time, however, the major emphasis is centered on metals. 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, J. Chow)

An additional Materials Test System (MTS) close-loop electrohydraulic fatigue machine has been delivered and is now fully operational. Six fatigue units are currently being used in the program with 2 machines being devoted to in-helium testing. This proportion will increase as in-helium test capability increases.

2.1.1 Mean Stress Effects on the High-Cycle Fatigue of Materials

More work was completed on the Type 422 stainless steel helium circulator material during this reporting period. Table 2.1 shows all of the data obtained at this time for testing in air. Figure 2.1 shows the temperature dependence for fully reversible deformation (mean stress $\sigma_m = 0$). A fatigue limit appears to be established after about 5×10^7 cycles have been accumulated at the 3 test temperatures. In the last quarterly report it was shown that Goodman's approach for estimating the effects of mean tensile stresses on failure was adequate for the 900°F data. In this period it was shown that this approach is also adequate at 600°F within the range of stresses selected (Figure 2.2).

The tests at 1000°F were originally devised to evaluate high-temperature properties for this material in the event that hot spots are present in the circulator. Although the test series has yet to be completed it is apparent that the application of a mean tensile stress is beneficial to fatigue life over the range of stresses evaluated. This is in direct contrast to the data for 600 and 900°F, reported in the last quarter, where it was demonstrated that a mean tensile stress greatly decreases the fatigue life. Additional testing, and metallographic evaluations will be initiated shortly to examine this anomalous behavior more fully.

2.1.2 In-Helium High-Cycle Fatigue Testing

Comprehensive work is now being carried out on in-helium testing of Incoloy 800H and Hastelloy X. The standard 1/4 inch diameter hourglass specimen is being used for both materials. The Incoloy 800H tests are being run at 1400°F and the Hastelloy X ones at 1600°F. An environment from the MTL is supplying the test gas and this contains the following impurity levels: 200 μ atm. H_2 , 40 μ atm. CO, 20 μ atm. CH_4 and 10 μ atm. CO_2 . The gas represents an environment which is essentially intermediate between the dry environment being evaluated by Oak Ridge National Laboratory and the wet environment being studied at the General Atomic Company.

Chemistries of the two materials are given below:

Table 2.1

High-Cycle Mean-Stress Fatigue Data for Type 422 Stainless Steel⁽¹⁾

Spec. No.	Test Temp. (°F)	Stress (ksi)		Cycles to Failure (N_f)
		Cyclic	Mean	
MFF-20	600	85.0	0	2.0×10^5
MFF-21	600	80.0	0	2.2×10^5
MFF-28	600	75.0	0	2.8×10^6
MFF-29	600	70.0	0	7.5×10^6
MFF-31	600	68.0	0	3.8×10^6
MFF-33	600	66.0	0	$>1.1 \times 10^8$
MFF-25	600	65.0	0	$>4.0 \times 10^7$
MFF-23	600	60.0	0	$>1.3 \times 10^7$
MFF-34	600	65.0	50.0	3.0×10^5
MFF-35	600	60.0	50.0	2.5×10^5
MFF-38	600	58.0	50.0	6.8×10^6
MFF-42	600	57.0	50.0	2.4×10^6
MFF-37	600	56.0	50.0	$>1.1 \times 10^8$
MFF-36	600	50.0	50.0	$>1.1 \times 10^8$
MFF-04 ⁽²⁾	900	100.0	0	3.0×10^3
MFF-03	900	75.0	0	3.0×10^4
MFF-05	900	70.0	0	1.5×10^5
MFF-06	900	67.0	0	7.6×10^5
MFF-07	900	63.0	0	4.6×10^6
MFF-11	900	58.0	0	1.2×10^6
MFF-27	900	54.0	0	$>1.0 \times 10^8$
MFF-17	900	52.0	0	3.3×10^7
MFF-24 ⁽³⁾	900	52.0	0	$>1.3 \times 10^8$
MFF-19	900	48.0	0	$>1.0 \times 10^8$
MFF-08	900	50.0	40.0	4.6×10^5
MFF-10	900	44.0	40.0	2.4×10^5
MFF-12	900	40.0	40.0	1.1×10^6
MFF-14 ⁽⁴⁾	900	34.0	40.0	5.3×10^6
MFF-32	900	31.0	40.0	$>1.2 \times 10^8$
MFF-18	900	28.0	40.0	$>1.1 \times 10^8$
MFF-41	1000	65.0	0	8.9×10^4
MFF-15	1000	52.0	0	5.6×10^5
MFF-39	1000	48.0	0	9.5×10^8
MFF-40	1000	45.0	0	$>1.0 \times 10^8$
MFF-44	1000	60.0	30.0	9.2×10^4
MFF-47	1000	56.0	30.0	1.5×10^6
MFF-45	1000	55.0	30.0	1.0×10^7
MFF-43	1000	50.0	30.0	5.6×10^6
MFF-46	1000	46.0	30.0	2.2×10^7
MFF-48 ⁽⁵⁾	1000	40.0	30.0	

- Notes:
1. Material austenitized at 1900°F and tempered at 1050°F.
 2. Just reached full load before failure.
 3. Furnace overheated causing specimen to fail.
 4. Failed at internal location.
 5. Test in progress.

EFFECT OF TEMPERATURE ON THE HIGH-CYCLE
FATIGUE OF TYPE 422 STAINLESS STEEL

Figure 2.1

GOODMAN DIAGRAM FOR TYPE 422 STAINLESS STEEL
AT 600°F (316°C)

Figure 2.2

	<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

Note: NA - not analyzed.

Figure 2.3 shows in-air and in-helium data for Incoloy 800H. For the very short lifetimes the helium environment is beneficial with respect to strength. It is possible, however, that as the number of cycles increases, i.e. at lower stress levels, the helium and air environments will show a converging trend. Plans for long term testing are being made to evaluate this more effectively.

The data for Hastelloy X given in Figure 2.4 show a much smaller environmental effect than Incoloy 800H. Only 2 in-helium tests have been completed to date and fatigue strengths for the 2 environments are comparable. For longer term tests the indication seems to be that the air and helium trends converge as was the case for Incoloy 800H.

2.2 Creep Rupture Properties of Primary Circuit Structural Materials in Helium (J. Chow, P. Soo)

The safety of an HTGR depends on the maintenance of structural integrity in the primary circuit materials throughout the life of the reactor system. In this phase of our program an elevated temperature creep testing effort has been established to study the long term mechanical behavior of alloys in prototypic HTGR helium environments.

The helium environment for the mechanical testing is being supplied from the MTL. The environmental creep tests are being performed in helium with 40 μ atm. of H₂O, 200 μ atm. H₂, 40 μ atm. CO, 10 μ atm. CO₂ and 20 μ atm. CH₄. It is believed that this "wet" environment simulates that which could exist in an HTGR with some leaking steam generator tubes.

At the present, tests on Hastelloy X and Incoloy 800H are being run. The compositions of the alloys were given in the previous section.

The Incoloy 800H alloy was solution treated by heating to 2100°F and water quenched. The Hastelloy X alloy was heated to 2150°F for one half hour and water quenched. In order to eliminate heat-to-heat variations the in-air and in-helium test specimens are made from a single reference heat.

2.2.1 Incoloy 800H

The stress rupture data for Incoloy 800H at 1200°F (649°C) and 1400°F (760°C) obtained to date are plotted in Figure 2.5. Most of the specimens that failed have been the in-air tests. Only one in-helium test has failed to date and this was performed at 1400°F (760°C) and stressed at 8,000 psi. It ruptured after 1,628.4 hours whereas the test performed in air at 1400°F (760°C) and 8,000 psi ruptured after 2,604 hours. Based on these 2 tests, it appears that the impure

EFFECT OF A HELIUM ENVIRONMENT ON THE HIGH-CYCLE FATIGUE OF
INCOLOY-800 H AT 1400 °F (760 °C)

Figure 2.3

FIG.8 CORRELATION OF HIGH-AND LOW-CYCLE FATIGUE DATA FOR HASTELLOY-X AT 1400°F (760°C) AND 1600°F (871°C)

STRESS RUPTURE PROPERTIES INCOLOY 800 H TESTED
AT 1200 °F AND 1400 °F IN AIR AND IN HTGR HELIUM

Figure 2.5

helium environment can lower the rupture life of Incoloy 800H alloy appreciably.

Metallographic studies are under way to compare the fracture morphology of these 2 specimens. Figure 2.6 shows a longitudinal cross section of the 2 specimens at and near the fractures. It is quite obvious that there are more grain boundary voids in the specimen tested in helium than in the specimen tested in air. These observations may be an indication of the general effect of the helium environment that is being used. Continuing metallographic studies and examination of the fracture surfaces by scanning electron microscopy are being carried out to learn more of the observed effect.

Figure 2.7 presents the creep strengths of Incoloy 800H tested at 1200°F (649°C) and 1400°F (760°C). The straight lines are drawn through the in-air test data points. Some of the in-helium data points are of tests still in progress. However, in all cases the creep rates plotted are believed to be the minimum rates. The creep data obtained so far do not show a definite effect of the helium environment.

2.2.2 Hastelloy X

The stress rupture data of Hastelloy X at 1400°F (760°C) determined to date are shown in Figure 2.8. One straight line seems to fit the 2 in-air and 2 in-helium data points and there appears to be no differences between the air and helium environments. However, if the rupture times of the 2 tests performed at 14,000 psi are compared, the in-helium test failed after 1,870.4 hours whereas the in-air test lasted 2,225.8 hours before fracture. This difference in rupture time could be within the data scatter of stress rupture tests. However, metallographic examination indicated the in-helium test specimen had considerably more grain boundary voids than the specimen fractured in air. This observation is illustrated in Figure 2.9. Further metallographic studies will be made to see if this is a generic effect of testing in the helium environment.

Figure 2.10 presents the creep data obtained to date on Hastelloy X tested at 1400°F (760°C) in air and in impure helium. The line is drawn through the in-air test data points. Two in-helium test data points also fall on the line. Two other in-helium data points show appreciably higher creep rates than the in-air tests. The test at 12,000 psi, however, is still in progress and the creep rate shown may not be the minimum rate for this specimen.

2.3 Materials Test Loop (L. G. Epel)

The Materials Test Loop (MTL) operated continuously and routinely during the quarter, supplying helium to one high cycle fatigue and 7 creep testing machines. The desired concentrations of impurities, together with their average values during this reporting period, are shown in Table 2.2 below.

Table 2.2. Impurity Levels in MTL (ppmv)

	<u>Desired</u>	<u>Average</u>
Carbon dioxide	10	8
Carbon monoxide	40	30
Hydrogen	200	230
Methane	20	20
Water vapor	40	44

(a)

(b)

Figure 2.6. Incoloy 800H stress-rupture tests at 1400°F (760°C) at 8000 psi. (a) tested in air, ruptured after 2604 hours; (b) tested in impure helium, ruptured after 1628.4 hours. Note the greater amount of voids in the areas near and adjacent to the fracture of the specimen tested in impure helium

CREEP STRENGTHS OF
INCOLOY 800 H AT 1200 °F AND 1400 °F
TESTED IN AIR AND IN HTGR HELIUM

Figure 2.7

STRESS RUPTURE PROPERTIES OF HASTELLOY X AT 1400 °F (760 °C) TESTED
IN AIR AND IN HTGR HELIUM

Figure 2.8

(a)

(b)

Figure 2.9. Hastelloy X stress rupture tests at 1400°F (760°C) at 14,000 psi. (a) tested in air, ruptured after 2258.8 hours; (b) tested in helium, ruptured after 1870.4 hours. Note the considerably greater amount of grain boundary voids near and adjacent to the fractured surface of the specimen tested in helium.

CREEP STRENGTHS OF HASTELLOY X AT 1400 °F (760 °C) IN AIR AND IN HTGR HELIUM

Figure 2.10

As can be seen from the table (and consistent with the startup period) the impurity levels were maintained reasonably close to the desired values. However, the control of hydrogen requires a great deal of operator attention and automatic control would be desirable. Therefore, some preliminary tests were conducted utilizing a tank containing Ti-Zr chips, through which helium-hydrogen was passed. In these tests the Ti-Zr bed temperature was varied and the fraction of hydrogen removed was noted. Figure 2.11 presents the results of these tests, done with dry helium containing hydrogen. It is clear that the effective operating range for hydrogen removal is above 300°C.

When a similar tank containing Ti-Zr chips was installed into the purification system of the MTL and operated at the appropriate temperature, it was found that the hydrogen concentration in the loop increased rather than decreased. This was attributed to water vapor dissociating on the hot walls of the tank. A second tank, containing molecular sieves at room temperature, was therefore installed preceding the Ti-Zr tank and this has alleviated the difficulty. The hydrogen control system is currently being automated so that signals from a gas chromatograph can be used to activate this system.

A multipoint recorder containing high and low alarms has been received from the vendor. This device will be added to the MTL during the coming quarter in an attempt to further automate the MTL. It is hoped that the recorder, together with the hydrogen control system described above and the Panametrics Dew Point Meter, can control all of the impurity levels continuously and automatically, requiring only occasional operator attention.

2.4 Characterization of HTGR Graphites (F. B. Growcock, A. Hosseini)

2.4.1 Flow of Gases Through HTGR Graphites

The oxidation of the PGX core support blocks by impurities in the He coolant stream may, at any given temperature, occur primarily on the surface or throughout the bulk of the graphite. Gases can flow through the porous sample as a result of a pressure gradient (permeability) or as a result of a concentration gradient (diffusion). These two phenomena were investigated this past quarter.

The gas permeabilities of ATJ, PGX and H451 graphites toward He, H₂, CO and H₂O were measured as functions of mean pressure, up to ~30 psia, and temperature up to 1000°C. An all metal system utilizing a seal-less technique, as described previously (Growcock, 1977), was used for the measurement of permeabilities (see Figure 2.12). Samples of the graphites (2 inch x 2 inch right cylinders) were sent to the Great Lakes Carbon Corporation for permeability determination by the standard ASTM method (see Table 2.3). The samples were cut to fit our apparatus and the room temperature results were found to be consistently within 10% of the Great Lakes values. The results will be presented in a forthcoming topical report.

Prediction of permeabilities from pore size distributions has become possible only recently. Using a bimodal pore size distribution obtained from Hg porosimetry and photomicrography, Dullien (Azzam, 1976) derived an expression which gives reasonably accurate permeabilities for sandstones. Yang (1977) obtained a pore size distribution for H451 graphite from N₂ desorption and Hg porosimetry data. J. Colman has calculated the permeability of H451 graphite by the Dullien method

Figure 2.11

Figure 2.12. High temperature gas permeability apparatus.

Table 2.3

<u>Sample</u>	<u>Orientation</u>	<u>Darcy Permeability</u>
H451 #1	AG	0.003, 0.004
#2	AG	0.016, 0.017
ATJ #1	WG	0.004
#2	WG	0.004
PGX #1	WG	0.071
#2	WG	0.066
PGX #1	AG	0.073
#2	AG	0.061
#1'	WG	0.168
#2'	WG	0.153

Note: The first 6 values are results obtained by Great Lakes Carbon Corporation. The last 4 values are averages obtained by Union Carbide for samples toward the inside (#1 and #1') and toward the outside (#2 and #2') of the log.

using Yang's pore size distribution. The values so obtained are a factor of 10^{-5} lower than experimental values. Diffusion coefficients calculated from the same pore size distribution are, however, of the same order of magnitude as experimental values. It can be shown that, whereas small pores control the rates of gaseous diffusion, the small pores contribute negligibly to the permeability. That the binary gas diffusion coefficients for H451 and PGX are quite similar while their permeabilities differ by X_4 to X_{24} suggests that their pore size distributions are similar except at large values of the pore diameter.

A transient method developed by Yang et al (1976) was used to measure room temperature diffusion coefficients for the pair $\text{CH}_4\text{-N}_2$. The theory was obtained from Crank (1975), modified slightly to give an expression for diffusion out of a sphere. Gas chromatography was used for analysis of $[\text{CH}_4]/[\text{N}_2]$ at the end of the diffusion period. Time-independent effective diffusion coefficients, D_e , are expected. As shown in Figure 2.13 for PGX and H451 graphites, this expectation is not realized. The log-log plot suggests that the assumption of zero gas concentration (for time >0) at the edge of the sphere may not be valid and perhaps should be a function varying exponentially with time. The curvature in the plot at small diffusion times is an experimental artifact arising from CH_4 which remained in dead spaces in the diffusion cell after sweeping with N_2 . A detailed writeup of the results will be available in a future topical report.

2.4.2 The Characterization of PGX Graphite

In the previous Quarterly Progress Report (Growcock, 1977), the oxidation of 2 samples of PGX graphite containing <40 ppm Fe and >1000 ppm Fe was described.

These 2 samples were examined under a scanning electron microscope. Photomicrographs and Fe X-ray Raster scans for the 2 samples at magnifications of X200 and X1000 are given in Figures 2.14 and 2.15. Two immediate differences are obvious: the Fe concentration is much greater in PGX-5A than in PGX-1A and large pits are present in PGX-5A which are noticeably absent in PGX-1A. As with other samples of PGX graphite, oxidized or not, the iron appears to be distributed

Figure 2.13. Effective binary gas diffusion coefficient for $\text{CH}_4\text{-N}_2$ in HTGR graphites as a function of time.

(a)

(c)

(b)

(d)

Figure 2.14. Scanning electron photomicrographs of oxidized PGX graphite, PGX-1A (≤ 40 ppm Fe):
(a) X200 (1 cm = 50 μ m), (b) Fe X-ray scan at X200, (c) X1000 (1 cm = 10 μ m),
(d) Fe X-ray scan at X1000.

(a)

(c)

(b)

(d)

Figure 2.15. Scanning electron photomicrographs of oxidized PGX graphite, PGX-5A (>1000 ppm Fe):
 (a) X200 (1 cm = 50 μ m), (b) Fe X-ray scan at X200, (c) X1000 (1 cm = 10 μ m),
 (d) Fe X-ray scan at X1000.

rather inhomogeneously and congregates at the edges of the pits.

A qualitative analysis of these effects is being undertaken using the theory of oscillating reactions.

2.5 Helium Impurities Loop (L. G. Epel)

The experiments described in the previous Quarterly Progress Report in which CO was injected into the Helium Impurities Loop (HIL) were repeated and expanded upon during the past reporting period. The first set of tests simply repeated the 65 cc, 125 cc, and 187 cc CO injection experiments in order to observe the reproducibility of the results. The very first run involved the injection of 187 cc of CO and it was observed that the decline of CO (and buildup of CO₂) proceeded at a very much faster rate than the corresponding "original" run. There were no other impurities in the loop during this run; particularly there was no hydrogen present. (See Figure 2.16 in which the original run is labeled "1" and the first repeat is labeled "2". A final repeat of this run is shown in the figure as "3"). Similarly, the next run, which involved injecting 125 cc of CO, resulted in a somewhat more accelerated depletion of CO (and buildup of CO₂) than had been observed in the original 125 cc injection experiment. The only concomitant impurity observed during this run was a moderate amount of hydrogen -- about 100 ppm. The next run was a final repeat of the 187 cc CO injection test. This time the decline and buildup of CO and CO₂, respectively, while somewhat more rapid than in the "original" test, was reasonably close to the "original" behavior. The prevailing hydrogen level was somewhat below 200 ppm during this test. Finally the 65 cc CO injection experiment was repeated. The hydrogen level was slightly more than 200 ppm and the decline of CO (and buildup of CO₂) was markedly slower than in the "original" 65 cc injection test. It could be surmised that hydrogen acts as an inhibitor for the disproportionation reaction, or possibly that the condition of the metal surfaces of the loop are very influential in determining the rates for this process.

In the second set of tests both CO and CO₂ were injected into purified helium in the loop in such proportions so as to reach the concentrations achieved at the end of the "original" 187 cc CO injection run. The results are shown in Figure 2.17 when the numbers "1", "2", and "3" simply allude to the chronological order in which the three runs were done. No hydrogen (or water vapor or methane) were observed during any of these runs. It is clear that a smooth extrapolation of the first part of the 187 cc CO injection test, beyond the end of the test (see "1" in Figure 2.16) would not result in the curves depicted in Figure 2.17. These latter curves indicate a relatively accelerated rate of disproportionation compared to what an extrapolation of the curve in Figure 2.16 would suggest.

The last set of tests again started with the injection of 187 cc CO into purified loop helium. After about a day of operation, during which the CO declined and the CO₂ built up, the gases in the loop were evacuated using a vacuum pump. Immediately after the pump-down, fresh helium was introduced into the loop and appropriate amounts of CO and CO₂ were injected to simulate the concentrations prevailing at the end of the first part of the test (i.e. before pump-down). The run was then continued. Figure 2.18 summarizes the observations for the two runs made in this fashion. The CO decline during the first part of the runs (dotted portion of the curves) was somewhat more rapid than the "original" case (see "1" in Figure 2.16) and then leveled out. It was at the beginning of

CO DECLINE AND CO₂ BUILD-UP FOLLOWING INJECTION OF 187cc CO

Figure 2.16

Figure 2.17

BEHAVIOR OF CO IN HIL

Figure. 2.18

each of these runs that the hydrogen concentration was found to be nil; after a short time the concentration of hydrogen reached a moderate level, slightly above 150 ppm. Following the pump-down and restart the CO decline was very slightly enhanced (solid portion of the curves) but approximated an extrapolation of the first part of the runs. During these last hours of the runs the hydrogen levels hovered around 100 ppm. Again, one can conjecture, based on the foregoing observations, that hydrogen tends to retard the disproportionation of CO. A series of tests in which the concentration of hydrogen is raised significantly from the levels found in these tests is planned for the coming quarter.

2.6 Safety Instrumentation and Control Systems (S. Nicolosi)

BNL has planned to install a research mass spectrometer into Fort St. Vrain as part of a novel effort to survey the coolant gas of an operating power reactor. Interest is directed to a description of the coolant composition with regard to all gaseous impurities. In addition to technical data, this is an opportunity to gain practical working experience in an operating power reactor.

The mass spectrometer is still under construction by the Nuclide Corporation. As a result of extensive delays and difficulties in contacting personnel at the company, a memorandum expressing extreme dissatisfaction has been sent to the buyer. Assistance from administrative personnel at Brookhaven is currently being sought to expedite the order.

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3. Structural Evaluation

3.1 Core Seismic (P. Bezler, M. Reich, H. Goradia)

A comparison between the analytical results using OSCVERT and the test results was obtained. Eight blocks were stacked in a single column in the test rig. The clearance on either side of the block was set to 3/8 inch. The linear portion of the spring deflection was 5/16 inch, after which the spring bottomed out on the stop brackets of the frame structure. A single displacement sine wave was applied in the horizontal direction in the column by the electromagnetic vibrator.

Three different frequencies were applied and the nature of the motion of the column was noted. The frequencies were selected because they were in the strongly nonlinear portion of the response curve where multiple roots exist. The same problem was solved by the computer program and the comparison of the qualitative results are shown in the following Table. The behavior of the column was the same in the test and in the analysis.

TABLE 3.1
Qualitative Comparison-Analysis and Test
Single Sine Wave Input

<u>Frequency</u> cps	<u>Input</u> <u>Displacement</u> inches	<u>Modal Response</u>	
		<u>Analysis</u>	<u>Test</u>
6	0.10	2nd mode	2nd mode
6	0.15	1st mode	1st mode
7	0.15	2nd mode	2nd mode
7	0.25	1st mode	1st mode
7	0.35	1st mode	1st mode
		(#8 mass jumps off pins)	(#8 mass jumps off pins)
8	0.15	2nd mode	2nd mode
8	0.30	1st mode	1st mode

For the input displacement of 0.10 at 6 cps, the second mode shape was obtained in both the test and the analysis. It should be noted that for this problem, the second mode response is smaller than the first mode response because the problem is nonlinear.

The investigation of the dynamics of a single-degree-of-freedom system with cubic nonlinearity was done with a decaying exponential term in the forcing function. The equation has the form:

$$m\ddot{x} + c\dot{x} + ax + \beta x^3 = F_1 e^{-\gamma_1 t} \cos \omega t - F_2 e^{-\gamma_2 t} \cos 3 \omega t$$

FIGURE 3.1

EFFECT OF DECAYING EXPONENTIAL ON MAXIMUM RESPONSE

FIGURE 3.2

PLAN VIEW - 3-D TEST RIG

Two sets of runs were made. The first was with $\gamma_1 = 0$ and the second was with $\gamma_2 = 0$. This study was done because of the significant effect that earthquake duration has on the dynamic response of the structure. The decaying exponential was introduced to shorten the duration of the term to which it is attached.

Figure 3.1 is for a forcing frequency of either:

- a) $f(t) = 1 \cos wt - 200 e^{-\gamma_2 t} \cos 3 wt$ shown by x, or
 b) $f(t) = 1 e^{-\gamma_1 t} \cos wt - 200 \cos 3 wt$ shown by •.

As γ_2 becomes smaller, the duration of the second term becomes larger. The successive points follow the curve previously reported for constant amplitude sine waves, starting out at the low end of the curve for $\gamma_2 = 1.0$ to $\gamma_2 = 0.2$, then following the same sloped portion of the curve as γ_2 is reduced from 0.2 to $\gamma_2 = 0.001$. Finally, when $\gamma_2 = 0$, the maximum value of the upper root of the nonlinear response curve is attained, as was also previously reported.

When the equation $f(t) = 1 e^{-\gamma_1 t} \cos wt - 200 \cos 3 wt$ was used ($\gamma_2 = 0$) the results shown on the left of Figure 3.1 were obtained. These results are important since they confirm the heuristic understanding we previously reported for the system. We stated that the response was a subharmonic one in which the $3 wt$ term was putting energy into the third harmonic component of the motion and that the first term was necessary only to provide enough starting motion to have a third harmonic component that was not mashed by the damping energy. Notice in Figure 3.1 that for a value of γ_1 from 0.1 to 0.058, the response is at the lowermost end of the nonlinear curve. In other words, there was insufficient time for the amplitude to build up so that the large subharmonic response could develop. But for $\gamma_1 \leq 0.056$ the maximum value of the upper root is attained. The critical duration of the first term is for a time corresponding to the decay $e^{-0.056t}$.

Design drawings of the three-dimensional test rig are near completion. Figure 3.2 is a copy of the plan view of the central portion of the test system. The blocks are to be arranged in seven columns as shown. Each column will have up to ten blocks. Each block is 3 inches high and 1.5 inches across the flats. The test structure will be capable of accepting input motions either along an axis through the flats or along a perpendicular axis through the pointed corners.

3.1.1 Modification of Current OSCVERT Program (M. Subudhi, P. Bezler, L. Lasker H. Goradia)

During this report period an effort was made to modify the existing OSCVERT computer code for HTGR core block dynamic response studies. The modifications were designed to reduce the computer running time as well as to enhance the accuracy of the results. The savings in computer time will be particularly advantageous when the program is extended from two-dimensional to three-dimensional analyses. The following summarizes the differences between the existing method and the modified method.

In program OSCVERT the system of dynamic equations for the planar motion of discrete blocks arranged in rows and columns and connected by linear and/or nonlinear force restoring elements such as springs and dampers are developed and solved. These equations are of the second order with time. These are then reduced to a new set of first order ordinary differential equations suitable for the GEAR method.

GEAR is an ordinary differential equation system solver containing the implicit Adams method, the backward differentiation formulas, and the methods or C. W. Gear as integrator options. In addition to a set of first order ordinary differential equations describing the system:

$$\frac{dy_i(t)}{dt} = f_i(y_i(t), t) \quad (3.1.1)$$

and given initial conditions:

$$y_i(t_0) = y_{oi} \quad (3.1.2)$$

where $i = 1, 2, \dots, N$ and N = number of first order equations

the GEAR routine requires an $N \times N$ Jacobian matrix at each time step given by:

$$J = \left(\frac{\partial f_i}{\partial y_j} \right)_{i,j=1}^N \quad (3.1.3)$$

GEAR has the capability of calculating the required Jacobian matrix in an approximate form using a finite difference scheme. This is termed the approximate Jacobian method. Alternatively, the user has the option to provide his own analytical expression in the matrix which defines the Jacobian. During this quarter, we have developed an optional Jacobian expression which appears to be more accurate than the original form as well as more economical. We have termed this approach the Exact Jacobian method.

In the existing OSCVERT program GEAR is supplied with the system equations but the Jacobian matrix is calculated by the approximate method. In the modified OSCVERT, the Jacobian matrix is calculated analytically and is input to GEAR together with the system equations.

The numerical computation of the Jacobian requires a careful choice of increments for the finite differencing. These increments are functions of certain parameters, the details of which are given in the GEAR manual. At each step, to calculate the entire Jacobian, GEAR calls routines in OSCVERT 'N' times. With each call a vector 'y', containing all the displacement coordinates, with one element incremented by Δy_i ($\hat{y}_i = y_i + \Delta y_i$) is operated on and a corresponding force vector 'f' developed. Then using finite differencing techniques all the contributions of the vector y_i in the Jacobian are calculated. As a result, the OSCVERT calculates the forces 'N' times in the approximate Jacobian method in comparison to one in Exact Jacobian method.

Mathematically, the difference in both the methods is well understood. Both methods adopt the expression:

$$\frac{\partial f_i}{\partial y_j} \approx \frac{f_i(y_j + \Delta y_j) - f_i(y_j)}{\Delta y_j} \quad (3.1.4)$$

However, in the second method (Exact) the right-hand side is subjected to the limit condition and hence is expressed as:

$$\frac{\partial f_i}{\partial y_j} = \lim_{\Delta y_j \rightarrow 0} \frac{f_i(y_j + \Delta y_j) - f_i(y_j)}{\Delta y_j} \quad (3.1.5)$$

Equation (3.1.4) can be interpreted as an approximate form of (3.1.5). Thus, the exact method should yield more accurate results in comparison to the approximate scheme.

It should be emphasized at this point that both equations (3.1.4) and (3.1.5) yield almost identical values provided $f_i(y_i(t), t)$ is a continuous (or piece-wise continuous) function and the first derivatives of f_i with respect to $y_i(t)$ exist. For the present HTGR core block models, the functions f_i possess first derivatives and, since they are piece-wise continuous, they can be calculated analytically. For the approximate method errors only occur when the system is close to a point of discontinuity in the forcing functions. The two methods differ significantly at these points. The degree of accuracy depends on the magnitudes of this difference and the choice of increments for the finite differencing.

In summary, the Exact Jacobian method has two advantages over the previously used method. Firstly, the time in formulating the Jacobian matrix can be markedly reduced by this method. As an illustration, a 3 x 3 nine mass model was run by both methods for the same computer time limit. It was found that the Exact method yielded an 80 percent greater output. For larger models, and in particular three-dimensional models, this savings in computer time would be distinctly beneficial. Secondly, with the strong discontinuities introduced into the present models by the spring-gap elements and friction, the approximate method will develop errors. These may be completely eliminated by employing the Exact method.

In conclusion, the modified OSCVERT code should be superior to the existing code. Both time savings and somewhat better accuracy should be realized with this method. However, at discontinuity points, both methods will produce errors in the Jacobian. With the approximate method these errors persist over a region in close vicinity to the point as well. Because the discontinuities are of a local nature the errors produced by either method do not appreciably affect the overall response characteristics.

3.2 Steam Generator Tube Weld Evaluation (M. Reich, M. Subudhi, P. Bezler)

This is the fourth reporting on the investigations of the structural integrity of the HTGR steam generator tube weldments. During this period the progress includes:

- (1) completion of an inelastic time-independent analysis of a bimetallic weldment in the superheater section of the steam generator; and
- (2) the initiation of a feasibility study on the development of models to include the parameters of the welding process in the calculation of the magnitude, direction, and distribution of residual stresses at the welds.

3.2.1 Time-Independent Inelastic Analysis

Based on the elastic results reported in the previous quarterly report, the bimetallic weld was found to exhibit the most severe stress condition at the fusion line between the parent metal I-800 and the filler metal I-82 when under normal operating conditions. In all the previous studies the joint materials were assumed to act elastically, however, if the total temperature range was large, the materials could be stressed to the plastic regime.

In this study an attempt was made to simulate the joint behavior during the cooldown from the heat treatment temperature to the normal operating temperature. Creep of the materials was not considered; only an elastic-plastic analysis is being performed.

The finite element model was based on the GA data reported in the last quarterly. The model incorporated straight lines at the material interfaces. Heat-affected or decarburized zones that exist in a real weld were considered.

The loading history input to the analysis was calculated corresponding to the GA cold tube data reported in the last quarterly. The external and internal fluid pressures were applied as step inputs since they alone were not large enough to cause plastic deformation. The temperature distributions were broken into ten pseudo-time steps for an incremental loading pattern for the analysis. Only one cycle of operation was considered.

A finite-element approach was adopted for this analysis. A stress-free state at the postweld heat treatment temperature (1350°F) was assumed. Temperature-dependent material properties such as modeling of elasticity, Poisson's ratio, coefficient of thermal expansion, and yield stress were used because of the large temperature range experienced at the joint. For the heat-affected zone in the 2 1/4 Cr - 1 Mo steel, the mechanical properties were taken to be those for normalized and tempered material. For the HAZ in the Incoloy-800 the parent material properties were used because of the absence of any available data.

FIGURE 3.3

PLASTIC DEFORMATIONS OF THE TUBE WALL OF A BIMETALLIC WELDMENT IN SUPER-
 HEATER FOR NORMAL OPERATING CONDITIONS CORRESPONDING TO GA COLD TUBE DATA

It was anticipated from the previously reported elastic results that the only region which would experience plastic deformation was the I-800 base metal at the fusion zone. The results of the present analysis confirmed this observation.

From the beginning of the cooling process the yield zone gradually expands as the temperature is decreased reaching its largest size at an average tube temperature of 1000°F, shown in Figure 3.3. In this temperature range the thermally induced stresses increase while the yield stress of the material decreases resulting in the large plastic zone. With a further decrease in tube temperature the yield stress increases causing the plastic zone to disappear completely. On still further temperature reductions, the yield zone gradually expands again due to the fact that the thermal stresses increase faster than the increase in the yield stress. For the present loading conditions, this plastic zone at the normal operating conditions was found to be smaller than the one at 1000°F.

Although the plastic zones changed their sizes as the weld experienced different temperature changes, plastic strains were accumulated during the same process. It has been noticed that the plastic zones existed at the material interfaces. The results showed that the highest accumulated plastic strain was ~.8 percent in the parent material I-800 next to the fusion line.

It is evident from Figure 3.3 that the inner face of the tube experiences larger plastic deformation than the outer face. This is because of the fact that the inner face experiences tensile stresses due to both pressure and temperature loadings whereas the outer face experiences tensile stresses due to pressure, but compressive stresses because of the thermal cooldown. As a result, the inner side of the wall experiences the plastic deformation first due to the cumulative stress effects.

Finally, it can be concluded that a welded joint would experience plastic strains when cooled down from the post heat-treatment temperature to the normal operating conditions. The stress conditions would be even more severe if allowed to cool down to room temperature in every cycle of reactor operations as is the actual case. The extent of plasticity and the total plastic strain would also be sensitive to the manufacturing process parameters and the routine of service preparation.

3.2.2 Residual Stress Analysis

Previous studies indicated that the presence of residual stresses and distortions induced by the welding operation would cause a more severe stress condition to exist in the welded structure under the normal operating conditions. The objective of this research is to develop mathematical models for calculating the magnitude, direction, and distribution of residual stresses at the bimetallic welds in the superheater section of the steam generator.

The prediction of the residual stress state and deformations in the welded structures is one of the most difficult as well as expensive tasks. However, at the present time we presume to have the facilities to perform the entire thermo-mechanical analysis of the welded structure using an axisymmetric finite element model. Some references are given at the end where similar techniques were adopted for this type of analysis.

A comprehensive analysis procedure is proposed at this stage of the investigation. A nonlinear transient finite element solution, including latent heat effects, material property temperature dependence, and thermal radiation boundary conditions will be obtained. This transient temperature distribution will then be used as a forcing function for a finite element analysis, using incremental plasticity theory with temperature dependent mechanical properties. However, the creep deformation and stress relaxation during post-weld heat treatment and the deformation arising during the molten state will not be considered at this stage of study.

We expect this study will yield a more thorough understanding of the welding process in addition to the safety study of thousands of welds present in the steam generator tube bundles.

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4. Analytical

4.1 Evaporation of the Nonvolatile Fission Products in the Core of an HTGR Following a Loss of Coolant Accident (J. M. Dickey)

Following a loss of coolant accident, the temperature of the core of an HTGR starts to increase. After about 10 hours the maximum temperature in the core is about 4800°F. As the core heats up the fission products are able to escape from the fuel particles. The nonvolatile fission products are not immediately thereupon released, as they must migrate through the graphite moderator and along the coolant channels. A code EVAP has been written, which emphasizes the evaporation stage of the problem. The code has been described briefly in a previous quarterly report (Dickey, 1977) and will be described in detail in a forthcoming topical report. Some of the results calculated using the code are reported here.

The model of the core and the data used are the same as in SORS (Schwartz, 1974). The core and top reflector blocks are divided into a number of segments and the temperature is taken to be uniform in each segment. The temperature history of the core is taken from the General Atomic code CORCON (Schwartztrauber, 1974). Figure 4.1 shows the release curves for 5 elements for the coolant channels in the control portion of the core. One of the most important parameters determining the rate at which fission products can be swept out of the core is the coolant flow rate. This has been estimated by GA using the code RECA. The value used for the flow rate in Figure 4.1 is 5.67×10^3 moles/hr. Figure 4.2 shows the effect of multiplying the flow rate by 10, 2, or 1/2. The release curve is very sensitive to the choice of the flow rate. These curves are for the central part of the core, the effect of the different temperature histories in other parts of the core are shown in Figure 4.3. The core is divided into 14 vertical regions, region 1 being the central region. The outer regions are cooler and so the release occurs more slowly.

4.2 Gas Dynamics in the Containment Vessel (J. Beerman, J. Boccio, J. Skalyo, Jr., H. Takahashi)

The problem of understanding the gas dynamics of a PCRV depressurization into a CV has been approached both analytically and experimentally. A small scale (1:100) model for CV/PCRv gas dynamic interaction study has been assembled and preliminary experiments have been performed with present emphasis placed on qualitative visual observations. These initial tests have a twofold purpose, namely, first, to determine by visual observations the existence and/or extent of layering within the CV model under various experimental test conditions, and second, to quantify the requisite instrumentation required for experimental/analytical evaluation.

To date, the experimental procedure has been to prepressurize a 2.25 liter steel cylinder (the PCRv) with a binary mixture of NH_4Cl particles and helium and to charge a 60 liter glass bell jar (the CV) with SF_6 . For all test conditions to be reported initial gas temperatures in both containers was 60°F. Also to insure the pressure in the bell jar does not exceed ambient pressure after depressurization, initial charge pressure has been approximately 4.5 psia. Subsequent depressurization is effected by rapid opening of a valve separating the two precharged containers with visual observation of the flow interactions

Figure 4.1. Fraction of element released from the central portion of the core.

Figure 4.2. Fraction of Eu released from the central portion of the core for 4 different flow rates.

Figure 4.3. Fraction of Eu released from different regions of the core. The core is divided into 14 vertical regions, the first region is the innermost region.

provided by the mixing of the NH_4Cl cloud.

Table 4.1 shows a series of tests which were performed using two different exhaust nozzle configurations and various helium charge pressures. The first nozzle was a sonic nozzle of diameter 0.042 inch while the second was a Mach 3 nozzle of diameter 0.020 inch; a 100 in² break in an HTGR would scale to a orifice ~ 0.1 inch in diameter. The parameters Π_i are the modeling parameters discussed in previous reports and are defined as:

$$\Pi_1 = \frac{P_c}{P_o}$$

$$\Pi_2 = \frac{\rho_c}{\rho_o}$$

$$\Pi_3 = \gamma_1$$

$$\Pi_4 = \gamma_2$$

$$\Pi_5 = \frac{\rho_{og} D}{P_o}$$

where

- P = initial gas pressure in the CV
- P^c = initial pressure of gas in the PCR
- ρ_o = initial density of the CV gas
- ρ^c = initial density of the PCR
- γ_1 = ratio of specific heats of the CV gas
- γ_2 = ratio of specific heats of the PCR gas
- g = acceleration due to gravity
- D = a characteristic system dimension (we have selected the CV diameter).

The NH_4Cl particles were not sized but appear to be submicron since settling did not appear for periods as long as ~ 10 minutes. The main visual observation was that in all cases the jet would strike the top of the CV and an unimpeded flow would follow down the sides of the bell jar; in all cases except runs 5 and 10 complete mixing of the PCR and CV gases seemed apparent.

For runs 5 and 10, the downward flow along the bell jar walls separated from the walls approximately 3 to 4 inches from the bottom resulting in a rather clear demarcation between fully mixed PCR/CV gas and partially (or totally) unmixed CV gas. From an experimental viewpoint this is clearly understandable since these two tests were performed at rather low precharged pressure ratios. The extrapolation of these observations to full scale HTGR behavior can not, as yet, be made since all scaling parameters have not been duplicated. Suffice it to say that from these observations, the degree of CV/PCR mixing is highly time dependent. In other words, for depressurizations at initially large pressure ratios, the degree of mixedness is rather large (and maybe even constant

Table 4.1

Test Series

<u>Run</u>	<u>Nozzle</u>	<u>P_o (psia)</u>	<u>Π_1</u>	<u>Π_2</u>	<u>Π_3</u>	<u>Π_4</u>	<u>$10^4 \Pi_5$</u>
1	1	220	.02	.73	1.4	1.67	.096
2	1	110	.04	1.4	↓	↓	↓
3	1	55	.08	2.9			
4	1	28	.16	5.8			
5	1	14	.32	11.7			
6	2	220	.02	.73			
7	2	110	.04	1.4	↓	↓	↓
8	2	55	.08	2.9			
9	2	28	.16	5.8			
10	2	14	.32	11.7			
FULL SIZE HTGR			.02	.29	1.4	1.67	3.0

for the initial stages of depressurization), but, as the pressure ratio subsequently decreases to values like those in runs 5 and 10, the degree of mixedness reduces to zero. That this can not be observed visually is probably due to the masking effect of the NH_4Cl cloud during the initial stages of depressurization. From a strictly model experiment viewpoint clarification of this inference can only be made if gas sampling at the base of the bell jar can be made at various times during the run. Only then can the extent of global (on the scale of the bell jar) mixing be assessed.

The analytical effort on the CIRC code is continuing with emphasis being placed on the following problem areas, viz,

- . nonuniform mesh size distribution
- . CV geometry modifications
- . convective and viscous single component mixing.

Modification of the code to use a variable mesh size has been completed and is continually being checked. Experimental flow observations have indicated where more flow definition, i.e., mesh size reduction, is required.

The restrictive limitation of the inviscid flow approximation presently built into CIRC is presently being reduced by the incorporation of two additional turbulence related equations into the analysis.

Changes in CV geometry appropriate to spherically capped cylindrical vessels are being considered.

To date, studies in all these areas have resulted in a code which makes more effective use of computer core space by eliminating redundant core locations.

Although the code is still limited to single gas component systems, the viability of the numerical approach can be assessed by direct comparison of predictions with experimental measurements made in depressurization studies involving single component gas in both the bell jar and steel cylinder.

Recourse to a more highly complex (in the sense of coding and core size) multigas component CIRC code is deemed unwarranted until the major fluid dynamic factors (pressure and temperature time histories) have been compared with attendant experimental data obtained from the small scale experiments.

4.3 Preliminary Investigation of Turbulent Reactive Mixing in PCRV/CV Gas Mixtures - Equilibrium Chemistry and Buoyancy (J. L. Boccio)

Within the past quarterly report (Boccio, 1977) an analytical/numerical technique has been described for investigating the turbulent, reactive mixing within free shear layers. The fluid dynamic flow model under investigation is the free, turbulent mixing of two, possibly chemically reacting, coflowing streams involving N chemical species made up of L elements and undergoing R chemical reactions. The model described in that report has been based upon a boundary layer formulation for the mixing flow field, in which the effects of large density and temperature nonuniformities are included. Closure of the

momentum equation is obtained through a relationship between the turbulent shear stress and the turbulent kinetic energy. The turbulent kinetic energy equation is also written in parabolic form and the closure hypotheses used with this equation are identical to those used in the successful calculation of flows without chemical reaction (Launder, 1976). Conventional closure hypotheses also have been adopted for the mean flow energy equation and jet species equations. The inherent chemical production terms were modeled using the so called classical, phenomenological approach together with a simplified, albeit adequate, chemical kinetics scheme. Also reported were the results from two select numerical experiments involving the subsequent reactive mixing of possible PCRV/CV gas mixtures during the initial and final stages of a penetration failure. A major consequence of these calculations indicated that under certain penetration type failures finite rate kinetics must be considered and with regard to CV gas layering the a priori assumption on the degree of mixedness must be examined more closely.

During this quarterly, the aforementioned numerical model has been augmented to take into account (1) the possibility of infinitely fast reaction kinetics and (2) the effects of buoyant forces on the turbulent exchange mechanisms. The former option has relevance largely in reducing the computational time under flow situations where chemical equilibrium is an adequate assumption; the latter option has direct bearing in analyzing the subsequent behavior during post penetration failures.

In this regard, section 4.3.1 describes the requisite modifications in the model to include the chemical equilibrium model; section 4.3.2 describes briefly the effects of buoyant forces on the turbulent exchange mechanisms. The results of code implementation of these two options are presented in section 4.3.3.

4.3.1 The Equilibrium Chemistry Option

The following discussions rely heavily on the analytical/numerical framework described by Boccio and Colman (1977), and as such, only those equations pertinent to the present discussion will be addressed.

In the limit of infinitely fast chemical reactions, i.e.,

$$\lim_{\substack{t_{\text{flow}}/t_{\text{chem}} \rightarrow \infty \\ t_{\text{chem}} \rightarrow 0}} (t_{\text{flow}}/t_{\text{chem}}) \rightarrow \infty$$

it can be shown that the phenomenological expression for the chemical production term, $\dot{w}_i$, entering in the i^{th} species conservation equation, i.e.,

$$\rho u \frac{\partial \alpha_i}{\partial x} + \rho v \frac{\partial \alpha_i}{\partial r} = r^{-1} \frac{\partial}{\partial r} \left\{ \frac{r \mu_T}{Sc} \frac{\partial \alpha_i}{\partial r} \right\} + \dot{w}_i; \quad i = 1, 2, \dots, N$$

becomes undefined precluding the use of the above equations to determine the mixture composition.

However the subset $\{\alpha_i\}$ of dependent variables can be replaced by the subset $\{\tilde{\alpha}_j\}$ of dependent variables where $\tilde{\alpha}_j$ defines the mass fraction of element, j , which is governed by the equation

$$\rho u \frac{\partial \tilde{\alpha}_j}{\partial x} + \rho v \frac{\partial \tilde{\alpha}_j}{\partial r} = r^{-1} \frac{\partial}{\partial r} \left\{ \frac{r \mu_T}{Sc} \frac{\partial \tilde{\alpha}_j}{\partial r} \right\} \quad j = 1, 2, \dots, L$$

and where the element mass fraction is related to the specie mass fraction through algebraic relations of the form

$$\tilde{\alpha}_j = \sum_{k=1}^N \mu_{jk} \alpha_k W_j / W_k \quad j = 1, 2, \dots, L$$

where μ_{jk} defines the number of atoms of element j in species k . Numerical integration of the governing system of differential equations yields for each generic node point values for the set of dependent variables $\{H, u, k, \varepsilon, \tilde{\alpha}_1, \tilde{\alpha}_2, \dots, \tilde{\alpha}_L\}$. Thus, to determine the chemical equilibrium composition at this point one can then usually find the set $\{\alpha_i\}$ which minimizes the Gibbs Free Energy subject to the constraints of known enthalpy, h ($h \equiv H - \frac{1}{2} u^2$), pressure, p , and $\{\tilde{\alpha}_j\}$. The standard solution technique is through the use of Lagrange's undetermined multipliers. However, for rather simple chemical systems this technique can be replaced by a less sophisticated approach requiring knowledge of the requisite equilibrium constants, K_{pr} , as a function of temperature, T . Accordingly, to determine the equilibrium composition of the N species subject to L elemental material balance equations one needs only $N-L$ statements of chemical equilibria.

Specifically, for the present chemical system, i.e., $H_2, O_2, H_2O, CO, CO_2, O, H, OH, N_2$ and He the following equilibrium constants may be set:

to yield together with the element conservation equations a set of coupled algebraic equations, viz,

$$X_{CO} + P^{\frac{1}{2}} K_{P_0} X_{CO} X_{O_2}^{\frac{1}{2}} = \tilde{\alpha}_c W_{mix}/W_c$$

$$2X_{H_2} + 2 P^{\frac{1}{2}} K_{P_4} X_{H_2} X_{O_2}^{\frac{1}{2}} + P^{-\frac{1}{2}} K_{P_2} X_{H_2}^{\frac{1}{2}} + K_{P_3} X_{H_2}^{\frac{1}{2}} X_{O_2}^{\frac{1}{2}} = \tilde{\alpha}_H W_{mix}/W_H$$

$$2X_{O_2} + P^{\frac{1}{2}} K_{P_4} X_{H_2} X_{O_2}^{\frac{1}{2}} + X_{CO} + 2P^{\frac{1}{2}} K_{P_0} X_{CO} X_{O_2}^{\frac{1}{2}} +$$

$$P^{-\frac{1}{2}} K_{P_1} X_{O_2}^{\frac{1}{2}} + K_{P_3} X_{H_2}^{\frac{1}{2}} X_{O_2}^{\frac{1}{2}} = \tilde{\alpha}_o W_{mix}/W_o$$

$$X_{H_2O} = P^{\frac{1}{2}} K_{P_4} X_{H_2} X_{O_2}^{\frac{1}{2}}$$

$$X_{CO_2} = P^{\frac{1}{2}} K_{P_0} X_{CO} X_{O_2}^{\frac{1}{2}}$$

$$X_O = P^{-\frac{1}{2}} K_{P_1} X_{O_2}^{\frac{1}{2}}$$

$$X_H = P^{-\frac{1}{2}} K_{P_2} X_{H_2}^{\frac{1}{2}}$$

$$X_{OH} = K_{P_3} X_{H_2}^{\frac{1}{2}} X_{O_2}^{\frac{1}{2}}$$

where the mole fractions $X_k \equiv W_{mix} \alpha_k / W_k$. Solutions of this set of equations together with solution of the dependent variable set $\{H, u, k, \epsilon, \tilde{\alpha}_1, \tilde{\alpha}_2, \dots, \tilde{\alpha}_L\}$ have been incorporated in the aforementioned numerical code thus providing solution through one of three chemical options, i.e.,

- . frozen chemistry
- . finite rate chemistry
- . equilibrium chemistry

4.3.2 Extension of the Turbulence Mixing Model for Buoyant Flows

Accountability of buoyant forces in turbulent mixing flows requires not only the addition of buoyant forces into the momentum equation but also some realistic description of the effects of buoyancy on turbulence production and dissipation. Work has been published (Taminini, 1975; Rodi, 1975; Plumb, 1977) demonstrating the ability of modified k - ϵ models in calculating buoyant plumes. In these works, the contribution of buoyancy to the turbulence kinetic energy and dissipation rate is made by implementing a third transport equation for the mean square density fluctuations. Thus, in this context, the governing differential equations for the turbulence are:

Turbulent Kinetic Energy:

$$\rho u \frac{\partial k}{\partial x} + \rho v \frac{\partial k}{\partial r} = r^{-1} \frac{\partial}{\partial r} \{ r \mu_t \sigma_k^{-1} \frac{\partial k}{\partial r} \} + \mu_t \left(\frac{\partial u}{\partial r} \right)^2 - \rho \epsilon + B_k$$

Turbulence Dissipation:

$$\rho u \frac{\partial \epsilon}{\partial x} + \rho v \frac{\partial \epsilon}{\partial r} = r^{-1} \frac{\partial}{\partial r} \{ r \mu_t \sigma_\epsilon^{-1} \frac{\partial \epsilon}{\partial r} \} + C_{\epsilon 1} \frac{\epsilon}{k} \mu_t \left(\frac{\partial u}{\partial r} \right)^2 - C_{\epsilon 2} \frac{\rho \epsilon^2}{k} + B_\epsilon$$

Variance of Jet Fluid Concentration:

$$\rho u \frac{\partial g}{\partial x} + \rho v \frac{\partial g}{\partial r} = r^{-1} \frac{\partial}{\partial r} \{ r \mu_t \sigma_g^{-1} \frac{\partial g}{\partial r} \} + C_{g1} \mu_t \left(\frac{\partial f}{\partial r} \right)^2 - C_{g2} \rho \epsilon g/k$$

where g describes the variance of the function f which, for the problem under discussion, denotes the mass fraction of injected helium, i. e.,

$$g \equiv \overline{(f - \bar{f})^2} = (\alpha_{He} - \bar{\alpha}_{He})^2; \quad \alpha_{He} = \bar{\alpha}_{He} + \alpha'_{He}$$

or simply f represents the instantaneous mass fraction of helium. The above three equations macroscopically relate:

advection of a scalar quantity = diffusion of a scalar quantity
+ generation of a scalar quantity
- dissipation of a scalar quantity

with the parameters B_k and B_ϵ symbolically representing the effects of buoyancy on the generation of a scalar quantity and are given by

$$B_k \equiv - C_\rho \rho a_g \overline{(\rho'^2/\rho^2 k)}^{1/2}$$

$$B_\epsilon \equiv - C_{\epsilon 3} \rho a_g (\epsilon/k) \overline{(\rho'^2/\rho^2 k)}^{1/2}$$

where C_ρ and $C_{\epsilon 3}$ are constants and a_g represents the acceleration due to gravity. To close this system, it is proposed that

$$\overline{(\rho'^2/\rho^2 k)}^{1/2} \sim (g k)^{1/2}$$

which closely follows the arguments of Launder and Spalding (1972).

Modifications to the turbulence model, as described by Launder, et al (1976), taking into account the aforementioned buoyancy effects have been incorporated into the numerical model.

4.3.3 Numerical Results

During this reporting period, the equilibrium chemistry option has been augmented into the overall turbulent reactive mixing code, CHEMX, and two sample cases have been run. The basic input differences in these two cases are delineated as follows:

Test Case	T_{jet}/T_{amb} (°K)	U_{jet}/U_{amb} (ft/sec)	α_{H_2}	α_{CO}	α_{He}
1	600/300	1700/17	.03	.35	.62
2	1000/300	1700/17	.01	.13	.86

Test case No. 1 represents an initial PCRV inventory of 400 moles of CO and H₂ in an otherwise pure PCRV helium inventory; test case No. 2 represents 100 moles each of H₂ and CO in a PCRV helium inventory. Computer generated plots of temperature and He, H₂O, CO₂ concentrations for each of these cases are respectively depicted in Figures 4.4 and 4.5. The respective temperature distributions (Figures 4.4a and 4.5a) clearly indicate a zone (or sheet) of maximum temperature well within the mixing region. This simply reflects the fact that as air becomes entrained there is at each axial station a point exhibiting a stoichiometric mixture of H₂, CO, and O₂ and, as such, a point (from a chemical equilibrium standpoint) of maximum temperature. This region of maximum temperature spreads laterally in the axial direction due to the effects of previous mass and thermal diffusion. These trends are also manifest in the composition of the major equilibrium products of combustion, i.e., H₂O and CO₂ as indicated by examining Figures 4.4b and 4.5b and Figures 4.4c and 4.5c. The last figure in each set simply portrays the distribution of He within the jet. It must be emphasized that these results have been generated from test cases using inputs that have no direct bearing on, as yet, possible penetration failure scenarios. Their main purpose is to show the viability of the options selected, and, in a qualitative sense, the true behavior subject to the assumptions and/or restrictions imposed by the mathematical model.

In Figure 4.6, the relative importance of buoyant forces as compared to inertia forces, for test case No. 1 conditions, is indicated. This figure shows the axial distribution of the ratio of the Grashof number to the square of the Reynolds number. For buoyant forces to be of significance this ratio, i.e. $Gr/Re^2 \sim O(1)$; and, as the figure shows, that although buoyant effects are increasing with axial distance, their overall effect on the jet structure is negligible. Indirectly this indicates that the initial stages of a penetration failure are highly dominated by inertia effects and that layering during this initial stage of penetration is highly unlikely. In this regard, a more definitive conclusion can only be drawn once modeling of the exhaust jet/CV wall interactions have been performed.

Investigation of the effects of buoyancy on the turbulent mixing process are depicted in Figure 4.7. The results shown are based on input data similar to test case No. 2 above except that the frozen flow option was exercised. To show the relative effects of buoyancy two runs were performed where the Froude number ($Fr = U_{jet}^2/g d_{jet}$) equals 150 and 2, respectively. As the figure shows,

Figure 4.4a. Spatial temperature distribution, Test Case 1.

Figure 4.4b. H₂O concentration spatial distribution, Test Case 1.

Figure 4.4c. CO₂ concentration spatial distribution, Test Case 1.

Figure 4.4d. He concentration spatial distribution, Test Case 1.

Figure 4.5a. Spatial temperature distribution, Test Case 2.

Figure 4.5b. H₂O concentration spatial distribution, Test Case 2.

Figure 4.5c. CO_2 concentration spatial distribution, Test Case 2.

Figure 4.5d. He concentration spatial distribution, Test Case 2.

Figure 4.6. Axial Variation of Buoyant Force/Inertia Force, Test Case 1.

Figure 4.7. Centerline velocity variation for two values of Froude Number, Test Case 2.

for large buoyant forces (relative to the inertia forces) the jet centerline velocity initially accelerates due to buoyancy and then gradually decays due to the subsequent mixing with the colder, higher molecular weight ambient gas. For the case where $Fr = 150$ there is a slight effect of the initial buoyant forces on the centerline velocity history. Although direct comparisons with experiments have not been made, these results, thus reported, do predict qualitatively the physical trends. Further investigation in these areas is forthcoming.

4.3.5 Summary

To date, a unified turbulent multicomponent mixing model has been described. The numerical code has the options of treating frozen, finite rate, or equilibrium chemistry involving the species CO , H_2 , H_2O , CO_2 , O , OH , H , N_2 , O_2 , and He . Effects of buoyancy on the turbulent structure have, based upon recent theoretical analyses, been incorporated into the code. Select numerical experiments, exercising these various options, qualitatively portray the ensuing mixing flow structure.

4.4 A Study of Flammability Under the Influence of Large Ignition Sources (R. A. Strehlow, K. R. Sivier - University of Illinois)

4.4.1 Description of Project

This project has two parallel tasks. In the first task, a large, cylindrical chamber (.45 meters diameter, 1.82 meters length) will be used to hold lean mixtures of carbon monoxide, hydrogen, helium and air to simulate those mixtures found in an HTGR containment vessel subsequent to blowdown and either the reaction of air or steam with the graphite core. These lean mixtures will be ignited by a relatively large pocket of near stoichiometric mixtures of hydrogen, carbon monoxide-air. The purpose of this task is to determine how much a large ignition source widens the lean flammability limits of these mixtures.

The second task of the study involves the use of a standard flammability tube apparatus (50 mm diameter, 1.8 meters long) mounted vertically with ignition from either the upper end or the lower end with the opposite end closed at the time of ignition. The same mixtures that are studied in the large chamber will be studied in this flammability tube to determine their conventional flammability limits for comparison purposes. The point here is that since the mixtures of interest to the HTGR Safety Group are rather unusual they have not been tested in a standard flammability tube. Thus, there is no comparison data available for any information which may be developed using the large chamber, and standard flammability apparatus testing must be performed.

4.4.2 Progress to Date

The design of the experimental apparatus, the acquisition of materials and equipment for the experiment and the construction of the apparatus represent the only activities during this quarter. At the end of this quarter the gas metering system design has been set and all the components for this apparatus have been ordered. To assure high accuracy and to also assure safety in handling large quantities of combustible mixtures, we decided that the mixtures would be prepared as they are used right in the chamber in which they are to be used. With

this design there is no necessity to have large storage tanks for the combustible mixtures. The flow systems that feed the apparatus will meter the gases using critical flow orifices. These are very appropriate because the flow rate through such an orifice is independent of downstream pressure. Thus, we can evacuate the large chamber and fill it from zero pressure to the desired pressure with a known mixture simply by monitoring the pressure upstream of the orifice and switching the flow at the time we want to stop filling. Before and after the filling process, the combustible mixture will be vented out of the building. The standard flammability tube apparatus will be filled in the same manner except the tube will not be evacuated prior to the filling. The filling process will just take place at atmospheric pressure. Normally we fill this tube by displacing approximately 10 tube volumes with combustible mixture. We had been using rotometers in our previous experiments with the flammability tube but these are much less accurate than critical flow orifices and high precision pressure gauges. The metering systems will be constructed during the next quarter.

The large chamber requires the preparation of a new end plate. This 600 pound piece of steel is now being machined in our shop and should be ready for installation in mid-January. The small "standard" tube has been constructed and a laboratory location for it has been selected. It has not been mounted as yet but should be soon. Additionally, components for pressure measurement in the large tube have been ordered and some of the components have been received.

4.5 HTGR Code Library (J. Colman, J. Beerman)

Work is continuing on the CHAP code received from LASL (Gilbert, 1977). Some problems have been encountered due to inconsistencies between the code and the users manual in the areas of data input and default values. It is expected that these will be resolved shortly so that test runs may be made to exercise the program as much as possible. Problems associated with the restart option have been solved for restart after the steady state calculation which should save a considerable amount of computer time in making further transient runs.

Table 4.2 summarizes the status of the 60 codes now being developed or maintained in the HTGR Safety Code Library.

4.6 Response of Thermal Insulation Cover Plate to Turbulent Flow Noise (H. Fenech - University of California at Santa Barbara)

The response of the PCRV thermal insulation cover plate to turbulent flow noise has been analyzed. The results are summarized below.

The base case considered was a 3 ft x 3 ft x 1/4 in. carbon steel plate (AISI-1020) with singly supported edges and a failed central attachment. The properties of that steel at 700°F are: Young's modulus $E = 24.8 \times 10^6$ psi, Poisson's ratio = 0.3, density $\rho = 518$ lbm/ft³. The constants α and β used in the log-log linear fit of the SN curve given in the ASME Pressure Vessel Code are $\alpha = 3.595$, $\beta = 3.25 \times 10^{20}$. The damping ratio ξ was determined experimentally for a multiple number of cases with variable torque on the holding bolts. This damping ratio was found to be mode and space dependent. For the purpose of this analysis the damping ratio of the first natural mode of frequency (204 rad/

Table 4.2

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.
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.

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.

Table 4.2 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
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	C	P	Calculates time dependent flow distributions and fuel and coolant temperatures in the primary system.
CORCON	OP	P	Computes the temperature history and fission product redistribution following a loss of all convective cooling of the core.
SORS	OP	P	Computes the release of volatile fission products from an HTGR core during thermal transients.
GOPTWO	OP	NP	Analyzes the steady state graphite burnoff 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.

Table 4.2 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
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.
HILDA	OP	NP	Analyzes data acquired from the Helium Impurities Loop.
OSCIL	OP	NP	Simulates one-dimensional spring mass system for seismic analysis of HTGR core.
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.

Table 4.2 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
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 Mössbauer 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.
SWIRL-T	B	NP	Calculates fluid dynamics for two-dimensional time-dependent recirculating flows.
CHAP	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	L	NP	Calculates fission product release from a reactor containment building.
SUVIUS	L	NP	Solves the behavior of fission gases in the primary coolant of a gas-cooled reactor.
FYSMOD	L	NP	Calculates the two-dimensional solution of HTGR core blocks subjected to external motion.
NONSAP-C	L	NP	Calculates static and dynamic response of three-dimensional reinforced concrete structures, in addition to creep behavior.

Table 4.2 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
LARC-1	L	NP	Calculates fission product release from BISO and TRISO fuel particles of an HTGR during the LOFC accident for single isotopes.
LARC-2	L	NP	Similar to LARC-1; in addition, handles release from isotope chains.
QUIL	C	NP	Solves complex equilibrium distributions in chemical environments.

sec) at the plate center was used. For a plate with fresh Kaowool and a normal torque on the retaining bolts of 50 ft lb the measured damping ratio was $\xi = 0.01$. When the Kaowool loses its resilience or is removed accidentally the damping ratio reduces to $\xi = 0.001$. These two extreme values were used in the analysis. Typical stress response spectra with $\xi = 0.01$ for turbulent boundary, vortex shedding and flow impingement noise as reported earlier were used as input forcing functions. Since the vortex shedding is the worst offender in the cover plate failure, and also because of the difficulty in predicting accurately the peak frequency and the amplitude of the pressure spectrum, 6 additional cases were analyzed with peak frequency at 235; 310; 385 rad/sec and amplitude of 15 and 30 $(\text{lb/ft}^2)^2/\text{rad}$ corresponding to noise levels of 156.9 and 160 dB (ref. 2×10^{-4} μbar), respectively. The results obtained, using the quasistationary model, are given in Table 4.3 for the lifetime and Table 4.4 for the rms maximum displacement and stress, for those values of the damping ratio $\xi = 0.01, 0.001$. At a noise level of 156.9 dB and a damping ratio $\xi = 0.01$ a shift in peak frequency from 385 to 235 rad/sec (toward the natural plate frequency of 206 rad/sec) reduced the plate cover lifetime from 16.64 days to 31.8 seconds. At a noise level of 160 dB the corresponding lifetime reduction is from 4.78 days to 9.17 seconds. At 235 rad/sec, the loss of Kaowool resiliency would further reduce the lifetime to failure down to 3.2 seconds at 156.9 dB and 1.06 seconds at 160 dB. It can thus be concluded that the ignorance factor in the prediction of the vortex shedding noise amplitude and pressure spectrum can make the difference between a detectable and "after the fact" failure.

Variations of the materials properties and their endurance limits in the range of $\pm 10\%$ on the SN curve were simulated by changing the value of β , where β is the constant in the SN curve fitting relation $S^\alpha N = \beta$. The results are given in Table 4.5. At a noise level of 156.9 dB, 235 rad/sec, $\xi = 0.01$, a reduction in the endurance stress of 10% reduces the lifetime from 31.8 seconds down to 21.8 seconds. For an increased noise level of 160 dB, 235 rad/sec, $\xi = 0.01$, the reduction in lifetime is from 9.17 seconds to 6.28 seconds. In general, departure from the SN curve due to material properties changes introduces variations in the failure lifetime ΔT_ℓ related to the fractional deviation on the endurance stress $\Delta S/S$ by

$$\Delta T_\ell / T_\ell = (1 + \frac{\Delta S}{S}) \alpha - 1$$

where α is the exponent in the SN curve fitting relation $S^\alpha N = \beta$.

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Table 4.3

Fatigue Lifetime of a 3' x 3' x 1/4" Carbon Steel Cover Plate
at 700°F Subjected to Vortex Shedding Noise - Quasistationary Model
(Plate Natural Frequencies = 206, 1030; 1854 rad/sec)

Amplitude ($1b_f/ft^2$) ² /rad	Vortex Shedding Frequency (rad/sec)	Damping Ratio ξ	
		0.01	0.001
15.0 SPL = 156.9 db	235	31.8 sec	3.2 sec
	310	1.52 days	1.52 days
	385	16.64 days	16.5 days
30.0 SPL = 160 db	235	9.17 sec	1.06 sec
	310	0.44 days	0.44 days
	385	4.78 days	4.78 days

Table 4.4

Maximum rms Displacement and Stress of a 3' x 3' x 1/4" Carbon
Steel Cover Plate at 700°F Subjected to Vortex Shedding Noise

Amplitude ($1b_f/ft^2$) ² /rad	Vortex Shedding Frequency (rad/sec)	Damping Ratio ξ			
		0.01		0.001	
		rms Displ. inches	rms Stresses lb/in ²	rms Displ. inches	rms Stresses lb/in ²
15.0 SPL = 156.9 db	235	0.497	1.6×10^4	0.926	3.05×10^4
	310	0.064	1.56×10^3	0.064	1.56×10^3
	385	0.0235	800	0.024	800.9
30.0 SPL = 160 db	235	0.7028	2.27×10^4	1.31	4.32×10^4
	310	0.091	2.2×10^3	0.09	2.2×10^3
	385	0.033	1.13×10^3	0.0335	1.132×10^3

Table 4.5

Effect of Variations of the Endurance Stress on the Fatigue Lifetime of a 3' x 3' x 1/4" Carbon Steel Plate at 700°F Subjected to Vortex Shedding Noise (Plate Natural Frequencies = 206; 1030; 1854 rad/sec)

Vortex Shedding Amplitude (1b _f /ft ²) ² /rad	Vortex Shedding Frequency (rad/sec)	Damping Ratio ξ					
		0.01			0.001		
		Endurance Stress σ Variations					
		-10%	0%	+10%	-10%	0%	+10%
15.0	235	21.8 (sec)	31.8 (sec)	44.9 (sec)	2.19 (sec)	3.2 (sec)	4.5 (sec)
	310	1.04 (days)	1.52 (days)	2.14 (days)	1.04 (days)	1.52 (days)	2.14 (days)
	385	11.39 (days)	16.64 (days)	23.43 (days)	11.30 (days)	16.5 (days)	23.38 (days)
SPL = 156.9 db							
30.0	235	6.28 (sec)	9.17 (sec)	12.92 (sec)	0.63 (sec)	1.06 (sec)	1.30 (sec)
	310	0.30 (days)	0.44 (days)	0.62 (days)	0.30 (days)	0.44 (days)	0.62 (days)
	385	3.27 (days)	4.78 (days)	6.75 (days)	3.27 (days)	4.78 (days)	6.75 (days)
SPL = 160 db							

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II. LMFBR SAFETY EVALUATION

SUMMARY

Design and construction of the boiling pool dispersal rig has neared completion. The γ -densitometer for use with this rig and for the hydrodynamic dispersal experiments has been designed and construction begun. Expected errors based on observation interval have been determined for the narrow-beam, colimated, 30 Ci source. Results from the first boiling pool dispersed test have been analyzed and briefly summarized herein. A report on this work will be issued shortly showing that churn turbulent models predict the behavior quite well for superficial vapor velocities up to twenty times the rise velocity and that dispersed flow is not visually observed at vapor velocities up to four times the Kutateladze dispersal limit. Work has continued on the boiling pool and the freezing work with a report on the latter to be issued shortly.

The Super System Code (SSC) development program now consists of three series of codes: SSC-L for loop-type designs of LMFBRs; SSC-P for pool-type designs; and SSC-S, an eventual heat link version for the loop-type designs. Although the entire SSC-L code was made operational on September 26, 1977, a number of modifications and improvements were made to this code. These include (1) coupling of both single-phase and two-phase coolant dynamics with the fuel heat transfer modules; (2) addition of control rod as a separate group of channels; (3) incorporation of the reactivity feedback effects; (4) debugging and exercising of the steam generating system; and (5) upgrading of the integration scheme for the loop hydraulic equations. Work plans on the SSC-P and SSC-S codes are being developed.

The SSC Code Validation effort consisted of reviewing and evaluating FFTF instrumentation with regard to function and location vis-a-vis SSC system code validation requirements. Sensitivity calculations were carried out with the FLODISC code to make some preliminary judgements on the adequacy of upper output thermocouples to represent the core transient temperature response.

1. Fast Reactor Assessment - Accident Sequence Studies (O. C. Jones)

1.1 Hydrodynamic Characteristics of Two-Phase Dispersal Systems (T. Ginsberg)

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 system which bracket the hydrodynamic parameters of fuel-steel boiling systems.

A. Experimental Investigations

A.1 Interim Test Section: Gas Injection Methods Evaluation

A Lexan test section has been designed for the purpose of evaluation of the proposed gas injection technique described by Ginsberg (1977a). This technique requires distribution of porous tubes through the volume of the test container. It has been shown (Ginsberg 1977b) that these tubes can cause a significant pressure drop within the two-phase flow field. An experimental evaluation of the effects of the injection tubes on the steady-state void distribution and on the statistical properties of the two-phase flow field will be carried out in the test apparatus shown schematically in Fig. (1.1).

The test apparatus contains provisions for insertion of porous plates at the bottom for use in adiabatic gas injection tests and also contains a pair of electrodes for diabatic, volume-heated boiling tests. The gas injection system will be employed in evaluation of the gas injection disturbance. The electrodes will be used to extend the two-phase boiling work (Ginsberg 1977a) carried out previously in a similar apparatus.

Experimental evaluation of the extent of gas-injection disturbance will be carried out in three parts:

- (i) Void distributions and statistical properties of the void field will be measured in pools of various static heights with gas injection through porous plates (as shown in Fig. 1.1).
- (ii) Step (i) will be repeated, but with roughened rods inserted horizontally into the flow field to simulate the gas injection tubes.
- (iii) The results of (i) and (ii) will be compared to determine the influence of the injection tubes.

Fabrication of the test container is near completion and is scheduled for delivery in December 1977.

The void distributions will be measured using a gamma densitometry system, the design of which is discussed below.

A.2 Design of Gamma Densitometer System

A fast response gamma densitometer system has been designed to measure the instantaneous chordal-average void fraction in the hydrodynamic dispersion experiments. The instantaneous void signal will be used to compute the planar average void fraction and to compute the statistical properties of the two-phase flow which may be used to characterize the existing flow regime (Jones 1974).

A schematic of the proposed apparatus cross section, along with the gamma source-detection system is shown in Fig. (1.2). The source-detector system will be mounted on a traversing device which will enable accurate positioning in both the axial and transverse (shown) directions.

The gamma source to be employed is Thulium-170, produced from irradiation of Thulium-169 in the BNL High Flux Beam Reactor. Thulium-170 has a half-life of 129 days and produces two gammas, of 84 Kev and 40 Kev energies, respectively. The 84 Kev energy gamma will be monitored using a single-channel analyzer. Cadmium-Telluride solid state detectors will be used in the system.

A narrow gamma beam diameter of 2 mm was chosen to provide good spatial resolution, and to avoid problems associated with non-linear spatial signal averaging in inhomogeneous void fields (Delhaye 1976). The detectors will be 2 mm square, and the collimators are designed accordingly.

A response system has been designed to provide sampling intervals of 1 msec. With this sampling time, the instantaneous void fraction can be measured by

Figure 1.1 Schematic of Gas Injected Pool.

linearizing the gamma attenuation signal. With long sampling times, errors arise because the void fraction is a non-linear function of the measured signal (Harms 1971).

The electronic system to be used to process the signal from the Cadmium-Telluride detectors is shown in Fig. 1.3. The timer will be equipped with 1, 10, and 100 msec averaging capability. The scalar BCD output will be directed to a Hewlett Packard digital acquisition system.

An error analysis has been performed to evaluate the error in the instantaneous void fraction measurement. For the system represented schematically in Fig. 1.2, a total number of uncollided gammas detected in time τ is given by

$$N = \frac{S_o A \eta \tau}{4 \pi R^2} \text{Exp} [-2\mu_{\text{Lex}} \ell] \text{Exp} [-\mu_{\text{H}_2\text{O}} (1 - \alpha) \omega] \quad (1.1)$$

where α is the average void fraction along the path of the gamma beam. Equation (1.1) is differentiated with respect to α . It can be shown that

$$\frac{d\alpha}{\alpha} = \frac{1}{\alpha \mu_{\text{H}_2\text{O}} \omega} \frac{dN}{N} \quad (1.2)$$

Figure 1.2 Schematic of Source-Detector System.

Figure 1.3 Nuclear Electronics System.

The errors in α and N are then

$$\frac{\epsilon_{\alpha}}{\alpha} = \frac{1}{\alpha \mu_{H_2O} \omega} \frac{\epsilon_N}{N} \quad (1.3)$$

But, the error in the counts recorded is

$$\epsilon_N = \sqrt{N} \quad (1.4)$$

So,

$$\frac{\epsilon_{\alpha}}{\alpha} = \frac{1}{\sqrt{N}} \frac{1}{\alpha \mu_{H_2O} \omega} \quad (1.5)$$

where N is given by Eq. (1.1)

The results of this analysis are presented in Fig. (1.4), for an air-water, two-phase system. The results show that with 1 msec sampling, accuracy in the void fraction measurement of better than 10 percent can be achieved for $\alpha > 0.15$, with a source intensity of 30 Curies. If a sampling time of 10 msec can be used, the inaccuracy of the instantaneous void fraction would be less than 6 percent for $\alpha > 0.10$.

Figure 1.4 Error Analysis Results

In the churn-turbulent flow regime (an important regime in transition phase studies) it is expected that $\alpha > .5$. In this case, the accuracy of the instantaneous void fraction measurement will be less than 3 percent for all sampling times greater than or equal to 1 msec. The time average void fraction, however, can be obtained with significantly better accuracy by averaging the instantaneous measurement as many times as necessary to get the desired accuracy.

The gamma densitometer system described here will also be used in conjunction with the interim test section discussed in Section A.1.

1.2 Liquid Dispersion in Internally Heated Boiling Pools (T. Ginsberg)

The objectives of this task are to conduct experiments and perform analysis to study the dispersive characteristics of internally heated boiling pools. Experiments using internal heat generation are used to simulate nuclear heating, and the two-phase boiling characteristics are studied in open and closed boiling systems with boundary heat losses.

A. Experimental Investigations (T. Ginsberg and C. E. Schwarz)

An informal report (Ginsberg 1977a) entitled "Observations of Flow Characteristics of Volume-Heated Boiling Pools," has been written. This report documents all the experimental work performed using the tall boiling pool apparatus (Ginsberg 1977c). In addition, the report summarizes previous boiling pool work in relation to flow characteristics. The BNL and other existing data for pool-average void fraction are compared with a model of two-phase flow field. A paper was presented at the 1977 ANS Winter Meeting, entitled, "Flow Regime Observations in Two-Phase Volume-Heated Boiling Pools." (Ginsberg 1977b).

Figures (1.5) and (1.6) show a set of typical photographic observations of behavior of volume-heated boiling pools. Presented with the data are values of the averaged void fraction α and the dimensionless superficial vapor velocity, $j_{g\infty}/U_{\infty}$. The collapsed height of the pool, H_0 , is 30 cm. The results indicate that for small $j_{g\infty}/U_{\infty}$ (directly proportional to power dissipation in the fluid) a densely packed boiling flow regime exists. As $j_{g\infty}/U_{\infty}$ increases, the flow undergoes a transition to a highly agitated, churn-turbulent regime.

Wallis (1969) suggests that a transition from bubbly to churn-turbulent flow should occur when $j_g/U_{\infty} = 0.3$. The photographs indicate that the bubbly regime is stable up to $j_{g\infty}/U_{\infty} \approx 1$. The Kutateladze dispersal relationship (Kutateladze 1960), applied to a water-air system, predicts the onset of dispersed flow when $j_g/U_{\infty} \approx 4-5$. Figure (1.6) shows that the churn flow regime is stable up to $j_{g\infty}/U_{\infty} = 9$. In fact, additional results indicate that the churn flow regime is stable at $j_{g\infty}/U_{\infty}$ up to nearly 20.

In addition to the photographic observations, measurements of pool-average void fraction were also measured in the experiments. The void fraction was computed from the collapsed pool height, H_0 and the boiled-up height, using the relation $\alpha = (H - H_0)/H$. A value of α was thereby obtained at each $j_{g\infty}/U_{\infty}$.

RUN NO.	000	006	007	003
$j_{g\infty}/U_\infty$	0.	.18	.36	.54
$\overline{\alpha}$	0.	.07-.10	.17	.24

Figure 1.5 - Photographs of Boiling Pool Flow Patterns $H_0 = 30$ cm.

RUN NO.

020

023

024

026

$j_{g\infty}/U_{\infty}$

4.63

5.97

7.46

8.95

$\bar{\alpha}$

.51-.58

.54-.60

.51-.64

.53

Figure 1.6 - Photographs of Boiling Pool Flow Patterns. $H_0 = 30$ cm.

(or, equivalently, power input). The data has been compared with a one-dimensional drift-flux model of the two-phase boiling flow field (Epstein 1977). Figure (1.7) presents a typical set of experimental results. The analytical results, using a churn-turbulent drift flux model (Zuber 1965) are also presented for two values of the distribution parameter, C_o . Reasonable agreement between the data and the theory is evident.

The results of this work can be summarized as follows:

- (i) Photographic observation indicate that churn-turbulent two-phase flow is dominant for $j_{g\infty}/U_\infty$ up to 20.
- (ii) A churn-turbulent drift flux model, with $1.2 < C_o < 1.35$ predicts the data reasonably well.
- (iii) The pool-average void fraction in this regime is in the range 0.5-0.7.
- (iv) Dispersed flow is not visually observed at vapor velocities up to four times the Kutateladze dispersal limit.

Figure 1.7 Pool-Average Void Fraction

Nomenclature

A	gamma beam cross-sectional area
C_o	drift flux distribution parameter
H	boiled-up pool height
H_o'	static pool height
j_g	superficial vapor velocity
$j_{g\infty}$	superficial vapor velocity at top of pool
ℓ	wall thickness
N	number of recorded counts
R	distance between source and detector
S_o	equivalent point source intensity
U_∞	bubble terminal rise velocity
w	flow channel width
α	beam-average void fraction
$\bar{\alpha}$	pool-average void fraction
ϵ_N	counting error
ϵ_α	void fraction error
η	detector counting efficiency
τ	counting time

1.3 Boiling Pools with Internal Heat Generation (G. A. Greene, C. E. Schwarz)

(a) Measurement of Properties of ZnSO_4 Solution:

It has been previously reported (Greene 1977 a) that a natural convection-type correlation was being sought to correlate the existing local heat transfer data for a volume-heated boiling pool adjacent to a vertical wall (Gustavson 1976). The details of the derivation have been presented (Greene 1977 b) and will be omitted here. It became evident that the properties of a 15% ZnSO_4 solution might be significantly different than those for water and if not accounted for, would change the effective Prandtl number and Grashof number; as such, a program to measure both the density and viscosity of $\text{ZnSO}_4/\text{H}_2\text{O}$ solution has been conducted. The density was measured by accurately weighing a precisely measured volume of solution over a range of temperature from 10°C to 90°C . This procedure was tested for accuracy by calculating the density of distilled water and comparing to accurately tabulated values in the literature. Good agreement was found. The viscosity was initially measured with a Brookfield rotating cylinder viscometer. It was abandoned when quite poor agreement with water values was realized. A Cannon - Ubbelohde viscometer was purchased and tested in a constant temperature bath. It is a capillary tube-type viscometer calibrated at 100°F by viscosity standards 1702 and 1703. The viscometer constant was found to be 0.002086 cs/cc . Accuracy as determined by measuring viscosity of water at various temperatures is $\pm 1\%$.

The viscosity data for a $\text{ZnSO}_4/\text{H}_2\text{O}$ solution (15% by weight) is presented in Fig. (1.8) and the tabulated data for density and viscosity is presented in Table (1.1). The uncertainty in these measurements should be taken as $\pm 1\%$. The 15% by weight solution is prepared at room temperature by adding a weight of ZnSO_4 equal to 15% the water weight. This data has been applied to the natural convection heat transfer correlation for volume-heated boiling pools and has been reported in a BNL formal report (Greene 1977 b).

At the present, the relation for the heat transfer correlation,

$$\text{Nu}(x) = K [\text{Gr}^*(x) \cdot \text{Pr}]^{.25} \quad (1.6)$$

where $\text{Gr}^*(x)$ is based on local void fraction,

$$\text{Gr}^*(x) = \frac{g\alpha(x)x^3}{\nu_f^2} \quad (1.7)$$

is being recalculated on the basis of average void fraction to determine the behavior of the coefficient. $K = 0.76$ for the local void correlation.

Figure 1.8 Kinematic Viscosity of ZnSO₄ (15% weight) Solution vs. Temperature.

Table (1.1)
Measured Density and Viscosity of ZnSO_4
Solution (15% wgt)

T($^{\circ}\text{C}$)	$\nu(\text{cs})$	ZnSO_4		H_2O	
		$\mu(\text{cp})$	$\rho(\text{gm/cc})$	$\mu(\text{cp})$	
12.0	1.413	1.309	1.080	—	
20.0	1.175	1.098	1.070	1.007	
21.5	1.089	1.018	1.070	—	
40.0	.756	.710	1.065	.654	
40.0	.750	.705	1.065	—	
60.0	.536	.505	1.060	.470	
60.0	.546	.515	1.060	—	
80.0	.415	.393	1.055	.357	
80.0	.415	.393	1.055	—	
90.0	.373	.355	1.050	.317	

(b) Numerical Solution of Local Void Fraction:

The use of the generalized natural convection-type heat transfer correlation for volume-heated boiling pools requires apriori knowledge of either the local or pool-average void fraction, depending on how the Grashof number is defined, i.e.,

$$\text{Gr}^*(x) = \frac{g\alpha(x)x^3}{\nu_f^2} \quad \text{or} \quad \text{Gr}^*(x) = \frac{g\alpha x^3}{\nu_f^2} \quad (1.8)$$

Analytical work was initiated and directed towards development of a model based on a churn-turbulent drift flux (Zuber 1965). The steady-state vapor mass conservation equation may be shown to be

$$\frac{d}{d\xi} [F(\alpha)] = K(1 - \alpha) \quad (1.9)$$

where

$$F(\alpha) = \frac{\alpha}{1 - C_o \alpha} \quad (1.10)$$

and

C_o = Zuber-Findlay distribution parameter,

$$K = \frac{Q''' h_o}{h_{fg} u_{\infty} \rho_v} \quad \text{and} \quad \xi = \frac{\text{depth from pool bottom}}{\text{initial pool depth}} \quad (1.11)$$

Equation (1.9) has been solved for a wide range of K from 0.10 to 2.50 and the results are presented as a family of curves in Fig. (1.9). The depth is zero at the pool bottom and increases to a maximum at the pool free surface. The detailed numerical data are indicated in Table (1.2)

(c) Data Acquisition

The 150°F reference junction box for the test plate thermocouples has been wired to the Cross-Bar Scanner and tested with type K thermocouples in place of the microthermocouples. Satisfactory operation was indicated. The data acquisition software routines have been modified for data storage (on paper tape) and retrieval.

(d) Design and Instrumentation:

The boron nitride test plate has been drilled and machined, and is cemented into place in the inclinable wall awaiting installation of the microthermocouples.

The gold-plated, 0.010 inch diameter microthermocouple sensors have been cut to 3 foot lengths, wired to the connectors, and gold-plated (see Fig. (1.10)) by electrodeposition of 0.001 inch of gold on the polished exposed hot junction. The thermocouples have been tested in boiling $\text{ZnSO}_4/\text{H}_2\text{O}$ solution for five hours successfully. Installation will proceed immediately after calibration. A schematic of a microthermocouple is included in Fig. (1.10).

Flow meters and thermocouples for all the coolant flows and heat fluxes have been installed and are operational. A traversing mechanism for the local void fraction probe has been installed and the remainder of the probe installation has been designed and is awaiting parts. A schematic diagram of the differential pressure void fraction probe may be seen in Fig. (1.11). A prototype probe assembly has been constructed and is currently being evaluated. Successful operation has been demonstrated however evaluation of the one hole vs. two hole probe design is being considered. Two hole design gives direct measurement of local pressure gradient while one hole requires regression analysis and numerical differentiation of the resulting data.

Experimentation with the inclined wall boiling pool apparatus is expected to begin in January 1978.

Figure (1.9). Local Void Fraction vs. Normalized Pool Depth
in a Volumetrically Boiling Pool. ($K = \frac{Q \cdot h_o}{h_{fg} u_{\infty} \rho_{\ell}}$).

Figure (1.10) Gold-Plated Micro-Temperature Sensors

Figure (1.11) Schematic Diagram of Local Void Fraction (Differential Pressure) Probe.

Table (1.2)
Local Void Fraction vs. Normalized Depth
in Volumetric Boiling Pool $K = \left(\frac{\dot{Q}'' h_o}{h_{fg} u_{\infty} \rho_l} \right)$.

VOID FRACTION DISTRIBUTION								
LOCAL VOID FRACTION								
NORMALIZED DEPTH K=	0.10	0.20	0.40	0.50	1.00	1.50	1.75	2.50
0.10	.009	.019	.037	.046	.085	.120	.135	.175
0.20	.018	.036	.070	.086	.149	.200	.221	.273
0.30	.026	.053	.100	.121	.199	.259	.281	.337
0.40	.035	.069	.126	.157	.241	.304	.328	.384
0.50	.043	.084	.150	.177	.275	.340	.365	.421
0.60	.051	.098	.172	.201	.305	.370	.395	.450
0.70	.059	.111	.191	.223	.329	.396	.420	.474
0.80	.066	.124	.206	.242	.352	.418	.442	.495
0.90	.074	.136	.226	.261	.372	.437	.461	.512
1.00	.081	.147	.242	.277	.389	.454	.478	.528
1.10	.088	.158	.257	.292	.406	.469	.492	.541
1.20			.270	.307	.419	.483	.505	.553
1.30				.320	.433	.495	.517	.564
1.40					.445	.506	.528	.578
1.50					.457	.577	.538	.587
1.60						.526	.547	.594
1.70							.556	.602
1.80								.608
1.90								.614

1.4 Two Phase Solidification Dynamics of Flowing Core Debris

(G. A. Greene, J. J. Barry, and M. S. Kazimi, MIT)

(a) Wood's Metal/Nitrogen Gas Transient Solidification Experiment

Initial experiments were conducted with a Wood's metal-Nitrogen gas mixture flowing vertically downward in a circular tube to simulate the transient solidification dynamics of a two-phase mixture of core debris with non-condensable gas freezing in cold axial structures of an LMFBR following a HCDA. The experiments were performed at inlet temperatures of 76 and 80°C and over a range of average void fraction from 0.00 to 0.40, approximately. The integrated mass flow rates and plugging times were directly measured, as well as average crust thickness vs. time in a separate experiment.

It was observed that there was a monotonic decrease in the integrated mass flow rate and plugging time for increasing void fraction for both 80°C (5°C above fusion) and 76°C (1°C above fusion) cases (Greene 1977 a). This trend is attributable not only to the two-phase hydrodynamics but also to the effect of the non-condensable void fraction on the freezing dynamics. A composite diagram of both 76 and 80°C transient solidification data may be seen in Fig. (1.12a-b). From this figure, it is also evident that there is a marked decrease in total integrated mass flow rate and plugging time with decrease in the inlet temperature from 80°C to 76°C. This comparison is more evident in Fig. (1.13) where the zero void cases for 80°C to 76°C freezing are presented. The case of 80°C freezing corresponds to 1½% sensible to latent heat ratio. However, Fig (1.13) indicates a 25-30% effect in mass displaced as well as plugging time. This indicates that a modified latent heat

$$h_{fs}^* = h_{fs} + C_p (T_i - T_f) \quad (1.12)$$

to take into account small liquid sensible heat, would not be correct, as reported by Cheung (1976).

The average solid crust thickness was measured by a series of runs for single-phase transient solidification of Wood's metal at 76°C. The following method was devised by trial and error. The reservoir tank was filled to variable depths. At lower depths, the cover pressure was increased to reproduce the same test section inlet pressure. The run was initiated and solidification progressed until the reservoir was empty. At that time, the test section experienced no further crust growth. The average crust thickness from these runs was determined by weighing the test section. The results of this crust thickness experiment are presented in Fig. (1.14), showing good agreement with the analytical prediction to be described in the next section.

(b) Analysis of Two-Phase Transient Radial Solidification Dynamics with Non-Condensable Void Fraction

An analysis has been performed to describe the process of transient solidification of a homogenous two-phase mixture flowing turbulently in a circular

-A-

-B-

Figure 1.12 Schematic of Two-Phase Integrated Mass Flow Rates vs. Time for Inlet Temperature Equal 76°C and 80°C.

Figure 1.13 Comparison of Integrated Mass Flow Rates for 76°C and 80°C Inlet Temperature Cases, $\alpha = 0.00$.

Figure 1.14 Transient Crust Thickness Measured From Centerline vs. Time (Calculated——, Measured $\odot$).

tube. The following assumptions are made:

- (1) The void fraction is non-condensable.
- (2) Radial Freezing.
- (3) Inlet mixture temperature equals liquid fusion temperature.
- (4) Pressure differential is constant.
- (5) Turbulent homogeneous two-phase flow.

Two-phase mixture mass flow rate:

The mixture mass flow rate is solved, assuming a form of the two-phase friction multiplier, in terms of the crust thickness as

$$G(t) = \left[\left(\frac{2\rho_{\ell} D_o^{1.25}}{C_1 \mu_{\ell}^{.25} L} \right) \left(\frac{\beta^{1.25} \Delta p}{\phi_{LO}^2 K} \right) \right]^{0.57} \quad (1.13)$$

where

$G(t)$ is transient mass flow rate per unit area

D_o is the initial tube diameter

ρ_{ℓ} is liquid density

C_1 is constant in friction relationship

μ_{ℓ} is liquid viscosity

L is test section length

Δp is overall pressure drop

ϕ_{LO}^2 is two-phase friction multiplier

K is equal to unity for $T_i = T_f$

β is (tube radius - crust thickness) $\div$ tube radius

The solution to Eq. (1.13) requires a solution for $\beta(t)$, the transient crust thickness, which is the subject of the next section.

Two-phase radial crust thickness:

Assuming a form for the solid crust temperature profile and substituting into the heat balance equation for a radial crust growth, the following relation for the crust growth may be derived,

$$\beta^2 [\ln \beta^2 - 1] = (t^+ - 1) = (2\gamma t^* - 1) \quad (1.14)$$

where t^+ is the time non-dimensionalized in terms of the plugging time, t_p , (where $\beta \rightarrow 0$), given in dimensionless form by

$$t_p^* = \frac{a_s t_p}{R_o^2} = \frac{1}{2\gamma} \quad (1.15)$$

and where

$$t^* = \frac{a_s t}{R_o^2} \quad (1.16)$$

and

$$\gamma = \left[\sqrt{1 + \frac{2\rho c (T_f - T_w)}{(1-\alpha)\rho_\ell h_{fs}}} - 1 \right] \quad (1.17)$$

Note that the expression in (1.14) is that required to determine the mass flow rate given by (1.13).

The solutions for β and G are shown in Figs. (1.14) and (1.15) as a function of normalized time, t^* . The mass flow rate per unit cross-sectional area is presented normalized to the instantaneous maximum flow rate at $t^*=0$.

Plugging time:

The void fraction may be set to zero to determine the maximum plugging time as follows:

$$t_{p \max} = \frac{\rho_\ell c_\ell R_o^2}{2k \left[\sqrt{1 + 2c\rho (T_f - T_w)/\rho_\ell h_{fs}} - 1 \right]} \quad (1.18)$$

It is thus seen that the dimensionless parameters expected to exert most control of the freezing process include the solid-liquid density ratio, ρ/ρ_ℓ , the average void fraction, α , and the phase change number, $N_{pch} = c(T_f - T_w)/h_{fs}$.

The two-phase plugging time has been solved as a function of average void fraction with ρ/ρ_ℓ and N_{pch} as independent parameters and is seen in Fig. (1.16).

Figure 1.15 Normalized Mass Flow Rate Per Unit Area vs. Normalized Time.

Table (1.3) lists the detailed data for plugging time and integrated mass flow rate for the 76°C transient solidification runs, compares the measured values with the calculated ones. Good agreement is indicated.

The details of this transient solidification program will be available in a preliminary report in preparation.

Figure 1.16 Ratio of Two Phase Plugging Time to Single Phase Plugging Time as a Function of Average Void Fraction ($\rho/\rho_l = 1.0$, $N_{pch} = .10, 1.0, 10.0$).

TABLE (1.3)

Plugging Time and Integrated Mass Flow Rate vs. Void Fraction
(Measured and Calculated)

$\langle \alpha \rangle$	t_p (sec)		Mass Displaced (kg)		t_p/t_{pLO}	
	<u>Measured</u>	<u>Calculated</u>	<u>Measured</u>	<u>Calculated</u>	<u>Measured</u>	<u>Calculated</u>
0.00	13.8	11.2	12.0	9.9	1.00	1.00
0.15	10.7	9.6	9.9	7.4	0.78	0.85
0.25	10.0	8.5	7.6	6.1	0.72	0.76
0.40	8.3	6.9	5.3	4.5	0.60	0.60

Inlet Temperature Equal To 76°C , $N_{pch} = 0.15$, $\rho/\rho_g \sim 1.0$.

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2. Technical Coordination of Structural Integrity (M. Reich)

A survey and review program for the application of fracture mechanics methods in elevated temperature design analysis and safety evaluation was initiated in December of 1976. The first report by REICH and ESZTERGAR surveyed and assembled the material for a critical review of the theories of fracture and the application of fracture mechanics methods to life prediction and safety analysis of piping components. The second report by REICH and ESZTERGAR provided the basic concepts and a review of the problem areas associated with the development of analytical and experimental programs for a systematic evaluation and comparison of the currently available fracture mechanics theories. The basis for such an evaluation was described in terms of a series of benchmark problems which accurately specify conditions of geometry, loading, and environment characteristics of large diameter piping systems in nuclear service.

During this report period a draft of a report was completed. The objective of this report is to establish a data base and detail the additional analytical techniques needed to confirm the validity of existing analytical methods. The need for such a program in the elevated temperature field has been demonstrated by a number of independent surveys on various safety aspects of LMFBR related structural analysis methods and materials problems. The results of this program, however, will be applicable not only to reactor plants operating at elevated temperatures, but will also lead to improvements of light water reactor evaluation methods for operating and accident conditions.

The current state of elevated temperature reactor design technology is embodied in the standards and codes which provide guidance and minimum requirements for systematic design and evaluation procedures. These, however, do not necessarily provide specific absolute values which, if satisfied in the course of design, will guaranty thirty to forty years of uninterrupted life. There are numerous assumptions and approximations embodied in these standards concerning materials behavior damage mechanisms and failure modes at elevated temperature. There are also numerous areas of uncertainty and conflicting opinion in the interpretation of the existing test data and in the analysis and evaluation methods. Furthermore the standards and codes leave some areas to the judgment of the designer, some of which require explicit justifications, but no standards or rules are provided.

Therefore, the overall safety, at the present time, lies in the combination of rigorous enforcement of current standards, judicious application of experience with high temperature equipment even if not in nuclear service and the surveillance of actual operating conditions. In the past, one criterion proposed for elevated temperature design has been that the time for crack initiation should exceed the design life. However, due to the complexities of the piping structures and the nature of the stress history during service, the evaluation of initiation times is difficult and often leads to uneconomical designs. In addition defects may exist in the component before it enters service. Hence, the knowledge of the growth rates of cracks and the residual strength of the components containing cracks is important in a realistic design evaluation.

In the report an in-depth review is presented in sufficient detail to illustrate the degree of agreement between the theoretical and empirical methods available in the literature and indicate the scope of the additional analyses and experimental work needed for the development of reliable safety evaluation methodology.

For pure cylindrical bending, cracks perpendicular to the load start to grow when

$$K_B = \frac{6M_o}{t^2} a^{1/2} \quad 2.1$$

reaches a critical value which is generally larger than the corresponding critical uniaxial tension value. There appears to be a thickness effect in the bending case which is probably due to interference from the compressive sides of the crack.

For a circular plate with lateral pressure and small lateral displacements, results agree with the bending data when using the nominal bending stress:

$$\sigma = 0.506 p \frac{R^2}{t^2} \quad 2.2$$

For larger displacements when bulging occurs, the results agree with the tensile data when the nominal tensile stress:

$$\sigma = 0.423 \left(\frac{E_p R^2}{t^2} \right)^{1/3} \quad 2.3$$

is used.

For curved surfaces such as a cylinder under internal pressure, the data agrees with the expression developed by Follies both for axial cracks under hoop stress σ

$$\frac{K^2}{\pi \sigma^2 a} = 1 + 1.67 \frac{a^2}{Rt}$$

and for circumferential cracks under axial stress σ

$$\frac{K^2}{\pi \sigma^2 a} = 1 + .34 \frac{a^2}{Rt}$$

Generally, the expressions were accurate up to

$$\frac{a^2}{Rt} \sim 3,$$

showing a tendency to be lower than the experimental data at higher values of

the parameter. The parameter

$$\frac{a^2}{Rt}$$

is a promising one.

To study the influence of cracks at different angles to the applied load, analyses and data are available including the stress component parallel to the crack in the stress field around a crack tip. This, together with the concept of a critical circumferential stress at a critical distance ($\alpha = 0.1$) ahead of the crack provides improved correlation with fracture predictions for both the angle of fracture and the critical stress intensity factor for the angled cracks in flat plates.

For a hollow cylinder under torsion with angled cracks, best correlation was given by the same analysis though the results were not as conclusive as for the flat plate. From elastic theory useful curves for the variation of K_1 , K_2 , and K_3 around the border of an elliptically-shaped crack are available.

In a plane stress fracture the addition of a biaxial stress produces an increase in the apparent fracture toughness compared with the uniaxial case. However, there is as yet no evidence to show that there would be the same increase in a plane strain situation. Hence, in the absence of biaxial information, the uniaxial fracture data may be the most conservative for flat plates. However, for shells there will also be a curvature effect.

In an analogous manner, fatigue crack propagation rates appear to be less rapid under biaxial stresses than under uniaxial stress. However, this shift is not great and generally will be masked by other effects such as environment and temperature service situations.

The analysis of cracks in weldments with residual stress effects are also available. In the case of a crack in a weld, the estimated residual stress distribution agreed reasonably well with some experimental data for elastic conditions. Results indicate that there can be a tensile stress intensity factor even when the original residual stress distribution has changed to compressive. A point to remember is that residual stresses near welds can be beyond yield.

An analysis based on Lagrangean mechanics is useful in indicating the different effects of liquids and gases and pressurizing media in hollow pipes. The results show that whereas gases maintain their pressure as a crack begins to propagate, the pressure in the liquid can quickly decrease so that subsequent catastrophic failure is less likely even in large diameter piping.

A number of reports and papers related to the topics discussed here became available in August 1977. The evaluation of these reports and papers will be included in the continuation of the present study.

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REICH, M. and ESZTERGAR, E. P., et. al., Compilation of References, Data Sources, and Analysis Methods for LMFBR Primary Piping System Components, BNL-NUREG 50650, March 1977.

REICH, M and ESZTERGAR, E. P., et. al., Concepts for Benchmark Problems Development for Fracture Mechanics Application in Safety Evaluation of Nuclear Piping in Sub-Creep Service, BNL-NUREG 50676, June 1977.

3. SSC Code Development (A. K. Agrawal)

The Super System Code (SSC) development program deals with the development of an advanced thermohydraulic code to simulate transients in LMFBRs. During this reporting period, work on two additional series of computer codes, SSC-P and SSC-S, in addition to the SSC-L code, was started. In the following, progress made on these series of codes is reported. Reference is made to the previous quarterly progress report (Agrawal, 1977) for a summary of accomplishments prior to the start of the current period.

3.1 SSC-L Code (J. G. Guppy and A. K. Agrawal)

The SSC-L code simulates thermohydraulic transients in loop-type LMFBRs. This code was made operational on September 26, 1977. A number of modifications and improvements were made on this code.

3.1.1 Transient Fuel Heat and Coolant Flow Computation (J. W. Yang)

This quarter of year was spent on debugging, correcting and modification of the single-phase in-vessel models. The following are the major achievements:

1. Coupling of coolant dynamics and fuel heat transfer
2. Computational logic for starting of boiling and fission gas release were developed. Modification to the flow redistribution is added.
3. Control rod (properties, computation logic, etc.) was added.
4. Coolant flow reversal was added. Corresponding changes in upper and lower plena were included.
5. Wire-wrap was added.

The axial mesh structure within the rod associated with any channel was altered to be identical with the axial mesh in the coolant of that channel. Previously, the two axial meshes were allowed to be different. The intent had been, that if boiling in the sodium were to occur, the capability would be present to allow a finer mesh in the coolant. Potential savings in the rod heat conduction calculations were considered significant enough to warrant this additional complexity. However, subsequent testing with the SSC-L transient have shown that computational time, particularly in the area of data interpolation and matching at the fuel-coolant interface, is considerably influenced by this dissimilar mesh structure. Consequently, the axial mesh associated with the rod was altered to match that in the coolant. This change has been successfully implemented, tested and debugged.

3.1.2 Reactivity Feedback Effects (G. J. Brown* and K. St.John*)

The purpose of this study is to identify and model reactivity feedback phenomena for incorporation into the point-kinetics package of the SSC-L code. The reactivity feedback loop is shown schematically in Figure 3.1.

The total reactivity at a given time, t , is the sum of an applied reactivity $\rho_a(t)$, (e.g., control rod movement) plus the sum of the various reactivity feedback

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Figure 3.1 Basic Feedback Loop

contributions, $\rho_i(t)$:

$$\rho(t) = \rho_a(t) + \sum_i \rho_i(t) \quad (3.1)$$

As indicated in Figure 3.1, the total reactivity is then incorporated into the point-kinetics model and used in the evaluation of the fission power, P_F . The total power, P , is then determined by adding the decay heat contribution, P_D . The thermal-hydraulic consequences (temperatures, T) are then evaluated for use in the feedback models. Due to the nature of the reactivity feedback loop at each discrete time interval, a check should be performed to determine the correct net reactivity.

It should also be noted that the reactivity effects are inherently spatially-dependent. This is not only due to the fact that the temperatures vary spatially, but even for the same temperatures the magnitude of the effect will depend on the location within the reactor. Since the point-kinetics equations suppress any spatial dependence, an appropriately weighted spatial integration of the evaluated local reactivity feedback effects must be performed.

A survey of several references (Hummel, 1970; Graham, 1971; Yevick, 1966; Palmer and Platt, 1961; Thompson and Beckerly, 1964; PSAR) and existing computer models (Dunn, 1975; Dunn 1974; Walter, 1972; DEMO, 1976) has identified the following feedback effects:

- Doppler
- Sodium density and voiding
- Fuel axial expansion
- Structural expansion
- Bowing
- Fuel slumping

The first three effects will be discussed in more detail. Models will be presented and equations developed for incorporation of these effects into the present version of SSC-L.

The remainder of this section will briefly discuss the last three effects.

● Radial Structural Expansion

As the core heats up there is radial expansion of the fuel assemblies and core support structures which tend to effectively increase the pitch-to-diameter ratio of the fuel lattice, reducing the reactivity. However, this effect has a long time constant relative to the fuel, for example, since it is related to the structural components. From a normal operational viewpoint (e.g., taking the reactor from zero to full power), this effect will be much more important than during the time frame of interest corresponding to the type of transients that SSC will be analyzing.

● Bowing

Bowing is caused by differential thermal expansion and is a result of radial temperature gradients. Positive reactivity can be added when fuel material bows towards the center of the reactor. To reduce this effect, spacers are placed between fuel elements and fuel assemblies. The temperature gradients now set up

stresses in the contacted material which is designed to be strong enough to prevent appreciable displacements.

Also, as with structural expansion, this effect will have a relatively long time constant since it is the fuel assembly and not the fuel pins themselves that provide the structural support (Hummel, 1970).

• Fuel Slumping

In the event of fuel melting, there is the possibility for fuel movement within the cladding material. If the fuel moves (slumps) towards the center of the core, then there will be positive reactivity added to the system. However, the current version of SSC does not treat the class of transients that would lead to this condition; hence, fuel slumping is not considered further.

3.1.2.1. Doppler Effect

The Doppler effect is the most important and reliable prompt negative reactivity effect in current thermal and fast reactor designs which utilize high fertile (U-238) material concentrations. Probably one of the better understood reactivity phenomena, the Doppler effect is due to the increased kinetic motion of the fuel atoms, as measured by an increase in fuel temperature resulting in the broadening of cross-section resonances and increased resonance absorptions.

The Doppler coefficient is defined as the change in multiplication factor, K , associated with an arbitrary change in fuel temperature, T . Since in fast reactors this coefficient is found to vary as the inverse of fuel temperature, a Doppler parameter, α_{DOP} , can be generally defined (Hummel, 1970) as

$$\alpha_{DOP} = T \, dk/dT \quad (3.2)$$

Equation (3.2) may be integrated, yielding

$$k^1 - k^2 = \alpha_{DOP} \ln \frac{T^1}{T^2} \quad (3.3)$$

where T^1 and T^2 represent two different fuel temperatures and K^1 and K^2 are the resulting multiplication factors. Rigorously, reactivity is defined as

$$\rho = \frac{k - 1}{k}$$

and changes in reactivity, $\Delta\rho$, as

$$\Delta\rho = \rho^1 - \rho^2 = \frac{k^1 - k^2}{k^1 k^2}$$

For small reactivity changes (the Doppler effect is of the order of 10^{-4}) and with $k \approx 1$ (prompt criticality is about 1.003)

$$k^1 - k^2 \approx \rho^1 - \rho^2$$

and the change in reactivity due to the Doppler effect, $\Delta\rho^{DOP}$, can be written as

$$\Delta\rho^{DOP} = \alpha_{DOP} \ln \frac{T^1}{T^2} \quad (3.4)$$

This equation may be applied locally or regionally, depending on how the temperatures and Doppler coefficient are defined. Specifically, in discrete notation, the Doppler reactivity, ρ_{JK}^{DOP} , for the JK-th region is

$$\rho_{JK}^{\text{DOP}} = \alpha_{JK}^{\text{DOP}} \ln \frac{T_{JK}^1}{T_{JK}^2} \quad (3.5)$$

where K represents the channel, J the axial position in the K-th channel, and T_{JK} is the effective local temperature at position JK. This effective fuel temperature (Hummel, 1970) which is defined as

$$T_{JK} = \frac{\sum_I V_{IJK} T_{IJK}}{\sum_I V_{IJK}} \quad (3.6)$$

where V_{IJK} is the fuel volume in channel K, axial slice J and between radial mesh I and I - 1 and T_{IJK} is the local fuel temperature at positions I, J, K.

It should be noted that the Doppler coefficient in Equation (3.5) is shown to be a spatially-dependent variable. Although α_{JK}^{DOP} will be a constant with respect to fuel temperature, there will be spatial variations due to different fuel types (e.g., enrichment, pin size, volume fractions of structural material coolant and fuel) and different sodium density.

In the present model, α_{JK}^{DOP} is allowed to be a spatially-dependent parameter supplied by the user. Thus, the first concern relating to fuel type can be directly addressed.

The sodium density dependence is actually a neutron spectrum dependence--the harder the spectrum, the smaller the Doppler effect (since there are less neutrons in the resonance range). The less sodium present, due to density decreases or voids, implies a harder spectrum. To treat this effect, an effective isothermal sodium void, x_{JK}^{NA} , is defined

$$x_{JK}^{\text{NA}} = \frac{NA_{JK}^{\text{DOP, REF}} - NA_{JK}^{\text{DOP, 2}}}{NA_{JK}^{\text{DOP, REF}}} \quad (3.7)$$

where $NA_{JK}^{\text{DOP, 2}}$ is the local, time-dependent sodium density of positions J, K, and $NA_{JK}^{\text{DOP, REF}}$ is the local reference sodium density at position JK. Thus, if β_{JK}^{DOP} and γ_{JK}^{DOP} are the Doppler parameters with and without sodium present, respectively, then the net Doppler parameter can be approximated by:

$$\alpha_{JK}^{\text{DOP}} = \beta_{JK}^{\text{DOP}} (1 - x_{JK}^{\text{NA}}) + \gamma_{JK}^{\text{DOP}} x_{JK}^{\text{NA}} \quad (3.8)$$

To get an overall Doppler reactivity, ρ^{DOP} , for use in the point-kinetics

equations, a summation of Equation (3.5) must be performed. Thus,

$$\rho^{\text{DOP}} = \sum_{J,K} \rho_{JK}^{\text{DOP}} = \sum_{J,K} \alpha_{JK}^{\text{DOP}} \ln \frac{T_{JK}^1}{T_{JK}^2} \quad (3.9)$$

The values of the Doppler coefficients, α_{JK}^{DOP} , for each JK-th region must be supplied by the user in units of reactivity for that mesh.

3.1.2.2 Sodium Density and Voiding Effect

Heating of the sodium coolant decreases the coolant density and can ultimately lead to vaporization (voiding). These density decreases affect the reactivity of the reactor through two competing effects -- increased leakage, which adds negative reactivity and is important away from the center of the core, and spectral hardening due to a decrease in the macroscopic sodium scattering cross section which adds positive reactivity. The net effect depends primarily upon the location in the reactor.

In modeling this effect, both sodium density changes and voiding can be treated in a similar fashion. Basically, what is required along with the spatial sodium density distribution is a table of spatially-dependent sodium reactivity worths. The sodium density will be determined internally by SSC from knowledge of the sodium temperature distribution. However, the reactivity worths will be user supplied.

There are several ways to present this reactivity effect depending on the form in which the reactivity worth data are known. For application in SSC, Equation (3.10), which can be used to treat either sodium density or voiding reactivity effects, is suggested:

$$\rho^{\text{NA}} = \sum_{JK} \beta_{JK} M_{JK} \quad (3.10)$$

where ρ^{NA} is the overall sodium density and voiding reactivity for use in the point-kinetics equations, β_{JK} is the user supplied sodium reactivity worth in axial J, channel K, in units of reactivity per unit mass of sodium in JK-th region, and M_{JK} is the effective mass of sodium voided in segment JK, and is defined in the following equation:

$$M_{JK} = \text{NA} \rho_{JK}^2 - \text{NA} \rho_{JK}^1 V_{JK}^{\text{NA}} \quad (3.11)$$

In this equation $\text{NA} \rho_{JK}$ is the local average, time-dependent sodium density and V_{JK}^{NA} is the local coolant volume in segment JK.

3.1.2.3 Fuel Axial Expansion Effect

Axial expansion of the fuel pellets tends to increase the active core height while decreasing the fuel density, resulting in a net decrease in reactivity. An upper limit to the magnitude of this effect is obtained if it is assumed that the fuel pellets are free to move within the cladding. However, the actual mechanisms for expansion are difficult to model, especially for ceramic fuel. Fuel pellet cracking or friction between the pellet surface and inner clad wall will reduce the expansion significantly. On the other hand, if the fuel pellets are not stacked in perfect contact within the clad, then there may be negative fuel expansion as the fuel pellets settle.

Physics calculations have been performed for CRBRP (PSAR) which indicate a maximum overall reactivity of -0.12% per mil of fuel axial expansion. The range of this effect is quoted to be 0.0% /mil to -0.12% /mil due to the uncertainties previously discussed. In fact, neglecting this feedback effect would tend to yield higher positive reactivity values during a positive temperature transient. This would result in a high estimate of the reactor power level and a conservative (i.e., more severe) prediction of the thermal-hydraulic performance.

Nevertheless, for the purposes of this report, the reactivity due to fuel axial expansion, ρ^{AX} , is based on a model which parallels the treatment of sodium density reactivity effects:

$$\rho^{AX} = \sum C_{JK} N_{JK}^* \quad (3.12)$$

where C_{JK} is the fuel reactivity worth in axial slice J, channel K, in units of reactivity per unit of mass of fuel in JK-th region and N_{JK}^* is the effective mass of fuel voided in segment JK.

It is expected that the user will supply values for the C_{JK} constants. An expression for the internal evaluation of N_{JK}^* will now be derived based on the logic that as the fuel temperature increases the fuel expands axially according to the following equation:

$$Z_{JK}^2 = \left[1 + \alpha (T_{JK}^2 - T_{JK}^1) \right] Z_{JK}^1 \quad (3.13)$$

where α is the linear fuel expansion coefficient in units of $\text{cm}/\text{cm}^\circ\text{K}$ and T_{JK} is the fuel volume average temperature as evaluated by Equation (3.6). To conserve mass, an axial increase in the fuel length implies a decrease in the fuel density, ρ_{FL} , (ignoring other dimensional changes); thus

$$\rho_{FL}^1 Z_{JK}^1 = \rho_{FL}^2 Z_{JK}^2 \quad (3.14)$$

or

$$FL^{\rho_1}_{JK} - FL^{\rho_2}_{JK} = FL^{\rho}_{JK} \left[\frac{\alpha(T_{JK}^2 - T_{JK}^1)}{1 + \alpha(T_{JK}^2 - T_{JK}^1)} \right] \quad (3.15)$$

The difference in density times the original fuel volume (before expansion), $FL^{\rho_1}_{JK}$, will give the amount of fuel voided from location JK, N_{JK} :

$$N_{JK} = FL^{\rho_1}_{JK} FL^{X}_{JK} FL^{V_1}_{JK} \quad (3.16)$$

where FL^{X}_{JK} is the fuel void fraction at location JK and follows from Equation (3.15) to be:

$$FL^{X}_{JK} = \frac{\alpha(T_{JK}^2 - T_{JK}^1)}{1 + \alpha(T_{JK}^2 - T_{JK}^1)} \quad (3.17)$$

The effective amount of fuel voided from location JK, N^*_{JK} , and the resulting net reactivity effect associated with fuel axial expansion are given by the following equations:

$$N^*_{JK} = \epsilon N_{JK} \quad (3.18)$$

$$\rho^{AX} = \epsilon \sum C_{JK} N_{JK} \quad (3.19)$$

where ϵ is a user-supplied constant that accounts for the fact that the present model does not explicitly evaluate the increase in reactivity due to the fact that the fuel that was calculated to be voided from location JK (Equation (3.16)) actually causes a net increase in axial fuel height, reducing leakage. An estimate on the size of this effect is included in Haas (1976) and a recommended upper limit for ϵ is thus given to be 0.3.

Also, for the reasons previously noted relative to the uncertainties associated with the mode of fuel expansion, it might be reasonable to expect values of ϵ to be still further reduced, especially for irradiated fuel.

3.1.3 Steam Generating System (W. L. Weaver, III)

The work during this quarter was directed toward coding, debugging and testing of the transient model of the steam generating system incorporating the new implicit integration scheme. Stability of this integration method was shown by computation and several different transients were run to assess the computing time requirements of this new method.

3.1.3.1 Stability of the Integration Scheme

Stability of the new implicit integration scheme was demonstrated by computation. A transient with no forcing function was computed beginning from a steady state using a variety of time steps ranging from 32 ms up to 1 s. Stability of the integration scheme was shown by the fact that the calculation remained stable and the calculated solution remained accurate for all time steps tested over transients of up to 500 s (i.e., 500 computational cycles with 1 s time step). There were small oscillations of the state variables which never exceeded $10^{-4}\%$ of the absolute value of the state variables.

Most of these oscillations were traced to the finite convergence criteria used to terminate the iterative calculation of the surface heat fluxes in the heat exchangers.

3.1.3.2 Transient Calculations

A series of transients were computed for the case of the coastdown of the intermediate sodium pump. The temperature of the sodium coming to the steam generator from the IHX was held constant at its steady state value and the recirculation pump remained on. In order to get a more realistic calculation of the transport delays between components of the steam generator, the steady state was calculated with a new geometric description of the CRBRP steam generator using actual pipe lengths. Transients were run with various numbers of nodes in each pipe corresponding to a) 1 node per pipe, b) 1 node per each 10m of pipe, c) 1 node per each 5m of pipe and d) 1 node per each 2m of pipe. The cpu time for each of these transients varied with the total number of nodes used to simulate the steam generator but in general, the ratio of cpu time to simulated time ranged from 1:1 to 2:1.

A similar series of transients in which the sodium conditions are the same as previous runs but in which the recirculation pump is also shut off and coasts down are presently being run and will be analyzed during the next quarter.

3.1.4 System Integration Method (J. G. Guppy)

The replacement of the fourth order, modified Runge-Kutta-Merson (RKM) integration package, which handles the time advancement of the loop hydraulic equations, with a more sophisticated package has been accomplished during this quarter. As discussed in the previous quarterly, the decision to replace the RKM package was made for reasons of computational efficiency and inherent stability limitations. The new integration package consists of a fifth order predictor-corrector (PC) scheme of the Adams type. The package contains built-in accuracy and stability checks.

While the PC scheme requires more storage, the savings in computational time are substantial. Also, larger time steps are allowed with this method. The package has been incorporated into SSC-L, tested and debugged.

3.1.5 Code Development (R. J. Kennett, T. C. Nepsee and E. S. Srinivasan)

During this quarter, a new data input file was created to read in certain data that is required only for the transient. The file name is TRNDAT. The

specified data records under this file name are broken down along component lines and the numbering scheme follows the SSC naming convention. Data read in under this file name include, scram actuation time, pump shut off times, reactivity feedback and neutronic data, pipe break information and various logic control flags.

In the on-going debugging effort of the entire code, various small errors and deficiencies were uncovered and corrected and several minor improvements made.

A data save routine has been written and tested. Under user control, this subroutine writes calculated results to a SAVE file during transient calculations. This alternate means of output handling allows separation of data generation from printing/plotting operations.

A data recall program (RECALL) has been designed and partly written. Its purpose is to read the above mentioned SAVE file and provide a comprehensive set of data scanning facilities to include output in both printed and plotted forms.

3.2 SSC-P Code (I. K. Madni and A. K. Agrawal)

The SSC-P code is intended to simulate thermohydraulic transients in a pool-type LMFBR. This code, in scope, is similar to the SSC-L code. A work plan is being written.

3.3 SSC-S Code (J. W. Yang and A. K. Agrawal)

The SSC-S code is intended to be a long-term, eventual heat sink removal capability for the loop-type LMFBRs. This code is an extension of SSC-L. In here, other heat removal paths or systems that are not part of SSC-L will be incorporated.

PUBLICATIONS

The following is a list of publications during the current reporting period from this activity:

MADNI, I. K., AGRAWAL, A. K., CAZZOLI, E. G., and GUPPY, J. G., "Hydraulic Response of an LMFBR Primary System to Pipe Rupture," Trans-Am. Nucl. Soc. 27, 739 (1977).

YANG, J. W., and AGRAWAL, A. K., "Impact of Bypass Flow on Thermal Transients in LMFBR," Trans-Am. Nucl. Soc. 27, 748 (1977).

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4. SSC Code Validation (A. K. Agrawal, R. J. Cerbone)

4.1 Analysis of Integral Experiments (H. Ludewig, D. Majumdar, R. J. Cerbone)

During this quarter, a topical report was issued (Majumdar, 1977) summarizing the status and applicability of reactor experiments carried out on SEFOR, FERMI and EBR-II to systems code validation. The main thrust of this quarter's work was directed toward the evaluation of preoperation tests planned for the Fast Flux Test Facility (FFTF) for use in the validation of the SSC code; emphasis is being placed on the natural circulation tests.

The initial task in this area is to survey FFTF instrumentation with regard to function and location; an evaluation will then be made of the instrumentation vis-a-vis SSC code validation requirements. In this work, it is recognized that the requirements of code validation such as the specification of clean geometry and respective sensors will not always be compatible with FFTF operating requirements. Therefore, the main thrust of the present work is to ensure that the experimental data will be relevant and that the tests can be reasonably simulated in SSC so that meaningful conclusions can be drawn from the results of the tests.

With these limitations in mind, some shortcomings of the instrumentation have already been observed, for example:

1. The bulk of the experimental data will be obtained from two FOTAs (Fuel Open Test Assembly). Therefore, the only detailed axial temperature profiles and flow measurements will be in these two locations. The impact of these restrictions on SSC code validation will need to be carefully assessed.

2. Temperatures and flow rates through the driver fuel subassemblies are being measured at the outlet end of the upper axial reflector. This is adequate for normal operation, but may not provide the means of measuring the core temperature response during a transient.

3. The ability to measure reverse flow through any of the core subassemblies does not seem possible at present. Similarly, no attempt is being made to measure intra-assembly flow redistribution. Both of these phenomena may play a significant role under conditions leading to natural circulation and should be measured.

4. The coolant flow rate at the outlet of the control and radial reflector assemblies should be measured since this could be a possible path for coolant flow redistribution during the transition to natural circulation.

5. Only one magnetic flow meter and two thermocouples are planned for the primary piping. This may not be adequate to obtain sufficient data to determine the onset and strength of flow stratification and backflow if it exists. At least one second flow meter should be provided for the primary system.

6. Axial temperature profiles along the primary and secondary piping should be measured. Furthermore, circumferential temperature profiles should also be measured at selected positions, since this would give an indication of possible gross flow stratification.

7. No provision is made for measuring flow redistribution within the IHX or any abnormal temperature gradients in the IHX, which might develop during a transient. Possible benefits of putting thermocouples on the outside body of the IHX should be explored.

8. There is no provision for measuring sodium flow rates through the modules of the DHX.

In summary, the present instrumentation is adequate for normal operation of FFTF and most likely adequate to ensure plant safety. However, sensitivity analyses are needed to determine whether the instrumentation can provide the data needed for SSC and other system codes. To this end a series of FLODISC calculations are being made to make some preliminary judgements on what is being measured, how it correlates with what is happening in the reactor and how it can be simulated in the SSC code.

4.1.1 FLODISC Calculations

The model used in the FLODISC calculations was the 12-channel FFTF model (401 MWt) given in the code manual. The case analyzed was the total loss-of-electrical power (the E-5 transient).

Figure 4.1 shows the results for the Row 1 assembly for several transient times corresponding to the nominal heat rate as well as for the cases where the heating rate was increased by factors of 1.5 and 5 times the nominal. Since the thermocouples for assemblies are located 156 in. above the inlet plenum, an objective of these calculations was to check how well these thermocouples track the temperature in the core (say at 61 in. above the inlet plenum).

The results are summarized in Table 4.1 where the temperature at the 61 in. position (core) is compared to the temperature at the thermocouple position (156 in.). For the normal heating cases the variation in the ratio of the outlet to the core temperature can vary by about 15% from the steady-state ratio. For the case where the heating rate in the row 1 assembly is increased, the ratio can vary by as much as 47%. The point of all this is that if sole reliance is placed on the outlet thermocouples to monitor what is happening in the core, a rather large uncertainty has to be placed on the measurements, especially for cases where there is some abnormal heating in one of the elements. These effects will be investigated more fully when the SSC calculations are made.

Finally, a similar study was made to investigate the possibility of coolant flow reversal in a subassembly. For this purpose, a 3-channel FFTF model (Muraoka, 1977) was used. It was found that during a nominal E-5 transient, flow reversal occurred in a channel in about 95 sec. Flow reversal was also observed in a channel somewhat earlier in about 85 sec in the transient, when the heat generation rate was turned off in that channel, a case which may be compared to the situation of a natural circulation transient in a core partially loaded with fresh fuel, or a radial reflector element without heat generation.

FIGURE 4.1

TABLE 4.1

Comparison of FLODISC Calculated Core and Outlet Temperatures

Heating Rate	Transient Time (sec)	Core Temp. °F (@ 61 in.)	Outlet Temp. °F (@ 156 in.)	R $\left(\frac{\text{outlet}}{\text{inlet}}\right)$
nominal	0 (steady state)	1055	1254	1.19
normal	10	888	1054	1.19
normal	20	903	983	1.09
normal	50	1010	1046	1.03
normal	110	1111	1263	1.14
normal	120	1097	1302	1.19
1.5Q	20 to 30	1294	1045	0.81
5.0Q	20 to 30	1040	1065	1.02

PUBLICATIONS

DURSTON, C., KRIEGER, T. and ALBRIGHT, D. C., "Sensitivity Analysis Methods Application to Code Validation," Brookhaven National Laboratory, BNL-NUREG-23430 (October 1977).

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III. LIGHT WATER REACTOR SAFETY

SUMMARY

Development work has continued to allow accurate predictions of flashing vapor generation rates to be obtained. The void-life, relaxation model shows promise in comparison with French data but indicates a possible important contribution of downstream nucleation in low velocity. Relaxation models have also been developed and applied to downcomer condensation and pipe condensation with cold water injection.

The test facility for the flashing experiments has neared completion with the piping having been hydrostatically checked to identify leaks which have been corrected, installation of the cooling tower completed, installation of the wiring almost completed, and a major portion of the control panel assembly completed.

Continuing problems with development of the probe rake seal have been encountered. An alternate parallel path has been chosen for the test section to insure testing can begin when the loop is ready. The first testing will be done on a test section not having the instrumentation slot and seal, and so the initial flashing data will be evaluated only on the basis of global flow, temperature, and pressure measurements in conjunction with local pressure and void γ -densitrometry data. Such evaluations require some assumptions which may yield significant bias in the results, and which may be relaxed in the event local internal void and velocity data are available.

Work has continued in the measurement, design, and development areas with γ -source fabrication and irradiation techniques having been verified and future progress made in understanding the hydro-optical behavior of the optical probes. It appears that the film-withdrawal ideas of Tallmadge and White coupled with the optical geometry of a specific probe explain the differences between the U-bend probe of Danel and Delhay and the wedge-tip geometry of the BNL probes, and, further, provide the understanding necessary for optimization of the behavior according to the intended end use, (i.e., low speed void and velocity or high speed void measurement).

Considerable progress was made in the THOR-code development effort. Development and application of floating flow regime zones separated by boundaries of discontinuities described by interface jump conditions has been demonstrated with a three zone core module. Results are shown for an exponential inlet pressure decay causing change from steady state upflow to steady state downflow with internal stagnation in the interim near the point of flow removal. The results are found to be quite sensitive to the drift velocity indicating the necessity for accurate physical modeling in this area.

Modeling of other components has proceeded with the downcomer demonstrating blowdown, and reflood capabilities and the pressurizer having been run to demonstrate bottom blowdown in typical PWR-sized geometries. In the latter case, calculation of the movement and exiting of a severe vapor concentration discontinuity was demonstrated showing the level tracking capability for low void - high void interfaces.

Finally, the THOR-I Operating System was completed including dump/restart and graphics capabilities. Continued improvement in the system will continue, of course, in response to the needs of the "in house" users, or as difficulties are encountered during the system verification process. Use of the system has been demonstrated on various single and multiple loop configurations and, in the case of an 18-pipe configuration of semiscale for blowdown and initial reflood has been found to run 2-3 times faster than real time.

Work during the next quarter will emphasize use of the THOR-I Operating System by the entire team for integration and verification of various components, completion and integration of the N-zone core and steam generator modules, and internal, peer review of the various component and process models.

The stress corrosion cracking of pressurized water reactor steam generator tubing program is a new effort of experimental work that started October 1, 1977, and this first report is, therefore, relatively short. The objective is to develop quantitative relationships between the various factors involved in the stress corrosion cracking (SCC) of Inconel 600 in high temperature aqueous solutions.

Two autoclaves are presently in use for this program, and five additional autoclaves are on order. Tests underway are at 360°C and 290°C. At 360°C the effort is being made to induce rapid cracking to determine a material for additional tests. The 290°C test is typical of actual service exposure, and will run for a long time. Other tests and additional materials are at various stages of planning and preparation.

1. Light Water Reactor Thermal/Hydraulic Development Program (O. C. Jones)

1.1 Development of Constitutive Relations

1.1.1 Analytical Modeling

(a) Effect of Pressure Change (P. Saha)

Model evaluation with the data of Reocreux (1974) is continuing. Two sets of data (Run nos. 403 and 400) were compared with the model developed at BNL (Saha 1977) with different constant values of the free parameter, C_T .

The comparison is shown in Fig. (1.1). The same comparison is shown in Fig. (1.2) with transport time as the abscissa. For run no. 403, a value of 35 cm^{-1} is used for C_T .

It can be seen from Fig. (1.2), that two different sets of data seem to fall on the same line when transport time is used as a parameter. However, for increasing transport time, i.e., run no. 403, the data on void fractions start to deviate from the prediction. Two possible reasons for this discrepancy can be cited. First, the measured values of the void fractions were diametral void fraction and not the area-average void fractions predicted by the model. It is plausible that as the transport time increases, i.e., as the two-phase flow develops, the diametral void fraction becomes larger than the area-averaged void fraction. Secondly, according to the present model, bubble nucleation occurs only in a narrow zone at the flashing boundary. In reality, however, bubbles can also nucleate downstream of the initial zone, and thus produce more vapor than the prediction. Either or both of the above two reasons can explain the discrepancy seen in Fig. (1.2). Further work will be done in this area.

1.2 Design, Construction, and Checkout of Experimental Equipment

(a) Flow Circuit (W. J. Leonhardt, J. H. Klein, and G. A. Zimmer)

Piping of the flow circuit has been completed except for the installation of some vendor supplied items. Spool pieces have been installed in place of these items. The circuit has been hydrostatically tested at 400 kPa (house water pressure) and all leaks repaired. Preparations are being made to test at full operating pressure. The cooling tower has been installed and has been connected electrically and hydraulically to the system. Figures (1.3), (1.4), and (1.5) show several of the circuit components.

The delivery of the outside vendor supplied heater system has slipped due to a strike at the main manufacturing facility, and this in turn has caused a delay in the in-house fabrication of the pressurizer. Intensive negotiation with the heater assembly vendor resulted in their shifting manufacturing to another facility and a recovery in production schedule sufficient, if kept, to meet our needs. Fabrication of the pressurizer is currently being accomplished in house.

All wireways for both control and instrument wiring have been installed, and the majority of the complete system wiring has also been installed. The main control panel Figure (1.6) has been designed and installed, and individual control elements are being inserted into the panel and connected to the wiring system.

Figure (1.1) Comparison of Reocreux's Void Fraction Data with the Present Model, Shown on Space Coordinates.

Figure (1.2) Comparison of Reocreux's Data with the Present Model, Shown on Transport Time Coordinates.

Figure (1.3) Heat Exchangers.

Figure (1.4) Main, Secondary, and Test Loop Pumps.

Figure (1.5) Purification Train

Figure (1.6) Main Control Panel Under Construction

(b) Test Section (W. J. Leonhardt and J. H. Klein)

All detail drawings of the test section were completed and sent to the BNL shop. In a parallel effort, continued testing and development of the probe seal was undertaken and further problems encountered. Since further development in this area is required, which would delay the testing, it was decided to perform initial tests without the probe rake and local instrumentation. The test section design was modified to allow machining of the probe rake slot at a later time, and test section fabrication and assembly has proceeded on this basis. Intensive probe rake seal development will be undertaken after the current test facility and hardware design and construction tasks have been completed, and while initial testing is in progress. The required modifications can be accomplished in time to meet the current milestone according to current shop estimates.

A ground handling structure for the test section has been partially designed. This ground handling structure is required since it is expected that the completed test section will weigh around 6000 N.

(c) Global Densitometer (W. J. Leonhardt and N. Abuaf)

A single channel gamma densitometer has been set up to be checked with the air-water loop. The Thulium sources encapsulated in aluminum holders were irradiated at the BNL High Flux Beam Reactor. The active sources are being checked by the Health Physics and Safety Group. The two detector holders were designed. An order was placed for the electronics necessary for five channels except for timers and scalers. Methods for analyzing the data have been initiated and are being developed.

(d) Facilities (W. J. Leonhardt and J. H. Klein)

Construction of the bench test facility described in the last quarterly (Leonhardt 1977) has been completed. A venturi was installed and a one channel gamma densitometer is currently being fabricated. Provision has been made to accommodate both the fiber optic and r-f probes, and testing is currently being performed as part of the instrumentation development program.

The water loop control room has been completed, and controls and instrumentation for the flow circuit is currently being installed there.

Plans are being prepared to expand the existing experimental area by 550 m². This will provide a more efficient area for the current flow circuit and better climate control of that area.

1.3 Development of Instrumentation Techniques

1.3.1 Design, Construction, and Checkout of Experimental Equipment

(a) Local Instruments (N. Abuaf, L. Westdyke, T. Feierabend, and G. A. Zimmer)

In order to study the effect of tip geometry, an optical probe similar to the one used by Danel (1971) was built and checked in the calibration set-up described by Abuaf (1977a). Figures (1.7a) and (1.7b) are photos of the probe tip which consisted of two 125 μ m fibers fused at the end and projecting out from the stainless steel tube (500 μ m O.D.). The probe output was recorded for various bubble velocities through a storage oscilloscope. Typical outputs

A

B

OPTICAL FIBER DIAMETER 0.125 MM , ST. STEEL TUBE O.D. 0.500 MM.

Figure (1.7) Photographs of "Half Circle" Optical Probe Tip

are presented in Fig. (1.8a) and (1.8b) for bubble velocities of 10 cm/sec and 184 cm/sec respectively. The output overshoot observed before (Abuaf 1977b) with the straight edge probes does not appear anymore. This fact strengthens our belief that the overshoot observed was not due to electronic response problems but was mainly due to the probe tip geometry and related optical effects. Another important observation is that the probe signal output does not go to zero as in the case of the flat edge probe, when immersed in water, but shows a positive dc level. The net probe signal output, I , at a given bubble velocity divided by the steady output in air, I_0 , is plotted in Fig. (1.9) as a

function of the bubble velocity. Since the overall size of the tip is reduced to 250 μm , a thinner water layer will be expected (Tallmadge 1968), and the signal should not drop as fast as the ones recorded with the 500 μm probe at increasing bubble velocities (Abuaf 1977b). Figure (1.9) shows that this is true in reality, and the attenuation of the signal observed is less for a smaller probe tip diameter. Figure (1.10) depicts the bubble penetration times as a function of the bubble velocity. The penetration times for the U-shaped probe tip geometry show a similar variation to the ones observed previously (Abuaf 1977a) but at the high velocity ranges ($U_B > 40$ cm/sec) are 33 percent longer than the penetration times previously reported for a straight edge probe. This graph shows that the probe tip geometry strongly affects the bubble penetration times, a point that has to be investigated.

In order to check the probe response at high bubble velocities, an air-water loop was constructed and rendered operational. Details on this loop were given by Leonhardt (1977). The probes to be tested can be inserted in the horizontal or vertical legs of the loop. The 500 μm probe with the 90° wedge angle was checked in the horizontal leg of the loop. An output signal at the passage of the bubbles was observed at a velocity range of 7.5 m/s. A more detailed investigation is needed and will be conducted after a Pitot tube is inserted in the loop for more accurate flow velocity measurements.

Parallel to this activity, a computer program was written and is operational in order to methodically study the geometrical optics and response characteristics of various probe tip geometries. The program first sets up a given probe tip geometry, half circle or linear (Fig. 1.11). It then divides the incoming fiber ($n_{\text{glass}} = 1.62$) into N equal segments. From each point, x_0, y_0 , rays of light traveling as straight lines and making an angle θ , with the vertical axis are drawn. The angle θ is varied from $-\theta_{\text{max}}$ to $+\theta_{\text{max}}$ in half degree increments. The value of θ_{max} is determined from the numerical aperture or acceptance cone provided by the optical fiber manufacturer. For our case, this was chosen as 30° . The program determines the intersection point of the specific light ray and the face B. At that point, Snell's law is applied to decide whether the ray is refracted out or reflected back assuming the probe tip to be in water or in air. The ray, if reflected, is followed to face C. The same procedure is applied to decide the refraction or reflection at point C. If the ray is once again reflected, and is traveling towards the detector D, the geometry is once again checked for refraction along the fiber assuming that the fiber is in air. Some of the rays reflected from the face B can also return through the fiber A. Keeping a close account of the rays which were initiated and the rays lost at A, B, C, or D, one is able to calculate the percent of the rays which go to the detector and provide a useful output signal. Figure (1.12) shows such a calculation and depicts the percent of rays lost at A, B, C, and D as a function of the half probe tip angle, α , when the tip was in air. The total loss subtracted from the incoming rays calculates the number of

A.

Bubble Velocity = 10 cm/sec
 Steady Signal in Water = -76 mV
 Steady Signal in Air = -184 mV

B.

Bubble Velocity = 184 cm/sec
 Steady Signal in Water = -80 mV
 Steady Signal in Air = -155 mV

Figure (1.8) Typical Oscillograms of the Probe Output

Figure (1.9) Plot of I/I_0 , Ratio of Signal Output Divided by Output at Steady Air as a Function of Bubble Velocity for Half Circle Probe Tip.

Figure (1.10) Penetration Time vs. Bubble Velocity for Half Circle Probe Tip.

Figure (1.11) Schematic Representation of the Probe Tip for Computer Program.

Figure (1.12) Percent of Light Rays Lost at the Profile Faces B or C or Along the Walls of the Two Fibers A and D, for the Flat Face Probe Tip as a Function of Half Profile Tip Angle.

rays transmitted to the detector. These results are plotted in Figure (1.13) for the case of the probe tip immersed in air and in water as a function of α . For the BNL probe geometry, the graph depicts a maximum for a half tip angle of 53° (106° total angle). The signal transmitted to the detector while the tip is in water is zero, until a half tip angle of 54° , and then shows a slight increase and decreases back to zero again. This behavior confirms our experimental results of zero output with a 90° total angle, immersed in water. In the same figure, we have also plotted the results for a probe tip which has the shape of half a circle. Although the signal in air is 35.5 percent, as compared to 32.5 percent (maximum for the linear probe), the water signal level for the circle is seen to be equal to 13 percent and not zero. This prediction is also agreeing with the observations reported in Figs. (1.8a) and (1.8 b), obtained with the probe shown in Fig. (1.7).

Figure (1.14) shows the variation of the signal output as a function of the numerical aperture or acceptance angle, $\theta_{\max}$, i.e., coherency of the light rays, for two probes with linear tips; one for $\alpha = 45^\circ$ (90° total angle) and one for $\alpha = 52^\circ$ (104° total angle). One observes that for coherent light $\theta_{\max} \rightarrow 0$, a probe tip angle of 90° will cause an almost 100 percent signal, implying no losses at all. For a noncoherent light and $\theta_{\max} = 30^\circ$, a probe tip angle of 104° shows a maximum signal output of 32.5 percent. Similar calculations were also performed for a half circular probe tip and are presented in Fig. (1.15). One observes that the effect of the light coherency or numerical aperture of the fiber is not very strong in the latter case.

This parametric study of the tip geometry and probe performance is being continued for the case of variable water layers over the probe tip. The predictions reported will be checked by building probes with various open angles and use of coherent light sources, i.e., He - Ne lasers.

Figure (1.13) Percent of Light Rays Reaching the Detector as a Function of the Half Probe Tip Angle for the Cases of the Probe Tip in Air and in Water. Comparison Between the Half Circle and Flat Face Probe Tip.

Figure (1.14) Percent of Light Rays Reaching the Detector as a Function of the Acceptance Angle for Two Half Probe Tip Angles of the Flat Face Probe Tip.

Figure (1.15) Percent of Light Rays Reaching the Detector as a Function of the Acceptance Angle for the Half Circle Profile Tip.

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2. THOR Code Development (W. Wulff, P. G. Kroeger, and G. S. Srikantiah)

2.1 General Program Objectives and Approach

The THOR Code Development Program is designed to produce an advanced computer code for the prediction of accident-induced thermo-hydraulic transients in water-cooled nuclear reactors.

The primary objectives are:

1. The model should include the effects of thermal non-equilibrium, as well as non-equal velocities between the vapor and liquid phases.
2. The reactor components should be modeled to include the specific characteristics of each component.
3. The axial variation of the transient fission power must be modeled.
4. The program should include the capability for an initial steady-state computation.
5. The code structure should be modular, permitting its application to different system configurations, as well as providing for ready improvement of individual component and process subroutines.
6. The code should be executing rapidly, for instance, for frequent application in parametric studies.

To achieve the above objectives a lumped parameter approach has been formulated for the various component models and a semi-implicit solution scheme is being followed.

2.2 Status and Current Goals

A considerable portion of the work directed towards the above objectives was done and has been reported in previous quarterly reports. This includes in particular the formulation of lumped parameter models for components with nonhomogeneous non-equilibrium flow, a system steady-state formulation and demonstration, as well as process models for drift flux relationships, choking conditions, and a model of the 1-dimensional neutron kinetics including the axial variation of fission power.

2.3 Current Achievements

2.3.1 Component Modeling

Reactor Core (P. G. Kroeger, C. J. Ruger, J. M. Kaufman, T. Wang)

In order to demonstrate internal stagnation and flow reversal in an inhomogeneous (drift flux) core model with a floating, flow regime dependent zone,

a three-region stand alone core model was programmed. This model is a direct extension of the two-zone model discussed in the previous quarterly report (Ruger, Kroeger, and Kaufman (1977)), and is being solved by an implicit solution scheme. The flow reversal algorithm automatically allows for through flow, or internal stagnation with simultaneous inflow or simultaneous outflow. The disappearance of flow regimes at the bottom of the core has also been accounted for.

Flow regime zones differ in the enthalpy profiles and drift fluxes. The mixture mass flux relative to each moving zone boundary $G_m - \rho_m U_m$, is checked to determine if the flow into the region through the interface is inward or outward. With through-flow one enthalpy profile must be provided per zone, while inlet enthalpy information provides a zone boundary condition. The zone average is thus the average of the terminal values, (linear profile assumption). With simultaneous inflow, enthalpy information is transmitted into the region through both boundaries thus requiring no enthalpy profiles. On the other hand, in the case of simultaneous zone outflow, no end point enthalpy information is provided to the zone. For this case, an additional enthalpy profile equation assumes the slope of the profile is identical to that of the previous time step. This assumption seems satisfactory since the mass flow and thus convection effects near the stagnation point must be small. Each element of fluid in the region, therefore, receives the same enthalpy change.

Details of a debugging run showing typical flow reversal and internal stagnation behavior are shown in Figures 2.1-2.6. Following startup, steady-state was achieved at $t=10$ sec at which time an inlet pressure decay shown in Figure 2.1 was begun. The internal zone boundaries (Figure 2.2) began to respond almost immediately by moving down and out the bottom of the core in ~ 200 msec, indicating a general increase of specific enthalpy in the core with friction. Figure 2.3 shows the total mass velocity transient from initial to final steady-state conditions, whereas Figure 2.4 contains the expanded scales near flow reversal/stagnation times. Similarly, Figures 2.5 and 2.6 show the enthalpy transients. Note that location 1 is at the core inlet, location 6 is at the core outlet, and location 2 and 3, and 4 and 5 are on either side of zone boundaries 1 and 2 respectively. Also note that the exit enthalpy, h_6 , was held fixed at the assumed upper plenum value following reversal at the core exit.

Enthalpy behavior has been found to be very sensitive to the assumed formulation for drift flux. For instance, use of a drift flux characteristic of slug flow during the flow reversal period allowed the liquid to drain preferentially out of the core, and the core power to cause high superheats in the remaining vapor. On the other hand, addition of a formulation which allowed the drift velocity to go uniformly to zero for vanishing liquid fractions, (also true of the real case), yielded nowhere near the preferential draining and much less severe peaking in the transient zone enthalpies. This demonstrates the importance of incorporating adequate physical models for phenomena of this type.

A subroutine to calculate the heat conduction in the core has been written. The routine links the thermal hydraulics with the reactor kinetics through the heat transfer equations. The fuel element assembly (fuel, gap, cladding) is subdivided axially and radially into a fixed nodal network, with prescribed axial spacing. This is required to adequately describe the coupling when

Figure 2.1 Assumed pressure conditions for three-zone core transient calculations.

Figure 2.2 Zone boundary locations during assumed core pressure transient.

Figure 2.3 Overall mass flux transient behavior showing attainment of final steady state.

Figure 2.4 Details of core mass flux behavior near the stagnation and flow reversal period.

Figure 2.5 Overall core enthalpy behavior during the pressure transient showing attainment of final steady state.

Figure 2.6 Details of the core enthalpy behavior near flow stagnation/reversal.

several axial heat flux and temperature profiles exist but when only one core flow region exist. The number of nodes axially and radially are selected as desired for computing accuracy.

At any time level, the heat source term obtained from the kinetics package and the fluid temperature obtained from the thermal hydraulics computations are used to solve for the temperature distribution in the fuel elements, iterating on the surface heat transfer coefficient, if necessary. The resulting heat flux at the surface will then be used explicitly to advance the core hydraulics.

The conduction package has been interfaced with the core hydraulics and debugging is now in progress.

Downcomer (U. S. Rohatgi)

A working model of the downcomer has been reformulated and programmed in semi-implicit form. In this model, which includes non-homogeneous flow and thermal non-equilibrium effects, the downcomer has been divided into two parts. The top part is treated as a small nonstorage mixing region, where the cold legs join. Its flow cross-section area is very large compared to the cold leg cross-section, so these junctions are treated simply as sudden area changes. The lower annular part of the downcomer is large, and all the storage of mixture and vapor mass, mixture momentum and mixture energy are considered here. All the sections of this downcomer model are described by the four equation drift flux formulation, including wall heat transfer in the lower annular section, governed by transient conduction.

This model of the downcomer requires standard constitutive relationships for wall heat transfer, wall friction as well as additional relations for drift flux velocity, and non-equilibrium vapor generation rate. The drift flux velocity is computed from the THOR drift flux routine, which has been modified to include drift flux velocities computed from the Wallis flooding correlation and uses the upper limit of the drift flux velocity as described by Bordelon et al., (1974). The non-equilibrium vapor generation rate is based on a relaxation model, (cf Jones and Swoboda (1977), Jones and Saha (1977), given in the form

$$\Gamma_{\text{non-equil}} = - \frac{\rho_m (h_f - h_l)}{\tau h_{fg}}$$

where

$$\tau = \frac{C_{pl} \rho_m}{A_s k_l} \sqrt{\frac{\pi K L}{3 V_m}}$$

This conduction controlled model is quite similar to that developed by Saha (1977) for the case of flashing. The characteristic time for thermal boundary layer penetration into liquid regions was taken as the downcomer transport time, $\tau_c = L/V_m$, L being the downcomer length. The interfacial area density was described as

$$A_s = \frac{6\alpha}{s}$$

where the linear dimension was scaled as the annular gap, s . This description for the condensation area is, of course, strictly true only for spherical voids of a diameter equal to the gap but was found to yield reasonable results and should suffice for development purposes until the area models are improved.

This modul has now been run beginning with a steady-state downflow in LOFT geometry with pressure decay from, 90 bar to ~2.5 bar without difficulty. This includes initial flow reversal, cold leg blowdown and initial flashing, followed by injection of subcooled liquid at the unbroken cold leg junction, (subcooling ~10% of saturation liquid enthalpy). While the LOFT data does not contain sufficient detail for accurate checkout of this stand-alone module, total and differential pressure histories may be used. Various approximations are being tried to adequately simulate alternate boundary conditions during the transient to enable the stand-alone module to be further tested.

A simple, non-LOFT geometry was tested for ECC-like debug and checkout purposes so as to insure the desired features performed in a reasonable manner. The results, shown in Figure 2.7 were calculated for an exponential pressure decay from ~90 bar, with an initial slope of 1.7 bar/s, beginning at time zero from steady-state upflow with a downcomer pressure drop of 0.75 bar. Cold water was injected from a 96 bar source beginning at ~2 sec. The jump in the relative velocity at ~7 sec is due to flow regime transition as the mass velocity drops below ~2700 kg/m²s, changing from a dispersed frothy mixture with drift characteristic of dispersed vapor, to a slug-like mixture with drift

Figure 2.7 Liquid and vapor velocities, and vapor mass concentrations during a downcomer transient.

characteristic of slug flow. Flow reversal with counter current flow is seen in the ~16-19 sec period resulting in downcomer penetration and a rapid decrease in overall vapor concentration, <c>.

Steam Generator (J. H. Jo, S. Stein)

The stand-alone model for one side of the steam generator considering non-homogeneous, thermal non-equilibrium flow with a variable number of flow regimes has been formulated in semi-implicit form, programmed, and is presently being debugged. Features include flow reversal, internal stagnation, internal kinematic shocks expressed by means of interface jump conditions. Current efforts also involve the development of algorithms to describe appearance and disappearance of flow regimes.

Assumptions used in the formulation include

1) Saturated vapor: $h_v = h_g(p)$

$$\rho_v = \rho_g(p)$$

2) Liquid density evaluated at saturation: $\rho_l = \rho_f(p)$

3) Pressure gradient in space is neglected in a flow regime for energy and vapor mass balance equations and all pressure related properties are evaluated at average pressure of the flow regime:

$$\frac{\partial p}{\partial z} = 0 \text{ (except for momentum) and } \frac{\partial p}{\partial \tau} = \langle \dot{p} \rangle$$

4) Linear h_m and c profiles.

5) Vapor generation rates may be expressed in either equilibrium form or non-equilibrium form for development purposes.

Work will continue to couple both sides of the steam generator, and to include wall heat conduction.

Pressurizer (P. Saha and A. Dubow)

A stand-alone program of the simplified pressurizer model, described in the previous quarterly report, has been operational. A schematic of the pressurizer model which can track the interfaces among various flow regimes, namely the single-phase liquid region, the low-void two-phase region and the high-void two-phase region, is shown in Figure 2.8. At the beginning of the transient, Z_1 is equal to Z_2 , and there is no flow in or out of the pressurizer. The liquid enthalpy in the single-phase region is assumed to be a constant, with the liquid-vapor interface at saturation. The transient is initiated by lowering the exit pressure, P_0^- , at a given rate.

The motion of the interface between the single-phase liquid region and the low-void two-phase region, i.e., Z_1 , is governed by

$$P_1^- = P_1^+ = P_{\text{sat}}(h_l)$$

whereas the motion of the interface between the low-void two-phase region and the high-void two-phase region, i.e., Z_2 , is calculated from the kinematic "jump" conditions across the same interface. Also, the following interface definition is used for the interface at Z_2 :

$$c_2^+ = 1 \quad (\text{no liquid entrainment})$$

$$\text{or,} \quad c_2^+ = \langle c \rangle_2 \quad \text{if} \quad \frac{G_2^+}{\rho_{m,2}} - \dot{Z}_2 < 0$$

Figure 2.8 Schematic of the pressurizer model.

Figure 2.9 Position of the interfaces.

The rate of vapor generation due to depressurization is assumed to occur according to the thermal equilibrium model. This assumption is justified because of the "moderate" depressurization rate inside the pressurizer and the "long" duration of the depressurization event during a typical LOCA. As mentioned in the previous quarterly report, the convective term of the depressurization, i.e., $v_m \frac{\partial P}{\partial Z}$, is neglected because it is small compared with the local rate, i.e., $\frac{\partial P}{\partial t}$. In addition, linear axial profiles for the vapor mass concentration, c , and the mixture mass-flux, G , are used in the low-void two-phase region.

A typical transient has been run with a PWR-size pressurizer with the following conditions:

a) Geometry:

Vessel cross-sectional area,	$A_v = 4.64 \text{ m}^2$
Surge-line cross-sectional area,	$A_s = 0.0525 \text{ m}^2$
Vessel height,	$Z_3 = 9.14 \text{ m}$

b) Initial Conditions:

Pressure at Vapor "Dome",	$\langle P \rangle_2 = 1.55.1 \text{ bar}$
Vapor-Liquid interface level,	$Z_1 = Z_2 = 2.835 \text{ m}$

c) Boundary Conditions:

$$\dot{P}_0 = - (3.8 + 0.74 \tau) \text{ bar/sec where } \tau \text{ is in seconds.}$$

Results are obtained by using a semi-implicit numerical technique and a non-linear equation solver called ZSYSTEM. Values of the pertinent parameters are shown in Figures 2.9 through 2.13.

Figure 2.9 shows the positions of the two interfaces, i.e., Z_1 and Z_2 . Notice that Z_1 i.e., the flashing boundary, reaches the bottom of the pressurizer in only 50 msec or so. The mixture level, i.e., Z_2 , however, drops rather slowly. In Figure 2.10 pressures at various locations of the pressurizer are shown. At the start of the transient, the pressure at the exit, P_0^- , was higher than the "dome" pressure, $\langle P \rangle_2$. However, during the transient, the rate of pressure drop at the dome is slower than the given rate of exit pressure drop. Therefore, at some point in time the "dome" pressure exceeds the exit pressure. Mixture mass-fluxes at various locations are shown in Figure 2.11. Everywhere in the pressurizer, the mixture-flow is in the downward direction. The large difference between the exit mass-flux, G_0^- , and the mass-fluxes inside the pressurizer is caused by the large area change at the exit. The vapor mass concentrations and the vapor void fractions at various locations are also shown in Figures 2.12 and 2.13, respectively. As the pressure drops in the "dome",

Figure 2.10 Pressures at various locations.

Figure 2.11 Mixture mass fluxes at various locations.

Figure 2.12 Vapor mass concentrations at various locations.

Figure 2.13 Vapor void fraction at various locations.

i.e., in zone <2>, some condensation takes place. However, the values of the vapor mass concentration and the vapor void fraction still remain close to unity. As expected, the vapor void fraction below the mixture level continues to increase due to continued depressurization. Finally, at $t \approx 8$ sec the high/low void interface exits the bottom of the pressurizer with resultant step changes in the mass concentration and mass flux.

In summary, a stand-alone pressurizer program has been made operational. Reasonable results have been obtained without any numerical instability, and the results of a test case have been presented.

2.3.2 System Modeling

THOR-1 Code Structure (S. V. Lekach, G. Srikantiah and R. Krasny)

The THOR-1 code structure has been completed during this quarter including a dump/restart capability. The structure makes use of the semi-implicit solution procedure to solve arbitrary pipe network problems based on the 4-equation drift flux model. Any pipe in a chosen system can be solved as either lumped or discrete. This structure has been tested with networks of increasing complexity and gave very good agreement between lumped and discrete modes of calculation including a Semi-Scale type 18-pipe, two-loop network. The results were presented at the Fifth Water Reactor Safety Information meeting in Washington, D. C.

Specific new capabilities have been added to the THOR-1 system routines. First, a counter-current flow formulation has been implemented in non-storage tee model. Then, a non-equilibrium vapor generation capability was added to the subroutine that formulates the drift flux equations. It has been tested in the realistic, 18-pipe, double loop network mentioned above (which is being used as the workbench for current investigations and code modifications). For both condensation and flashing, the routine includes the effects of low mixture velocity and pressure in the vapor generation rate. Also, vapor generation rates have been implemented such that they depend more on the future hydraulic solution vector. This has allowed us to use large time steps even with the non-equilibrium vapor generation/condensation (referred to briefly as non-equilibrium Γ), described elsewhere in this report by Saha (1977 b,c).

A single pipe pump loop, consisting of four pipes, a tee and a pump was also formulated to test certain features of the THOR-1 operating system. After attainment of steady state the pump is turned off and a blowdown is initiated at the interface of two pipes in the loop by imposing a pressure decay boundary condition of one bar per sec. The initial pressure in the system is about 40 bar and vapor concentration is about 0.5. When the pressure reaches a value of about 20 bars, injection is initiated at the inlet end of the tee. Several numerical experiments have been performed. Firstly, the injected fluid is two phase thermal equilibrium with a vapor concentration of about 0.5. Secondly, the vapor concentration of the injected fluid is reduced to zero within a short period of time after injection is started. Both experiments worked well without numerical instability. Presently an improved non-equilibrium (Saha, 1977 b) formulation for condensation is being tested.

Work has started on cold water injection into the double loop semiscale simulation using the new non-equilibrium Γ . The results look encouraging and no numerical oscillations have been observed for the particular test conditions that have been simulated thus far.

Interfacing of Components With Systems Code (D. Garber, R. Krasny and S. Lekach)

The reactor components reference both the interface variables (P, h, G, c) at each end, and an additional number of internal variables. Each component subroutine provides a non-square coefficient matrix (n by n+4) and the associated right hand side vector, which are reduced to 4 equations relating the 8 interface variables. This 8 x 4 matrix and its associated right hand side is in a form compatible with the existing code structure which eliminates equations to reduce the system of equations and variables (with a square matrix) to a minimum number for solving. Subsequent to solving the minimum set of equations, the variables which have been eliminated are backsolved, including the interface variables of our component. At this point the internal variables of the component being interfaced are backsolved. The above interfacing is being coded in two ways: one which involves inverting a matrix, the other involves Gaussian elimination and backsolving of a non-square matrix.

Graphical Display (D. Garber and R. Krasny)

The capability to display graphically the computer output has been developed. Three-dimensional plots of the primary variables (i.e., P, h, G, c) vs. distance along flow path and time have been generated. The ability to provide a reference diagram showing the "lay-out" of the components has also been implemented. Work continues to develop motion picture capability.

2.3.3 Process Modeling

Direct Contact Condensation (P. Saha)

When subcooled water mixes with saturated steam or steam-water mixture, direct contact condensation takes place. Such is the situation when emergency core cooling (ECC) water is injected into the primary system of a PWR during the refill stage of a hypothetical LOCA. Continuation of a calculational procedure at this point requires specification of a non-equilibrium condensation rate of steam.

It is generally agreed upon that an actual rate of direct contact condensation should be based on both the available vapor-liquid interfacial area, and on the interfacial heat flux to the subcooled liquid. However, in reality, the two-phase mixing process is so chaotic in nature that it is difficult even to estimate either of these two parameters. Therefore, as a first step, one may assume a liquid enthalpy distribution along the liquid-flow direction or (for co-current situation) an actual vapor flow quality distribution. One can then use the mixture thermal energy equation to calculate the actual rate of vapor condensation. Similar approaches have already been taken by Zuber, et al., (1966) and Levy (1967) in their studies on subcooled boiling.

Let us consider an adiabatic channel where subcooled liquid and saturated vapor are introduced through one end. Two-phase mixing will take place at the downstream of the entrance, and the actual vapor flow quality, x , will decrease along the axial direction. If the channel is long, the flow will eventually reach the equilibrium condition. A logical distribution of vapor flow quality, in this situation, can be the following:

$$x^* \equiv \frac{x - x_e}{x_i - x_e} = \exp \left(-n \frac{Z}{D_h} \right) \quad (2.1)$$

where x is the actual vapor flow quality, x_e is the equilibrium vapor flow quality, x_i is the actual vapor flow quality at the inlet, Z is the axial distance from the inlet, D_h is the hydraulic diameter of the channel, and n is a positive coefficient to be determined for experiments. By differentiating Equation (2.1) with respect to Z , and by using the steady-state mixture thermal energy equation, one can obtain the following expression for the rate of vapor generation (negative of the rate of condensation) per unit volume, Γ_v :

$$\Gamma_v = G \frac{dx}{dZ} = -n \frac{G (1-x)}{D_h} \frac{(h_f - h_l)}{h_{fg}} \quad (2.2)$$

where G is the mixture mass-flux, h_f is the enthalpy of the saturated liquid, h_l is the enthalpy of the subcooled liquid, and h_{fg} is the latent heat of vaporization. Notice that the rate of condensation, according to Equation (2.2) is indeed proportional to the local liquid subcooling, $(T_{sat} - T_l)$, and thus similar, in form, to the relaxation-type models discussed by Jones and Saha (1977).

For counter-current flow, the vapor flow quality, x , becomes meaningless. To circumvent this problem, one may specify the liquid enthalpy distribution along the liquid-flow direction. Since the liquid enthalpy increases as the condensation process proceeds, a logical distribution of the liquid enthalpy can be the following:

$$\frac{h_f - h_l}{h_f - h_{l,i}} = \exp \left(-m \frac{Z}{D_h} \right) \quad (2.3)$$

where $h_{l,i}$ is the liquid enthalpy at the liquid injection point, Z is the distance in the liquid-flow direction, and m is a positive coefficient. By assuming vapor to be at saturation and the pressure drop to be negligible compared to the system pressure, one can write the steady-state mixture thermal energy equation as

$$\Gamma_v = \frac{(1-\alpha) \rho_l v_l \frac{dh_l}{dZ}}{h_{fg} + (h_f - h_l)} \quad (2.4)$$

α is the vapor void fraction, ρ_ℓ is the liquid phase density, and v_ℓ is the liquid phase velocity. By differentiating Equation (2.3) and by combining it with Equation (2.4), one can write

$$\Gamma_v = - \frac{m}{D_h} \frac{(1-\alpha)\rho_\ell v_\ell (h_f - h_\ell)}{h_{fg} + (h_f - h_\ell)} \quad (2.5)$$

Recognizing that $G(1-x)$ in Equation (2.2) may be replaced by $(1-\alpha)\rho_\ell v_\ell$, one can notice the similarity between the two expressions for Γ_v , i.e., Equations (2.2) and (2.5). Either of the two may be used to evaluate the coefficient n or m .

Data of Aoki et al., (*) have been used for evaluation purposes. In these experiments, saturated steam and subcooled water flowed co-currently through a 5 cm x 2 cm horizontal rectangular channel. The condensate flow rates along the axial length were presented based on the measured liquid temperatures not reported in the papers. Therefore, the actual vapor flow qualities were calculated and compared with Equation (2.1) for different values of the coefficient, n . The comparison is shown in Figure 2.14, and a value of 0.35 is chosen for n . Final comparison of the prediction (with $n = 0.35$) with the data is shown in Figure 2.15.

In view of the similarity between expressions (2) and (5), the following correlation for Γ_v is proposed for both co-current and counter-current flows:

$$\Gamma_v = - 0.35 \frac{(1-\alpha)\rho_\ell |v_\ell| (h_f - h_\ell)}{D_h h_{fg}} \quad (2.6)$$

$(h_\ell \leq h_f)$

The above correlation is recommended for interim use only. As more data become available (particularly for counter-current flow situations), the correlation will be refined.

(*) Year of publication not known at this time. The author thanks Dr. N. Zuber of USNRC for providing a copy of each of the two papers by Aoki, et al.

Figure 2.14 Comparison of Equation (2.1) with the Data of Aoki, et. al.

Figure 2.15 Comparison of prediction ($n=0.35$) with Data of Aoki, et al.

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4. Stress Corrosion Cracking of PWR Steam Generator Tubing

(D. van Rooyen)

4.1 Test Equipment and Material

A large portion of time was spent in the first two months on ordering pressure vessels, associated instrumentation and materials for test specimens. This phase was completed, although many items have not yet been delivered. Present status indicates no serious delay or bottlenecks. A start has been made to set up a separate laboratory for this work, and this should be completed in good time to permit setting up the additional tests as planned. Two autoclaves are in service (pure H₂O at 360°C and 290°C).

One outstanding item is the selection of a system for applying cyclic stresses to test pieces. A decision will be possible in January, 1978, on whether to purchase an Instron or MTS hydraulic unit for this purpose, or whether a suitable device has to be constructed on-site.

At this time we can heat treat, process and fabricate all the required specimen types. Special jigs and controlled atmosphere furnaces have been set up.

4.2 Tests to Date

An initial test at 360°C in pure water was run for twenty days with material then on hand. No cracks were generated, and, since several additional heats were received in the interim, the same experiment was restarted in December, 1977 with the new specimens. The first twenty eight days have also shown no cracks and the test continues. Treatments are as in Table 4.1. The prior pickling is intended to accelerate cracking, and the various annealing treatments also influence SCC performance.

A very promising way of inducing susceptibility in Inconel 600 is believed to be a heat treatment of cold worked metal in a hydrogen atmosphere in the range of 870-980 C (1600-1800 F). Cold worked tubing was ordered from Babcock and Wilcox (B&W) (.04% C) and Pacific Tube Company (Patco) (.01% C) for this purpose. Special hope for early cracking is placed on the .01% C Patco alloy, since it is promoted by very low C content, and the surface layers may be even further depleted by the heating in hydrogen. The B&W tubing arrived (as cold worked) on January 9, 1978, and the Patco shipment is to be made in the week of January 16, 1978. Specimens will be added to the 360°C test in February, 1978.

Testing of the various materials at 360°F in pure water is intended to ascertain the most susceptible material for extensive further testing, which is outlined in the Brookhaven National Laboratory (BNL) test proposal. These additional tests to start during 1978 involve slow strain, cyclic stress, several temperatures and stresses and controlled potential in pure water and simulated primary water.

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In addition to the 360°C test, a 290°C test is now underway for the regular production material and some selected plate U-bends. Three stress

levels are included. Since this will be a long-term test, the second of the two available autoclaves was chosen for this purpose. As other materials become available, they will be added at 290°C, at more than one stress level, as C-ring and U-bend specimens. Results at this low temperature will probably start appearing in one to three years.

Metallography and full chemical analyses have been requested for material on hand, as well as determination of mechanical properties at room and elevated temperatures.

TABLE 4.1
LIST OF PREPARATIONS

TYPE	%C	PROCESSING
Plate	.08	Rolled 1800°F + 1775°F/15 mins/H ₂ + pickle HNO ₃ /HF
Plate	.08	1600°F/20 hrs/air + rolled 1800°F + 1775°F/15 min/H ₂ + pickle HNO ₃ /HF
Plate	.08	1700°F/1 hr/air + rolled 1800°F + 1775°F/15 min/H ₂ + pickle HNO ₃ /HF
Plate	.08	1600°F/3 hrs/air + rolled 1800°F + 1700°F/3 hrs/air + pickle HNO ₃ /HF
Plate	.08	1600°F/20 hrs/air + rolled 1800°F + 1700°F/3 1/2 hrs/air + pickle HNO ₃ /HF
Blairesville Tubing**	.04(?)	Split along axis, plus: <ul style="list-style-type: none"> (1) none (2) pickled HNO₃/HF (3) 1775°F/15 min/H₂ (4) 1775°F/15 min/H₂ + pickled HNO₃/HF
Patco Tubing* .095" wall	.01	Same four conditions as tubing above.
Patco Tubing*	.01	Same four conditions as tubing above.
Cyclops Tubing .046" wall	.04(?)	As received only. (Other three conditions being prepared for adding when ready).

(?) = To be confirmed, nominal value listed.

* = This is not the material supplied in cold worked condition for our own final annealing.

** = All tubing is bent in "reverse", so as to expose the inside surface in tension. U-bends allow for axial strain, as well as hoop strain that results from ovalizing during bending.