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AEROSPACE SAFETY PROGRAM .
SUMMARY REPORT

AEC Research and Development Report

MASTER

**Atomics International Division
Rockwell International**

P.O. Box 309
Canoga Park, California 91304

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AEROSPACE SAFETY PROGRAM
SUMMARY REPORT

J. M. OTTER
K. E. BUTTREY
R. P. JOHNSON

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ABSTRACT

Projects in support of the Aerospace Nuclear Safety Program are summarized. The objective of the program was to evaluate the radiological safety of SNAP systems and to develop methods and designs to assure that safety.

Projects summarized are analyses and experiments for determining: (1) the disintegration of reactors reentering the atmosphere; (2) the burnup of fuel elements reentering the atmosphere, including ablation and dispersal of particles; (3) reactor criticality conditions and the assurance of subcriticality in water; (4) the behavior of reactors in transient power operation; (5) the nuclear behavior of reactors on impact with the earth and in handling accidents; (6) the assurance of shutdown at the end of the power production lifetime; (7) risks by various exposure modes from different methods of reactor disposal; and (8) thermophysical properties of SNAP fuel materials.

I. INTRODUCTION

The ultimate use of the results of the Aerospace Safety Program was to aid in the determination of the probability of an overexposure to radiation from a SNAP mission. This evaluation was performed by summing the probable number of overexposures over all exposure modes of all events of all disposal modes or accident conditions. The importance to, and sensitivity of, the overall hazard to each parameter was determined to guide the investigations. As information was accumulated and models developed, the importance and sensitivity values were recomputed, and the direction of the program was reexamined.

The response of the SNAP system on a particular mission was predicted by means of analytical models. These models were based on experimental data and correlations, where possible, plus theoretical techniques. The models were used to calculate results of program experiments. Comparisons were used to suggest model improvements and further experiments. Extrapolation of the correlations to mission conditions were made, where needed.

Section II presents a definition and evaluation of the program, together with a summary of project results. More detailed reviews of projects are given in Section III.

II. SUMMARY

A. PROGRAM DEFINITION

Before an atomic reactor is launched into a satellite orbit about the Earth, the hazards to the population must necessarily be considered in the event that the reactor reenters the Earth's atmosphere and possibly impacts with the ground. In addition, safe prelaunch handling must be assured.

The objectives of the Aerospace Nuclear Safety Program were to evaluate the nuclear hazards associated with the construction, launch, operation, and disposal of SNAP systems and to develop methods and designs to assure their radiological safety.

In assessing the safety inherent in the use of SNAP reactor systems, it is important to consider not only the conventional problems associated with conducting space launches, but also possible nuclear problems unique to reactor space power systems. Potential nuclear safety problems for reactor systems can only arise following criticality and the subsequent generation of fission products.

Accidental criticality may present problems during the prelaunch activity associated with preparation of the system for its space mission. Techniques were developed and successfully used in the SNAP reactor program for all safety problems associated with the fabrication, testing, transportation, handling, and launch of one nuclear-powered system.

Post-operational disposal of the orbiting reactor may also pose problems of public safety. Present space reactor systems are maintained subcritical until a satisfactory space orbit has been achieved. A reactor operating in space does not constitute a hazard; but, upon return to Earth, the fission products which have been generated could compromise public safety unless appropriate steps are taken. Techniques were developed to safely dispose of the generated fission products.

In general, there are three techniques for safely disposing of these fission products. The first of these techniques is fission product decay. In any space application, fission products will have decayed to a safe level if reactor operation is terminated within 5 yr and the fission products are allowed to decay for 50 yr

or more. The second technique is to recover the reactor and use standard reprocessing and disposal techniques. The third technique is to dilute the fission products to background or acceptable levels in either the atmosphere or the ocean.

Exposure can result from external or internal radiation sources. External sources result from direct radiation from reactor debris and from reactor operation on the Earth's surface; internal sources are caused by inhalation of fission or activation products released to the atmosphere, and by ingestion of these products released to drinking water or available through the food chain. The three alternative disposal modes lead to somewhat different types and levels of exposure.

A typical disposal mode events tree for a SNAP mission is shown in Figure 1. Results of aerospace safety studies were used to quantitatively assess a mission's safety. Hazards were assessed by summing the probability of exposure for each of the consequences of an event, times the probability of the event for all events of all possible disposal modes. Probabilities were evaluated from the results of detailed analytical and experimental studies of various aspects of accidents and disposal modes.

The definition of analyses and experiments and the evaluation of disposal modes and exposure probabilities were refined iteratively throughout the program. Projects are summarized in Section II-B followed by an evaluation of the program in Section II-C.

B. PROJECTS

1. Reactor Disintegration

One method of eliminating any significant hazard from a SNAP reactor reentering the atmosphere would be for the core to disassemble at a high enough altitude for the fuel elements to ablate and disperse in the upper atmosphere. An analytical and experimental program was conducted to determine conditions and designs which would effect disassembly, using the reentry heating available during orbital decay.

The study of reactor reentry behavior (temperature response to aerodynamic heating, mode of failure of the vessel, and release of the fuel rods) consisted essentially of an analytical approach, at first. Studies were made of the

Figure 1. Events Leading to Potential Exposure

aerodynamic and inertial characteristics of the SNAP 10A-Agenda D reactor and vehicle, the reentering satellite trajectory, reentry attitude and oscillation, stagnation and local aerodynamic heat rates, flow regimes, and failure modes. Thermal networks were used to describe the temperature response of the reactor when subjected to aerodynamic heating. Preliminary studies led to the conclusion that the available reentry heating during orbital decay was adequate to effect complete reactor meltdown and fuel ablation of SNAP 10A. This conclusion was based on theoretical aerodynamic calculations.

Subsequently, experimental data were obtained on the aerodynamic force and heating coefficients for the SNAP 10A system. Wind tunnel tests were run to determine local heating coefficients at various angles of attack and at various simulated altitudes. A flight test, Reentry Flight Demonstration No. 1 (RFD-1) of a full-scale mockup of the SNAP 10A reactor was conducted in an actual reentry environment to obtain aerodynamic data and confirm the theoretically predicted response of the reactor to reentry. A NASA Scout booster rocket launched a nonradioactive SNAP 10A model into a suborbital flight path, with an apogee of 490,000 ft. Much valuable data were obtained by telemetry during the descent. A study of aerodynamic heating in nonequilibrium flow and in transition regimes between free-molecule and equilibrium flow was initiated because of the results of this test.

In addition to these experimental data, the analysis was improved through the use of more refined methods for calculating aerodynamic heating rates to the reactor. Based upon these experimental data and on the improved analysis, the conclusion was drawn that the reactor vessel walls would melt away allowing the fuel rod core assembly to fall free at an altitude of not less than 237,000 ft.

A test program was conducted to determine the adhesion that could occur between the stainless steel core vessel and the beryllium reflector. Such self-welding could delay or prevent reactor disintegration. Tests were made in space environments with and without colloidal graphite coatings on the contacting surfaces. Adhesion occurred between uncoated surfaces, but was effectively prevented by the coating.

Tests to determine the values of emissivity for core vessel material in a reentry environment were made. The emittance is a key parameter in the

calculation of the rate of ablation and, thus, of reactor disintegration. The emittance appeared to be a complex function of pressure, time, and temperature. Near the melting point, the values obtained were considerably higher than was desired for rapid core vessel ablation.

A hyperthermal wind tunnel experiment, Reactor Ablation Disintegration Experiments (RADE), was conducted using half-scale models of the SNAP 10A and SNAP 8 reactors. Aerodynamic heat fluxes were measured, and ablation and disintegration sequences were photographed in detail. Ablation to the point of fuel element release was not achieved. The tests were made to provide data for verifying and improving calculational models for complicated SNAP reactor geometries. Results indicated that considerable welding between the upper grid plate and the fuel element bundle and between fuel elements probably would occur upon reentry. Analytical models were developed and measured parameters were calculated. Accurate predictions of the preablation temperature response of reentering SNAP reactors were indicated by the good agreement with experiment.

Reentry aerodynamic heating distributions for SNAP 8 were obtained based on hypersonic shock tunnel experiments. Results formed the basis for heating distributions used in analytic models. A highly instrumented, 1/5-scale model was used. Heat transfer correlations derived from the data provided a relatively complete heat-transfer distribution over the SNAP 8 configuration for 0 to 70-degree angles of attack.

An analysis of the thermal behavior of a SNAP 8 reactor system reentering the Earth's atmosphere was made. Analysis was made for a spinning reactor with the neutron shadow shield attached and for a separated reactor tumbling randomly. The heating for the latter reentry mode was less than the former. In both cases, however, it was concluded that the SNAP 8 reactor vessel would not ablate sufficiently to release fuel elements at altitudes necessary for complete fuel ablation.

Conversely to long orbital decay missions where the disintegration of the core by reentry heating is desirable, core survival may be desirable for short orbital decay missions to avoid the scatter of fuel elements over a wide area. Tests were made on the ablation of lithium hydride (LiH) to evaluate its

effectiveness as a protection for the top surface of the reactor core vessel to assure core survival. Measurements of heating rates and ablation were made in an arc-heated wind tunnel. Using the measured data, calculations of LiH ablation were made with the Thermal Analyzer Program (TAP). All studies indicated that LiH would provide effective reentry protection.

2. Fuel Rod Reentry Burnup

To determine the conditions of reentry which would cause meltdown and dispersion of a fuel rod, it was necessary to understand the characteristic behavior of the fuel material when it is subjected to a reentry environment. First, analytical descriptions were formulated and used to predict the expected behavior of a fuel rod for various reentry trajectories. However, the formulations were complex and required a number of approximations to the physical phenomena involved (i. e. , the aerodynamic heating, the material thermodynamics and metallurgy, and the fuel rod dynamics). The refinement and verification of these analytical approaches required experimental support.

Initial tests were made in the Rockwell International arc-heated, hyper-thermal wind tunnel. Overall fuel rod behavior was studied, as well as separate effects of cladding and fuel oxidation. The cladding was observed to fail early in the heating sequence. The remaining fuel material was observed to form cracks and heat without ablation for a period, then to melt, flowing viscously back and ablating rapidly from the leading edge. Oxidation appeared to cause the flow to be less viscous and the ablation to occur faster.

Heat flux to the specimens was determined experimentally. Post-test microstructural analysis of specimens indicated dehydriding adjacent to surfaces and cracks, discrete interfaces between metallurgical phases, and surface oxidation of zirconium.

Measured temperature histories were compared with results of computations, using a simple thermal approximation to the fuel material. Reasonably good agreement was obtained. By making preliminary assumptions with regard to reentering rod trajectories and motions and other simplifying assumptions, the arc-jet behavior was extrapolated to the case of reentry. The calculations indicated that, with a 30% uncertainty, a fuel rod released at 241,000 ft would melt down on reentry.

A detailed analysis of reentry burnup of SNAP 10A reactor fuel elements was undertaken. The aerodynamic heating, transpiration cooling, and the chemical reactions between the fuel and air were analyzed to determine the net surface heat flux. An uncertainty analysis was made for each significant parameter to provide direction for experimental support investigations. The analytical description was correlated with the hyperthermal wind tunnel test data. The analysis was confined to fuel element flight modes which effect a uniform heating around the surface of the element, namely: (1) cross-axial spinning, (2) random tumbling, and (3) end-over-end tumbling.

Oxidation of zirconium-uranium hydride ZrUH fuel was studied. A temperature independent rate constant was employed. Oxide film thicknesses during the period of rapid hydrogen evolution were calculated to be 1 to 4 mils.

Fuel element material properties were evaluated. The reduction in heating due to noncatalytic wall effects was estimated. A need for experimental investigation was identified.

A set of calculations was performed for various release altitudes and initial temperatures to define the minimum release altitudes for which complete ablation of the fuel material is attained. The minimum altitudes were determined to be:

- 1) 245,000 to 252,000 ft for cross-axial spinning
- 2) 238,000 to 245,000 ft for end-over-end tumbling
- 3) 235,000 to 241,000 ft for random tumbling.

The fuel element altitudes at the time of initial melting ranged from 174,000 to 230,000 ft for cross-axial spinning and from 162,000 to 230,000 ft for random tumbling. In a typical case, complete ablation occurred 30,000 ft below initial melting.

The results of statistical analyses were strongly influenced by the values assigned to the various initial conditions, thermophysical properties, and trajectory parameters. Several parameters which strongly affect the heating requirements were not well defined, including the hydrogen diffusion coefficient, fuel emissivity, heat of fuel oxidation, and thermal conductivity. The analyses indicated that the optimum release altitude for a SNAP 10A fuel element lies around 300,000 ft. The probability of initial ablation was enhanced if the fuel

element tumbled at random rather than spun about a stabilized cross-axial orientation.

Systematic investigations of the thermal properties of SNAP fuel were made to obtain reliable thermophysical properties data needed to describe its behavior during reactor operation and reentry. Considerable care was exercised to control hydrogen mobility effects. Heat capacity, thermal diffusivity, electrical resistivity, and thermal conductivity were measured for zirconium-10 at . % uranium, hydrided to H/Zr ratios ranging from 0 to 1.9. Some companion measurements were made at Battelle Memorial Institute under a specially controlled comparison program. Highly accurate data were obtained as functions of temperature and hydrogen content. Qualitative deductions of possible phase transformations in the ZrUH system were made from the results.

Advanced arc-jet tests were performed to provide experimental support for SNAP fuel element reentry burnup analytical programs and associated fuel particle disintegration studies. Data were obtained on the transient temperature and hydrogen effusion behavior, surface catalycity effects on heat flux, and ablating particle size distributions and compositions. The large number of specimens tested and flow conditions and configurations used allowed several confident correlations and conclusions to be made.

Heavy zirconium-oxide buildup occurred at the surface of all specimens, resulting in flaking and spalling of the oxide at high temperatures. Hydrogen outgassing was usually evidenced. Outgassing was uniform and continuous along the stagnation line except for profuse emission from open cracks. Completely solid specimens developed a fine network of microcracks. Large cracks were rare. The oxide layer inhibited fuel ablation until a substantial portion of fuel material became molten underneath it. Ablation of axially exposed specimens was consistently initiated at the center.

Correlations of temperature history data were made with analytical models. Good correlation was obtained, except for the sharper thermal responses. A preliminary analysis substantiated the omission of surface catalycity effects in the calculational model as the likely cause of the discrepancies.

Ablated material was collected and chemically analyzed. The major component was alpha zirconium, void of hydrogen. Size distributions were measured

for collected material, but accuracy was limited by splattering. Other results included measurement of oxide layers up to 68 mils thick, and a determination that fuel ablation is not enhanced by surface irregularities.

A digital computer code was written to describe the behavior of liquid metal particles. Heat, mass, and momentum transfer mechanisms were described. The code determined the temperature, radius, and velocity of a particle as functions of time. The particle was heated by aerodynamic and oxidation heating and cooled by convection, radiation, and evaporation. A simple fission product inventory model was included.

The code was used to perform a statistical analysis of the behavior of reentering fuel particles. Parameter sensitivity coefficients and uncertainties in the final radius due to individual parameters were calculated. Much of the data on the physical properties of molten SNAP fuel alloy had to be borrowed from other states or materials. Particle size ranges were taken from arc-jet results and particle stability studies. By far the most important parameter in determining the final radius was the initial radius. The 2σ uncertainties in the final radii for SNAP 8 and SNAP 10 fuel elements ranged from 55 to 72%.

A special purpose trajectory code was written to compute the aerodynamics and heating of ablation particles. The code was used to study parametrically the heating regimes important for nonablating spherical particles. The equations of motion and aerodynamic heating and drag equations were solved by the code. A drag coefficient varying with the ratio of the particle mass to the mean free path was used. Transition regime aerodynamics and heating were shown to be particularly important when evaluating particles released from SNAP reactor systems during reentry.

Needs were identified for experiments to: (1) better define the distribution of particles from fuel element ablation in terms of size, composition, density, and shape; (2) investigate methods of enhancing breakup of fuel into submicron particles; and (3) determine oxidation of reentering particles. Guidelines for experiments were set forth. Several experimental techniques for simulating the oxygen mass flux to the surface were investigated, and a fuel particle experiment was outlined.

3. Critical Configurations

Ocean impact disposal modes and land impact disposal modes or handling or launch incidents under the proper set of circumstances would result in water reflection and/or flooding of the SNAP reactor core. Consequently, a program was conducted to determine the reactivities of SNAP 10A and SNAP 8 type reactor cores under such conditions, and to test the effectiveness of devices designed to maintain subcriticality.

Initial experiments were conducted with a SNAP 10A core immersed in water. Test apparatus and equipment were designed and tests conducted in the SNAP 2 Environmental Test Facility. The excess reactivity of a water-reflected bare core was determined to be $\$6.4 \pm 0.6$. The additional worth of internal water was estimated to be $\$7.2 \pm 0.7$. Simple boron poison sleeves surrounding the core were found to render the water-reflected dry core subcritical, but were not adequate to prevent criticality in the water-flooded-and-reflected case.

In subsequent tests, it was demonstrated that:

- 1) A combination void and poison sleeve (designated a shipping sleeve), surrounding the bare core, was capable of maintaining SNAP 10A and SNAP 8 type reactor cores subcritical when they were immersed in, and internally flooded by, water.
- 2) Combination void and poison filler blocks that fill the four control drum voids and cover two radial beryllium faces of the reactor were capable of maintaining a beryllium-reflected SNAP 10A type reactor subcritical when it is immersed in, but not internally flooded by, water.

Since the foregoing procedures did not preclude the possibility of accidental criticality during a launch abort into water, a concept which would maintain adequate core performance under normal operating conditions but safety in a water environment was sought. Two such Intrinsic Water Subcriticality (IWS) methods were investigated in detail; these were:

- 1) Use of highly spectrum-dependent thermal -neutron absorbers in the fuel element and core reflector interface region
- 2) Use of modified core and reflector geometries.

The first method takes advantage of the shift in the thermal neutron spectrum, upon water immersion, to increase the absorption in an isotope whose cross section increases rapidly in energy just below the normal spectrum peak. Burn-out of this poison in the operational mode would have the advantage of compensating for normal reactivity losses. The second method would exploit the high neutron leakage characteristic of SNAP reactors and the superior neutron-reflecting properties of beryllium relative to water. This protection would not burn up with operation as the other, but would incur system weight and power lifetime penalties.

Initial experiments with gadolinium-oxide for the first method and beryllium-oxide reflector arrangements for the second were used to normalize calculational models. Potential IWS capability was indicated for both methods.

Analytical studies were performed on intrinsically subcritical SNAP 10A and SNAP 8 reactors. System weight and power lifetime penalties associated with each method were evaluated. On the basis of these studies, gadolinium-155 was selected for further study.

Measurements were made of the reactivity effect and effect on the reactor temperature coefficient in both water-immersed and operational core configurations. Results indicated that higher Gd^{155} loadings were needed than predicted. The loading for SNAP 10A was found to reduce the magnitude of the negative prompt temperature coefficient by an unacceptable amount. In other SNAP systems, the reactivity penalty from residual gadolinium poisoning from the amounts required would be unacceptably large.

A search for other isotopes to use with or instead of Gd^{155} was conducted. By using a combination of isotopes which had neutron absorption resonances just below and just above the median fission energy, the effect on the temperature coefficient could be tolerable. Two such isotopes, having potentially appropriate resonance locations and cross section magnitudes, are samarium-149 and europium-151.

The SNAP Transient (SNAPTRAN) reactor was used to measure the reactivity loss, isothermal temperature coefficient, and prompt temperature coefficient of a SNAP 10A reactor loaded uniformly with Sm^{149} or Eu^{151} . Eu^{151} was

not indicated for use by itself because of a slow burnout rate and a possible large increase in the temperature defect. The results indicated a preference for combinations similar to the following:

- 1) \$6 of Gd¹⁵⁵ and \$2 of Eu¹⁵¹
- 2) \$6 of Eu¹⁵¹ and \$2 of Sm¹⁴⁹

Under certain, albeit unusual, circumstances, the reactor might achieve steady-state operation during launch procedures or intact reentry. Radiation dose rates were calculated for a launch accident in which the reactor fell into a basin retaining water left over from rocket fuel loading procedures. Doses were also calculated for reentry of a reactor impacting intact into soil which retained water in a crater around it. A person conducting a 1-min close inspection was calculated to be exposed to a dose of about 500 rem from a reactor operating at the ultimate power of 6 kw.

4. Reactor Transient Behavior

Destructive nuclear excursions of SNAP reactors are not entirely precluded under certain launch accident or reentry impact conditions. Analysis to determine the maximum reactivity accident was performed, supported by a program of transient behavior tests.

A NaK-filled SNAP 10 A/2 type reactor was submerged in water and a transient was initiated by a step insertion of \$3.60 excess reactivity. The transient was terminated by core disassembly. Called SNAPTRAN-3, the test was performed at the National Reactor Testing Station (NRTS) by Phillips Petroleum Co. The test was instrumented to record fast and thermal neutron and gamma ray fluxes, temperatures and energy depositions, vessel strain, displacement, acceleration, and pressure, noise, and fission product release activity, and was documented photographically.

Reactivity as a function of time was obtained from power history (energy probe) data by solving the reactor-kinetics equations. Continuous energy deposition was indicated up to fuel disintegration or massive mechanical disassembly. Temperatures measured at disassembly ranged from 1800 to 2000°F. Photographs showed the vessel to bulge at the center, becoming circular as expansion proceeded. Radial and axial temperature distributions were obtained from fitted

energy probe data. The energy probe data were integrated to obtain the total core energy release of 32.5 ± 3.6 Mev-sec. A fuel-moderator temperature coefficient of $-0.22 \text{ } \phi / ^\circ \text{F}$ up to about 1400°F was derived from the data. A reactivity feedback coefficient of expansion was derived, using expansion photographs. The value was $-\$6.65/\text{in.}$ up to 0.12-in. expansion, and $-\$11.05/\text{in.}$ thereafter.

Specific results were calculated using theoretical models. Good agreement was obtained for power, energy, inverse period, reactivity, and maximum radial expansion as functions of time. Using improved analytical models based on SNAPTRAN-1 and -2 experimental results (described later in this section), the temperature coefficient was calculated to be $-0.24 \text{ } \phi / ^\circ \text{F}$, in good agreement with the experiment.

Analysis of the remains of five fuel elements was made which showed that fuel disintegration was caused by the generation of hydrogen pressure.

Electrical pulse-heating experiments were performed to determine the specific heat, thermal expansion, and hydrogen evolution characteristics of ZrUH_x fuel material. Heating rates in excess of $80,000^\circ \text{C/sec}$ were employed. Samples had H/Zr ratios of 0.50, 1.20, 1.74, and 1.81.

Specimens heated in excess of $80,000^\circ \text{C/sec}$ exploded between 20 and 30 msec without evolving hydrogen; those heated between 20,000 and $80,000^\circ \text{C/sec}$ exploded prior to 40 msec and began to evolve hydrogen just before failure; while those heated below $20,000^\circ \text{C/sec}$ evolved hydrogen over a longer period and exhibited characteristics of three metallurgical phases. Precise values of electrical resistivity and specific heat were obtained. Approximately 2% expansion was observed for the higher heating rates at the point of failure.

ZrUH samples were subjected to 1 to 5 neutron bursts in experiments in the Transient Reactor Test (TREAT) reactor at NRTS. The capsules were instrumented for temperature and pressure. Temperature from 1100 to $1500+^\circ \text{C}$ were recorded. Final hydrogen contents ranged from 0.15 to 1.50 wt % compared to the original 1.8 wt %. Burnup of up to 3.0×10^{-5} at % was measured.

A series of experiments was performed to determine the fission product release from SNAP 10A/2 fuel in order to evaluate the radiation hazards from nuclear incidents. Both clad and unclad samples were used. Samples irradiated

for the equivalent of 1 yr operation at 60 kw were subsequently heated to 2000 to 3500° F. Temperatures were maintained for a few minutes for melts and 8 hr for nonmelts. All fission products were collected. The activity of isotopes of interest was determined by radiochemical analyses. Fission product activities were measured with a scintillation detector.

Data indicated that fission product gas release was relatively constant during a run; it was not evolved in a large burst when the temperature was raised. Release fractions were obtained for the main isotopes of interest. Using the maximum excursion temperature from SNAPTRAN-3, these results indicate releases from clad samples would be several orders of magnitude less than maximum theoretical releases for postulated accidents.

An experimental study of the boiling of water in a SNAP 10A reactor coolant channel was made to improve the analysis of the behavior of the reactor should it achieve criticality by water reflection or flooding. Depending on its dynamic characteristics, the reactor would either operate stably or destroy itself. A mockup of a single channel was operated both with the channel completely submerged and with it only partially filled with water.

An analytical model for a partially filled channel was developed. A cyclic chugging of the water was predicted.

Experimentally, heat rates up to 6 kw were applied with a cosine distribution. Axial temperatures were measured. For the partially filled channel, temperatures increased with power increases. Above a threshold power, chugging and boiling occurred, the frequency of which increased with the power. It was shown that steady boiling could be achieved in a single channel for powers up to 6 kw (reactor power of 440 kw).

Capsules containing SNAP fuel were placed outside a Kiwi reactor core, which was destroyed in a nuclear excursion. The capsules were designed to measure thermal expansion, peak pressure, impulse during disintegration, and disintegration of constrained fuel. Four different positions outside the test reactor were used. Heating occurred from fissioning of test fuel. Extensive postirradiation examinations were conducted.

The capsules were damaged before peak pressures were measured. Impact energies which corresponded to about 540 kcal/gm mole H_2 were measured. The fuel mostly remained in good condition, even though disintegration temperatures were exceeded. Expansion appeared to decrease with decreased neutron exposure. A linear correlation existed between energy release and fuel burnup.

A series of nondestructive transients were run on the SNAPTRAN-1 assembly to enable a detailed study to be made of the prompt neutron kinetics of the SNAP core with a beryllium reflector. Extensive analyses of the tests were performed to extract basic kinetic data over a wide range of variables.

The SNAPTRAN-1 assembly was specially designed for reactivity pulsing and was highly instrumented. Peak power, energy release, temperature, pressure, and strain were monitored. Average core temperatures up to 1500°F and reactivity inputs up to \$4.15 were attained.

Radial and axial energy profiles were derived from energy probe data. The magnitude of the temperature coefficient increased with mean temperature and inverse period. Analysis of several fuel elements showed that only a minor degree of peripheral dehydriding had occurred.

The analytical transient behavior model was refined, using the results of the SNAPTRAN-1 tests, and used to predict the results of a planned destruct test, SNAPTRAN-2. The SNAPTRAN-2 reactivity insertion was a step to slightly supercritical followed by a second step. The object of the test was to extend the knowledge of neutronic behavior beyond the range observed in SNAPTRAN-1 tests and to observe the mechanical and fission product release behavior. The results were factored into the analytical model to provide an accurate determination of consequences of maximum reactivity accidents in SNAP 10A/2 reactors.

A complete radiation monitoring grid for fission product gases was set up. Instrumentation included detectors to measure reactor power, temperature, radial and axial fuel element, vessel and reflector strain, acoustics, and motion, plus photography. \$5.05 reactivity was inserted resulting in a peak power of 7.1×10^4 Mw and a total energy release of 54 Mw/sec.

Photograph showed that fuel disintegration began at the center of the core and proceeded outward. Theoretical calculations of power and the time of fuel disintegration agreed very well with the experimental results. Analysis of fuel debris showed that a significant portion had melted.

5. Mechanical and Thermochemical Incidents

Accidents such as impacts, chemical interactions, explosions, and fires could be postulated during various handling, transportation, and launch situations. To obtain data and information which could be analyzed for evaluating the associated hazards, a series of tests was conducted at Holloman Air Force Base, New Mexico. Full-scale mockup reactors and system were subjected to conditions similar to the various postulated accidents. Tests were carefully selected to give a statistically representative sampling of credible conditions.

The reactor with and without NaK was exposed to a liquid oxygen spray. No damage occurred. A suspended reactor was subjected to an over pressure of 400 psi for 28 msec caused by an explosion of TNT. The reflectors were blown off and the bottom of the reactor vessel deformed inwardly. A reactor immersed in water underwent simulated rupture of the NaK nozzles. Two explosive pulses were followed by a gradual pressure buildup. The core vessel was not damaged.

The reactor assembly was dropped side-on, tail-on, and nose-on onto concrete from 100 ft. Reflector drums were severed. No nuclear hazards were indicated. The reactor was subjected to a simulated launch abort fire. The reflector separated; the vessel remained intact. A fire test on control drum lockout pins indicated that they would not fail during a free fall to the pad during a launch fire.

Three reactor assemblies and one unit including the converter and shield were impacted, at different attitudes, into an 8 by 8 by 16-ft water tank at 400 to 600-ft/sec velocities simulating a fall into water from orbit apogee. The reflector separated and other damage occurred in some cases, but disassembly was not sufficient to prevent criticality in water. A reactor was impacted onto concrete at 560 ft/sec. The reactor was completely destroyed. A reactor assembly was impacted at terminal velocity into compacted soil. The reflectors separated but the core vessel survived.

The tests indicated that no significant nuclear hazard would occur in postulated accidents unless the reactor were immersed in water.

Because disassembly would prevent a nuclear excursion in impact accidents, tests were conducted to investigate mechanisms to facilitate disassembly yet maintain the strength required for operation. Cylindrical vessels filled with steel bars and water were dropped 100 ft onto concrete. Various vessel wall thicknesses and special machined stress concentration zones were tried. Disassembly was accomplished with 3 equally spaced, longitudinal 90-degree V-grooves, 0.016 in. deep, in a 0.032-in. wall.

A second series of tests was made with grooves 0.005 to 0.013 in. deep, and having various groove root radii, in a 0.032-in. wall. No destructive failures occurred. In a third series, peripheral element and external end ring modifications were tried. Some improvement was found, especially with the latter. However, disassembly of Type 316 stainless steel without major modifications was found to be difficult. Use of a less ductile material was suggested.

In order to further study the effect of impact on the SNAP 10A reactor, two series of impact tests of its component materials were performed. Specimens were shot from a gas gun onto a granite block. The first series was conducted on solid cylinders of stainless steel, lithium hydride, tungsten, tantalum-10 tungsten, and normal zirconium hydride fuel material. Orbit reentry velocities were used. The results were used to check assumptions made in the analytical model developed to predict the reactor post-impact configuration. The experiments were designed to measure ductility, the ratio of dynamic-to-normal energy absorption per pound (K), and size-dependent effects.

The first series showed stainless steel to deform plastically and to be amenable to scale-model testing. Tungsten, LiH, and fuel material failed brittly. Tantalum-10 tungsten deformed plastically and was suggested for use as the radiation shield material instead of tungsten. Velocity-dependent K values were measured for plastically deforming materials.

The second series of tests was conducted on stainless steel cylindrical shells, LiH, stainless steel-LiH composites, and Inconel 800 cylinders. Models with different scales were used to test modeling laws. Stainless steel tests verified that scale-model testing is feasible for predicting post-impact

deformation. LiH tests showed that modeling laws do apply to such materials and that LiH absorbs considerable energy on impact. Post-impact deformations and the mode of failure of composite samples scaled very closely. Inconel 800 deformed plastically. It was indicated to absorb more energy before rupture than stainless steel.

6. End-of-Life Shutdown

In many aerospace reactor missions, a reliable shutdown device was required to reduce the potential radiological hazards from a reentering reactor. From early studies it was concluded that:

- 1) From potential radiological consequences of reactor operation, orbital shutdown was always desirable and, in some cases, necessary for safety in reentering missions in which controlled reentry or complete burnup and dispersal in the upper atmosphere is not achieved.
- 2) Shutdown was not a natural consequence of reactor operation or space environments, so that a highly reliable shutdown mechanism was needed.
- 3) The incorporation in the reactor of radioactive tritium, which decays with a half life of 12.26 yr to helium-3, which is a strong neutron absorber, provided an inherently reliable mechanism for shutdown of the SNAP aerospace reactors.
- 4) In orbit lifetimes greater than ~ 5 yr, the tritium shutdown mechanism was practically as effective in reducing radiological hazards from reentering SNAP reactors as prompt shutdown at the end of a 1-yr mission.
- 5) The tritium-helium shutdown mechanism would essentially eliminate radiological hazards from reactor missions in orbits of greater than 400 yr.
- 6) If tritiding were feasible, the tritium could be incorporated in the present SNAP reactors with a minimum of design modifications or reactivity penalties.
- 7) Reactivity penalties of the tritium system would not significantly affect reactor performance for most missions.

- 8) Incorporation of tritium in a reactor would not significantly increase the radiological hazards of reactor handling after fabrication.
- 9) From preliminary development considerations, incorporating tritium as a tritide appeared feasible, but experimental information was needed on the tritiding process to demonstrate the technique.

Later detailed studies were made of preorbital radiological safety and the feasibility of incorporating tritium into SNAP fuel. The radiological safety study concluded that hazards are not materially increased by the addition of tritium to a SNAP reactor.

The fuel incorporation studies included:

- 1) The effect of additions of tritium on the disassociation pressure of SNAP fuel.
- 2) The amount of fuel swelling that would occur due to the presence of He^3 in the fuel.
- 3) The permeation rates of hydrogen and tritium through a SNAP fuel-cladding assembly.
- 4) The redistribution rate of tritium with SNAP fuel as a function of temperature.
- 5) The rate of He^3 evolution from SNAP fuel as a function of temperatures.
- 6) The retention of tritium in the cladding.
- 7) A practical process to incorporate tritium into SNAP fuel elements.

Dissociation pressure and annealing experiments showed that expected tritium additions or resulting He^3 would not appreciably affect the total dissociation pressure or fuel swelling and growth.

Knowledge of factors 3 to 5 was needed to know the distribution and the amount of nuclear poisons, in order to determine the level and distribution of power in the reactor. The retention of tritium in the cladding was needed to determine whether excessive helium embrittlement of the cladding would occur. Experimental studies showed that:

- 1) The loss of helium-3 was easily tolerable (less than 5% in 5 yr at operating temperatures).
- 2) Tritium distributed sufficiently rapidly to maintain desired power distributions (uniform distribution in less than 72 hr at 1300° F).
- 3) The loss of tritium from fuel elements was negligible for proposed loadings (cladding permeation rate less than 10% that of hydrogen).
- 4) No discernible effects on the mechanical properties of the cladding would occur due to the decay of dissolved tritium.

The feasibility of a process for incorporating tritium into SNAP fuel was also demonstrated experimentally. Thus, based on nonirradiated fuel measurements, the feasibility and practicality of incorporating a tritium-helium shut-down mechanism into SNAP fuel was established.

7. Disposal Mode Studies

Methods for safely disposing of SNAP reactors after space operation were evaluated. Three principles were employed: decay, dilution, and recovery. Three basic methods had evolved for implementing the disposal principles: (1) boost to a long-lived orbit, (2) destruct, and (3) controlled reentry. The value of each of the methods depended on the characteristics of the reactor.

Boost to a sufficiently long-lived orbit would allow fission products to decay until no reentry hazard would exist. If a reliable end-of-life shutdown device were used, orbit lifetimes as short as 50 yr could be adequate. For shorter-lived orbits, reentry radiation hazards are functions of the reactor power history, shutdown mechanism, and orbit lifetime.

The effect of solar flux on orbital decay was studied as functions of shut-down date and ballistic coefficient. The solar flux was found to have a period of about 11.4 yr during which the magnitude varied by about a factor of 4. An expression for the incremental time for a vehicle to sink from a given altitude to another was derived as a function of solar flux density. A body tended to orbit at a nearly constant altitude during periods of low solar activity, but to decay substantially as activity increased. Reentry usually occurred following periods of high solar activity. For a NASA space station, the reentry time varied from 1.2 to 8.0 yr, depending on what year orbit stabilization was discontinued.

The hazards associated with the radiation field produced by large particles from the ablation of a SNAP 10A core reentering the atmosphere were evaluated. Fallout calculations were made, using conservative assumptions. It was shown that the equivalent residual dose limit would not be exceeded, unless the activity concentration was at least one core per square mile. It was concluded that reentry burnup would virtually eliminate SNAP 10A radiological hazards.

An analysis of the factors affecting particle deposit concentration at the Earth's surface, which result from the injection in the high atmosphere of radioactive particulate, was made. The need for experimental measurements of particle cloud growth was outlined. Techniques for utilizing gun-launched probes to emplace selected particles at appropriate altitudes and observe the fallout were described. An experimental procedure for conducting atmospheric fallout tests was outlined and the requirements for data collection and analysis were developed. Available facilities were identified.

Reentry burnup of SNAP reactors may produce low levels of worldwide fallout. The International Commission on Radiological Protection (ICRP) had recommended a population dose limit of 2 rem in 30 yr. The factors affecting the choice of a population dose criterion were reviewed. These factors included radiological safety history and tradition, radiobiological knowledge, other radiation criteria, and the biological significance of criteria doses. No safety justification was found for reduction of the ICRP limit. This 2-rem in 30-yr dose was well within the range of variation of natural background radiation. Population safety requirements were satisfied if the dose was less than the ICRP criterion. Many possible sources of artificial, nonmedical radiation could contribute to the population dose. For planning purposes, it was recommended that one-eighth the population dose, 0.25 rem/30 hr, be taken as the allowable dose from SNAP reactor applications.

A study was directed toward the disposal problems associated with orbits of short duration (< 50 yr). Combinations of disposal methods and system failures were considered. Eight combinations were examined in detail to determine the probability of overexposure. The estimated overexposures for these combinations were as follows:

Disposal Method	Average Number of Overexposures per Mission
1) Orbital boost to parking orbit with reentry burnup	0.0036
2) Orbital boost to parking orbit with immediate destruct and reentry burnup	0.001
3) Controlled reentry with intact impact in deep ocean	0.36
4) Controlled reentry with reentry disassembly and burnup and ocean impact	0.0046
5) Controlled reentry with destruct and reentry burnup and ocean impact	0.001
6) Controlled reentry with intact impact on land; recovery and storage	0.26
7) In-orbit destruct (random reentry) with reentry burnup	0.14
8) Random reentry with reentry burnup	0.36

The study showed that reentry burnup, as a backup to orbital boost or controlled reentry, may provide a two-order-of-magnitude improvement over all other disposal methods. Development of an effective destruct device was recommended.

C. PROGRAM EVALUATION

The Aerospace Safety Program provided substantial support for the assurance of radiological safety in the use of SNAP systems. The results provided a firm basis for the evaluation of the probability of potential radiological exposure for particular SNAP missions.

The mechanical and thermochemical incidents experimental program provided assurance of safety in handling and prelaunch activities which did not result in immersion of the reactor in water. Use of a shipping sleeve, designed and tested in this program, provided safety under combined water reflection and flooding of the reactor core during prelaunch handling. Techniques were developed and successfully used for all safety problems associated with the fabrication, testing, transportation, handling, and launch of one nuclear-powered SNAP system.

The mechanical and thermochemical incidents experimental program also provided assurance of safety upon intact impact of a reactor reentering the atmosphere at terminal velocities which did not result in water immersion. Experiments to develop assurance of water immersion subcriticality through dispersal of fuel elements on impact did not find a solution short of major design modifications; design recommendations were made. Single-channel boiling experiments showed that water accumulation in an impact basin or crater provided a mechanism for steady-state operation, for which the radiation exposure to approaching humans had to be accounted. Experiments which more closely mock up the reactor were needed to better define this mechanism. The likelihood of such operation, however, was found to be extremely small.

Experimental and analytical searches for intrinsic water subcriticality methods identified potential combinations of rare earth isotopes which would: (1) provide the needed negative reactivity on immersion, (2) allow normal power operation, and (3) affect the temperature defects and coefficients in a tolerable manner. Establishment of the feasibility of an IWS system for any particular mission would require additional experiments.

Confident prediction of the maximum radiological hazard from a nuclear excursion of a SNAP 10A or SNAP 8 type system on Earth was provided by results of the series of transient effects experiments and fission product release experiments.

Disposal of an orbiting reactor with an accumulation of fission products was studied in terms of decay, dispersal, and recovery. For longer-lived orbits, the potential of guaranteeing decay through an intrinsic shutdown mechanism was developed. The feasibility of this tritium-helium system was experimentally demonstrated. Its preorbital safety was established. The desirability, feasibility, or practicality of this end-of-life shutdown device for shorter-lived orbits was not demonstrated. Boost to longer-lived orbits was assumed possible, but no feasibility studies were made in this program.

The altitudes necessary for fuel element release to result in adequate burnup and dispersal were established. Considerable experimental support for core vessel ablation and fuel element ablation determinations was provided. The elimination of significant hazards by adequate dispersal was determined

analytically. Better definition of the requirements for dispersal could be obtained from large particle cloud dispersal experiments and further transition flow region heating and particle ablation measurements. Adequate dispersal was predicted for the SNAP 10A reactor system, but was negatively indicated for the SNAP 8 reactor. An investigation of the feasibility of assisting dispersal with a chemical explosive was begun and further tests were planned.

For low-orbit missions, the possibility of assuring intact recovery of the core by using a LiH shield was established experimentally. Targeting reentry by deboost was assumed to be possible, but its feasibility was not studied in this program.

A procedure for evaluating the overall radiological overexposure hazard for a mission was developed. Probabilities based on the reliabilities of disposal systems were determined. The hazardous consequences of each event of each disposal mode were found for the possible modes of exposure. Radiological exposure limits were examined and found to be adequate and appropriate to SNAP systems.

Results of the analytical and experimental results of the Aerospace Safety Program were applied to specific systems. One such example is the NASA Space Station study.^{(1)*} The program provided guidance for design and disposal procedures such that the expected number of overexposures per SNAP mission is much less than one.

The Aerospace Safety Program was guided by uncertainty analyses of the overall mission hazards for the various independent parameters. Thus, the results are most applicable to the SNAP 10A and SNAP 8 systems. However, many of the results of the program have wide applicability. These include the analytical models developed in the areas of reentry trajectories and stability, heat transfer and fluid flow, and flow-dependent chemical reactions. Also, much data on the thermophysical, thermochemical and mechanical properties of fuel, structure, and shield materials have general applicability.

*Superscripts identify bibliographic references listed in the References section at the end of this report.

III. EXPERIMENTS AND ANALYSIS

A. REACTOR DISINTEGRATION

Safe disposal of a reentering reactor is accomplished if the reactor structure disintegrates and releases the fuel at a sufficiently high altitude for it to melt and disperse. A project of thermal analysis of SNAP designs supported by experiments was conducted to determine if this disposal mode was operative.

1. Preliminary Theoretical Analyses⁽²⁾

Preliminary analytic studies were made to describe the general characteristics of a SNAP reactor reentering the atmosphere and to help define and design experiments for improvement of the characterization and for substantiation of analytical techniques. The system considered was primarily the SNAP 10A-Agena D reactor and vehicle.

Studies were made of the aerodynamic and inertial characteristics of the system, the reentering satellite trajectory, reentry attitude and oscillation, stagnation and local aerodynamic heat rates, flow regimes, and failure modes. The RESTORE code was written to calculate trajectories.

Transient temperatures in various components of the system were calculated with the aid of a digital computer code, TAP-2. The physical system is assumed to be divided into several discrete sections, called nodes, and the thermal capacity of each node is calculated from the volume and the specific heat capacity of the material. Thermal resistances between each pair of connected nodes are calculated from the heat flow path length, cross-sectional area, and thermal conductivity. Provisions are available for inclusion of radiant heat exchange between surfaces, heat conduction and convection, internal heat generation, and heats of phase transformation. An important feature of the code is that any parameter of the system may be varied as a function of time or of any other parameter according to tabular inputs. This makes it possible to disconnect nodes which have exceeded their melting temperatures and to connect the sources of aerodynamic heat to other nodes as the ablation of the material takes place. A typical thermal network is shown in Figure 2.

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Figure 2. Thermal Network for Core Vessel Lip Heating

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A typical calculated aerodynamic heating rate profile during reentry is shown in Figure 3. Local heating rate data from early wind tunnel tests were used in predicting the ablation rate and time and mode of failure of the various system components.

2. Initial Wind Tunnel Tests⁽³⁾

The necessity for predicting the disintegration of SNAP units along their reentry trajectories made it mandatory to establish the magnitude of the hypersonic heat transfer rates. This task could not have been done by theoretical work alone, since the available hypersonic boundary layer theory was limited mainly to relatively clean aerodynamic shapes. Wind tunnel tests were used to derive comparative laminar, hypersonic boundary layer heat transfer data found on the irregularly shaped envelope of the SNAP 10A reactor. The data were accumulated at various angles of attack and simulated altitudes. The results of the experiments were used in predicting the disintegration and ablation of SNAP-type reactors during a reentry trajectory.

Because of the relatively high cost and the instrumentation problems involved in the remote measurements during an actual reentry, it was highly desirable to initially accumulate data from tunnels that simulate the desired flight conditions. The aerospace safety wind tunnel test program was designed to accomplish the following objectives:

- 1) To gather data for the heat transfer rates from laminar, hypersonic boundary layers on the complex aerodynamic shapes of the SNAP 10A reactor at various flight conditions.
- 2) To provide aerodynamic heat transfer input data for calculating the temperature distribution on the reactor components during reentry.
- 3) To evaluate possible restrictions on the quality of the tunnel data.

The Rhodes and Bloxson facility was selected for the program because of its unique capability for determining aerodynamic heat transfer rates on the surface of complex shapes. Heat-sensitive paint color distribution was the technique used by the facility to provide the required information. This hotshot-type tunnel produced a simulated reentry environment for periods of 1 to 10 msec. Scale models of the SNAP 10A reactor were coated with the thermopaint for

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Figure 3. Aerodynamic Heating During Reentry of SNAP 10A Vehicle from a Polar Orbit

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testing at simulated flight environments with relative surface heat transfer rates being indicated by the discoloration of the paint. One/fiftieth-scale models of the SNAP 10A-Agena Vehicle and full-scale and 1/4-scale models of components were tested.

The heat transfer data from the tunnel were presented to Atomics International in the form of color number contours for several components of the reactor. These were drawn on a surface development of the geometry of each component. A typical drawing is shown in Figure 4.

3. Reactor Flight Demonstration⁽⁴⁾

A high-altitude flight test of a full-size, nonradioactive model of the SNAP 10A reactor was conducted in May 1963 to investigate aerodynamic heating effects and to demonstrate that space reactors can be designed to break apart and disintegrate when they reenter the Earth's atmosphere.

This flight test, called Reentry Flight Demonstration No. 1 (RFD-1), was launched by the National Aeronautics and Space Administration (NASA) from its station at Wallops Island, Virginia. The test was under the direction of the Sandia Corporation. The NASA Scout booster vehicle was launched at 0438 GMT on May 22, 1963, and carried the payload (Figure 5) consisting of the reactor model and a reentry vehicle which contained telemetry and recovery equipment.

The flight path was suborbital, with an apogee of approximately 490,000 ft and a range of approximately 800 n. mi. The impact point was in the ocean about 250 n. mi. southeast of Bermuda. Much valuable data was obtained by telemetry during the descent.

Figure 6 summarizes the events which occurred during the flight. Reception of the telemetered data was good until about 350 sec after liftoff, at which time the ionized gas layer which normally surrounds reentry bodies caused loss of the radio signals. The data that were received prior to radio-frequency (RF) blackout indicated significant temperature rises at the instrumented locations and separation of the reflectors as predicted.

The instrumented test model was a full-scale replica of the SNAP 10A reactor, but without fuel inside the core vessel. An actual reactor vessel was equipped with a dummy thermoelectric pump and coolant pipes. The pump

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Figure 4. Top of Fin and Pump Magnet

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Figure 5. RFD-1 Payload

magnet was simulated with a steel box, around which were mounted aluminum cooling fins as on the actual pump. The coolant pipe which extends forward from the top head of the reactor and branches into two downcomer lines was simulated in the same material (stainless steel) and wall thickness as the actual article. No coolant (NaK) was used in the system.

Demonstration of the release of the reflectors which surround the core vessel was an essential part of this test. The actual beryllium reflectors were simulated in the test model by hollow aluminum shells, hinged near the base of the vessel and held in place by a steel band of the actual design current at the time of construction of the model.

The inside of the core vessel contained an upper and a lower grid plate held apart by six stainless steel struts. The top head was held in place by a seam weld around its periphery. A lip was provided at the location of the weld, and it was intended that the normal mode of opening the vessel during reentry would be by the melting of this lip.

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Figure 6. RFD-1 Trajectory

The test article was attached to the nose of a reentry vehicle as shown in Figure 5. This vehicle was designed and built by the Sandia Corporation. The vehicle contained telemetry equipment, gyroscopes for attitude sensing, and flotation gear to aid recovery after impact.

Twenty-one thermocouples and eight microswitches were installed on the reactor to provide data on temperatures of critical components and on events associated with the release of the reflectors and the top head. The locations of most of the thermocouples are shown in Figure 7.

The trajectory simulated an orbital reentry as well as possibly using the Scout booster. The velocity at 400,000 ft was 20,000 ft/sec compared to 25,600 for orbital reentry. The flight path angle with respect to the local horizontal was -5.6 deg at 400,000 ft compared to about -0.025 deg for a satellite decay path.

Heating rates were calculated from the thermocouple data for the various components. Comparison of these rates to equilibrium flow theory indicated that the theory was not well followed. An intensive study of aerodynamic heating in nonequilibrium flow and in the transition regimes between free-molecule and equilibrium flow was initiated because of the results of this test.

4. Improved Analysis for SNAP 10A-Agena⁽⁵⁾

The analysis of the disintegration of the SNAP 10A-Agena during reentry was improved through the use of more refined data and methods for calculating aerodynamic heating rates and their effects on structural components of the reactor and vehicle.

Inertial characteristics of the SNAP 10A-Agena vehicle were calculated by Lockheed Missiles and Space Company under a subcontract. Values for axial and radial force and pitching moment coefficients are shown in Figures 8 through 14. Data for hypersonic continuum flow were obtained by Ling-Tempco-Vought in their hypervelocity wind tunnel under another subcontract. Results are shown in Figures 11 through 14. The data were used in calculating a six-degree-of-freedom trajectory. The angle of attack was established as 32 deg.

An analog computer model of the coolant flow system was devised and used to study the NaK flow and its effects on the heating of the system during

THERMOCOUPLE LOCATIONS

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Figure 7. RFD-1 Reactor Thermocouple Locations

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Figure 8. Axial Force Coefficients for Free-Molecule Flow

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Figure 9. Normal Force Coefficients for Free-Molecule Flow

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Figure 10. Pitching Moment Coefficients for Free-Molecule Flow

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Figure 11. Axial Force Coefficients for Continuum Flow

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Figure 12. Normal Force Coefficients for Continuum Flow

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Figure 13. Pitching Moment Coefficients for Continuum Flow

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Figure 14. Pitching Moment Coefficient for Various Amounts of Residual Rocket Fuel

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reentry. Heating rates were input for the vehicle at 30-deg angle of attack, with a rapid rate of spin about its major axis. Cases without NaK and with NaK, either with or without an operable pump, were studied. Results are given in Table 1.

TABLE 1
RESULTS OF ANALOG STUDY OF COOLANT PIPE FAILURE

Calculation		$\alpha = 30$ deg No Spin			$\alpha = 30$ deg With Spin		
		No NaK	NaK not flowing	NaK flowing	No NaK	NaK not flowing	NaK flowing
NaK flowrate (max)	(lb/sec)	-	0	0.76	-	0	0.76
Altitude of flow stopping	(kiloft)	-	-	310	-	-	310
NaK temperature at stopping	(° F)	-	-	940	-	-	940
Altitudes of pipe node failure	(kiloft)	293	289	294	294	279	278
Altitudes of support leg failure	(kiloft)	263	263	264	195	195	195

The Thermal Analyzer Program (TAP) was extensively revised to enable it to find the heat input which would yield measured system responses, such as in the RFD-1 flight test, in addition to the reverse problem.

Aerodynamic heating rates obtained from the measured temperature histories of the instrumented components of RFD-1 were applied to the case of a SNAP 10A reactor in an orbital decay trajectory, with basic corrections for differences in velocity and time of descent through various altitudes. The exact altitude of reflector ejection was shown to have little influence on the altitude of vessel melting provided the NaK is released soon enough.

A detailed thermal model for the TAP code was devised to calculate the altitude of separation of the reactor from the system. Local heating rates were based on the results of the wind tunnel tests.

A similar analysis was made for the disintegration of the reactor vessel. The thermal model is shown in Figure 15. The altitude of disintegration varied less than 10,000 ft for different combinations of reflector and NaK release.

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Figure 15. Thermal Model of Section Through SNAP 10A Reactor Core

EFFECT OF EMISSIVITY

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Figure 16. Effect of Emissivity - SNAP 10A Reactor Reentry Disassembly

Parameter studies showed that the thermal emissivity of the outer surface of the vessel was important in determining the vessel wall melting attitude. The relationship is given in Figure 16.

Thermal analysis indicated that the fuel elements would be released by melting of the vessel side walls rather than melting of the lip around the top head.

The calculated minimum altitudes for each of the important events in the reentry disintegration of a SNAP 10A-Agena were determined to be as follows:

<u>Event</u>	<u>Altitude (kiloft)</u>
1) Pump fins melt off	310
2) NaK flow stops	310
3) Converter fins melt off	290
4) Reflectors eject	280
5) Agenda oxidizer tank fails	287
6) Coolant system ruptures	275
7) Reactor support legs fail	262
8) Reactor vessel melts open	237

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Based on new data and on the improved analysis, the following conclusions were drawn:

- 1) The Agena vehicle, during reentry, will be oscillating about an approximately nose-first attitude, with the amplitude of the oscillations tending toward smaller values as the vehicle descends. Tail-first reentry is impossible because the vehicle is aerodynamically unstable in a tail-first attitude.
- 2) The external components of the SNAP 10A system will begin to disintegrate by melting at altitudes above 300,000 ft. Portions of the vehicle will fail at altitudes above 280,000 ft, and the reactor will separate from the vehicle at an altitude above 260,000 ft.
- 3) The reactor vessel walls will melt away, releasing the partially melted remains of the top and bottom heads and allowing the core assembly to fall free at an altitude of not less than 237,000 ft.
- 4) The foregoing statements are based on the analysis of a system that is filled with NaK-78 (sodium-potassium eutectic alloy) at the time of reentry. If the NaK escapes from the system prior to reentry, the coolant pipes will melt sooner than if they were filled with NaK, but the altitude of fuel-element release will not be affected appreciably.

5. Emissivity and Self-Welding Experiments

a. Self-Welding

Final shutdown of the SNAP reactor requires that the external beryllium reflector be ejected away from the core vessel. This must occur prior to reentry into the Earth's atmosphere. Should the reflector not eject, it could act as an ablative shield to the core preventing disintegration of the nuclear fuel rods. Although the reflector assembly is designed to be separated from the core vessel and reactor assembly, the possibility exists whereby, due to thermal growth, the reflector assembly could be in contact with the core vessel during full-power operation of the reactor. This condition, in the combined environments of space vacuum, high temperature, and radiation, presents a potential reflector-core vessel adhesion or "self-welding" hazard.

A special test program was conducted to determine the adhesion that could occur between Type 316 stainless steel and beryllium when subjected to the "worse case" environmental conditions. Evaluations were also made of the effect of coating the contacting surfaces with a thin film of colloidal graphite to prevent adhesion.

Eight tests between Type 316 stainless steel and beryllium were conducted in the final study. Four of the tests were run for 10,000 hr and four for 5000 hr. In the groups of four, two each were coated with the colloidal graphite and two were bare metal. Each test combination was tested under vacuums of 10^{-7} to 10^{-9} torr with the samples at 1000°F and 100 psi continuous load on the contact surface.

The data indicate that in all cases, where the contacting surfaces had been coated, adhesion was very small or immeasurable. The uncoated, bare surfaces, were grossly adhered to each other from contact welding; and, under subsequent separation, large quantities of beryllium were pulled out and transferred to the stainless steel. These data verified the preliminary adhesion test measurements made for shorter time periods.

Extensive screening programs were conducted on over 200 material combinations while they were in the high-temperature, ultra-high-vacuum environment, to establish what combinations have the least adhesion when they are in contact under load for long periods of time. Results of these studies indicate that adhesion will not occur, or will be minimized, if dry film lubricants such as MoS_2 plus graphite, graphite foil or cloth, or solid, mechanical grade, carbon-graphite materials are used as one of the contact surfaces, or between metal contact surfaces. On the other hand, if bare metals are allowed in contact, a five-fold increase in adhesion forces can be expected.

The tests shown in Figure 17 were conducted on 120 material pairs in oil-free high-vacuum chambers at 950°F. The samples were loaded together with a force of 15 lb projected over a 0.25-in.² contact area. The contacting surfaces were not separated for 1000 hr while in the ultra-high-vacuum, high-temperature environment.

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Figure 17. Relative Adhesion of Material Groups for High-Vacuum Static Adhesion Studies

The beryllium and Type 316 stainless steel specimens are simple geometries designed to represent two flat surfaces in contact. The base is a square block with 1-in. sides and 3/8 in. thick. Tests for adhesion are made by carefully removing the extra load and reverse loading.

In the 10,000-hr tests, the adhesion force was about 1.5 lb for the uncoated tests and < 0.1 lb for the coated tests. A diffusion zone was observed in both coated and uncoated tests, although the temperature was below that where diffusion is expected. Practically all the diffusion and material transfer occurred in the first 5000 hr.

The graphite coating effectively prevented the adhesion that occurs on the bare surfaces in a SNAP orbital environment. Improved bonding of graphite would be needed for flight reactor systems.

b. Emissivity

Emittance tests on the core vessel material were performed to provide data for the calculation of the ablation of the core vessel on reentry. Since the emittance properties of a surface are dependent on surface finish, oxide film thickness, and temperature, extensive effort was made to simulate actual reentry conditions.

An initial phase of tests was used to define and refine procedures and techniques for a final phase of measurements. Variations in surface roughness were studied.

In the final test, samples were punched from an actual core vessel. The surface finish of the SNAP 10 flight system vessel was duplicated. The samples were Type 321 stainless steel, 1 in. in diameter by 0.032 in. thick, with a surface roughness of 25 to 30 A. A. (arithmetic average).

Samples were oxidized by simulating the reactor flight reentry pressure and temperature conditions for both RFD-1 and orbital trajectories. Oxidation runs were stopped at 1200, 1500, 1800, or 2000° F for emittance measurements at the corresponding temperatures. The test apparatus is pictured in Figure 18.

Figure 18. Detail of Reentry Simulation System

The total normal emittance was measured relative to black body emittance with a radiometer at the B1 Division of Rockwell International. Results are shown in Figure 19. The emittance appears to be a complex function of pressure, time, and temperature, which would require a study to evaluate the kinetics of oxidation during reentry.

From this study, it was concluded that the emittance of a core vessel surface near the melting point under orbital reentry conditions is approximately 0.8. For the RFD-1 trajectory, the emittance is 0.7. These values were considerably higher than was desired for rapid core vessel ablation.

6. Reactor Ablation Disintegration Experiment⁽⁶⁾

The Reactor Ablation Disintegration Experiment (RADE) was conducted to measure the aerodynamic heat flux to the complicated geometry of SNAP reactors and to demonstrate the capacity and accuracy of the analytical techniques used in the reactor disintegration analysis. The RADE test was performed in the hyperthermal wind tunnel of the Gas-dynamics Branch Facility at NASA Ames Research Center.

Figure 19. Emittance Values for Stainless Steel

Half-scale models of the SNAP 10A (RADE-A) and SNAP 8 (RADE-B) nuclear reactor configurations were tested in the NASA Ames hyperthermal wind tunnel under simulated atmospheric reentry conditions. Surface heat flux measurements were made with calorimeter models employing asymptotic calorimeter heat sensors. The experimental RADE models were ablated at both 0 and 30-deg angles of attack in the flow stream. The temperature response of the models to the aerodynamic heating of the jet stream was obtained throughout each test run from thermocouples strategically mounted in the models.

Throughout each ablation test, motion picture film coverage (24 frames per second) and high-speed motion picture film coverage (128 frames per second) were obtained which provided a detailed visual description of the sequence of events during reactor ablation disintegration. These events clearly defined the meltdown and mode of disintegration of the reactor components under simulated reentry conditions.

The RADE A model major components are shown in Figure 20 and the assembled model is shown in Figure 21. The RADE B model is shown in Figure 22. Typical post-test conditions are shown in Figure 23. A typical event sequence during a test run is shown pictorially in Figure 24.

Post-test analysis showed that considerable welding between the upper grid plate and the fuel element bundle and between fuel elements probably would occur upon reentry.

A standard value of reference heat flux was established at the stagnation point by comparison of measured and theoretical local-to-stagnation point heating ratios. These were in excellent agreement as shown in Figure 25. These data were used as initial condition input to analog models developed for the thermal analyzer program (TAP).

The analytical results were correlated to the measured temperature-vs-time traces. A thermal analyzer program computer code (TAP-3) analog was developed to represent a 180-deg portion of each RADE experimental model configuration for both the head-on (0 deg) and 30-deg angle-of-attack test positions. The analog of the RADE-A experimental model consisted of the external NaK pipe network, magnets, thermoelectric pump, cooling fins, top head, upper grid plate, and reactor core assembly. The TAP analog of the

Figure 20. Disassembled RADE-A Model Showing Major Components

Figure 21. RADE-A Ablation Model

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Figure 22. RADE-B Ablation Model

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Figure 23. RADE-A Model After Test Run at 30-degree Angle of Attack
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t=0 sec

t=1.38

t=3.29

t=4.50

t=5.50

t=27.33

t=29.17

t=50.0

t=65.17

t=70.79

t=88.38

t=111.25

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Figure 24. Event Sequence for RADE-A Ablation Test Run at 0-degree
Angle of Attack
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BLANK

Figure 25. Heat Transfer Distribution Around Hemispherical (Dome) Heat Calorimeter

RADE-B experimental model included the NaK outlet pipe, top head, upper grid plate, and reactor core assembly. These analogs were used in correlating the RADE ablation data with the analytical methods and procedures. A typical analog network is shown in Figure 26. A typical correlation is shown in Figure 27. The good agreement demonstrated that the analytic capability of accurately predicting the preablation temperature response of SNAP reactors reentering the Earth's atmosphere had been achieved.

7. Aerodynamic Heating Experiment⁽⁷⁾

Reentry aerodynamic heating distributions for the SNAP 8 configuration were obtained based on hypersonic shock tunnel experiments conducted in the Cornell Aeronautical Laboratory (CAL) 48-in. hypersonic shock tunnel. The heat transfer correlations were determined as a function of angle of attack from 0 to 70 deg. The local aerodynamic heating factors for the 0, 30, and 70-deg angle-of-attack cases obtained in this experiment formed the basis of the heating distributions used in analytical calculations in which the shadow shield was attached to the reactor.

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Figure 26. RADE-A External NaK Pipe Analytical Network

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Figure 27. RADE-B Temperature vs Time Traces (45 deg from Top of NaK Outlet Pipe)

The aeroheating model was one-fifth scale; 44 heat-transfer gages were installed on it. The test model with thin-film thermometers in place is shown in Figure 28.

Test conditions were set to a simulated altitude of 250,000 ft and a free-stream Mach number of 18.4 in a hypersonic, continuum flow regime. The gages sensed the variation of temperature after the 4-msec test interval. Theoretical aerodynamic heat transfer rates were substantiated (see Figure 25) and a baseline for the test series was established in a 0-deg angle-of-attack measurement with a hemispherical reactor vessel head.

Typical heat-transfer distribution data from the SNAP 8 configuration runs are shown in Figure 29. Runs with different angles of attack were made to permit consideration of dynamic flight conditions. The heat-transfer rates as functions of the angle of attack are shown in Figures 30, 31, and 32. A typical shock wave in the flow field is shown in the Schlieren photograph of Figure 33. In general all heat-transfer trends were substantiated by theory.

Figure 28. Aeroheating Test Model Installed in Tunnel (Angle of Attack = 30 deg)

Figure 29. Aerodynamic Heat-Transfer Distributions on SNAP 8 Configuration at 30-deg Angle of Attack

Figure 30. Aerodynamic Heat-Transfer Distributions on SNAP 8 Configuration Piping

Figure 31. Aerodynamic Heat-Transfer Distributions on SNAP 8 Configuration Upper Vessel and Head

Figure 32. Aerodynamic Heat-Transfer Distributions on SNAP 8 Configuration Vessel

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Figure 33. Schlieren Photograph of Typical Flow Field Over Aero-heating Model (Free-Stream Flow from Left to Right)

Heat-transfer correlations were derived from the data. These provided a relatively complete heat-transfer distribution over the SNAP 8 configuration for 0 to 70-deg angles of attack.

8. SNAP 8 Analysis⁽⁸⁾

An analysis was performed to simulate the thermal behavior of a SNAP 8 type reactor system during reentry into the Earth's atmosphere. The objective of this analysis was to determine whether sufficient ablation will occur to release the reactor core at an altitude that will ensure complete burnup of the fuel elements in the upper atmosphere.

The general sequence of events which the system must undergo is: (1) the melting off or removal of the upper head portion of the reactor system, (2) breakup of the reactor core by melting open the vessel wall or melting off the grid plate, (3) ablation and breakup of the fuel elements, (4) burnup of fuel elements, and (5) dispersal of the remaining fine particles into the upper atmosphere. The successful completion of this sequence of events is dependent upon reactor core release from the reentering system. Of particular interest is the aerodynamic heating generated by the reentering body and its thermal effect upon the reactor top head and vessel wall.

Analysis was made for the heating and subsequent removal of the reactor top head and vessel wall during two modes of reentry. In the first mode, the reactor system was assumed to be fastened to the neutron shadow shield assembly, and to be spinning about its axis. Three cases were investigated for this mode: 0, 30, and 70-deg angles of attack. Under the second mode of reentry, the reactor was assumed to be released from the shield at an altitude of 400,000 ft, followed by random tumbling into the Earth's atmosphere.

The RESTORE (Reentering SNAP Trajectory on an Oblate Rotating Earth) computer program was used to calculate the trajectory of the reentering system. The drag coefficient acting on a body of the configuration, as shown in Figure 34, was determined by use of the NEWTON code, which evaluates the force and moment coefficients on axisymmetric bodies in hypersonic flow based on Newtonian flow theory. The results of the NEWTON code calculations are shown in Figure 35. They justify using the same reentry trajectory for all angles of attack between head-on ($\alpha=0$ deg) and side-on ($\alpha=90$ deg) orientation. A

Figure 34. Assumed Reentry Geometry of Reactor-Shield Assembly

Figure 35. Drag Coefficient vs Angle of Attack for Reference Geometry of Reentry Vehicle

variable ballistic coefficient was used in the RESTORE calculation to determine the trajectory of the system during the second mode of reentry. The results of the RESTORE code calculations are shown for the reference trajectories in Figure 36.

Figure 36. Altitude and Reference Heating vs Time for Reference Trajectories

A detailed analytical model of the thermal behavior was developed for use with the TAP-3 code; 700 nodes were used. The techniques were substantiated by correlation with the results of the RADE experiments.

The aerodynamic heating calculations were made, using the Detra-Hidalgo correlation. The local heating factors were taken from the results of the Aerodynamic Heating Experiment and the Reactor Ablation Disintegration Experiment.

For cases with the shadow shield attached, melt-through of the reactor top head occurs at 226,000, 169,000, and 218,000 ft for 0, 30 and 70-deg angles of attack, respectively. For cases where the shadow shield was released at 400,000 ft, both the reference heat rate and the random tumbling motion reduce the heating at a given altitude, compared to the first mode. Thus, the ablation occurs at lower altitudes.

From the results of this analysis, it was concluded that the SNAP 8 reactor vessel would not ablate to a sufficient extent, for probable modes of reentry, to release the fuel elements at altitudes necessary to obtain complete fuel ablation.

9. Reentry with Lithium Hydride Shield

Conversely to long orbital decay missions where the disintegration of the core by reentry heating is desirable, core survival may be desirable for short orbital decay missions to avoid the scatter of fuel elements over a wide area.

a. Lithium Hydride Shield Ablation

Tests were made on the ablation of LiH to evaluate its effectiveness as a protective material for the top surface of the reactor core vessel to assure core survival. The tests were conducted in the NASA-Ames low-density, constricted-arc, supersonic jet, which is capable of simulating the heating rates that occur during orbital reentry, but at lower stagnation pressures. The heating rates were typical of the peak-heating period. Heating rates were measured by calorimeters in the size and shape of the test specimens.

The test specimens were short lengths of 2-in. -diameter cylinders supplied by AI. Some of the specimens contained a matrix of stainless steel wool, or steel honeycomb about 0.001 in. thick in about 1-in. -diameter cells placed either axially or crosswise. Some specimens had thermocouples installed in their interior. Tests were from 4 to 11 sec long. After each test, the amount of ablation was measured. The test results were correlated to the ratio of the net heat input to the rate of mass loss. This was compared to the theoretical value for a simple melting process. The results agreed with the theoretical value within 25% for all specimens. The presence of the steel matrix tended to reduce the rate of ablation.

Tests with higher impact pressures, increased oxygen contents in the driver gas, and with a shell over the LiH were recommended.

LiH ablation was calculated with the TAP code. All studies indicated that LiH would provide an effective reentry protection. For the nominal conditions of successful shutdown and tumbling reentry from a low altitude (space station) orbit, the ablation was about 2.5 in. or less. For the unlikely events of no shutdown and a stable nose-first reentry, as much as 7.4 in. ablated, leaving 1.1 in. of LiH unablated in the current design.

b. Space Station Reentry Analysis

(1) Study Objective

At the end of the useful life of the ZrH reactor power system, it is proposed that the power system be separated from the space station so that a new power system can be installed. These studies were made to predict, analytically, the probable physical condition of the reactor after it returns from an operating orbit of 270 n. mi. The aerodynamic parameters, trajectory, and thermal analysis were to be determined.

(2) Analysis of Aerodynamic Parameters

The reentry configuration of the ZrH reactor power system were identified as A, B, and C. Configuration A comprises the entire power system with the radiator attached. Configuration B comprises the shielded reactor with the power conversion equipment gallery and aft shield. Configuration C comprises the shielded reactor only. It is estimated that separation of subsystems (from Configuration A to Configuration B) will occur approximately 3200 sec after initiation of reentry and from Configuration B to Configuration C at approximately 3220 sec. This latter configuration, with a part of the shield melted away, is expected to prevail during the final minutes of the trajectory prior to impact.

To enable characterization of postoperation reentry effects on the reactor power system, coefficients affecting vehicle attitude as a function of angle of attack had to be determined. These included free-molecule flow, hypersonic flow, and supersonic and transonic flow effects on drag coefficients. Newtonian theory aided in the development of a computer program to calculate lift, drag, and pitching moment coefficients in the hypersonic flow regimes. Drag coefficients were based on that of a sphere for transonic flow regime. The ballistic coefficients, which were based on drag coefficients resulting from 0 to 180-deg attack angles, were used to predict vehicle attitude during reentry.

The power system starts reentry in a nose-first attitude. With separation of the radiator, the remaining reentry body is weakly stable, but in the tail-first position. Tumbling begins with the tendency to seek the tail-first attitude. Structural failure and separation of the aft shield leads to the shielded reactor configuration, where tumbling is expected to continue with a slight tendency to

seek the nose-first attitude. This analysis did not account for moments that are imparted on the reentry body as structural failure and separation of the radiator and aft shield occur. These effects are expected to assure a tumbling reentry for both the latter vehicle configurations.

(3) Trajectory Analysis

A point-mass trajectory calculation was made. Oscillations of the reentry body were not considered. The lift and side forces were considered to null as the body oscillated. Drag forces were based on mean values over the expected range of angles of attack. A range of ballistic parameters associated with various attitudes on the final reentry body were used to calculate reentry trajectories. Figure 37 shows the final portion of the reentry trajectory of the shielded reactor for the expected attitude of tumbling, and for the unlikely maximum velocity attitude of tumbling, and for the unlikely maximum velocity attitude of the nose-first trajectory. The tail-first attitude results in an impact velocity between the values shown on Figure 37, and the side-on attitude results in an impact velocity 30 ft/sec less than the tumbling attitude.

6531-4512

Figure 37. Final Portion of Reentry Trajectory of Shielded ZrH Reactor Configuration C

Calculations of the effects of body angle and oscillations on trajectory were made at different times along the trajectory, using various initial angular displacements and velocities. During the initial reentry when the radiator and shield are expected to fail, dynamic pressures on the system could cause increasing angular velocities. At this point, angular velocities greater than 6.1 radians/sec would be required to cause the body to tumble. At altitudes below 125 n. mi, the effects of the gravity-gradient moment will be lost, and the vehicle will return to a zero angle of attack, oscillating with an unknown amplitude. If the vehicle were at an extreme attack angle (90 deg) above this altitude, calculations indicate that the oscillation amplitude induced as the body returned to a zero attack angle would be reduced to 32 deg with a period of 2.7 sec. The angular velocity would be 1.3 radians/sec as the angle of attack passed through zero. For smaller initial angles, the angular velocities would be less. After the radiator assembly has separated from the system, the pitching moments on the shielded reactor and power conversion system indicate that the angular velocity would be greater than 0.4 radians/sec, which is sufficient to cause tumbling. When the power conversion system and the aft shield separate from the shielded reactor, calculations show that the reactor is somewhat more stable; but because of the weakness of this stability and the addition of potential torques resulting from structure failures, tumbling is expected to continue.

(4) Thermal Analysis

Using the flight trajectories for reentry, discussed in the preceding paragraphs, aerodynamic heating rates as a function of altitude were calculated for various angles of attack. Minimal heating rates occur above altitudes of 230,000 ft. Below this altitude, where heating rates maximize, component failures and ablation occur. At attack angles other than 0 deg, heating rates increase in intensity; but absorbed heat by the body is less owing to the shorter time required to descend through the atmosphere. During reentry, a representative heating factor of 0.55 was selected for a stable nose-first attitude. At an attack angle of 90 deg, the heating factor was approximated to be 0.4. If the expected tumbling is confined to one plane of rotation, the heating factor is further reduced, and a conservative 0.3 factor was adopted.

Accounting for the thermophysical properties of vehicle components, and considering trajectories and modes of flight attack (tumbling, etc.) and aerodynamic heating, conservative temperature profiles and ablation rates were determined. The thermal analyzer computer program (TAP) was used to determine component temperature profiles as a function of time. The thermal analysis also considered aerodynamic heating resulting from a no-shutdown operating condition whereby the LiH shield is initially elevated in temperature owing to reactor operation. Results of these calculations show that for normal shutdown reentry, LiH ablation does not exceed 2.5 in. for a natural-decay trajectory (tumbling) or 1.2 in. for a deboost trajectory (tumbling). Maximum ablation occurs from no-shutdown natural-decay trajectories from stable attitudes and results in 7.4 in. of LiH ablation. These calculations indicate that the reactor vessel is well protected from the aerodynamic heating during reentry.

As a result of the thermal analysis calculations, predictions of time of component failure could be made. Figure 38 summarizes these predictions. Illustrated on the figure are separation of the radiator, then aft shield, and then the tumbling reentry of the shielded reactor.

Effects resulting from oxidation upon the ablation of the shield at the rate of oxygen flux through the boundary layer and to the surface of the body were calculated. These analyses showed that oxidation heat amounting to 29% of the aerodynamic heating could be added to the aerodynamic heating during reentry. This increases the effective heating factor to 0.39. The expected ablation would increase from 2.5 to 3.3.

(5) Summary and Conclusions

Based upon the foregoing analysis, the following summary and conclusions are presented:

- 1) The ZrH reactor power system with radiator attached tends to become aligned reactor first, pointed in the direction of motion of the system, long before the period of high aerodynamic heating begins. Oscillations about the stable attitude persist, but they decrease in amplitude and increase in frequency as the density of the air increases at lower altitudes.

6531-4513

Figure 38. Expected Reentry Behavior

- 2) Separation of the radiator from the system occurs at an altitude of approximately 215,000 ft for a natural-decay trajectory and 135,000 ft in the case of deboost at an initial flight path of -3 deg.
- 3) After separation, the shielded reactor has a weak tendency to stabilize in either a nose-first or a tail-first attitude, but this tendency could be completely overcome by the angular velocity which might be imparted to it at the moment of separation. This rotation might arise from shear forces at the failure line, from aerodynamic forces on unevenly broken components, or from the jet reactions as the coolant escapes.
- 4) Separation of the aft shield and the power-conversion equipment gallery should occur simultaneously with, or soon after separation of, the radiator.
- 5) The expected amount of LiH ablation due to aerodynamic heating for the natural-decay trajectory is 2.5 in. In the unlikely event of a stable nose-first attitude through the heat pulse, the ablation would be 5 in. In the improbable event of no reactor shutdown prior to reentry, the initially high temperature of the shield decreases its heat absorption capacity. A worst case, but extremely unlikely, is represented by no shutdown and a stable nose-first attitude, in which case the ablation depth would be 7.4 in. , leaving 1.1 in. of LiH at that location. This analysis did not include the effects of the drum drive motor, which first must be burned off before ablation of the LiH can begin. This effect would reduce the amount of ablation for the nose-first condition.
- 6) The total depth of ablation of the shield for the intentionally deboosted trajectory (attack angle = -3 deg) will be about half that for the natural-decay trajectory. This is because the time of descent through the period of high heating is reduced greatly, even though the maximum intensity of the heating is somewhat increased.
- 7) The effects of oxidation on the total amount of ablation were not completely evaluated. There is a potential increase of about 29% in the

heat supply over that resulting from aerodynamic heating. Much of this heat could be carried away by the molten LiH and never contribute to the heating.

- 8) The temperature of the LiH next to the reactor vessel increases an insignificant amount during the heat pulse. The reactor vessel is further protected by Min-K insulation that surrounds the reactor.
- 9) The final configuration of the system just before impact will consist of an intact reactor surrounded by some LiH. The impact velocity is about 660 ft/sec if the system is tumbling, as expected. In the unlikely event of a stabilized nose-first attitude, its impact velocity is about 820 ft/sec.

B. FUEL ROD REENTRY BURNUP⁽⁹⁾

1. Arc-Jet Tests and Analysis

Arc-heated, hyperthermal wind tunnel (arc-jet) tests were performed to provide experimental support for formulating an analytical description of the SNAP fuel rod reentry behavior. Fuel rod meltdown calculations were made for orbital reentry.

The Rockwell International, Los Angeles Aircraft Division (LAD), wind tunnel facility was used for the tests. It produces a nominal Mach-3 gas flow with variable stagnation enthalpies and pressures. The air properties are such that the Fay and Riddell theory for equilibrium flow could be used to obtain a reference heat flux.

The test facility and procedures were carefully designed for the testing of radioactive fuel material and for the safeguarding of the classified aspects of the fuel material.

The test series was designed with essentially the five groups of following tests:

- 1) Series No. 0 tests — Specimens of stainless steel and aluminum were used to check computational methods and data with materials thermally simpler than the SNAP fuel.
- 2) Series No. 1 tests — Fuel material specimens were tested in a helium jet to separate out the effect of surface oxidation on the fuel behavior in an aerodynamic heating environment.
- 3) Series No. 2 tests — Fuel material specimens were tested in air, the runs being terminated at various time intervals to study the oxide buildup and internal state of the specimen.
- 4) Series No. 3 tests — Fuel material specimens clad with Hastelloy-N were used to study the effect of the cladding on the behavior.
- 5) Series No. 4 tests — Combined phenomena tests were made of fuel material specimens in air to study the overall behavior of the fuel in the simulated reentry environment.

Specimens, 1.25 in. in diameter and 2.0 in. long, were exposed to the jet with their axis normal to the jet. In some tests, they were rotated approximately 4 rps. Specimens, 1.25 in. in diameter and 3.0 in. long, were exposed with their axis parallel to the jet. Both flat and hemispherical end surfaces were exposed. Parallel-oriented samples contained thermocouples installed internally.

The fuel test material was nonirradiated, modified zirconium hydride-10 wt % natural uranium, hydrided to 6.34×10^{22} at./cm³ of hydrogen.

All tests were photographed with a 24-frames/sec camera, and the black body surface temperature was measured with a brightness pyrometer. Some tests were photographed with a 1000 to 1500-frames/sec camera.

General results for each test series are discussed in the following paragraphs. The test configurations are shown in Figure 39.

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Figure 39. Arc-Jet Test Configurations

a. Series No. 0 Tests

Type 304 stainless steel (SS), and aluminum samples, in Configuration II-A (flat-face axial) were exposed for 1 to 2 sec. Reference heat fluxes were obtained at three different stagnation enthalpy-pressure points for Type 304 SS. A time sequence of behavior of a typical stainless steel run is shown in Figure 40.

b. Series No. 1 Tests

Test conditions and results are given in Table 2 for the helium jet tests. Where ablation occurred, the fuel material appeared to flow in a very viscous manner from the leading edge.

c. Series No. 2 Tests

The test conditions and configurations for the preablation oxidation tests are shown in Table 3. The specimens were exposed for different predetermined time intervals. This provided information on the oxide buildup on the specimen as well as data on the internal state of the specimens at various times during a test run.

d. Series No. 3 Tests

The test conditions used and comments on each of these Hastelloy-clad specimens are tabulated in Table 4. Configurations I-A and I-B were used for this group of tests at a spectrum of heating rates which were characteristic of different ejection altitudes of the rods from the reactor vessel.

The lowest heat flux used is characteristic of the orbital reentry heating (assuming an equilibrium boundary layer) at an altitude of 280,000 to 290,000 ft. For the nonrotating specimens, the cladding was observed to melt along the stagnation line and eventually disperse. In one of two rotating test cases, the specimen was immersed in the jet for a period of over 10 min with no dispersal of the cladding. In the other run (at a slightly higher heat flux), the cladding blew off the end of the specimen, apparently due to a buildup of pressure within the cladding.

For nonrotating specimens at higher heating rates, the cladding was observed to melt along the stagnation line almost immediately. The cladding then melted back and eventually dispersed. This is shown in Figure 41.

$t = 0$

$t = 36.6$

$t = 44.5$

$t = 50$

$t = 58$

$t = 70$

$t = 75$

$t = 100$

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Figure 40. Stainless Steel Calibration Run — Arc-Jet Tests
(Run No. 0.4) ($t = \text{sec}$)

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TABLE 2
SERIES NO. 1 ARC-JET TESTS
(Helium Jet)

Run No.	Configuration	Reference Heat Flux $q_s \sqrt{r}^*$ (Btu-ft ^{1/2} /ft ² -sec)	Stagnation Enthalpy H_s (Btu/lbm)	Time in Jet Δt (min:sec)	Comments
1.1	I-B	90	18,500	6:25	No ablation was observed for runs 1.1 - 1.4
1.2	I-B	100	17,000	6.18	
1.3	II-A	106	16,800	4:08	
1.4	II-A	97	17,200	8:45	
1.5	II-B	108	16,600	9.13	Ablation at tip
1.6	II-B	126	21,300	3:28	Ablated back approxi- mately 1 in.

*Measured with water-cooled LAD calorimeter - the value reported is at center of jet

TABLE 3
SERIES NO. 2 ARC-JET TESTS
(Preablation Oxidation, Simulated Air Jet)

Run No.	Configuration	Reference Heat Flux* $q_s \sqrt{r}$ (Btu-ft ^{1/2} /ft ² -sec)	Stagnation Enthalpy H_s (Btu/lbm)	Pitot Pressure P_T (atm)	Time in Jet Δt (min:sec)	Comments
2.1	I-B	73	5000	0.135	2:04	Runs 2.1-2.12 were terminated at a pre- determined time. The time of termi- nation was deter- mined by examina- tion of previous runs. In all three cases (2.1-2.4, 2.5-2.8, 2.9-2.12), a good spectrum of oxidation was ob- tained.
2.2	I-B	70	4790	0.135	1:41	
2.3	I-B	70	4780	0.135	1:43	
2.4	I-B	72	5000	0.132	1.25	
2.5	II-B	73	5010	0.133	2:18	
2.6	II-B	72	5000	0.130	1:00	
2.7	II-B	72	5000	0.128	0:30	
2.8	II-B	72	4990	0.130	0:45	
2.9	II-B	40	4280	0.056	2:18	
2.10	II-B	38	4130	0.055	1:28	
2.11	II-B	35	4190	0.045	3:59	
2.12	II-B	33	4190	0.041	1:00	

*Reference heat flux calculated from stagnation enthalpy (H_s) and pitot pressure (P_T).

$t = 0$

$t = 2$

$t = 4$

$t = 10$

$t = 20$

$t = 25.6$

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Figure 41. Series No. 3 Arc-Jet Tests (Run No. 3.6 ($t = \text{sec}$))

TABLE 4
SERIES NO. 3 ARC-JET TESTS
(Simulated Air Jet)

Run No.	Configuration	Reference Heat Flux* $q_s \sqrt{r}$ (Btu-ft ^{1/2} /ft ² -sec)	Stagnation Enthalpy H_s (Btu/lbm)	Pitot Pressure P_T (atm)	Time in Jet Δt (min sec)	Comments
3.1	I-B	123	7590	0.154	1 43	Cladding dispersed, holder failed
3.2	I-B	101	6530	0.142	0 48	
3.3	I-A	100	6460	0.141	0 36	
3.4	I-B	38	4210	0.054	3 06	Explosive dispersal, no ablation
3.5	I-B	95	6120	0 142	1 27	Ablation
3.6	I-A	98	6360	0.142	0 47	Cladding dispersed, holder failed
3.7	I-B	73	5040	0.128	0 51	
3.8	I-B	71	4970	0.129	2 28	Complete ablation
3.9	I-B	94	6180	0.139	1 43	Complete ablation
3.10	I-B	33	3770	0.051	10 18	Cladding remained, no ablation
3.11	I-A	33	3850	0.049	10 47	Cladding burned off, no ablation
3 12	I-B	73	5020	0 130	2 16	Complete ablation

*Reference heat flux calculated from stagnation enthalpy (H_s) and pitot pressure (P_T)

For higher heating rates (characteristic of the heating at altitudes of 240,000 to 260,000 ft) with rotating specimens, the cladding was again observed to fail early in the test run. The remaining fuel material specimen then continued to heat and eventually ablated. These runs are most characteristic of the actual reentry case, since the specimens were clad and since the test conditions used were similar to those expected for the actual rod trajectories.

e. Series No. 4 Tests

Sixteen runs were made with specimens in Configuration I-B (cross-axial, rotating). In general, the specimens were observed to form cracks and eventually melt during the test run.

Six runs were made in Configurations II-A and II-B. After heating without ablation for a period, the specimens then ablated rapidly from the leading edge, as shown in Figure 42. The behavior was quite different than for a similar run in the helium jet, in that the flow appeared much less viscous and ablated much faster.

$t=0$

$t=20$

$t=46$

$t = 56.4$

$t = 65.4$

$t = 66.6$

$t = 72.1$

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Figure 42. Series No. 4 Arc-Jet Tests (Run No. 4.18) ($t = \text{sec}$)

In order to analyze the test data on the behavior of the fuel specimens in the arc-jet, it was necessary to accurately determine the heat flux to the specimens for the various jet conditions. This was difficult, since limited information was available on the heat-flux distribution on even simple specimen geometries in high-speed flow. Also, there were nonuniformities in the jet itself which affect the distribution of heat flux to the specimens. Therefore, water-cooled assemblies in the shape and size of the specimens were fabricated and instrumented with heat-flux sensors. These asymptotic calorimeters consist essentially of a constantan disk with copper leads from the edge, which measures the temperature difference between the center and edge of a disk. The output may be calibrated to determine the heat flux to the disk.

Reference heating values measured with hemispherical-tipped unit compared well with theoretical values. Heat-flux data for the nominal theoretical values of the reference heat flux are plotted in Figure 43. For the flat-face cylinder, the average heating was about 0.65 times the theoretical stagnation heating to a hemisphere with the same radius.

f. Discussion

Select specimens from the various groups of tests were examined after their removal from the test chamber. The most significant information was obtained from the Series No. 2 test specimens, since the remains from these specimens represent essentially the specimens of the other series at particular time intervals during the test runs.

The initial study involved microstructural analysis with conventional metallographic techniques and spot chemical analyses. Subsequent to preparation, the sections were examined, and photomicrographs and macrographs of significant structures were prepared. Chemical sampling was then used to provide information on the distribution of elements within the section. Macrographs of four specimens are shown in Figures 44 and 45.

The general observations and conclusions drawn from this effort were as follows:

- 1) In the sectioned specimens, the concentration of the hydrogen increased with distance from the heated surface and from cracks

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Figure 43. Calorimeter Assembly Test Data — Arc-Jet Tests

RUN 2.4 - $q_s \sqrt{r} = 72$, RUN TERMINATED AT 85 SEC

RUN 2.1 - $q_s \sqrt{r} = 73$, RUN TERMINATED AT 124 SEC

H1 = 0.026
H2 = 0.040 HYDROGEN CONTENT, WEIGHT PERCENT

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7611-0406

Figure 44. Macrographs of Preablation Oxidation
Test Specimens — Arc-Jet Tests
(Run Nos. 2.1 and 2.4)

RUN 2.10 - $q_s \sqrt{r} = 38$, RUN TERMINATED AT 88 SEC

H1 = 1.00
H2 = 1.74 **HYDROGEN CONTENT, WEIGHT PERCENT**

RUN 2.11 - $q_s \sqrt{r} = 35$, RUN TERMINATED AT 239 SEC

6-1-64

7611-0407

**Figure 45. Macrographs of Preablation Oxidation
 Test Specimens - Arc-Jet Tests
 (Run Nos. 2.10 and 2.11)**

within the specimen. This is particularly evident in specimens No. 2.4 (see Figure 44) where the apparent islands of material are δ -hydride, the material around these islands contains less hydrogen, and the surface is nearly devoid of hydrogen.

- 2) The hydrogen is lost such that discrete interfaces are formed between the phases present. This is particularly evident in Figure 45, where it is apparent that the phase interfaces have moved back for the longer test run.
- 3) The zirconium-oxygen reaction at the surface is apparently preferential to the reaction of zirconium with nitrogen.
- 4) The material in the vicinity of a molten surface was always devoid of hydrogen.

Microcracks were evident in all specimens and are characteristic of the hydrided fuel. These were extremely narrow and were short compared to the fuel dimensions. Another type of crack appeared to originate at the surface of the specimen in the dehydrided region and to extend a short distance from the surface.

In order to obtain confidence in the computational techniques and the jet conditions, the first series of test runs having thermally simpler materials was used as a check on the computational methods as well as the data reduction and interpretation. Configuration II-A (flat-face, axial) used for these specimens best approximated a one-dimensional heat conduction problem within the specimen.

This problem was solved with the digital Thermal Analyzer Program (TAP). The heat flux to the specimens was determined from the calorimeter assembly measurements.

Results of calculations performed for two stainless steel specimens are compared to the test data in Figure 46. Good agreement was found between the calculated and measured time for the leading edge to reach the melting temperature as seen in this figure. As a result of these stainless steel calibration test runs, it may be concluded that the interpretation of the test data is accurate.

Figure 46. Calibration Run Results — Arc-Jet Tests
(Run Nos. 0.4 and 0.5)

Similar calculations were performed for the aluminum test runs. The high thermal conductivity of the aluminum made the results extremely sensitive to the lateral heating of the specimens and the losses to the holder. No useful results were obtained.

The cross-axial fuel tests (Configuration I) best simulate the case of re-entry, since the stable attitude of a cylinder is with its axis normal to the velocity vector. A reentering rod would either tumble or oscillate about a cross-axial attitude, both of which tend to cause the heating to be distributed along the axis as opposed to end heating.

This problem was also solved with the application of TAP. However, one difficulty with executing a solution is that the specific heat of the fuel material is a function of the temperature and the hydrogen content of the fuel. The equivalent network for TAP did not account for the effects of hydrogen diffusion or dissociation.

A highly simplified thermal approximation to the effect of the hydrogen on the behavior was used. It was assumed that the hydrogen is dissociated (or activated) at a temperature of 2000°F, chosen because, in the vicinity of this temperature, the effective time for hydrogen to diffuse from the specimen goes from a long time to a short time compared to the test time. The TAP code was revised to hold the temperature at each node constant at 2000°F until integrated net heat input to the node equaled the heat of hydrogen dissociation. The pyrometer data was corrected for window losses and surface emissivity to obtain the black-body temperature.

Typical comparisons are shown in Figure 47 for two types of runs. The cladding was assumed to have been dispersed immediately, since, as seen in the film data, this was generally the case. Good agreement is found between the analytical results and the reduced data.

In general, it appeared that the use of simple thermal approximations to the fuel material results in good agreement between the predicted behavior and the observed behavior in the simulated reentry environment. The important assumptions used were as follows:

- 1) Axial conduction of heat is negligible. Calculations were performed on the basis of the center circumferential average heat flux.

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Figure 47. Comparison of Calculated to Measured Surface Temperatures — Arc-Jet Tests

- 2) Hydrogen is dissociated and lost from the specimen at 2000° F, with no effect other than that of an endothermic, constant-temperature reaction.
- 3) Aerodynamic heating to the specimen is taken as that to a specimen with no transpiration of material. Any effect of the hydrogen on the heat flux to the specimen is neglected.
- 4) Oxidation of the specimen surface is neglected.

The thermal approximation of the fuel material was also used for computing the expected behavior of a flat-face axial specimen, Configuration II-A. The calculated internal temperatures were compared to those measured with a thermocouple. The heat flux to the specimens was as determined from the calibration specimens. Results of these computations are shown in Figure 48 for the leading thermocouple and the second thermocouple from the leading edge. The apparent temperature of hydrogen dissociation for the test data is approximately 1600° F. However, as seen by the sectioned specimens, the hydrogen in these specimens may diffuse to the thermocouple holes, thus decreasing the effective diffusion length. The gross effects for the cross-axial test computations would not have changed appreciably if the hydrogen were assumed to have been lost at the temperature of 1600° F instead of 2000° F.

The extrapolation of the behavior observed in the arc-jet to that expected in a reentry trajectory required some approximations. Therefore, conclusions with respect to the predicted behavior of a reentering SNAP fuel rod, derived from these test results, had to be appropriately qualified.

A direct comparison of the heating expected during typical reentry trajectories to that used in the arc-jet, assuming that for the same heat flux the behavior will be the same, was used to extrapolate the behavior of the arc-jet to the case of reentry. It was assumed that the behavior is a function only of the heat flux to the fuel rod. Agreement was found using this assumption in analyzing the test data.

During reentry, analytical correlations were necessary to determine the trajectory and the resulting heating to a free-flight rod. The initial conditions of the rods depend on the trajectory of the reactor vehicle, and the trajectory

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Figure 48. Comparison of Calculated to Measured Internal Temperatures — Arc-Jet Tests

of the rods depends on their ballistic characteristics and dynamics. At a given time, cross-axial orientation results in a greater net heating to the rod than tumbling. On the other hand, the tumbling orientation increases the weight-to-drag ratio and, therefore, extends the trajectory, increasing the total time period of heating.

Cross-axial orientation was assumed. A vehicle trajectory was computed for orbital decay. From this trajectory, the initial conditions of the rod were determined. A trajectory for rods was determined for release from an altitude of approximately 240,000 ft, as shown in Figure 49. The reference heat flux was determined with the Detra and Hidalgo correlation. Nonequilibrium boundary layer (catalytic efficiency) and mass injection (hydrogen loss) effects were not accounted for.

The results of these computations for a rod released at an altitude of 241,400 ft are compared to the results for a clad specimen run in Figure 50. Results show that specimens undergo complete meltdown while aerodynamic heating is still available. In the one case (run 3.9), the heating was slightly less than that predicted by equilibrium boundary layer theory and, in that case, the rod has considerable heating still available after meltdown. In the other case (run 3.12), the test heating was approximately 30% less than that predicted for the trajectory using equilibrium boundary layer theory; in which case, the meltdown of the rod occurs with little available heating remaining.

The calculations indicated that, with a 30% margin for error, the fuel rod released at 241,000 ft would melt down on reentry.

2. Rod Burnup Analysis⁽¹⁰⁾

Analytical studies of the transient behavior of zirconium-uranium hydride SNAP reactor fuel elements under atmospheric reentry conditions were undertaken to describe the conditions which are necessary for burnup and dispersal of the radioactive fuel material. Trajectory transient calculations were made for SNAP 10A reactor fuel elements released from the reactor vehicle at selected altitudes between 200,000 and 400,000 ft. The aerodynamic heating, transpiration cooling, and the chemical reactions of the fuel with air were analyzed to determine the net surface heat flux to the elements. An analytical model was

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Figure 49. Trajectory Parameters for a Rod ($W/C_{DA} = 25 \text{ lb/ft}^2$) Released from an Orbital Decay Trajectory ($W/C_{DA} = 132 \text{ lb/ft}^2$) at an Altitude of 241,000 ft

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Figure 50. Comparison of Arc-Jet Test Run No. 3.12 to Heat Flux Transient for a Rod Released at an Altitude of 241,000 ft

developed with which simultaneous solutions of the heat and hydrogen transport equations were obtained, including variable material properties and material phase changes.

Using current information concerning fuel material properties, minimum fuel element ejection altitudes were estimated from which complete ablation of the fuel can be achieved. The computations were performed for fuel element trajectories with cross-axial-spinning, end-over-end-tumbling, and random-tumbling flight modes. A statistical analysis of the computations was made, from which probabilities of initial fuel ablation were derived as a function of release altitude. The statistical procedure was concluded to be a valid and optimum method of analysis. The influence of each parameter was precisely determined and ranked to provide direction for experimental support investigations. The procedure also enabled an organized, reiterative narrowing of the accuracy attributed to the study results.

Data from experimental tests on the transient heating and burnup of the fuel material in a hyperthermal wind tunnel were used to correlate the analytical description. The agreement was found to be good and within deviation due to uncertainties in parameters.

An analytical model was developed which incorporated all known processes occurring at or near the fuel element surface and within the fuel material. More specifically, the model generated simultaneous heat and mass transfer solutions considering: (1) temperature and hydrogen concentration-dependent material properties; (2) material phase changes; and (3) a complex surface heat flux which varies with time, surface temperature, and the hydrogen loss rate. In addition, fusion and ablation of the cladding and fuel material were simulated. A principal advantage of this analytical model was that it utilized a known, familiar computer code (Thermal Analyzer Program, TAP-3) for obtaining these solutions.

The determination of the net surface heat flux to the fuel element was based on an evaluation of the aerodynamic heating, transpiration cooling, and the heating contributions of the surface chemical reactions (oxidation of the fuel material and hydrogen combustion).

The analysis was confined to fuel element flight modes which effect a uniform heating around the surface of the element. Such modes are: (1) cross-axial spinning, (2) random tumbling, and (3) end-over-end tumbling. In these cases, the analysis was simplified to a one-dimensional, radial description. The description began with a fully clad fuel element, released at a given altitude along the reference vehicle trajectory, at a given initial temperature, and experiencing one of the flight modes. The material response of the fuel element following release was then computed by means of the analytical model upon specification of the aerodynamic heating, stagnation enthalpy, and fraction of completion for hydrogen combustion, all as functions of time.

The overall fuel element reentry behavior, including evaluation of the trajectory heating and the material response calculations, was analyzed using statistical methods. The objective of this statistical analysis was to pinpoint those parameters and processes requisite of further experimental and analytical research, and to arrive at probabilities of fuel element survival or burnup for the conditions considered. The process of analysis was shown to be iterative. That is, with each set of subsequent data factored in, the process was repeated to further refine the accuracy of the analytical method and the corresponding results.

a. Initial Conditions at Fuel Element Release

In order to properly evaluate the reentry performance of a SNAP fuel element, location, dynamics, initial temperature, and hydrogen composition of the element at the instant of its release from the reactor, must be known. Generally, the normal sequence of events for the SNAP 10A-Agena vehicle during reentry following orbital decay was used to determine initial conditions. Studies show that both the nominal and non-nominal orbits would decay to a narrow range of velocities and reentry angles at 400,000-ft altitude.

From the narrow range of parent vehicle decay trajectories, a representative trajectory was selected and used as a basis for the individual fuel element trajectories. At 400,000 ft, this reference trajectory has the following characteristics:

1) Orbit	Polar
2) Longitude	0 deg

3) Latitude	90 deg
4) Velocity	25,694 ft/sec
5) Flight angle	0.05 deg
6) W/C_{DA} (ballistic parameter)	132 lb/ft ²

Subsequent velocities, altitudes, and aerodynamic heating to the vehicle are shown as a function of time after 400,000 ft in Figure 51. Calculations of these trajectory transients were performed using the RESTORE digital code.

Consideration of the angular dynamic motions showed that if a fuel element should receive even a small angular impulse during release, random tumbling of the elements will occur throughout atmospheric descent. Other possible flight orientations considered were:

- 1) Stabilized cross-axial spinning (axis of rotation perpendicular to line of flight)
- 2) End-over-end tumbling
- 3) Stabilized cross-axial nonspinning
- 4) Stabilized end-on (spear reentry).

Random tumbling, cross-axial spinning, and end-over-end tumbling effect a uniform aerodynamic heating distribution around the cylindrical surface of the fuel element, provided that a minimal rotational speed of about 60 rpm is maintained. Of these three orientations, the spinning, cross-axial case results in the lowest average heat input to a SNAP fuel element.

End-on reentry is characterized by an intense localized heating on the front flat face of the cylindrical element. Similarly, a nonspinning, cross-axial orientation results in high localized heating along the stagnation line of the cylindrical surface. In these cases, melting of the fuel material occurs sooner and the ablation period is longer than for other orientations.

The initial temperature and hydrogen content of the fuel elements at release are dependent on the sequence of events leading to the separation of the elements from the reactor core. SNAP 10A reactor disintegration studies were used to

Figure 51. Trajectory Transients for an Orbital Decaying SNAP 10A Reactor Vehicle in a Polar Orbit

obtain an appropriate range of initial temperatures. If the NaK coolant remains in the system during reentry, the reactor core temperature was calculated to be about 940° F at an altitude of 310,000 ft. If the NaK is lost from the core prior to reentry, the core temperature at the same altitude was calculated to be about 100° F, assuming that fission-product-decay heat is negligible. Accounting for the heat release from the fission products, this temperature was estimated to be about 200 to 450° F, depending on whether reentry is made in sunlight or dark sky. Heat transfer calculations for the reactor core without NaK indicated that the vessel wall ablates away with little or no heat transfer to the inner elements of the fuel element bundle. The outermost elements would be at higher temperatures at release. The lower boundary for element release temperature was taken to be 200° F.

Empirical hydrogen leakage data showed that a SNAP 10A fuel element loses a negligible amount of hydrogen during its full, normal lifetime of operation. After reactor shutdown, the hydrogen is essentially frozen within the fuel element and no further hydrogen loss occurs. Thus, the hydrogen concentration at fuel element release was set at 6.61 lb of H₂/ft³ ± 1%, corresponding to an initial H/Zr = 1.80 ± 0.02.

b. Analytical Model Description

The fuel element was treated as an infinitely long cylinder with radial heat and hydrogen transfer. Heat generation due to fission product decay was neglected commensurate with the assumption of a long (about 3000-yr) orbital lifetime for the SNAP 10A reactor system.

The calculation contained three successive stages. First, beginning at the time of fuel element release or ejection from the reactor vessel, the transient temperatures of the cladding material and the fuel material were computed up to the time of cladding meltdown. In the second stage, the calculation accounted for hydrogen dissociation and migration within the fuel, and for chemical reactions at the fuel surface. As the fuel reached the melting temperature, a third stage was initiated in which melting and ablation of the fuel increments were approximated.

Immediately following fuel element release, the following heat transfer processes were simulated by means of the analytical model:

- 1) Aerodynamic heating to the cladding surface
- 2) Heat storage within the cladding
- 3) Heat radiation to space
- 4) Heat radiation across the element gap
- 5) Heat conduction and storage within the fuel.

Heat transfer equations which describe the temperature response of the cladding material and the fuel were solved.

As the cladding reached the melting temperature (2400°F), a latent heat of fusion (133 Btu/lb) was attributed to the Hastelloy-N cladding. When this latent heat was absorbed, the entire cladding was assumed to be sheared away leaving the bare fuel-moderator material exposed to aerodynamic heating.

The second stage in the model calculations extended between the time of cladding burnoff and the time of initial melting at the fuel surface. That stage was by far the most important, since the greater portion of the total heat necessary to completely ablate a fuel element was consumed there. The behavior of the fuel throughout this period was described by heat and hydrogen continuity equations. Possible individual heat and mass transfer processes which occur within the fuel and at the fuel surface were considered. These were:

- 1) Heat conduction and storage
- 2) Hydrogen transport under the influence of a concentration gradient
- 3) Heat transfer due to hydrogen enthalpy redistribution
- 4) Thermal radiation from the fuel surface to the environment
- 5) Heat of hydrogen dissociation.

Calculations were based on uniform hydrogen outflow around the fuel surface, as well as uniform heating and temperature response. An empirical diffusion coefficient was used to describe the hydrogen diffusion through the microcracked fuel material. Microcracking occurs when the surface temperature reaches about 1900°F, causing rapid dehydriding.

A number of simplifying assumptions were made in the third-stage analytical model, which is concerned with fuel meltdown and ablation; these are summarized as follows:

- 1) A heat of fusion of 90 Btu/lb of fuel was used.
- 2) A gradual and steady recession of the fuel surface during meltdown was simulated.
- 3) Aerodynamic heat flux changes due to changes in shape of the fuel were neglected.
- 4) Oxidation heating of the fuel material during melting was assumed to be constant and equal to the oxidation heating at the melting point.

The simplified approach is justified for the uniform surface heating case because of the relatively small amount of additional heat necessary to completely ablate the fuel once surface melting begins. A reasonable correlation with the available experimental data was attained. However, more specific knowledge of the heating and shearing phenomena associated with the ablation process could have improved the description.

c. Aerodynamic Heating and Surface Reactions

The basic heating equations employed in the SNAP 10A reactor reentry behavior analyses were applied to the calculations of the fuel element aerodynamic heating.

For a SNAP 10A fuel element, transition from free-molecular to continuum heating occurs over an altitude range centered around 285,000 ft. The computations performed were based upon a sharp transition from free-molecular heating to continuum heating at the point where the two heating curves intersect (about 285,000 ft).

For selected fuel element release altitudes and for each assumed flight mode, the digital code, RESTORE, was run to provide the information needed by the analytical model to evaluate the aerodynamic heating. Tables of the cold-wall heat rate and of the stagnation enthalpy as functions of time after release were formulated from the RESTORE output and used as input to the analytical model. At each iteration in the material response calculations, the aerodynamic heat rate equation was evaluated using the analytical model according

to these tables and according to the surface temperature (or wall enthalpy) of the fuel element.

Plots of the RESTORE-calculated, cold-wall heat flux transients are shown in Figures 52 and 53 for selected fuel element release altitudes. In these figures, changes in the shapes of the curves at the lower release altitudes are evident. The integrated heating under these curves was evaluated. The integration was cut off at $52 \text{ Btu/ft}^2\text{-sec}$, since this was the point at which the aerodynamic heating equaled the radiation heat loss of the fuel at the melting temperature.

For release altitudes around 250,000 ft and below, constant, average ballistic coefficients were used in the RESTORE computations. For release altitudes above 250,000 ft, variable ballistic coefficients were used. These ballistic coefficients were based on drag coefficients varying with the Knudsen number. Ballistic coefficients at 400,000 ft and for continuum flow are given in Table 5.

After the fuel element cladding ablates away, a heat release due to oxidation of the zirconium-uranium hydride fuel augments the aerodynamic heat flux at the fuel surface. A study of this oxidation was undertaken to determine the magnitude of the oxidation heating contribution. In addition, the amount of oxide film buildup was of interest, since these oxide layers could, conceivable, inhibit hydrogen evolution as well as affect the catalytic efficiency of the fuel surface.

TABLE 5
SNAP 10A FUEL ELEMENT BALLISTIC COEFFICIENTS

Flight Orientation	400,000 ft (lb/ft ²)	Continuum Flow (lb/ft ²)
Cross-Axial Spinning	11.1	20
End-Over-End Spinning	24.8	44
Random Tumbling	18.5	34

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Figure 52. Average Cold-Wall Aerodynamic Heat Rate to a SNAP 10A Fuel Element Spinning Cross-Axially vs Time after Release from Various Altitudes

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Figure 53. Average Cold-Wall Aerodynamic Heat Rate to a SNAP 10A Fuel Element Tumbling at Random vs Time after Release from Various Altitudes

Oxidation rate constants were obtained by extrapolation of available measured data shown in Figure 54. Considerable uncertainty was incurred because:

- 1) Oxidation rate data for the fuel material were based on those of zirconium metal.
- 2) The oxide layer formed under reentry conditions was assumed similar to the layer formed under static conditions.
- 3) The influence of hydrogen outgassing on oxide formation was not considered.

Consequently, a simplified approach using a temperature-independent constant was employed. Using the Cubicciotti value, the resulting calculated heat of oxidation was found to be only about 5% of the total heat needed to initiate melting of the fuel. The calculated time behavior of this heating is shown in Figure 55. The calculation assumed that the sluggishness of the chemical kinetics controls the oxidation process. This is true if the heat calculated for the complete reaction of all oxygen diffusing to the fuel surface is always greater than that calculated using the kinetic reaction rate constants. This was the case in the calculation made.

According to the data of Harkness, the oxidation of the (unhydrided) fuel proceeds at much faster rates at higher temperatures than that initially calculated using the rate data of Cubicciotti. At temperatures approaching the fuel melting point, the heat calculated for the complete reaction of all oxygen diffusing to the fuel surface became less than that calculated using the kinetic reaction rate constants of Harkness. At these temperatures, therefore, an instantaneous kinetic reaction rate was assumed and only the oxygen flux supplied to the fuel surface was considered.

The mass rate of reacting oxygen was expressed in terms of the kinetic rate constants (kinetics controlled) and in terms of the oxygen flux diffusing through the boundary layer (oxygen supply controlled). The heating contribution was taken to be the lesser of the respective two calculated heats. The minimum heating rate as a function of surface temperature is shown in Figure 56. Calculations for different release altitudes all gave similar results.

Figure 54. Parabolic Oxidation Rate Constants vs Reciprocal Temperatures

Figure 55. Oxidation Heating Rate vs Time Based on Parabolic Oxidation with a Constant Reaction Rate Constant

Figure 56. Heat of Oxidation as a Function of Fuel Surface Temperature

Under the assumption that all of the reacting oxygen contributes to the build-up of an oxide film, the thickness of this film was computed, using the heating rates given in Figure 56. These oxide film thicknesses during the period of rapid hydrogen evolution (2000 to 2500°F) were calculated to be about 1 to 4 mils.

The flow of hydrogen out of the fuel element occurring simultaneously with heat transfer to the fuel element surface produces an effect known as transpiration cooling. Actually, this effect is twofold, and is characterized by the following processes:

- 1) The relatively cool hydrogen gas is injected into the boundary layer with an ensuing chilling effect.
- 2) The boundary layer becomes thickened, resulting in added resistance to heat transfer across the boundary layer.

The theory and the experimental aspects of transpiration cooling were studied and an equation to compute its effect was used in the analytical model.

As the hydrogen gas is injected into the high-temperature boundary layer, it reacts with oxygen. The heat released due to the reaction,

was evaluated by considering the chemical kinetics and the supply of the reactants to the reaction zone. In order for the combustion reaction to be pertinent, hydrogen must react in the boundary layer or behind the shock. Using a reaction time of 10^{-6} sec and a detachment distance (10% of the fuel radius) of 6.25×10^{-2} in., a hydrogen injection rate greater than 5×10^3 ft/sec was found to be necessary before hydrogen could escape into the free stream outside the shock without reacting with oxygen. The high velocity of 5×10^3 ft/sec is highly improbable for this process.

The fraction of completion for the reaction was evaluated as a function of time after rod release using the atmospheric pressure and the temperature behind the shock, which were obtained from the fuel element trajectory (RESTORE) computations. Because of the relatively high temperature behind the shock immediately following fuel element release, the combustion of hydrogen

was not large. The combustion proceeded to a higher fractional completion at the last portion of the reentry trajectory where the aerodynamic heating became small.

The heating rate due to hydrogen combustion used in the analytical process was taken to be the lower of the two values computed, assuming each reactant was limiting. The total contribution of the combustion of hydrogen toward the heating of the fuel element was calculated to be less than 10% for all release altitudes considered. The characteristic transient behavior of the calculated heat of combustion is shown in Figure 57, where it is compared to the corresponding heat of oxidation and the heat rate losses due to transpiration cooling and radiation.

d. Fuel Element Material Properties

Data on the thermophysical properties of SNAP fuel material were analyzed and evaluated. The ZrH constitutional diagram used for the fuel element burnup studies is shown in Figure 58. Representative phase transits of the fuel material calculated by means of the analytical model during reentry are shown by the dashed line in the figure. No points of discontinuity are seen at the phase boundaries, since the phase changes occur gradually across the radius of the fuel.

The melting temperature of the alloy was taken to be 1805°C (3280°F). The presence of dissolved oxygen markedly increases the melting temperature, which creates uncertainty in the melting temperature of the alloy in an oxygen atmosphere.

Post-test microstructural analyses performed for both the experimental arc-jet tests and the electrical pulse-heating tests indicate that a fuel segment becomes essentially devoid of hydrogen before the segment melts. Analytical calculations, using the standard set of parameter values, were characterized by this same behavior. Accordingly, the melting temperature and heat of fusion for the fuel material were taken to be that for unhydrided Zr-10 wt % U alloy.

Generally, the specific heat data were characterized by noticeable variation with both temperature and hydrogen content. For some of the data, empirical relationships, which adequately describe the bivariate dependence, were derived. The expressions were found to change drastically from phase to phase.

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Figure 57. Comparison of Fuel Surface Heating Contributions During Reentry

Figure 58. Zirconium-Hydrogen Phase Diagram

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The heat of dissociation [2H (dissolved in Zr) $\rightarrow$ H_2 gas] was assumed to be concentration-dependent within the delta-epsilon phase region. In the $\beta + \delta$, two-phase region, the constant average value of -51.0 kcal/mole of H_2 was used. Finally, throughout the beta phase, the high-temperature average value of -40.0 kcal/mole of H_2 was adopted. These values are uncertain by about 6%, except for the beta phase values, which are uncertain by about 15%.

Hydrogen loss was described by the ordinary diffusion equation with the "appropriate" diffusion coefficient and boundary conditions. The appropriate diffusion coefficient was dependent, in addition to temperature, on the environmental conditions and on the mechanism of hydrogen transport. The diffusion may take place through the matrix material in atomistic form via a vacancy mechanism or through microcracks, voids, or grain boundaries. The effective diffusion coefficient was determined from pulse-heating data.

For values of thermal conductivity greater than about 12 Btu/ft-hr-°F, its influence upon the heat necessary to initiate melting was negligible. A constant thermal conductivity of 13.3 Btu/ft-hr-°F was assumed for the fuel material. Some additional experimental data for the thermal conductivity was needed, especially at higher temperatures for rapidly dehydrated fuel material.

A fuel density of 6.09 gm/cm³ (380.2 lb/ft³) was used, corresponding to H/Zr ratios in the range of 1.7 to 1.8. This constant value was used throughout the reentry calculations, commensurate with the assumption of fixed geometrical dimensions. The density value used is accurate to within a few percent.

A reentering SNAP reactor fuel element passes through regions of altitude and velocity where the air behind the shock and in the boundary layer may not be in equilibrium. The equilibrium aerodynamic heating rate must then be adjusted to account for the chemical kinetics effects. The reduction in heating is expressed as a function of altitude and the catalytic efficiency of the material surface is defined as:

$$\gamma_A = \frac{\text{Atomic collisions effective in recombination}}{\text{Total number of atomic collisions}}.$$

A constant value of 0.95 was assumed (at all altitudes) for the aerodynamic heat reduction due to noncatalytic wall effects. Experimental investigation was needed to determine a more accurate surface catalytic efficiency value.

The material properties of the Hastelloy-N cladding tube used are listed in Table 6. Hydrogen barrier material was not considered.

TABLE 6
HASTELLOY-N CLADDING PROPERTIES

Property	Value	Comment
Specific Heat	0.13 Btu/lb-°F	Average high-temperature value
Density	0.317 lb/in. ³	-
Emissivity	0.3	Typical value for nickel alloys
Heat of Fusion	133 Btu/lb	Typical value for nickel alloys
Melting Point	2400 ± 30° F	-

e. Method of Solution and Results

The methods by which the thermal history of a SNAP fuel element were calculated throughout reentry were as follows:

- 1) Given a release altitude along the reference reactor vehicle trajectory and assuming a particular flight orientation, the independent fuel element trajectory transients (aerodynamic heating, stagnation enthalpy, velocity, altitude, etc.) were computed using the trajectory code, RESTORE. Necessary initial conditions for the RESTORE code computation, such as velocity, flight angle, location, and heading, were obtained from the reference vehicle trajectory at the altitude of fuel element release.
- 2) The appropriate heating factors were applied to RESTORE-computed heating rates to obtain tabular values of the average cold-wall aerodynamic heat rate to the element as a function of time. Also, tabular values of the stagnation enthalpy and the fraction of completion for hydrogen combustion were obtained as functions of time, based on the RESTORE code results.

- 3) The tabular values obtained in step 2) were specified to the analytical, material response model. Calculations are then made, using the TAP-3 code, to determine the thermal behavior of the fuel element under the specified conditions.

In TAP-3, beginning at the time of fuel element release, the heat and mass transfer equations were solved simultaneously by finite difference methods. These solutions were coupled to the iterative calculation of the net surface heat flux by way of the fuel surface temperature and the hydrogen outflow. The purpose of these results was to define the conditions necessary for complete melt-down and ablation of the fuel material, based on up-to-date research regarding the processes and parameters involved.

The analytical model was correlated with a series of hyperthermal wind tunnel (arc-jet) experiments. The arc-jet tests used were those in which cylindrical specimens were exposed cross-axially to the jet and rotated at approximately 4 rps. The analytical fuel element model discussed previously was adapted to simulate the conditions of the arc-jet tests. Results indicated that the analytical model with the evaluated set of parameters could be used confidently for reentry behavior calculations.

Material response calculations were made for a reentering SNAP 10A fuel element in cross-axial-spinning, random-tumbling, and end-over-end flight orientations. Typical results of these calculations are presented in Figures 59 and 60, in which the transient behavior of the fuel surface temperature, average fuel temperature, average hydrogen content, and the cold-wall, average aerodynamic heat rate are shown.

A set of calculations was performed for various release altitudes and initial temperatures to define the minimum release altitudes for which complete ablation of the fuel material is attained. Curves of these minimum release altitudes as functions of initial temperature are shown in Figure 61 for all flight orientations considered. These results indicate that the ranges of initial release altitude that are just sufficient to attain complete burnup, assuming correct input parameter values, are:

- 1) 245,000 to 252,000 ft for cross-axial spinning
- 2) 238,000 to 245,000 ft for end-over-end tumbling
- 3) 235,000 to 241,000 ft for random tumbling.

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Figure 59. Reentry Behavior of a SNAP 10A Fuel Element Tumbling at Random after Release from 232,000 ft

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Figure 60. Reentry Behavior of a SNAP 10A Fuel Element Spinning Cross-Axially after Release from 250,000 ft

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Figure 61. Minimum Fuel Element Release Altitude to Attain Burnup vs Initial Temperature

These ranges are appropriate for a 0 to 1000°F range in initial temperatures. Further, they reflect the approximate procedure for simulation of the ablation process.

The trajectory parameters and conditions of the fuel element at the time of initial ablation are of interest for analytical and experimental investigations of fuel particle behavior. The conditions at the time of initial ablation were calculated for various release altitudes for a cross-axial-spinning orientation and a random-tumbling orientation were calculated. The fuel element altitudes at the time of initial melting ranged from 174,000 to 230,000 ft for a cross-axial-spinning orientation and from 162,000 to 230,000 ft for elements tumbling at random. For a typical, probable case (release altitude = 242,000 ft, random-tumbling orientation), the fuel begins to melt by about 185,000 ft and is completely ablated by about 155,000 ft.

Information on the size of the ablated particles was lacking for putting fuel particle behavior studies on a quantitative basis.

f. Statistical Analysis

Reentry computations were made for a variety of initial conditions and parameter values. To organize and categorize the results, a program of statistical analysis was undertaken. The method employed in the statistical analysis was based on the Law of Propagation of Errors. The integrated aerodynamic heat necessary to initiate fuel ablation and the integrated aerodynamic heat available in the fuel element trajectory were evaluated and compared. Evaluations were made for the entire range of reasonable (within 95% probable) parameter values and initial conditions. Comparisons were made for various release altitudes and flight orientations.

The objectives of the statistical program were:

- 1) To determine the change in the resulting analytical solution due to a perturbation in any given input parameter.
- 2) To tabulate those parameters which most strongly influence the analytical solutions.

- 3) To define the parameters which are requisite of further experimental refinement according to the criterion of effective elimination of uncertainties in the analytical solutions.
- 4) To calculate the composite range of deviation in the integrated aerodynamic heat necessary to initiate ablation due to the range of deviation in each input parameter.
- 5) For each release altitude, to calculate the composite range of deviation in the integrated aerodynamic heat available in the fuel element trajectory due to the range of deviation in each trajectory parameter.
- 6) Utilizing the results achieved from objectives 4 and 5, to calculate the probabilities of initial fuel melting for various release altitudes and modes of reentry.

The overall statistical program is outlined in flow-chart form in Figure 62. The focal point of the program is the calculation of the probability of initial fuel ablation for each release altitude.

A preliminary analysis was made to develop methods and to scope results. Using the Law of Propagation of Errors, influence coefficients were developed for specific heat, heat of hydrogen dissociation, emissivity, initial temperature, initial hydrogen concentration, diffusion coefficient, heat of oxidation, thermal conductivity, aerodynamic heating, outside fuel element radius, and airstream stagnation enthalpy.

Two series of test problems were made to determine the accuracy of the influence coefficients and to investigate cross-coupling effects. In both series, two parameters were varied simultaneously and the influence coefficients were used to predict the corresponding value of the integrated heat required to initiate ablation area.

The first series of test problems was designed to determine the accuracy of the influence coefficients when wide variations of parameters were used. The maximum error in the prediction of Q_{REQ} was 3.16%.

A second series of test problems was analyzed to determine the accuracy of the influence coefficients around the reference case. Variations in the ratios

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Figure 62. Program of Statistical Analysis – Fuel Rod Reentry Burnup

of integrated heats, $F_{Q_{REQ}}$, were made from 0.9865 to 1.0135 due to each parameter. Parameters were varied in such a manner as to leave the required integrated heat ratio approximately unchanged. The maximum error in the prediction of Q_{REQ} was less than 0.5%.

The confidence limits for Q_{REQ} were constant for all release altitudes, since they were independent of heat input. The integrated heat necessary to initiate ablation for the reference model varied with release altitude and with the mode of reentry for any given release altitude. This was due to the influence of the heating rate upon Q_{REQ} .

Aerodynamic heating rates obtained from the RESTORE trajectory code were substituted into the analytical fuel element model to determine the value of Q_{REQ} for various release altitudes from 200,000 to 400,000 ft. The total available heat over the trajectory, Q_{AVAIL} , obtained from each run is the cold-wall heat rate integrated from the instant of release to the time at which it reached a value of 52 Btu/ft²-sec.

The second phase of the preliminary analysis was to determine the influence of certain trajectory parameters on the integrated aerodynamic heat available (Q_{AVAIL}) to a reentering SNAP 10A fuel element. The trajectory parameters of significant influence were the ballistic coefficient, velocity, and flight-path angle.

Using the trajectory code RESTORE, a series of computer runs was made to determine the influence coefficients for the above parameters. Fuel element release altitudes from 200,000 to 400,000 ft were considered. The optimum release altitude for fuel ablation lies around 300,000 ft. Probabilities for survival rise sharply for release altitudes below 275,000 ft.

Additional parameters investigated were fuel melting temperature, catalytic efficiency of the fuel element surface, and an approximation of the error inherent in the analytical solution. A second set of reference parameters was used in which initial fuel element temperature, the heat of oxidation, and the hydrogen heat of dissociation for the fuel in the delta-phase parameters were improved. A constant aerodynamic heat input rate of 100.0 Btu/ft²-sec was used to evaluate Q_{REQ} . The value of Q_{REQ} was 10,855.0 Btu/ft².

The standard values and the estimated uncertainty ranges for the second set of reference parameters are listed in Table 7. Also given in this table are the values of ΔQ_{REQ} for each parameter.

TABLE 7
DEVIATIONS IN Q_{REQ} DUE TO $\pm 2\sigma$ DEVIATIONS IN THE
SECOND SET OF STANDARD PARAMETER VALUES

Parameter and Standard Value of Parameter	2σ Uncertainty Limit	ΔQ_{REQ} (Btu/ft ²)
$D = 1.0$	$\pm$ factor of 10	1804
$q_{oxid} = 1.0$	$\pm$ factor of 1	1403
$*R\gamma_A = 0.95$	+0.05	1021
	-0.10	
$\epsilon = 0.4$	± 0.15	830
$H_{diss} = 1.0$	$\pm 10\%$	693
$T_i = 500^\circ\text{F}$	$\pm 500^\circ\text{F}$	606
$T_M = 3280^\circ\text{F}$	$\pm 200^\circ\text{F}$	462
$C_p = 1.0$	$\pm 10\%$	360
$H/Zr_i = 1.80$	± 0.05	272
$k = 13.3 \text{ Btu/hr-ft-}^\circ\text{F}$	$\pm 5.0 \text{ Btu/hr-ft-}^\circ\text{F}$	258
Analytical Solution	$\pm 6\%$	326
$R_o = 0.625 \text{ in.}$	$\pm 0.005 \text{ in.}$	97
$H_s = 10,000 \text{ Btu/lb}$	$\pm 2500 \text{ Btu/lb}$	38

* $R\gamma_A$ is defined as the ratio of the hot-wall aerodynamic heat rate for a noncatalytic surface ($\gamma_A < 1$) to the corresponding heat rate for a fully catalytic surface ($\gamma_A = 1$).

Q_{REQ} was calculated for cross-axial and random-tumbling modes for release altitudes from 200,000 to 400,000 ft. For random tumbling, 2σ deviations in the individual trajectory parameters were chosen to be identical to those in the cross-axial case. Calculations were made, varying each parameter separately to obtain influence coefficients. The resulting influence coefficients and the parameter confidence limits and standard values used in this parameter survey are summarized in Figures 63, 64, and 65. Values of Q_{AVAIL} , Q_{REQ} , σQ_{REQ} , and probabilities of initial ablation at each release altitude are summarized for cross-axial and random-tumbling reentry in Tables 8 and 9. Probabilities of survival (nonablation) for random-tumbling reentry are presented graphically as a function of release of altitude in Figure 66. Corresponding probabilities for a cross-axial-spinning mode of reentry are plotted in Figure 67.

Figure 63. Influence of Velocity on Heat Available to Reentering SNAP 10A Fuel Element Tumbling at Random

Figure 64. Influence of Ballistic Coefficient on Heat Available to Reentering SNAP 10A Fuel Element Tumbling at Random

Figure 65. Influence of Flight Path Angle on Heat Available to Reentering SNAP 10A Fuel Element Tumbling at Random

TABLE 8
Q_{REQ} AND Q_{AVAIL} WITH STANDARD DEVIATIONS AND
PROBABILITIES OF INITIAL ABLATION FOR CROSS-
AXIAL REENTRY USING THE SECOND SET OF
REFERENCE PARAMETERS

Release Altitude (ft)	Q _{AVAIL} (Btu/ft ²)	σ _{Q_{AVAIL}} (Btu/ft ²)	Q _{REQ} (Btu/ft ²)	σ _{Q_{REQ}} (Btu/ft ²)	Probability of Initial Ablation
200,000	5,951	±632	-	±1450	0.0006
225,000	9,145	±706	-	±1450	0.105
239,500	11,185	±830	11,185	±1450	0.50
250,000	13,065	±1064	11,050	±1450	0.8698
250,000	13,065	±848	11,050	±1450	0.885*
275,000	17,025	±1087	11,043	±1450	0.9995
300,000	17,662	±978	11,717	±1450	0.99955
350,000	17,632	±977	12,310	±1450	0.9988
400,000	17,346	±925	12,845	±1450	0.9956

*Values used to calculate probability of initial ablation used in graphical approximation of probabilities for release altitudes below 239,500 ft

TABLE 9
Q_{REQ} AND Q_{AVAIL} WITH STANDARD DEVIATIONS AND
PROBABILITIES OF INITIAL ABLATION FOR RANDOM-
TUMBLING REENTRY USING THE SECOND SET OF
REFERENCE PARAMETERS

Release Altitude (ft)	Q _{AVAIL} (Btu/ft ²)	σ _{Q_{AVAIL}} (Btu/ft ²)	Q _{REQ} (Btu/ft ²)	σ _{Q_{REQ}} (Btu/ft ²)	Probability of Initial Ablation
200,000	7,116	±743	-	±1450	0.0053
225,000	10,577	±782	-	±1450	0.35
229,000	11,110	-	11,110	±1450	0.50
250,000	14,554	±1108	10,862	±1450	0.978
250,000	14,554	±925	10,862	±1450	0.982*
275,000	18,332	±1197	11,093	±1450	0.99994
300,000	19,938	±1176	11,882	±1450	0.999992
350,000	19,749	±1050	12,570	±1450	0.99997
400,000	19,508	±1040	13,251	±1450	0.99977

*Values used to calculate probability of initial ablation used in graphical approximations of probabilities for release altitudes below 229,000 ft

Figure 66. Probability of Survival (Nonablation) of a SNAP 10A Fuel Element Tumbling at Random vs Release Altitude

Figure 67. Probability of Survival (Nonablation) of a SNAP 10A Fuel Element Spinning Cross-Axially vs Release Altitude

The results of the statistical analyses were strongly influenced by the values assigned to the various initial conditions, thermophysical properties, and trajectory parameters involved in the computation of Q_{AVAIL} and Q_{REQ} . Several parameters which strongly affect Q_{REQ} were not well defined, including the hydrogen diffusion coefficient, fuel emissivity, heat of fuel oxidation, and thermal conductivity.

The statistical analyses indicated that the optimum release altitude to attain melting and ablation of a SNAP 10A fuel element lies around 300,000 ft. Further, the probability of initial ablation was enhanced if the fuel element tumbled at random rather than spin about a stabilized, cross-axial orientation. The maximum probability that a SNAP 10A fuel element would initially ablate was about 0.999992 for an element tumbling at random compared to about 0.99966 for an element spinning cross axially.

Results of the parameter surveys for Q_{REQ} and Q_{AVAIL} have shown the comparative influence of the various parameters and initial conditions on the integrated heats. It was found that cross-coupling effects existed between some parameters so that deviations in the integrated heat due to deviations in these parameters was dependent on the choice of reference parameter values.

3. Thermophysical Properties Measurements⁽¹¹⁾

Systematic investigations of the thermal properties of SNAP fuel were made to obtain reliable thermophysical properties data needed to describe its behavior during reactor operation and reentry. Conventional measurement methods had often proven inadequate because of the high sensitivity of properties to hydrogen content coupled with the high hydrogen mobility in ZrH_x . Considerable care was exercised to derive reliable data in these measurements.

Heat capacity, thermal diffusivity, electrical resistivity, and thermal conductivity measurements were made on the SNAP fuel alloy, zirconium - 10 wt % uranium, as well as on this alloy hydrided to H/Zr atom ratios of 0.50, 1.26, 1.58, 1.81, and 1.90. Of the foregoing six compositions, the unhydrided alloy and the 1.81 H/Zr material were investigated jointly by Atomics International (AI) and Battelle Memorial Institute (BMI) under a specially controlled program.

The flash technique was employed by both laboratories in measuring thermal diffusivity. The specific heat measurements at AI were conducted, using an electrical pulse heating technique, while BMI employed an ice drop calorimeter. Electrical resistivity measurements were also made at AI, since, in the pulse heating technique, electrical resistivity as a function of temperature is one of the inputs required in the solution of heat capacity. Thermal conductivity was calculated from the heat capacity, thermal diffusivity, and density of the materials. BMI used a steady-state longitudinal comparative method to measure the thermal conductivity of unhydrided fuel.

The materials investigated were the same as those used in the SNAP reactors. Several hydrogen compositions, however, were employed to study the effect of this variable in the thermal properties of the fuel. The materials were fabricated using natural isotopic uranium instead of enriched. The fuel alloy was triple-arc-melted and extruded to obtain homogeneous materials.

The nominal composition of the SNAP fuel alloy is 90 wt % Zr and 10 wt % U. The alloy was hydrided to the various H/Zr levels desired before machining into specimens. The hydrogen content of the specimens was maintained at constant level during the properties measurements by using hydrogen overpressures. Figure 68 shows typical photomicrographs of the specimens measured.

A schematic diagram of the thermal diffusivity apparatus is shown in Figure 69. A system, also shown in Figure 69, was constructed which completely eliminated temperature fluctuations and greatly improved sensitivity of gas pressure and control of gas purity. The operating temperature of the furnace was limited to $\sim 1100^{\circ}\text{C}$ when pressurized with a 10-atm pressure of hydrogen. Ultra-pure hydrogen gas (99.99⁺% purity) was used. The thermal diffusivity systems were calibrated, using standard Armco iron samples. The accuracy of the thermal diffusivity measurements are believed to be $<\pm 5\%$.

Measurements were made at AI on two samples of unhydrided material in the range of room temperature to 975°C . The results are shown in Figure 70. Two phase transformations are apparent from the graph, one at $595 \pm 10^{\circ}\text{C}$ and the second at $795 \pm 10^{\circ}\text{C}$. These phase changes are in good agreement with the $\delta + \epsilon \rightleftharpoons \gamma + \epsilon$ and the $\gamma + \epsilon \rightleftharpoons \gamma$ phase transitions reported in the literature for Zr - 10 wt % U.

6-13-66 6339-5-1
a. Unhydrided Material

6-13-66 5785-1-1
b. 0.50 H/Zr Material

6-13-66 5785-2-1
c. 1.26 H/Zr Material

6-13-66 6167-6-1
d. 1.58 H/Zr Material

6-13-66 6639-11-1
e. 1.81 H/Zr Material

6-13-66 6167-4-3
f. 1.90 H/Zr Material

Figure 68. Typical Microstructures of Specimens Fabricated for Thermo-physical Properties Measurements of SNAP Fuel

AI-AEC-13100

BLANK

6-13-66

7611-0583

a. Schematic

6-13-66

7611-4052

b. Apparatus

Figure 69. Thermal Diffusivity Apparatus

AI-AEC-13100

3-93

6-13-66

7611-4747

Figure 70. Thermal Diffusivity of Unhydrated Material

The results of thermal diffusivity measurements made on three samples of 0.50 H/Zr material in the range of room temperature to 800°C are given in Figure 71. Although sharp discontinuities in property data, such as those observed in the diffusivity at 390 and 510°C, often indicate a phase change in the material being studied, none of the three binary systems (Zr-H, U-H, or U-Zr) show phase changes at 390 or 510°C. The ternary U-Zr-H had only been studied in detail at temperatures above 600°C.

Data from measurements of 1.81 H/Zr material are shown in Figure 72. There is an inflection in the curve at $\sim 550^\circ\text{C}$, indicating that perhaps the $\epsilon \rightleftharpoons \delta + \epsilon$ boundary exists near this temperature for the 1.81 H/Zr material. The agreement between the AI and BMI data are extremely good.

The best thermal diffusivity values of the unhydrided alloy and the four hydride compositions, as functions of temperature, are shown in Figure 73. It can be seen that the thermal diffusivity goes through a minimum as the H/Zr ratio is increased from 0 to 1.90.

The potentiometric method of measuring electrical resistance was used. Photographs of the sample holder and the resistivity apparatus, as well as a schematic diagram of the electrical system, are shown in Figure 74. Four thermocouples extended into the sample area to monitor the sample temperature. The measurements on unhydrided fuel material and on the 0.50 and 1.26 H/Zr materials were made in vacuum, because at the temperatures of measurements, the dissociation pressure was negligibly small. All other compositions were measured with a balanced hydrogen pressure. The greatest uncertainty in the measurements, however, is in the composition of the material and in the ability to impose an accurate hydrogen balancing pressure. The total uncertainty is believed to be approximately $\pm 3\%$. The apparatus was calibrated, using a tantalum sample (99.9⁺% purity). The results were in excellent agreement with other reported measurements.

Results of measurements made on four unhydrided samples are given in Figure 75. The abrupt change in the ρ -vs- T curves at $\sim 590^\circ\text{C}$ (cf, $595 \pm 10^\circ\text{C}$ in thermal diffusivity measurements) indicates a phase transition, probably the $\delta + \epsilon \rightleftharpoons \gamma + \epsilon$ transition. The electrical resistivity is not a monotonic function of temperature above 590°C , because of the changing proportions of the γ and ϵ phases.

6-13-66

.611-4748

Figure 71. Thermal Diffusivity of 0.50 H/Zr Material

AI-AEC-13100
3-97

Figure 72. Thermal Diffusivity of 1.81 H/Zr Material

Figure 73. Summary of Thermal Diffusivity Data

a. Sample Holder

b. Apparatus

c. Schematic of Electrical System

6-13-66

7611-0575

Figure 74. Electrical Resistivity Apparatus

AI-AEC-13100

3-99

6-13-66

7611-4755

Figure 75. Electrical Resistivity of Unhydrided Material

AI-AEC-13100

3-100

The electrical resistivity measurements made on all compositions are shown in Figure 76 as functions of temperature. The break in the 0.50 H/Zr curve near 500°C is expected because of the eutectoid reaction in the Zr-H binary system. Systematic variation in electrical resistivity with H/Zr atom ratio and temperature are apparent. The electronic contribution to thermal conductivity was calculated for each fuel composition.

Heat capacity measurements were made using the pulse heating technique. Required data on electrical resistivity as a function of temperature were obtained from the electrical resistivity measurements. A computer program was written to simplify the calculation of specific heat. A photograph of the pulse heating apparatus, the test chamber with the vacuum bell removed, and a schematic diagram are shown in Figure 77. BMI employed the ice drop calorimeter for measuring enthalpy, from which heat capacity was derived.

Because some of the values for the specific heat of SNAP fuels, measured by the pulse heating technique, were surprisingly high, a complete instrumentation and method standardization program was conducted, using tantalum metal as the reference material. The hydride data were judged accurate to $\pm 5\%$.

Heat capacity measured results are shown in Figure 78. Values shown are weighted averages of AI and BMI results. Heat capacity was relatively independent of hydrogen content near room temperature. Discontinuities, similar to those in the electrical resistivity measurements, were observed.

Because of hydrogen mobility in a temperature gradient, only unhydrided samples were measured for thermal conductivity. BMI used a steady-state longitudinal comparison method to make the measurements. The thermal conductivity of hydrided and unhydrided compositions was calculated as the product of the heat capacity, thermal diffusivity, and density. The electronic contributions to thermal transport were calculated, using the Wiedemann-Franz-Lorenz relationship. The accuracy of the measurements was estimated to be $< \pm 5\%$. The accuracy of the calculated thermal conductivity values was $< \pm 11\%$.

Up to 600°C, measured and calculated values are in excellent agreement for the unhydrided material. There, the electronic thermal conductivity accounts for about 75% of the total thermal conductivity. At higher temperatures, the agreement was poor, probably due to overestimation in specific heat values at phase changes.

6-13-66

7611-4761

Figure 76. Summary of Electrical Resistivity Data

a. Apparatus

b. Test Chamber

LEGEND:

I = SAMPLE CURRENT	P = SYSTEM PRESSURE
E_1 = VOLTAGE DROP ACROSS SAMPLE LENGTH, l_1	T_F = FINAL SAMPLE TEMPERATURE
E_2 = VOLTAGE DROP ACROSS SAMPLE LENGTH, l_2	T_G = EVOLVED GAS TEMPERATURE

c. Schematic

6-13-66

7611-0576

Figure 77. Pulse Heating Apparatus

AI-AEC-13100

3-103

6-14-66

7611-4768

Figure 78. Summary of Heat Capacity Data

The thermal conductivities of the various materials are summarized in Figure 79 and the electronic thermal conductivities are summarized in Figure 80, both as functions of temperature.

The thermal conductivities of the materials increased with temperature, at least over most of the temperature range covered. This was opposite from published results for zirconium hydrides, of compositions $\text{ZrH}_{1.5}$, $\text{ZrH}_{1.3}$, and $\text{ZrH}_{0.83}$ containing no uranium. The difference in the temperature coefficients between the materials, with and without uranium, agreed with trends produced by combining the coefficients of the constituents.

Qualitative deductions of possible phase transformation in the Zr-U-H system were made from the results of the various measurements. These are summarized in Table 10.

Highly accurate thermophysical properties data on SNAP fuel materials were obtained in these experiments. Data on thermal diffusivity, heat capacity, thermal conductivity, and electrical resistivity were obtained as functions of temperature and hydrogen content. Particular attention was paid to hydrogen mobility in the advanced techniques employed. An anomaly remained in the 1.58 H/Zr material thermal conductivity, where calculated electronic contributions exceeded measured total values between 200 and 400°C.

4. Advanced Arc-Jet Tests⁽¹²⁾

Experimental support for SNAP fuel element reentry burnup analytical programs and associated fuel particle disintegration studies was provided by a series of ablation tests on actual fuel material (nonenriched uranium) in a partially simulated reentry environment. Data were obtained on the transient temperature and hydrogen effusion behavior, surface catalycity effects on heat flux, and ablating particle size distributions and compositions.

The test series evolved from the results of previous experimental programs and from the development of analytical descriptions of the fuel material thermochemical behavior. The testing was necessary to provide data for the use in, and verification of, a complete description of SNAP fuel behavior during reentry.

6-13-66 UNC

7611-4774

Figure 79. Summary of Thermal Conductivity Data

6-13-66 UNC

7611-4775

Figure 80. Summary of Electronic Thermal Conductivity Data

TABLE 10
DISCONTINUITIES OR INFLECTIONS IN PROPERTY vs
TEMPERATURE CURVES

H/Zr	Property [†]	Temperature (°C)	Phase Transition
0.00	ρ	585-600	} $\delta + \epsilon \rightleftharpoons \gamma + \epsilon$
	α	595 ± 10	
	c_p	560-600*	
	ρ	~800	} $\gamma + \epsilon \rightleftharpoons \gamma$
	α	795-800	
	c_p	790-815*	
0.50	ρ	> 475?	} $(\alpha + \delta \rightleftharpoons \alpha + \beta)$ and $(\alpha + \beta \rightleftharpoons \beta)$
	α	390-510	
	c_p	~400	
1.58	ρ	460-515	
	c_p	475-515	
	ρ	720-780	
	c_p	750-800	
	c_p	1150 ± 50	
1.81	ρ	560 ± 10	} $(\epsilon \rightleftharpoons \delta + \epsilon)$
	α	550 ± 10	
	c_p	~585	
1.90	c_p	850 ± 50	$(\epsilon \rightleftharpoons \delta + \epsilon)$

*BMI data

() Probable phase transformation

[†] ρ = electrical resistivity

α = thermal diffusivity

c_p = heat capacity

The important feature of the test series lay in the large number of specimens tested and the large number of flow conditions and configurations used. The abundant data permitted correlations and conclusions to be confidently made. Five groups of tests were designed.

The objective of Test Group I was to obtain data on the aerodynamic heat flux to certain materials, primarily zirconium and zirconium oxide, when catalytic effects are significant. These data were used in correlating the thermal history data obtained in the tests.

The purpose of Test Group II was to evaluate the radiant heat losses for simulated reentry temperature transients. The method of evaluation was based on a comparison of thermocouple and optical pyrometer measurements. This evaluation was needed to determine the effect of the surface condition (oxide buildup) on the fuel emissivity. A secondary objective for these tests was to provide an indication of the effect, on the ablation characteristics, of not rotating the cylindrical sample.

The primary purpose of Test Group III was to evaluate the effect of fuel element radius on the reentry behavior. This was needed because surface phenomena, as well as heat and hydrogen diffusion effects, depend on the specimen radius. Specimen diameters corresponding to those for SNAP 10A and SNAP 8 fuel elements were used. A second objective in this test group was to evaluate the effects of different types of specimen holders. Both completely solid specimens and specimens with a central tungsten holder rod were made.

Also, an attempt to photographically record hydrogen effusion from the fuel specimens was made to test the assumption, used in the analytical model calculations, that the hydrogen outflow is reasonably uniform over the surface and is diffusion-controlled. A laser-schlieren system was designed and fabricated for the measurement. Other objectives were the acquisition of data on fuel integrity, microcracking, and end heating effects, and the determination of the state (size, composition, etc.) of the particulate matter just as it leaves an ablating fuel specimen.

Test Group IV was designed to study the possibility of enhancing ignition and ablation of the fuel material by surface irregularities. Grooved or cross-hatched surfaces were tried.

The primary objective of Test Group V was to obtain photographic data on particle size distributions during fuel ablation. Laser photographic methods were designed to supplement normal film coverage.

The arc-jet test series was performed in the Rockwell International Los Angeles Aircraft Division hyperthermal, electric-arc wind tunnel, which is capable of producing nominal Mach-3 gas flows at extremely high temperatures.

A reference heat flux was obtained for a Newtonian velocity distribution of equilibrium air from values determined for stagnation enthalpy and pressure. The average stagnation enthalpy was determined from a heat balance on the arc head and the measured flow of gas. The stagnation pressure was measured directly with a pitot tube.

The test configuration for Test Group I was an asymptotic calorimeter, which consists basically of a thin constantan disk over a cavity in the heat sink of copper. The temperature difference between the center and periphery of the disk is directly proportional to the heat flux impinging on the surface of the disk, assuming a steady state, a constant thermal conductivity, and no gradient between the face and rear of the disk.

Materials were coated on the surface of the calorimeters. Coatings of 1 to 3 mils of stainless steel and zirconium metals and oxides were applied by spraying, and gold and silver by plating. The calorimeters were mounted in a water-cooled copper adapter as shown in Figure 81. The material used for all other test groups was nonirradiated, modified SNAP fuel material, ZrH_x -10 wt % U, with an H/Zr ratio of 1.8.

Test Group II specimens were cylinders oriented cross-axially in the jet stream and held stationary. The specimens were 1.25 in. in diameter and 2.00 in. long. A thermocouple was installed near the surface 90 deg from the front at the axial center.

Specimens for Test Group III were 2.0 in. long by 1.25 in. or 0.562 in. diameter, corresponding to SNAP 10A and SNAP 8 fuel elements. The cylinders were oriented cross-axially to the jet stream. The support for some specimens was a reduced-diameter extension of the fuel material, while for others it was a tungsten tube along the axial center. The support tubes were surrounded by

Figure 81. 0.625-in.-Diameter Calorimeter Mounted in Copper Adapter

Al_2O_3 annuli having the specimen outside diameter. For some specimens, the alumina tube was placed at both ends to assure two-dimensional flow.

Test Group IV specimens were the same as those for Test Group III, except that half of the length was grooved in one of four ways. Two such specimens are shown in Figure 82. Test Group V specimens were similar to those of Group II without the thermocouple hole. Specimens, 0.562 in. in diameter by 3.0 in. long, were mounted axially parallel to the jet stream for Test Group V also; the exposed end was flat-faced.

For every test, the pitot tube was inserted in the jet stream to measure stagnation pressure while flow data were simultaneously recorded. The tunnel setup for Group I is shown in Figure 83.

Test Group II was photographed with a 24-frames/sec motion picture event camera, and the black-body surface temperature was measured with an optical pyrometer. The thermocouple operated on only 3 test runs. Film coverage at 400 frames/sec was obtained for one test run. Specimens were rotated at 120 rpm.

9-8-65

7611-5175CN

9-8-65

7611-51114CN

Figure 82. Arc-Jet Test Configurations IVC and IVD

10-22-65

7611-01002

Figure 83. 0.125-in.-Diameter Calorimeter Installed in Holder and Lowered to Test Position in Tunnel

The primary setup for the first ten runs of Test Group III consisted of a double-pass, laser-schlieren system. Motion picture coverage and pyrometer readings were obtained. A particle catcher was used in half the additional 18 runs made. The catcher was a solid aluminum block, 1/2 in. thick by 2-1/2 in. square, with a concave front face. It was inserted behind the sample at the onset of ablation and removed when an adequate sampling had occurred. Motion pictures and pyrometer readings were made.

The test run procedure for Test Group IV was the same as for Test Group III. An optical pyrometer was focused on the grooved side of the specimen as well as on the smooth side. Motion pictures were made.

Schlieren photography was obtained for Test Group V runs along with motion picture and pyrometer data. Schlieren particle photographs were taken every 2 sec. Some thermocouple output was recorded.

Health, safety, and security procedures developed in previous arc-jet test series were employed.

The test conditions employed are compared with SNAP fuel element reentry conditions in Figure 84 (equilibrium flow theory). The heat flux was measured with a cross-axial calorimeter in the shape of a sample. Five heat sensors were distributed on the surface to observe nonuniformities. Runs were made for for all test conditions. The results, shown in Figure 85, are somewhat lower on the backside than those from previous analysis and experiments. The average-to-stagnation point heating ratio, for a rotating cylinder in cross-flow, was calculated to be 0.31.

Figure 84. Stagnation-Pressure, Enthalpy, Heat-Flux Diagram Based on the Fay and Riddell Equation

Figure 85. Local-to-Stagnation Point Heating Ratio
Around the Periphery of the Cross-Axial
Calorimeter

For conditions of high stagnation enthalpy, the axial heating was fairly uniform. Average axial heating rates were used to correlate temperature response data for these cases. For other conditions, the axial heating profile was pronounced, dropping to 0.2 to 0.35 of the center at the ends. Measured heating rates were higher than Fay-Riddell calculations by up to 52%, the discrepancy increasing with the severity of the axial heating profile. The discrepancy for relatively uniform shapes was small ($\sim 8\%$).

In Test Group I, a significant variation in heat flux was found between materials. The ratio of the measured heat flux to the theoretical heat flux for an equilibrium boundary layer varied from 0.44 to 1.35. The differences were attributed to different catalytic efficiencies. Results are given in Figure 86. The test conditions differ somewhat from those of reentry, in that the jet air is about 40% dissociated prior to expansion through the nozzle. Thus, the reentry problem of a nonequilibrium shock layer provides only a secondary effect in the wind tunnel.

5-3-67

7700-25164

Figure 86. Measured-to-Theoretical Heat-Flux Ratio for Coated Asymptotic Calorimeter Tests

In Group II tests to determine emissivity, the temperature difference between the thermocouple and optical pyrometer temperature measurements were much larger than anticipated. This was found to be due to a combination of thick oxide buildup, poor thermocouple contact caused by fuel shrinkage, void formation, and a distance from surface to thermocouple larger than desired. These problems made emissivity derivation difficult and uncertain. These stationary specimens ablated much less rapidly than corresponding rotating ones, which may have been due to a larger oxide buildup (52 mils vs 13 to 32 mils).

The fuel thermochemical behavior in Test Group III was outlined by the following film observations, which summarize the results of the motion picture film analysis:

- 1) Heavy zirconium-oxide buildup occurred at the surface of all specimens. Except for those tests in which ablation occurred relatively soon after insertion, this oxide buildup resulted in flaking and spalling of the oxide at high temperatures. Typical flaking is pictured in Figure 87.
- 2) Hydrogen outgassing was visually evidenced in the 24-frame-sec film data for many of the test conditions used. The outgassing appeared for the most part to be uniform and continuous along the stagnation line. However, hydrogen was seen emitting profusely from one or several open cracks in the fuel material in some runs.
- 3) The occurrence of large, radial cracks appeared to be confined to configurations with tungsten rod holders. Completely solid specimens developed a fine network of microcracks, but were rarely noted to incur large, radial cracks. The large cracks influenced the manner in which the specimens ablated. These specimens usually ablated in a piecemeal fashion, while the integrity of the solid fuel specimens was maintained up to ablation through the center.
- 4) The oxide layer was found to be tenacious enough to inhibit fuel ablation until a substantial portion of fuel material became molten underneath the oxide layer. Ablation sequences were always characterized by the expulsion of molten fuel material out of cavities or gaps in the oxide layer, as in Figure 88.

Figure 87. Arc-Jet Test Run No. 3-25 (400 Frames/sec Motion Picture Film)

4-28-67 7700-25176
 Figure 88. Arc-Jet Test Run No. 3-16 (400 Frames/sec Motion Picture Film)

- 5) An uneven distribution of heating along the axial specimen length was evident in many test runs as indicated by ablation being consistently initiated at the center.
- 6) The 400-frame/sec motion picture film was found to be highly effective in recording the details of the ablation sequence.

Attempts to photograph hydrogen escape from the fuel specimens by means of a laser-schlieren system were inconclusive. Images of two shock waves in front of the specimen were seen in some photographs. These were tentatively identified as the usual bow shock wave and the boundary of the hydrogen gas escaping from the specimen.

The optical pyrometer readings were converted, for an assumed surface emissivity of 0.4, and plotted as functions of time with the use of special computer programs. The data for all tests run at any particular test condition were converted and plotted on the same graph for the same specimen size. A typical result is shown in Figure 89. Data reproducibility was good, within any test condition, at low and intermediate temperatures. The data scatter is a maximum at high temperatures where the oxide layer effects dominates.

Correlations of the pyrometer data were made, using the analytical computer models which simulate the thermochemical behavior of the fuel specimens. The measured heating rate inputs to the model were not corrected to account for noncatalytic wall effects. Good correlation with the computer-plotted experimental data points was obtained for several test conditions, while the correlation was poorer when thermal response was sharper. The measured thermal response lagged behind that of the analytical. Heating reductions due to noncatalytic wall effects, expected on the basis of the coated calorimeter test results, offered a reasonable explanation for the observed deviations.

To help determine the extent of noncatalytic effects under the specific conditions of reentry, a preliminary analysis was performed. The evaluated heating reductions were examined in terms of the governing parameters of boundary layer flow. Both nonequilibrium and completely frozen flows were considered. The measured heating reductions correlated with the effective catalytic efficiency and the gas phase recombination parameter, which controls nonequilibrium

Figure 89. Comparison of Calculated to Measured Surface Temperatures
(Arc-Jet Runs Nos. 3-02, 3-03, and 3-04)

boundary layer theory, but did not correlate reasonably with frozen boundary layer theory. This substantiated surface catalycity as the likely cause of the heating reductions.

Three particle catchers after Group III Test runs are shown in Figure 90. The material collected on three of the catchers was chemically analyzed. Results are given in Table 11. The major material in the analysis was alpha zirconium, being void of hydrogen, and the minor compound zirconium oxide.

Three catcher blocks were selected for the determination of the size distribution of particles. An area enlarged to 40X for evaluation and analysis is shown in Figure 91. Examination of the enlarged photographs yielded particles of four general types: (1) small, well defined spheres which appear to have been solidified at the time of impact with the catcher; (2) large particles with craters in the center, which appear to have been partially solidified upon impact; (3) large irregular-shaped particles; and (4) splatter from the larger particles and from large pieces of molten mass.

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Figure 90. Particle Catchers after Group III Test Runs
(Arc-Jet Test Runs Nos. 3-21, 3-23, and 3-25)

TABLE 11
CHEMICAL ANALYSIS RESULTS FROM PARTICLE CATCHERS

Test Run and Catcher Number	Relative Size and Location of Samples	wt % U in Sample	Compounds Observed
3-16	1. Large piece. Top of catcher face.	10.4	α Zr only
	2. Large piece. Bottom of catcher face.	11.4	α Zr plus a small amount of monoclinic ZrO_2
	3. Small pieces. Taken at random.	10.4	α Zr plus an unidentified compound
3-23	1. Large piece. Top of catcher face.	10.5	α Zr plus appreciable tetragonal ZrO_2 plus some monoclinic ZrO_2
	2. Large piece. Bottom of catcher face.	10.0	α Zr plus some ZrN
	3. Small pieces. Taken at random.	—	α Zr plus unidentified compound (same as 3-16)
3-25	1. Large piece. Top of catcher face.	8.6*	Some oxides* of Zr
	2. Large piece. Bottom of catcher face.	9.7	α Zr plus ZrN and a small amount of tetragonal ZrO_2
	3. Small pieces. Taken at random.	10.1	α Zr

*Questionable due to geometry of sample surface

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Figure 91. Particle Catcher No. 3-16, Area 2-1 (19X Enlarged to 40X)

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The data obtained from these particle catchers was limited due to the splat-
tering of molten material, the craters created upon impact causing irregularities
in particle shape, and the piling up of material on large portions of the surfaces.

Metallurgical analysis was performed on some fuel specimens which did not
ablate to determine oxide thickness and the state of the material adjacent to the
oxide. Specimens were cross-sectioned at the center and macrographs were
taken. Oxide layers up to 68 mils thick were found. Cracks and void areas
were also found. Examples are shown in Figures 92 through 94.

The film data from Test Group IV indicate that fuel ablation is not enhanced
by surface irregularities. The radiative heat losses apparently increased at
least as much as the net surface heat input.

In 3 of the 13 test runs of Test Group V, laser photographic determination
of the size distribution of ablating particles was successful. Figure 95 is com-
posed of sequence particle photographs taken during an end-on exposure. The
continuous traces of many particles can be seen as well as discrete particles
of various sizes whose flight has been stopped by the short laser flash. On the
back portion of the cylinder, numerous cracks and splattered droplets are
clearly distinguishable.

The ablation of the cross-axial exposed specimens was quite unlike that of
end-on exposure, in that they ablated in a "stop and go" manner while the end-on
specimens ablated smoothly. Figure 96 shows the ablation event sequence from
motion picture film taken during a stationary side-on exposure.

The particles from each useful photograph taken were carefully measured
and counted. From the total number of particles counted and measured for each
test run, the mass was computed assuming spherical geometry and a density
($\rho_{H/Zr=0}$) of 6.95 gm/cc. The resulting distribution for end-on exposure is
plotted in Figure 97. The peak of the distribution for the end-on test runs occurs
in the approximate particle diameter range of from 575μ to 650μ and that for the
cross-axial test run occurs from 410μ to 490μ . This difference was attributed
to the erratic ablation of the cross-axial orientation.

In general, these tests were characterized by heavy zirconium oxide buildup
at the surface, usually resulting in flaking and spalling; uniform and continuous

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Figure 92. Macrograph of Specimen Configuration IIIB1 (Arc-Jet Test Run No. 3-05)

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Figure 93. Macrograph of Specimen Configuration IIIA1 (Arc-Jet Test Run No. 3-12)

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Figure 94. Macrograph of Sample 2-04 Cross-Sectioned at Center (Configuration II)

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Figure 95. Laser Particle Photography for Arc-Jet Test
Run No. 6-05 (Film Frames No. 3-8)

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Figure 96. Arc-Jet Test Run No. 2-08 (24 Frames/sec Motion Picture Film)

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Figure 97. Particle Distribution for Arc-Jet Test
Runs Nos. 6-05 and 6-06

hydrogen outgassing along the surface; a fine network of microcracks in solid specimens; inhibition of fuel ablation by an oxide layer; and absence of corner heating effects. It was determined that fuel ablation was not enhanced by surface irregularities, and that heating reductions due to noncatalytic fuel surface effects were such that the effects of nonequilibrium chemistry on fuel burnup predictions could not be ignored. Also, the composition and size distributions of ablating particles were determined.

5. Particle Ablation Model

A digital computer code, PAM-6, was written to describe the behavior of liquid metal particles in both reentry and laboratory environments, using the best available analytical tools. Heat, mass, and momentum transfer mechanisms were described. PAM-6 provided a means of studying particles formed from the reentry ablation of SNAP reactor fuel elements and for providing analytical support to SNAP fuel particle disintegration experiments. The code determined the temperature, radius, and velocity of a particle as functions of time. PAM-6 was the last of a series of analytical models of increasing sophistication.

The particle was assumed to be initially at a temperature high enough for oxidation by a limited oxygen supply (linear oxidation). The particle was heated by aerodynamic and oxidation heating and cooled by convection, radiation, and evaporation. The oxide of the particle was assumed to form as a coating, which was assumed to evaporate, followed by evaporation of the unoxidized material, if exposed. Effects of vapor blocking and determination of the final fission product inventory were optionally calculated. Fission products were assumed to evaporate directly with the oxide, followed by fractional distillation from the unoxidized material, if exposed.

Reentry aerodynamic heat transfer could be calculated in five ways: (1) cold-wall, free-molecular heating; (2) the lesser of hot-wall, free-molecular heating and the aerodynamic heating described by the Detra-Hidalgo relationship; (3) the Detra-Hidalgo relationship, if the Stanton number is less than 0.5, otherwise using a modified Stanton number based on a fillet relationship; (4) hot-wall, free-molecule heating; or (5) using a modified Stanton number which includes vapor blocking effects. Four options for describing reentry oxygen mass transfer were

incorporated. These options correspond to options 1, 2, 3, and 5 for aerodynamic heat transfer. Mass transfer due to vaporization was expressed by the Langmuir relationship. Deceleration of the particle from momentum transfer between the particle and the atmosphere was calculated from the drag-force equation.

For laboratory tests, the aerodynamic heating term was replaced by a convective heating term determined from heat transfer for spheres, assuming the constant value for the Prandtl number of argon at 212° F. Other equations parallel those for the reentry case. The difference in mass transfer between reentry and laboratory environments was in the relationships for the mass fluxes.

A simple fission product inventory model having the following assumptions was included:

- 1) The particle is a liquid metal droplet with a thin oxide coating.
- 2) The oxide is a solid and is impervious to fission products.
- 3) As the oxide vaporizes, the same fraction of fission products and uranium vaporize as are present in the total particle.
- 4) The diffusion coefficient of the fission products within the liquid fuel material is infinite.
- 5) All fission products vaporize from exposed unoxidized fuel material according to their own vapor pressure and the Langmuir relationship.

For the most part, these assumptions are conservative in relation to fission product removal.

Since the physical properties of the material were input variables, PAM-6 could be used in the study of most any particle. Incorporation of a newer particle aerodynamic trajectory (see Section B.6) with variable drag coefficient and changing air density would improve the model. Estimates of the diffusion of oxygen through zirconium and zirconium-oxide have become available, which would improve the description of oxidation kinetics at low temperatures.

6. Particle Ablation Analysis

a. PAM-6 Calculations

A statistical analysis was made of the behavior of reentering fuel particles from ablating SNAP 10A and SNAP 8 fuel rods, using the PAM-6 code. Sensitivity coefficients and uncertainties in the final radius due to individual parameters were calculated.

The physical properties of the particles were taken from various measurements. Initial altitudes, velocities, and size ranges were taken from the arc-jet experiments. Since little data on the physical properties of molten SNAP fuel alloy existed, data of the solid state were frequently used. Data for zirconium were used when information for the fuel material was unavailable.

Several sets of starting conditions were considered. The radii of droplets leaving the ablating rods were assigned ranges from 250 microns, based on arc-jet results, to the critical radius of stability, determined from a surface tension and instability study. Initial particle conditions and final radii ranges calculated by PAM-6 are given in Table 12.

Sensitivity coefficients, namely the ratio of the percentage change in the final radius to the percentage change in the parameter, were calculated for a 525-micron particle released at 177,000 ft with an initial velocity of 14,200 ft/sec. Uncertainties, based on estimated 2σ deviations of the various parameters, were derived from the coefficients assuming they were constant functions of the initial parameters. The results are given in Table 13. By far the most important parameter in determining the final particle radius was the initial radius. Work to better define the initial particle size distribution was recommended.

The standard deviation of the final radius was found using the Law of Propagation of Errors. The results for the various reentry modes are given in Table 14.

b. PATH Calculations

A special purpose trajectory code, PATH, was written to compute the aerodynamics and heating of reentry bodies with small ballistic parameters, such as ablation particles for flight angles greater than 10 deg. The code was used to study parametrically the heating regimes important for nonablating spherical particles.

TABLE 12
INITIAL PARTICLE CONDITIONS AND FINAL RADII RANGES

Mode of Element Reentry	Particle Release Altitude (kft)	Velocity (ft/sec)	Free-Stream Particle Radii Range (microns)	Final Particle Radii Range (microns)
SNAP 10, Cross-Axial	228	21,600	250 to 2080	321 to 2343
	213	19,776	250 to 1428	307 to 1297
	197	17,500	250 to 990	304 to 1118
	186	15,600	250 to 902	280 to 1046
SNAP 10, Random Tumbling	223	22,300	250 to 1650	309 to 1829
	210	21,000	250 to 1090	296 to 1212
	196	19,100	250 to 730	293 to 799
	185	17,900	250 to 595	278 to 662
SNAP 8, Cross-Axial	247	22,269	250 to 4450	365 to 4905
	227	20,723	250 to 2605	352 to 2814
	204	16,835	250 to 1370	309 to 1553
	192	13,958	250 to 1280	298 to 1488
SNAP 8, Random Tumbling	247	23,008	250 to 4450	374 to 4974
	227	21,638	250 to 2605	352 to 2952
	204	18,832	250 to 1098	314 to 1494
	192	16,874	250 to 878	308 to 964

TABLE 13
DEVIATIONS IN FINAL RADIUS DUE TO DEVIATIONS
IN PARAMETER VALUES

Order of Sensitivity	Parameter	Sensitivity Coefficient	Parameter 2σ Uncertainty (%)	Radius 2σ Uncertainty (%)
1	Initial radius SNAP 8 cross-axial reentry	0.96	77.4	71.4
2	Initial radius SNAP 8 random-tumbling reentry	0.96	71.2	68.4
3	Initial radius SNAP 10 cross-axial reentry	0.96	66.3	63.6
4	Initial radius SNAP 10 random-tumbling reentry	0.96	56.8	54.5
5	Oxide emissivity	0.12	50	6
6	Release altitude SNAP 10 random-tumbling reentry	0.70	4.9	3.4
7	Release altitude SNAP 10 cross-axial reentry	0.70	4.6	3.2
8	Initial oxide fraction	0.022	100	2.2
9	Initial velocity	-0.17	10	1.7
10	Metal density	0.31	5	1.55
11	Release altitude SNAP 8 cross-axial reentry	0.70	2	1.4
12	"A" coefficient in oxide vapor pressure relationship	0.36	3.7	1.33
13	"B" coefficient in oxide vapor pressure relationship	0.44	2.7	1.18
14	Release altitude SNAP 8 random-tumbling reentry	0.70	1.5	1.05
15	Heat of oxidation	-0.082	10	0.82
16	Oxide density	-0.37	2	0.74
17	Particle specific heat	0.07	50	0.35
18	Model (PAM-6)	0.003	100	0.33
19	Heat of oxide vaporization	0.026	10	0.26
20	Gamma (ratio of specific heats)	0.24	0.8	0.19
21	Specific heat of air	0.03	5	0.15
22	Initial temperature	0.026	6	0.135
23	Air temperature	0.0008	100	0.08

TABLE 14
2 σ UNCERTAINTY IN FINAL RADIUS

Type of Fuel Element	Mode of Fuel Element Reentry	Average 2 σ Uncertainty (%)
SNAP 8	Random-tumbling	68.8
SNAP 8	Cross-axial	71.8
SNAP 10	Random-tumbling	54.6
SNAP 10	Cross-axial	64.0

The equations of motion in the Earth's force field and aerodynamic heating and drag equations were solved by the code. The heating was classified into the free-molecule, transition, and continuum regimes. The transition regime heating rates were based on the Stanton number fairing method developed at AI.⁽¹³⁾ A drag coefficient dependent on the flow regime was used. The coefficient varied continuously with the ratio of the particle radius to the mean free path.

A parametric study was performed to indicate the sensitivity of particle dimension and release conditions on the percent of total aerodynamic heating received in the transition regime. Results for particle radii from 500 to 1500 microns, release altitudes from 200,000 to 275,000 ft, and initial flight angles from 0 to -90 deg are given in Figure 98. Release velocity, providing it was hypersonic, did not affect the relationships. Transition regime aerodynamics and heating were shown to be particularly important when evaluating particles released from SNAP reactor systems during reentry.

7. Particle Ablation Experimental Requirements

Needs were identified for experiments to: (1) better define the distribution of particles from the final ablation of SNAP fuel elements in terms of size, composition, density, and shape; and (2) investigate methods of enhancing the breakup of fuel into submicron particles. Results of such experiments would enable reduction and more accurate prediction of the potential fallout hazard

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Figure 98. Spherical Particle Transition – Continuum Heating Relationship

from a reentering SNAP reactor. Guidelines for an experimental program were set forth. In addition, experimental analysis of the oxidation of reentering particles was recommended. Several experimental techniques for simulating the oxygen mass flux to the surface were investigated and a fuel particle experiment was outlined.

Complete simulation of the reentry conditions was found to require consideration of several basic parameters. No single technique or facility would replicate the values of all parameters. The available methods were surveyed and evaluated. Arc-jet wind tunnel facilities were recommended for simulating the high enthalpy processes which occur during the primary stages of fuel-particle ablation under relatively high-speed gas flows of long duration. The principal limitations identified were in observing the mechanisms and processes of ablation, in obtaining particle samplings, and in contamination of the gas stream from electrode erosion. However, still and motion picture studies could be made and particle collection devices employed, which would provide data input for more specific experimental methods. Then, supplemental methods which subject precisely formed particle sizes to heat and further breakdown (secondary ablation) could extend the study in controlled gas environments and heat conditions. Flash or laser heating methods, as well as plasma torches and furnaces, could be used. These tests would allow more careful observation of disintegration mechanisms and better particle samplings.

Hypersonic wind tunnels at high Mach numbers would be needed to duplicate reentry motion. A hypervelocity, free-flight facility could be used by having a high-muzzle-velocity, light-gas gun fire a model through a countercurrent gas stream.

Typical particles from ablating fuel were shown to reach a temperature of about 5500°F in a few hundred milliseconds. This temperature is about 2200°F above the melting point. Effects of surface tension reduction on particle stability, oxidation and nitriding rates (including possible particle explosion), and fission produce release rates at this high temperature were reviewed and found to be in need of measured data. Key experimental parameters were found to be oxygen mass flux and the temperature transient. Experiments of the aforementioned supplemental type were recommended to provide the detailed observation of particle behavior required.

A porous cup technique was investigated for freely suspending a single particle within a very small space. Cones of high-porosity graphite were used. The particle did not appear to contact the cup walls. Extension of a successful electromagnetic technique to smaller particles was judged feasible. Forces from induced currents in the liquid metal are balanced against gravity to produce a stable location. Mixing within the particle would occur.

A recommended apparatus for a particle disintegration experiment is shown in Figure 99. An optical thermometer and a photographic film densitometer could both measure the temperature. A laser beam would be the heat source. Particle radioactivity measurements would be possible with this setup.

Figure 99. Apparatus Schematic – Particle Disintegration Experiment

C. CRITICAL CONFIGURATIONS

1. Water Immersion Experiment (SCA-4B)

The overall objectives of this program were to determine the reactivities of SNAP 10A- and SNAP 8-type reactor cores immersed in water, and to test the effectiveness of devices designed to maintain subcriticality.

In initial experiments, the excess reactivity of a water-reflected, bare SNAP 10A-type core was determined to be $\$6.4 \pm 0.6$. The additional worth of internal water was estimated to be $\$7.2 \pm 0.7$. Simple poison sleeves surrounding the core were found to render the water-reflected dry core subcritical, but were not adequate to prevent criticality in the water-flooded-and-reflected case.

In subsequent tests, it was demonstrated that:

- 1) A combination void and poison sleeve (designated a shipping sleeve), surrounding the bare core, was capable of maintaining SNAP 10A- and SNAP 8-type reactor cores subcritical when they were immersed in and internally flooded by water.
- 2) Combination void and poison filler blocks that fill the four control drum voids and cover two radial beryllium faces of the reactor are capable of maintaining a beryllium-reflected SNAP 10A-type reactor subcritical when it is immersed in, but not internally flooded by, water.

a. Initial Water Experiments - SNAP 10A Core⁽¹⁴⁾

Equipment was designed and used to experimentally measure the reactivity effects of water on reactor cores of the SNAP 10A type. The experiments started in February 1962 in the SNAP 2 Environmental Test Facility.

The test apparatus consisted of an effectively infinite water reflector contained in three removable sections. A photograph of the critical assembly machine is shown in Figure 100. A schematic diagram of the core vessel and water tank assembly is shown in Figure 101. The system is described in detail in References 14, 15, and 16.

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Figure 100. SCA-4B Critical Assembly Machine — Lower Tank in Lowered Position

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Figure 101. Schematic Diagram of Core Vessel and Water Tank Assembly

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The SNAP Critical Assembly -4B (SCA-4B) reactor core contained 37 cylindrical fuel-moderator elements. The rods were clad with Hastelloy-N, with end caps and grid plate indexing pins at each end. The fuel-moderator was the δ -phase hydride of Zr + 10 wt % of 93.2% enriched uranium alloy.

The core fuel-moderator material had a volume of 520 in.³ (8540 cm³) and contained 4.77 kgm of U²³⁵ and 464 gm-mole of hydrogen. The fuel-moderator for the critical experiments was made up of SNAP 2 Development Reactor (S2DR) fuel elements. The overall fuel length was 12.225 in. Each fuel-moderator piece was 1.212 in. in diameter. The fuel-moderator pieces were contained in Hastelloy-N tubes of 0.011-in. wall thickness. The OD of the clad rod was 1.250 in.

The 37 elements were arranged on 1.260-in. centers in a triangular array. The resulting reactor core was a hexagonal right cylinder, 7.80 in. across the flats and 8.81 in. across the corners by 12.225 in. long. The fuel rods were supported and held laterally by aluminum grid plates, 0.375 in. thick at the top and 0.312 in. thick at the bottom.

During the series of experiments with water in the core, the interstices of the core were filled with water. During the series with the core voided, the interstices were filled with Styrofoam spacers. The Styrofoam, having a density less than 2 lb/ft³, had a calculated total reactivity effect of about 10¢ with respect to a void.

The core vessel was a 0.032-in. cylindrical shell of Type 316 stainless steel. Beryllium filled the space between the hexagonal core and the core vessel. Figure 102 shows a diagram of the core region of the assembly.

(1) Basic Configurations

Preliminary experiments determined the characteristics of water-immersed assemblies. Criticality of the water-flooded-and-reflected assembly was established at 24.5 ± 0.2 fuel rods, with vacant lattice positions filled with Lucite rods. With water reflection only (no water flooding), the critical loading was 30.95 ± 0.05 fuel rods, with Lucite rods in the vacant lattice positions.

From the critical water-reflected configuration, additional fuel rods were singly substituted for Lucite rods while the amount of water reflection was

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Figure 102. Core Configuration of Critical Assembly

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reduced. The incremental reactivity effect of each rod loading was determined, using pulse neutron techniques. A final critical configuration of 36 fuel rods and 1 Lucite rod was obtained with 2/3 height radial water reflection and axial reflection on one end. From these experiments, the average incremental rod worth was estimated to be \$1.1. The excess reactivity of the water-reflected, fully loaded core was estimated as $\$6.4 \pm 0.6$.

Inferring the worth of the core water from the difference in just-critical fuel loadings and the value of \$1.1/fuel rod gave a reactivity worth of $\$7.2 \pm 0.7$. An independent measurement based on pulsed neutron decay of the void core gave $\$7.4 \pm 0.1$.

The fuel loading used in the water immersion tests duplicated the general nuclear characteristics of both SNAP 10A and SNAP 2 reactors. However, excess reactivity requirements of the SNAP 10A flight system are such that the flight reactor is approximately \$2.00 less reactive than the core studied in the initial water immersion tests.

(2) Borated Water and Initial Sleeve Studies

A neutron poison of natural boron in a salt of $\text{NH}_4\text{B}_5\text{O}_8 \cdot 4\text{H}_2\text{O}$ was added to the flooding and reflecting water. A boron concentration in water of 3.33×10^{20} atoms/cm³ was sufficient to reduce the reactivity of the fully loaded assembly to critical.

The reactivity effects of two types of poison sleeves surrounding the core vessel were studied. These were: (1) natural boron powder, packed to 22% of theoretical density, canned in 0.032-in. -thick Type 304 stainless steel; and (2) Binal, containing 10.2 wt % of natural boron in the form of a powder metallurgy dispersion of B_4C in an aluminum matrix. The reactivity effects of these two materials were identical at 0.25-in. thickness. The addition of Binal in 0.25-in. increments to a total of 1.0 in. resulted in a maximum reduction in reactivity at 0.75 in. This maximum reactivity with increasing sleeve thickness results from a greater reflection of high-energy neutrons, compared to the increase in absorption of neutrons reflected by the surrounding water.

When surrounded by any of these poison sleeves, the water-reflected core was far below critical. However, the water-reflected-and-flooded core, when surrounded with the most effective of these sleeves (0.75-in. Binal), was

critical at 34.4 ± 0.1 fuel rods. Lucite rods filled the vacant lattice positions. Measurements of incremental rod worths to determine the excess reactivity of the fully loaded core yielded a best estimate of 1.6 ± 0.5 for the excess. The results of the initial experiments are summarized in Table 15.

These experiments gave the first information on the reactivity effects of water environments on the SNAP 10A core. The simple poison sleeves proved inadequate in preventing criticality from combined water immersion and flooding.

b. Shipping Sleeve and Filler Block Experiments⁽¹⁷⁾

In mid-October 1962, additional experiments were initiated to demonstrate the capabilities of poison and void shipping sleeves and filler blocks. The SCA-4B critical machine was the same as that used in the initial experiments.

(1) SNAP 10A Shipping Sleeve Evaluation

The purpose of the shipping sleeve is to protect against nuclear accidents during shipping and handling operations. Nuclear criteria of the shipping sleeves are to protect against criticality from simultaneous water-flooding and immersion of the bare (without external beryllium reflector) core. Meeting the requirement permits complete assembly and nuclear checkout of the reactor systems at established nuclear laboratory facilities. After complete nuclear checkout of the reactor system, the shipping sleeve would be substituted for beryllium reflectors and the reactor system safely transported to launch facilities. A simple external radial sleeve design permits easy assembly and does not require any modifications in the reactor design at the launch facilities. The beryllium reflectors would be accurately reinstalled with no nuclear checkout required. Thus, all nuclear operations could be performed at a single facility prior to shipment to the launch site.

Mechanical criteria required a sleeve design that would maintain its integrity, to the extent of always meeting nuclear criteria during possible accident conditions. Integrity during fire was considered adequate if the melting points of the sleeve materials were comparable to that of stainless steel core construction. Integrity after impacts following a 15-ft free fall onto concrete is considered adequate.

TABLE 15

CRITICAL LOADINGS AND EXTRAPOLATED EXCESS REACTIVITIES OF CONFIGURATIONS
WITH AN EFFECTIVE INFINITE WATER REFLECTOR (6 in.)

Assembly Configuration					Critical Loading (number of fuel rods)	Extrapolated Excess Reactivities, Fully Loaded (37-Rod) Core (dollars)
Lucite Rods in Vacant Lattice Locations	Beryllium Inserts	Internal Water	Binal Annulus (10.2 wt % B 0.75 in. thick)	Ammonium Pentaborate in Water (3.33×10^{20} atoms B/cm ³)		
		X			27.8 ± 0.1	
X*	X	X			24.5 ± 0.2	13.7 ± 1.3
X†	X	X	X		34.4 ± 0.1	1.6 ± 0.5
	X	X		X	37.0 ± 0.1	0.0 ± 0.1
X§	X				30.95 ± 0.05	6.4 ± 0.6

*In locations 16, 23, 24, 29, 30, 31, 32, 33, 34, 35, 36, and 37 of Figure 102

†In locations 35, 36, and 37 of Figure 102

§In locations 32, 33, 34, 35, 36, and 37 of Figure 102

The particular radial sleeve design evaluated was a 3-in. simulated void, followed by 1 in. of natural boron powder, followed by 1/4 in. of steel. The simulated void and boron powder container were constructed of Type 304 stainless steel, and these two regions provided the nuclear shutdown. The void region was divided into a number of watertight regions so that accidental damage to the sleeve would not necessarily flood the entire void region. The simulated void contained approximately 2.75% steel volume, and the boron powder was packed to approximately 20% theoretical density. Protection from puncture or distortion was provided by the 1/4-in. steel shell.

With the void sleeve in place and with full water reflection and internal flooding, the fully loaded core was critical with 12.41 in. of water above the bottom of the fuel. Since the SCA-4 fuel was known to have an excess reactivity exceeding flight system requirements, the core reactivity was reduced by substituting rods of lower hydrogen concentration. With five special fuel elements loaded, the assembly was subcritical with full water reflection and internal flooding. In this configuration, the worth of cadmium sheets over the ends of the void sleeves were found to be approximately -15¢ to -30¢. Similarly, the worth of the 1/4-in. -thick steel sleeve surrounding the poison sleeve was found to be approximately +15¢ to +30¢.

The foregoing results were instrumental in establishing the following shipping sleeve design criteria:

- 1) The construction material should be Type 304 stainless steel.
- 2) The first layer should be a minimum 3-in. simulated void containing less than 3.75% nonvoid.
- 3) The second layer should consist of 1-in. -thick boron powder packed to 20 to 22% of theoretical density.
- 4) The third layer should be less than 1/2 in. of steel.
- 5) Axial ends of voids should be covered with a 1/4-in. -thick layer of boron powder followed by 1/4 in. of steel.
- 6) The boron powder container and the surface of the simulated void next to the core vessel should have a thickness less than 0.031 in.

- 7) The sleeve should extend the total length of the fuel rods.
- 8) The maximum allowable circumference of the core not covered by the sleeve should be 1/4 in.

A shipping sleeve, designed and fabricated according to the experimentally established criteria, was tested, using flight system fuel, designated as S10FR-1. The purpose of this test was the demonstration of sleeve effectiveness in maintaining the fully water-reflected-and-flooded flight system core subcritical. A cutaway diagram of this sleeve is shown in Figure 103. The shipping sleeve tested is shown in Figure 104. The core vessel is entirely below the top tank and completely surrounded by the shipping sleeve.

With the shipping sleeve in place, a full 37-fuel-element core loading was attained with the assembly remaining subcritical with full water reflection and flooding. The average value of extrapolated critical loading obtained from inverse multiplication plots of data from three neutron detector channels was 41.8 ± 1.0 fuel elements. Using a value of 55¢ per rod determined from another test, the subcritical reactivity was determined to be $\$2.64 \pm 0.55$.

These results proved the adequacy of the shipping sleeve concept.

(2) SNAP 10A Filler Block Evaluation

Experiments were performed with the basic SNAP 10A flight reactor reflector design to evaluate the poison filler block concept.

The purposes of the filler blocks were to: (1) protect against criticality from reflection of neutrons by handling personnel, (2) prevent accidental insertion of the beryllium control drums, and (3) protect against criticality if the beryllium-reflected reactor (with drums out) should be immersed in water. This hardware would be used during handling and limited transporting operations on closed control sites, where precautions and handling procedures will limit the possibilities of accidents. During all handling operations of the beryllium-reflected reactor, the filler blocks would be installed. They would be removed immediately before launching.

The maximum degree of nuclear protection afforded by the filler blocks is limited to water reflection only. No external protective device can prevent criticality during water-flooding of the beryllium-reflected core (with control

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Figure 103. SNAP 10A Reactor Shipping Sleeve Schematic Diagram

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Figure 104. SNAP 10A Reactor Shipping Sleeve Installed on Demonstration Reactor

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drums out), because this configuration is supercritical even without water reflection. However, this limitation offers no serious problem, since the filler blocks would be used only where necessary safety precautions against core flooding could be taken.

Initial studies utilizing SCA-4 fuel were made to establish the design criteria for the filler block concept. The fact that the SCA-4 core had a larger excess reactivity than required in the flight system core was taken into consideration. No reactivities were measured, because only a subcritical configuration was of interest. Hence, modifications were made to the assembly until this was accomplished. The unshimmed beryllium reflector and boron filler block assembly used in this experiment are shown schematically in Figure 105. A 1/2-in. thickness of boron powder was encased in stainless steel with the poison sections covering all four drum voids completely and two of the reflector surfaces partially. The control drums were attached to the reflector for this experiment.

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Figure 105. SNAP 10A Reflector and Filler Block Assembly

With the foregoing assembly attached, the critical loading with water reflection and dry core was 33 fuel elements and 4 Lucite rods and 11.75 in. of water above the bottom of the fuel. Adding the 34th fuel element decreased the critical water height to 9.5 in.

The following modifications to the configuration of 34 fuel elements and 3 Lucite rods were required to achieve a 37-fuel-element subcritical loading:

- 1) Simulation of flight plenum chamber
- 2) Substitution of special low-hydrogen content (low N_H) fuel elements into the central core region to simulate the flight system reactivity
- 3) Placing Styrofoam in the remaining drum voids and behind the poison filler blocks on the reflector faces
- 4) Styrofoam mockup of the lower plenum and the neutron shield to exclude water from that volume.

The foregoing modifications were used to help establish the following filler block design criteria:

- 1) Filler blocks shall cover at least the reflector area outlined in Figure 105.
- 2) Filler blocks shall consist of at least 1-1/2 in. of void followed by 1/2 in. of natural boron powder packed to at least 20% theoretical density.
- 3) Structural materials shall be Type 304 stainless steel.

Filler blocks designed and fabricated according to established criteria were tested. The S10FR-1 flight system fuel and unshimmed beryllium reflector were used in this test. The combination void-poison filler blocks were attached to the reflector. A Styrofoam mockup of the neutron shield was attached to the bottom of the core to exclude water from that region. This configuration is shown in Figure 106.

The full core loading of 37 fuel elements was achieved with the assembly remaining subcritical. The average value of extrapolated critical loading from three inverse multiplication plots was 39.4 ± 0.4 fuel elements. Using a

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Figure 106. Combination SNAP 10A
Void-Poison Filler Blocks Installed
on Demonstration Reactor

previously determined average worth of 55¢ per element, the assembly was found to be $\$1.32 \pm 0.22$ subcritical.

This test successfully demonstrated the effectiveness of the filler block concept.

(3) SNAP 8 Core Experiments

Some details of the SNAP 8 fuel element and core assembly are shown in Figure 107. The fuel elements are right-circular cylinders of Zr-10 wt % U hydrided to an N_H of 6.0×10^{22} atoms/cc and clad with Hastelloy-N tubing and end plugs. The fuel rods are 0.532 in. in diameter and 14 in. long. The Hastelloy-N tubing is 0.540 in. ID and 14.47 in. long, with a wall thickness of 0.010 in. The inside of the tubing is coated with a ceramic hydrogen barrier

Figure 107. SNAP 8 Fuel Element and Core Details

material. Fuel element end plugs, 0.532 in. in diameter and 0.35 in. thick, are inserted into the tubing and welded. The finished fuel elements are 0.560 in. in diameter and 14.47 in. long (excluding the 9/64-in. -diameter, 1/2-in. -long positioning pins located at each end).

The reactor core is composed of 211 fuel elements, in triangular arrays on 0.570-in. centers, and 18 internal reflector pieces. The outline of the fuel element complex is a regular hexagon with slightly chopped corners. Internal reflectors occupy the space on the periphery of the core between the hexagonal fuel element pattern and the cylindrical reactor vessel. These internal reflectors are made of BeO, hot-pressed to 98% of theoretical density and clad with 0.010-in. -thick Hastelloy-N. They extend the full length of the core between the top and bottom grid plates.

The fully loaded, bare, SNAP 8 core proved to be subcritical when fully water-reflected. The extrapolated critical loading was no less than 260 ± 10 fuel elements. Based upon this value and an estimated average value of 10¢ each for the remaining 49 elements, the subcritical reactivity was found to be $\$4.90 \pm 1.00$.

The SNAP 8 core, internally flooded and water-reflected, was critical with 173 fuel elements and 38 Lucite rods. Full fuel loading was accomplished by compensating the excess reactivity with poison splines. The splines were right-circular cylinders which fitted the interstices. They were filled with a mixture of samarium and gadolinium rare-earth oxides. The cumulative excess of reactivity was found to be $\$5.87 \pm 0.32$ with a full core loading of 211 fuel elements.

Void sleeve and void-poison sleeve tests were performed with the SNAP 8 core. The core was water-reflected and internally flooded, with the beryllium oxide inserted in the core. The sleeves surrounded the entire core vessel radial surface.

With a 3-in. void sleeve in place, the full core loading of 211 fuel elements was found to have a subcritical reactivity of $-\$2.10 \pm 0.45$. Addition of a 1-in. boron poison sleeve surrounding the void sleeve increased the subcritical reactivity of the system to $-\$7.45 \pm 1.1$.

These tests demonstrated the capability of void sleeves and/or combination void-poison sleeves to maintain SNAP 8-type cores subcritical when they are both immersed in, and internally flooded by, water.

2. Intrinsic Subcriticality Experiments and Analysis

The water critical experiments demonstrated the feasibility of using void-poison sleeves and void-poison filler blocks to preclude the possibility of accidental criticality of SNAP reactors due to water immersion and/or human body reflection during reactor shipment, ground handling, and prelaunch check-out operations. Such devices did not, however, preclude the possibility of accidental criticality during a launch abort into water. Consequently, the concept of intrinsic water subcriticality was developed to provide effective safeguards against accidental criticality during the launch phase, as well as to simplify and provide economical core shipment.

The Intrinsic Water Subcriticality (IWS) concept provided safety in a water environment, yet maintained adequate core performance under normal operating conditions. Two methods were investigated in detail:

- 1) Use of highly spectrum-dependent thermal-resonance neutron absorbers in the fuel elements and in the core reflector interface region
- 2) Use of modified core and reflection geometrics.

The first method takes advantage of the shape of the low-thermal-energy neutron cross-section characteristic of gadolinium and other rare-earth elements. The special shape of this cross section increases the poison effectiveness of the material in a water-immersed SNAP reactor relative to that in the operational mode. This occurs because the water softens the thermal neutron spectrum. The reactivity change that results from increasing the poison effectiveness can be sufficient to render a reactor subcritical in water while maintaining operational capabilities. In addition, burnout of the poison in the operational mode provides compensation for normal reactivity losses. Thus, prepoisoning of SNAP systems produces two desired results: (1) an engineered safeguard against launch aborts and (2) a burnable poison for normal operation.

The second method exploits the high neutron leakage characteristic of SNAP reactors and the superior neutron reflecting properties of beryllium relative to water. In general, the core geometry is designed to make the reactor configuration subcritical in water yet supercritical when beryllium-reflected. This technique offers the advantage of permanent water-immersion safety; i. e., the reactor is always subcritical in water whether clean or having been operated. It has the disadvantage that the system weight is increased and the power lifetime capabilities are reduced.

a. Initial Experiments⁽¹⁸⁾

Initial experiments to study the intrinsic subcriticality concept were initiated in 1966. Tests were performed to measure the reactivity effects, in both the operational environment and the water-immersed environment, of various concentrations of natural Gd_2O_3 added to fuel elements and to the core vessel. Critical dimensions in water for various core geometries were obtained, and the effects of BeO axial reflectors were measured. These experiments were primarily directed toward providing data for the normalization of calculational models. The results indicated that both methods have IWS potential.

The operational environment studies were conducted with the SCA-4A reactor (SNAP 10A type) located in the SNAP Critical Facility at the Santa Susana Nuclear Field Laboratory.

The reactor core contained the same 37 rejected S2DR fuel moderator elements used in the water critical experiments (Section III. C. 1). The elements were contained in a cylindrical core vessel, 9 in. in diameter by 16 in. long, and were arranged in a hexagonal array measuring nominally 8 in. across flats and 9 in. across the corners. A beryllium reflector surrounded the core vessel and was adjustable radially and axially for parametric studies. Figure 108 shows the SCA-4A critical assembly machine; the assembly is described in detail in Reference 19.

The SCA-4B reactor (Section III. C. 1. a), with the SCA-4A fuel elements, was used for the water-immersion poison worth experiments. The bottom of the core vessel was perforated to allow simultaneous water flooding of the core and base tank.

Figure 108. SCA-4A Critical Assembly Machine

The basic SCA-4B machine was modified to perform the water immersion core geometry experiments. A larger base tank replaced the original base tank and top tank. Grid plates, capable of holding up to 721 fuel elements, were suspended in the base tank. Figure 109 is a photograph of the modified machine. Fuel tubes, 0.45 in. in diameter and 24 in. long, contained SCA-5 fuel pellets. These pellets were composed of $\text{ZrH}_2\text{-UO}_2$, containing 10 wt % UO_2 enriched to 93.22 wt % U^{235} . Beryllium-oxide pellets, 1-in. long, were used to provide beryllium axial reflection, as required.

(1) Natural Gadolinium Poison Experiments

The objective of this experimental series was to establish the worth of gadolinium applied to the fuel elements and core vessel in operational and water-immersion environment. A coating of gadolinium was applied to Mylar tape, which was then attached to the various surfaces with an adhesive. Gadolinium-oxide coatings varied from $\Sigma_a t$ of 0.1 to $\Sigma_a t$ of 2.0. Strips were attached to the outside of the core vessel with adhesive and thin steel bands. The oxide coating on the Mylar was a mixture of iron oxide and gadolinium oxide to provide the desired $\Sigma_a t$ concentration.

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Figure 109. Modified SCA-4B Critical Assembly Machine

In the operational system (SCA-4A), the experimental procedure consisted of first applying layers of unpoisoned tape to the core vessel and determining the critical configuration with the control drums. Boron splines of known worth were inserted in the core to decrease the reactivity of the system by the amount expected from the gadolinium. Then they were removed as the gadolinium was applied to the core vessel. This procedure was followed in order to minimize drum movement and the corresponding uncertainties in the effect of gadolinium poison on the drum worths.

A similar procedure was followed in applying the poison to the fuel elements; however, it was necessary to increase the reflector thickness to 3.00 in. to supply the excess reactivity required for this experiment.

The poison experiments were repeated in the water-immersed system. Additional boron splines were required to hold down the larger excess reactivity of the totally immersed system.

Finally, the effect of gadolinium as a poison in a system immersed in seawater was determined by replacing the fresh water in the reactor complex with seawater.

The reactivity worths of gadolinium concentrations, applied to the core vessel, were measured over the range of $\Sigma_a t = 0.1$ to 10.2 in both the operational environment (SCA-4A) and in the water-flooded environment (SCA-4B). The results are plotted for comparison in Figure 110.

The reactivity worths of gadolinium concentrations, applied to the fuel elements, over the range of $\Sigma_a t = 0.1$ to 0.3 were also measured. These results are plotted for comparison in Figure 111.

Following the poison worth measurements, the fresh water in the SCA-4B assembly was replaced with seawater. The negative worth of a gadolinium concentration of $\Sigma_a t = 4.0$ on the core vessel relative to no poison was measured to be 28¢ greater in seawater than in fresh water.

(2) Core Geometry Experiments

The objective of this experimental series was to ascertain the critical configuration for cores of various radii immersed in an infinite water environment. Critical heights were to be determined for hexagonal cores having 10 to 14 rings of fuel elements, or equivalent diameters ranging from 8.16 to 11.35 in., respectively. Critical heights were established for both bare and beryllium-oxide, axially reflected cores.

The procedure in each case was essentially the same. Pellets were inserted in each tube to the predicted critical height. The reactor was brought to critical by loading the fuel tubes. The fuel height was adjusted to a minimum critical configuration for a specified number of elements. Then an additional pellet was added, and the excess reactivity was measured to find the incremental pellet worth.

All excess reactivity measurements were obtained with, or corrected to, a water level in the tank equivalent to 3 in. over the top of the fuel or beryllium oxide. For most core configurations, this was simulated in the fuel tubes by placing 3 in. of Lucite rod in each tube over the fuel-beryllium-oxide column.

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Figure 110. Gadolinium Reactivity Worth on Core Vessel Wall

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Figure 111. Gadolinium Reactivity Worth on Fuel Cladding

Beryllium axial reflection was effected by inserting 1-in. pieces of beryllium oxide to the desired length above and below the fuel pellets.

A total of 5 bare cores, from the 14-ring to the 10-ring core, were assembled. The results are summarized graphically in Figure 112. The measured critical heights for the bare cores were 0.3 to 0.5 in. less than the minimum predicted value.

During the 12-ring bare core experiment, 4 and 6 in. of beryllium oxide were added to the top of the fuel. With a fuel loading of 25 pellets per tube, the excess reactivity with 4 in. of beryllium-oxide axial reflector above the fuel was 11¢; while with 6 in. of beryllium oxide above the fuel, the excess reactivity was only 14¢. Since the 2-in. addition of beryllium oxide to the existing 4-in. beryllium-oxide reflector increased the reactivity by only 3¢, it was concluded that 4-in. of beryllium-oxide axial reflector in a water-reflected environment is essentially an infinite reflector.

Based on the results of the foregoing experiments, the critical dimensions were established for the water-immersed cores, axially reflected with 4-in. of beryllium oxide. Data obtained from cores containing 4 in. of axial reflector above and below the fuel column are summarized graphically in Figure 112, along with the bare core results. A comparison with the values and predictions for the bare core experiments suggests that the calculated worth of the beryllium-oxide axial reflector requires some correction.

(3) Summary

The experimental results are presented as normalization for establishing the reactivity of SNAP-type cores immersed in water and for evaluating methods that reduce the positive reactivity effect of water immersion. The design of an intrinsically subcritical core will involve a combination of factors, including core composition, core geometry, beryllium-oxide axial reflectors, and the use of gadolinium poison. The experimental results provide a basis for these designs. Additional experimental effort is required to study the effect of core composition and the combined effects of all these factors.

The excess reactivities of a SNAP 10A-type core measured during seawater and fresh water immersions were \$9.54 and \$11.60, respectively.

Figure 112. Critical Core Dimensions

The greatest measured reactivity worth of gadolinium on the fuel cladding of a SNAP 10A-type core was \$10.65 in a water environment. The measurements indicate that greater reactivity reductions could be obtained with increased gadolinium poison. The maximum possible reactivity effect of gadolinium on the SNAP 10A-type core vessel in a water environment was measured as \$9.12. This reactivity effect is not sufficient to overcome the cold, clean excess reactivity of the present SNAP 10A-type reactor, indicating that additional means (such as limiting core dimensions) must sometimes be employed to achieve subcriticality in water environments.

Critical dimensions were obtained for several cores immersed in water. The effects of BeO axial reflectors were considered. From this information, data curves were plotted (see Figure 112) which furnish a normalization for evaluating calculated methods that can be used in studies of geometry variations in SNAP reactor designs.

b. Analytical Studies

Analytical studies were conducted on intrinsically subcritical SNAP 10A and SNAP 8 reactors, in which the system weight and power lifetime penalties associated with each method were evaluated.

Perturbation to the basic SNAP reactor design will, generally, result in a reactivity, weight, or performance penalty, since the basic designs were chosen to provide a minimum weight system for a particular power level.

The penalty associated with the use of gadolinium to provide intrinsic subcriticality results from reactivity losses due to residual (unburned) gadolinium. This is converted into a weight penalty when the initial excess reactivity is increased to compensate for the residual poison. The initial excess reactivity is increased by increasing the thickness of the beryllium reflector, which results in an increase in the shielding envelope diameter and a corresponding increase in shield weight. The penalty is dependent upon the degree to which the gadolinium isotopes meet the burnable poison requirements for each specific mission.

Core geometry changes to provide intrinsic subcriticality have two principal operational disadvantages. First, the overall reactor envelope is increased due to replacing fuel with a (less effective) beryllium reflector. This results in a weight penalty because of increased shadow-shield weight. Second, changes in power density, cladding temperature, etc., that result from core geometry changes, can result in a reduction in reactor lifetime capability.

It was concluded that SNAP reactors could be made intrinsically subcritical using either gadolinium or a poison or by core geometry changes. Since gadolinium also acts as a burnable poison and since core geometry changes result in more severe operational penalties, gadolinium poison was the recommended method. It was found that the dominating Gd^{157} isotope had a very

large cross section which made it unsuitable because of its rapid burnout rate. Hence, Gd^{155} , whose cross section has a more appropriate magnitude, was selected for further study.

c. Gd^{155} Experiments

The effects of gadolinium-155 on both the reactivity and the temperature coefficient of reactivity of a SNAP 10A-type reactor were measured.

The program included measurements of the reactivity effect of Gd^{155} in both water-immersed cores and the operational core configuration, and measurements of the effect of Gd^{155} on the temperature coefficient. Because the Gd^{155} resonance lies below the median fission energy of a SNAP reactor, the effective cross section of Gd^{155} decreases as the reactor temperature and, hence, the neutron energy increases. This effect leads to a positive contribution to the reactor temperature coefficient and potential problems in reactor control.

(1) Operational Environment Experiments

The operational environment tests were conducted in Test Cell No. 1 of the SNAP 8 Environmental Test Facility. The SNAPTRAN-1 Reactor, a SNAP 10A reactor specially adapted for high-temperature and transient experiments, was used. The core consisted of 37 U-ZrH fuel elements fully enriched in U^{235} , which were mounted on gridplates with a lattice spacing of 1.27 in. It was reflected by beryllium in the radial direction, with control provided by four beryllium drums. This arrangement is shown in Figure 113.

To provide for high-temperature operation, the core and reflector were enclosed in an insulated box. Since the normal control-drum-drive speeds are not adequate to initiate prompt transients, an additional drive mechanism was provided to permit rapid insertion of one or two control drums. Figure 114 is a photograph of the SNAPTRAN machine.

Gadolinium-155, in the form of Gd_2O_3 , was incorporated in a nickel matrix and rolled into foils of 2-mil thickness. These were formed into sleeves so that they could be slipped over each fuel element. Four types of sleeves were fabricated, with gadolinium concentrations ranging from $\Sigma_a t = 0$ (nickel only) to $\Sigma_a t = 0.23$. Five test configurations were constructed using these sleeves —

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Figure 113. SNAP 10A Reactor Layout Diagram

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Figure 114. SNAPTRAN Assembly

four contained 37 sleeves of one gadolinium concentration and one was a mixed loading.

The total reactivity worths, at ambient temperature, for each of the configurations were measured, using control drum worth calibrations. Following these tests, a series of transient tests were performed to evaluate the prompt temperature coefficient of reactivity. The transient test data were analyzed using the STAC kinetics-analyzer program. The code produced graphs of normalized power, mean temperatures, and temperature coefficient of reactivity.

(2) Water-Immersion Experiments

The SCA-4B critical assembly, used in all previous studies of water-immersed SNAP reactors, was also used in this water-immersion program. The same fuel, gridplates, and gadolinium sleeves as those used for the operational environment experiments were used in these tests. Reactivity measurements were made as a function of gadolinium concentration in bare water-reflected-and-flooded cores.

The method of determining the reactivity of an assembly that is potentially 10 to \$11 supercritical is a critical aspect of these measurements. In these experiments, boron-filled aluminum tubes, referred to as "boron splines," were used to offset the excess reactivity. Their reactivity effect was determined by measuring the worth of one or a few splines at each characteristic location, and summing these worths over the core. This method assumes no interaction between the splines. The reactivity was also deduced by measuring the reactivity effect of removing one or more of the gadolinium sleeves from each typical location and summing the worths over the whole core. The two methods gave consistent results for the reactivity effect of the gadolinium. It was found that a Σ_a of Gd^{155} equal to 0.217 was required to achieve a reactor that was just critical in water.

(3) Summary of Results

The reactivity worths of Gd^{155} as a function of concentration in SNAPTRAN and in SCA-4B are shown in Figure 115. In SNAPTRAN, the reactivity loss vs concentration is almost linear, with no saturation effect. A significant curvature exists in the SCA-4B worth curve. The earlier experiments with natural

Figure 115. Calculated and Measured Worth of Gadolinium-155 in SCA-4B and SNAPTRAN

gadolinium did not show this effect. The differential worth curve, also shown, demonstrates a slowly varying worth for Gd^{155} in water relative to the operating environment. Plots of the prompt-temperature coefficient of reactivity as a function of gadolinium concentration, for system temperature of 0 and 1000°F, are shown in Figure 116. These show a progressive reduction in the magnitude of the coefficient with increasing gadolinium content, and a tendency toward a positive-temperature effect at high loadings.

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Figure 116. Temperature Coefficient vs Gadolinium Loading

(4) Conclusions

The enhancement of Gd^{155} worth between the water-immersed and the operational environments is not as large as supposed, based on earlier experiments and analysis with natural gadolinium. Further, the mechanism that enhances the worth of Gd^{155} in water also produces an adverse effect on the prompt-temperature coefficient (it becomes less negative).

The concentration of Gd^{155} necessary to assure subcriticality for a SNAP 10A reactor in water, yet permit operation under normal conditions, would deplete the negative prompt-temperature coefficient by an unacceptable amount. In addition, the reactivity penalty associated with large concentrations of Gd^{155} is such that this IWS application to SNAP systems other than the SNAP 10A-type is virtually impossible.

The effect of Gd^{155} on the temperature coefficient is such that its use as a burnable poison is also severely limited. Each specific reactor, however, must be analyzed to determine the applicability of the concept and its effect on transient reactor performance.

d. Samarium-149 and Europium-151 Experiments

The reactivity effect of the addition of Sm^{149} and Eu^{151} to a SNAP reactor core was studied as a function of temperature. This work was an extension of the work previously performed on Gd^{155} and was directed toward the selection of poison materials that would be useful as burnable poisons and as spectrum-sensitive poisons to assist in the achievement of intrinsic subcriticality upon water immersion. In earlier experiments, it was found that Gd^{155} had a pronounced effect on the prompt-temperature coefficient, making it less negative with increasing gadolinium content. This effect is due to the large resonance in the gadolinium cross section which lies below the median fission energy of a SNAP reactor.

Samarium-149 has a resonance which also lies below the median fission energy, while Eu^{151} has a resonance which lies just above this energy. Thus the two materials should have opposite effects on the temperature coefficient. Measurements were made to obtain quantitative information that could be used to evaluate methods for calculating the reactivity temperature coefficient.

The SNAPTRAN reactor was used in a series of tests to measure the reactivity loss, the isothermal temperature coefficient, and the prompt temperature coefficient of a SNAP 10A reactor loaded uniformly with Sm^{149} or with Eu^{151} . The techniques used in the measurements were identical to those used in the Gd^{155} experiments.

The poisons investigated were dispersed in nickel sleeves which were fitted around each fuel element. The measured worths, at ambient temperature, are presented in Table 16.

TABLE 16
REACTIVITY WORTHS OF RARE-EARTH
POISONS IN SNAPTRAN-1

Material	Reactivity Change (\$)
Nickel sleeve	0.00
Gadolinium-155*	-2.71
Samarium-149	-3.22
Europium-151	-2.42

*Results of previous experiments.

The isothermal temperature defect for samarium and europium were measured. The reactivity losses between 75 and 1000° F are presented in Table 17.

TABLE 17
TEMPERATURE DEFECT REACTIVITIES
OF RARE-EARTH POISONS IN
SNAPTRAN-1

Material	Reactivity Change (\$) (75 to 1000° F)
Nickel sleeve	-2.04
Gadolinium-155*	-1.65
Samarium-149	-1.60
Europium-151	-2.20

*Results of previous experiments.

It may be noted that with samarium, like gadolinium, the temperature coefficient, relative to zero prepoison (nickel sleeve) was reduced; while with europium, the defect was increased.

A series of prompt-temperature coefficient measurements were made by means of temperature-limited power transients. For each of the poison materials, a set of transients was run, starting with an initial insertion of 60¢,

each from room temperature. A cascaded series was then run, consisting of 1.20 steps from room temperature, 300, and 900° F. These data were analyzed with the STAC code.

It was found that, in the concentration used, Sm^{149} gave a temperature coefficient quite similar to that of Gd^{155} . The Eu^{151} gave a coefficient more negative than the unpoisoned core, and the amount of additional coefficient increased sharply with increasing temperature. This could result in an unacceptably large temperature defect if Eu^{151} were used alone as a burnable poison in large quantities. Even so, it might be useful in small quantities together with samarium and/or gadolinium.

e. Summary

Initial experiments were performed to provide data for the evaluation of the use of natural gadolinium poison and core geometry changes as potential techniques for the design of SNAP reactors that would be subcritical when immersed in water, yet retain their operational capabilities. Reactivity worths of various concentrations of gadolinium, applied to the surface of the fuel elements and separately to the surface of the core vessel, were measured in both the water-immersed environment and in the operational environment. Critical dimensions in water, as a function of core geometry, were also determined.

Analytical studies, utilizing the experimental results as a basis for normalization, were performed in which the system weight and power lifetime penalties associated with each technique were evaluated. These studies showed that the penalties associated with the use of core geometry changes for attaining IWS were significantly more severe than those associated with the use of a prepoison. It was also found that the dominating isotope, Gd^{157} , had a burnout rate which was too rapid for use as a prepoison material.

Subsequent experiments were performed to evaluate the effectiveness of Gd^{155} , Sm^{149} , and Eu^{151} as prepoison materials. These studies included tests to determine the effect of the prepoison on the reactor prompt-temperature coefficient of reactivity. The results showed that gadolinium and samarium reduced the negative magnitude of the coefficient and that europium increased the negative magnitude of the coefficient.

On the basis of the experiments and analysis, it was concluded that none of the prepoison materials should be used alone in the quantity ($\sim \$8$) required for sustained (20,000 hr) SNAP reactor operation. The preferred approach would be to use combinations of two or three of the rare earths. The basic choice appeared to be between the following two schemes:

- 1) \$6 of Gd^{155} and \$2 of Eu^{151}
- 2) \$6 of Eu^{151} and \$2 of Sm^{149} .

The choice between these schemes depends upon the effect of Eu^{151} on the prompt-temperature coefficient. The rationale for either options 1) or 2), above, is as follows:

- 1) Neither Gd^{155} or Sm^{149} can be used alone, due to the positive effect each has on the prompt-temperature coefficient.
- 2) Eu^{151} should not be used by itself, due to its slow burnout rate and possible large increase in the temperature defect.
- 3) A combination of Eu^{151} and either Gd^{155} or Sm^{149} (or possibly both) could preserve the unpoisoned temperature coefficient and defect.

3. Accidental Steady-State Operation

During launch procedures of the SNAP 10A flight test, or on reentry, steady-state reactor operation is considered possible. Potential radiation hazards due to accidents of this nature were examined.

a. Prelaunch Accidental Criticality

The fuel loading operations will introduce hydrogenous fluids into the vicinity of the reactor that could, in the event of an abort, cause accidental criticality. The rate of reactivity addition would most likely be rapid, causing a supercritical excursion resulting in destruction of the reactor. If reactivity addition is slow, steady-state operation will ensue. Personnel exposure to radiation resulting from this accident would only occur while enacting emergency procedures intended to effect shutdown. Quasi steady-state operation of the reactor at 130 kw was assumed for these analyses. Radiation dose rates (see Figure 117) were calculated for a reference position of 3 ft, 10 ft from the edge of the retention basin, or 39 ft from the reactor surface. In the event of fuel cladding failure

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Figure 117. Dose Rate Near Edge of Retention Basin

curves around a particular crater for a reactor operating at a power level of 1 kw. Other calculations show that the reactor could operate at an ultimate power level of ~ 6 kw. A person conducting a 1-min close inspection of the reactor would receive a dose of ~ 500 rem. A power level less than 6 kw, an exposure duration less than 1 min, a more distant point of closest observation, or the combination of these, would reduce the dose considerably. Calculations

or destruction resulting from an excursion, airborne contamination would result. Figure 118 shows the thyroid dose as a function of reactor operating time for various distances from the reactor. Shutdown through enactment of emergency procedures by removal or poisoning of retention basin water would prevent air contamination.

b. Post-Reentry Accidental Criticality

A reactor reentering with its reflector intact could theoretically survive soil impact. If, upon impact in soil, a crater were formed under conditions in which rainwater were retained, the reactor could establish a steady-state 1-kw power level without damage. Operation would continue until overheating occurred, resulting in fuel cladding rupture. An estimate of the exposure magnitude may be obtained from Figure 119, which presents isodose

were also made for an assumed series of events such that an excursion occurred after 5 hr of reactor operation at 6 kw. Figure 120 shows the whole body dose and thyroid dose resulting from the cloud and the external dose rate 3 ft above the ground as a function of distance from the reactor.

Figure 118. Thyroid Dose vs Operating Time
Steady-State Accident on Launch Pad

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Figure 119. Isodose Rate Curves for SNAP 10A Operation in a Crater

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Figure 120. Radiation Hazards Due to
Steady-State and Excursion —
No Orbital Operation

D. REACTOR TRANSIENT BEHAVIOR

1. SNAPTRAN-3 Experiment⁽²⁰⁻²²⁾

a. Scope and Objective

The SNAPTRAN-3 destructive experiment was conducted at the National Reactor Testing Station (NRTS) as one of the major projects in the Aerospace Safety Program. In this test, a NaK-filled SNAP 10A/2-type reactor was submerged in water and subjected to \$3.60 step transient above critical, which was terminated by core disassembly. Phillips Petroleum Company (PPCo) was responsible for conducting the test and for data acquisition, while Atomics International (AI) performed analysis of the data to determine nuclear energy release and reactivity feedback effects. Further analysis of the data lead to a comparison of experimental and theoretical results.

The prompt nuclear energy release as determined from calibrated power traces were to be obtained from nuclear detectors calibrated with fuel moderator energy probes. Photographic coverage provided a documentary sequence, by silhouette, of core expansion and disassembly.

b. Test Apparatus

The SNAPTRAN-3 reactor consisted of a standard SNAP 10A/2 core containing 37 fuel-moderator elements placed on a hexagonal, 1.260-in. center-to-center array. Six beryllium sleeves filled the hexagonal lattice to conform with the cylindrical core vessel. The fuel-moderator material was an alloy of zirconium hydride and fully enriched uranium. The reactor core inventory consisted of 4.75 kg of U^{235} and 464 g-mol of hydrogen. Each fuel element was clad with Hastelloy-N and had an active length of 12.25 in. The elements were positioned within the core vessel with upper and lower grid plates.

The reactor vessel was positioned at an elevation of 5.5 ft in an environmental tank constructed of steel-reinforced concrete, 14 ft in diameter by 10 ft deep. When filled with 10,000 gal of water, the surface extended approximately 4.0 ft above the vessel centerline. Since the reactor vessel was not fitted with external beryllium reflectors and control drums, reactor power was controlled with a cylindrical binal (B_4C) sleeve. The binal control sleeve was provided with two in-line drive systems: (1) motor drive for slow-speed movement,

2.3 in./min, and precise positioning; and (2) a pyrotechnic actuator for rapid removal of the sleeve, approximately 90 ft/sec, for the destructive test. Secondary test apparatus support equipment included environmental water pumping, discharge, and purification.

The entire test apparatus was mounted on a four-rail railroad dolly for removal after the destruct test. A schematic layout of the SNAPTRAN-3 test apparatus is shown in Figure 121.

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Figure 121. Schematic Diagram of SNAPTRAN-3 Test Package Mounted on Flatcar

The SNAPTRAN-3 reactor was provided with instrumentation to monitor the functions listed in Table 18. The nuclear power detectors were calibrated during low-power reactor operations tests using standard calorimetric techniques. The neutron and gamma detectors positioned inside the core vessel, in the environmental tank, and at remote distances provided reactor power coverage from zero to that expected during the destructive test. A series of radially and axially positioned energy probes provided energy deposition data. Energy deposition in an energy probe, in the absence of significant heat transfer, is directly

TABLE 18
SNAPTRAN-3 INSTRUMENTATION

Function	Measurement	Comment
Nuclear	Fast neutron	External
	Thermal neutron	External
	Gamma	External
Thermal	Energy probe, in-core	Radial and axial
	Chromel-Alumel thermocouple	External system
Strain	Reactor disassembly	Vessel and external, in-tank
Displacement	Reactor disassembly	External, in-tank
Acceleration	Reactor disassembly	External, in-tank
Pressure	Reactor disassembly	External, in-tank
Acoustic	Reactor disassembly time of event	External
Photography	Reactor disassembly — slow, intermediate, and high-speed	Documentary
Radiological	Fission product release	External

related to the temperature through the heat capacity of the fuel material. Energy probes having uranium densities of 5 and 10 wt % were used in these tests. Probe locations are shown in Figure 122. The energy probes were precalibrated in the AI-KEWB reactor. During the destruct test, time-correlated propagation and disassembly of the SNAPTRAN-3 reactor were monitored with a series of strain gage, displacement, acceleration, pressure, acoustic, and photographic measurements.

Photographic coverage was provided by both PPCo and Edgerton, Germeshausen, and Grier (EG&G), Inc. Slow and intermediate (16 to 3000 frame/sec) motion pictures were taken by PPCo from different angles and elevations to produce data film of rapidly changing events during the test. Fast motion pictures (17,600 frames/sec to 1 frame/ μ sec) were taken by EG&G, viewing the reactor vessel through periscopes.

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Figure 122. Fuel Loading Plan and Energy Probe Location

c. Discussion of Test Results

Two power traces and 19 energy probe channels yielded useful data over most of the increasing power portion of the transient during the destruct test. Eleven of the energy probes yielded data slightly past the time of peak power. Figure 123 is a smoothed plot of the power trace of one detector. Using that smoothed power trace, a solution of the six-delayed-neutron group, space-independent, reactor-kinetics equations was calculated to obtain reactivity as a function of time. Figure 124 shows the results of these calculations. The value of 1.75 msec used for the ratio of ℓ/β_{eff} was similar to that obtained in SCA-4B experiments, using noise analysis techniques (1.71 msec for ℓ/β_{eff}).

The value of the initial asymptotic period was obtained by averaging the individual values from three sensitive power detectors. The average value of 0.649 msec was observed 1.2 msec before the time when the volume-averaged fuel temperature had increased 1° F.

Examination of all the energy probe data (smoothed) indicated continuous functions of energy deposition until erratic patterns developed arising from either fuel disintegration or massive mechanical disassembly of the core.

Figure 123. Smoothed Composite Power Trace (N-13)

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Figure 124. Reactivity vs Time (N-13)

Indicated temperatures at this point ranged from 1800 to 2000°F. Typical data from energy probes are given in Figures 125, 126, and 127 for the SNAPTRAN-3 destruct test.

The Dynafax cameras provided by EG&G were set to give 256 exposures, each of about 2- μ sec duration at 40- or 80- μ sec intervals. These cameras and associated lights were triggered at a power level of 3 to 4 kw by a gamma scintillation detector located in the water about 3 ft from the core vessel. The ultrafast cameras were set to give six exposures during disassembly. Six selected frames of the Dynafax coverage are shown in Figure 128. The sequences of silhouetted photographs taken during core expansion cover a time interval of

Figure 125. Unsmoothed
Temperature Trace
(Energy Probe EP-2)

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Figure 126. Smoothed Tempera-
ture Trace
(Energy Probe EP-8)

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7611-0479-3

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7611-0479-5

Figure 127. Smoothed Temperature Trace
(Energy Probe EP-13)

about 1.36 msec, approximately 0.8 msec after peak power. The vessel is seen to bulge at the center, its silhouette becoming circular as the expansion proceeds.

d. Analysis of Results and Conclusions

The core-average fuel temperature and integrated energy as functions of time were deduced from the energy probe data by fitting a calculated distribution of energy density, as a function of position, to calculated probe energies. This technique was employed to obtain continuously varying energy density distributions consistent with the measurements made at specific locations.

Smoothed energy probe temperature data were treated in the aforementioned manner to obtain radial and axial temperature distributions, volume-averaged core temperature distributions, and the integrated core energy. The radial and axial temperature distributions are shown in Figures 129 and 130 from 5.44 to 9.94 msec. Temperatures above the line of fuel disintegration (1930° F) do not account for energy absorption effects of hydrogen dissociation. The volume-

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Figure 128. Photos of Core Vessel During Expansion

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Figure 129. Radial Temperature Distribution Plots from Energy Probe Data

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Figure 130. Axial Temperature Distribution Plots from Energy Probe Data

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Figure 131. Volume-Average Fuel Moderator Temperature vs Time from Energy Probe Data

AI-AEC-13100

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averaged core temperature as a function of time is shown in Figure 131. Each normalized energy probe data point was fitted with a least-squares fit and integrated to obtain the total core energy. A plot of calculated core energy is compared with the integral of the power trace of detector N-13 in Figure 132. Agreement between these two traces is $\pm 1.3\%$ in the range of 6.5 to 8.5 msec. The uncertainty in the energy-probe-calculated core energy associated with the fit of the energy probe data to the transport theory fission density distribution was $\pm 5\%$. Total uncertainty in the calculated energy, due solely to uncertainties in the treatment of data, is approximately $\pm 11\%$. The energy release of the SNAPTRAN-3 test is 32.5 ± 3.6 Mw-sec (peak power $13,700 \pm 1510$ Mw) calculated as the integral of the normalized N-13 power trace. This corresponds to 1.22×10^{18} fissions and a total fission energy release of 39.7 ± 4.4 Mw-sec.

One of the objectives of the analysis of the SNAPTRAN-3 test data was to evaluate reactivity feedback effects. A fuel-moderator temperature coefficient of reactivity was determined graphically from reactivity derived from neutron kinetic analysis of the EP-13 power trace vs energy-probe volume-average fuel-moderator temperature. Figure 133 shows that the data points fall very close to a straight line whose slope, temperature coefficient of reactivity (α) is $-0.22\text{¢}/^\circ\text{F}$ up to approximately 1400°F . Above 1400°F , the reactivity feedback increases sharply due to core expansion. Previous temperature coefficient reactivity measurements (S2DR) and theoretically predicted values indicate values ranging from $-0.7\text{¢}/^\circ\text{F}$ below 600°F to $-0.08\text{¢}/^\circ\text{F}$ above 700°F , respectively. An approximate model that treats the reflector neutrons as a 7th-delayed-neutron group was developed, and evaluations applicable to SNAPTRAN-3 were conducted. Discussion of these results appear in a later section.

An analysis was made of the photos showing core expansion during the SNAPTRAN-3 test. A composite drawing was made, showing core expansion as a function of time (Figure 134), from individual Dynafax camera frames (see Figure 128). Effects on reactivity were estimated, using the results of the nuclear analyses by extrapolating that portion of the reactivity-vs-energy plot that was calculated prior to core expansion. The deviation of the actual reactivity during the time of core expansion from the extrapolated line is attributed to an expansion reactivity feedback. This reactivity difference was plotted as a

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Figure 132. Comparison of Energy from Energy Probe Data with Energy from Normalized N-13 Power Trace

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Figure 133. Reactivity from N-13 Power Trace vs Volume-Average Fuel-Moderator Temperature and Energy from Energy Probe Data

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7611-0495B

Figure 134. Core Outline vs Time from Dynafax Camera Photographs

function of change of radius. During the initial core expansion, 0.0 to 0.3 cm, the expansion reactivity feedback was found to be $-\$6.65/\text{in.}$; thereafter, the coefficient became $-\$11.05/\text{in.}$

Specific results of the SNAPTRAN-3 test were calculated on a theoretical basis. The theoretical equations used included: (1) space-independent neutron kinetics equations with prompt-neutron and reactor expansion effects; (2) space-dependent energy balance equations, where energy at reactor core temperatures below disintegration temperature (1930°F) was accounted as sensible heat and energy above the disintegration temperature resulted in hydrogen dissociation; (3) equations of pressure in the disintegrated fuel region; and (4) equations of motion for the portion of the reactor core in which fuel disintegration has not yet occurred. The comparison of the theoretical calculation with the experiment is shown in Figures 135 through 139, in which the power, energy inverse period, reactivity, and maximum radial expansion are plotted as a function of time. The comparison between the experimental data and the analytical curve of maximum radial expansion is a sensitive measure of the mechanical expansion model used. Since the volume fraction of microscopic voids in the fuel is not known, the calculation for both 1 and 10% void fraction is performed. It is shown that the result is not very sensitive to this parameter.

Studies performed on the SNAPTRAN-1 and -2 results used updated cross-section data and included Doppler coefficient effects. The calculations were performed with a spatially dependent kinetics code called "TSN." This code includes spatial importance distribution effects as well as spatially dependent calculations of flux, power density, temperature, and feedback effects. These calculations used a fitted parameter, $460\ \mu\text{sec}$, for the water-reflected SNAPTRAN-3 reactor, rather than $132\ \mu\text{sec}$, for beryllium-reflected reactors. Then, the theoretically calculated SNAPTRAN-3 temperature coefficient becomes $-0.24\text{¢}/^{\circ}\text{F}$, which is within 10% of the experimentally determined $-0.22\text{¢}/^{\circ}\text{F}$.

Analysis of five fuel elements used in the SNAPTRAN-3 destructive test provided information that fuel disintegration was caused by the generation of hydrogen pressure resulting in stresses in excess of the strength of the material. One fuel element remained intact after the test on which extensive analysis could be performed. This element was located in position 19 (see Figure 122). The remaining four elements were in fragments. Analysis of the intact fuel

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Figure 135. Comparison of Calculated Power vs Time
With Normalized N-13 Power Trace

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7611-0500A

Figure 136. Comparison of Calculated Energy vs
Energy from N-13 Power Trace

Figure 137. Comparison of
Calculated Inverse Period
with Inverse Period from
N-13 Power Trace
(Simulation Cal-
culations)

7611-0501A

Figure 138. Comparison of Calcu-
lated Reactivity vs Time with
Reactivity vs Time from N-13
Power Trace (simulation
calculations)

7611-0502A

7611-0503A
Figure 139. Calculated Maximum Radial Expansion vs Time

element showed extensive cracking in the center of the element with the end pieces remaining relatively unaffected. A photomacrograph (Figure 140) taken of the damaged central section of the intact element shows extensive cracking due to the evolution of hydrogen. The release of hydrogen in this cross-sectional area is not uniform, since areas of undisturbed material were found (see area "B").

The fuel element located in position 37 showed areas of blistering on the outer surface of the element's lower section (see Figure 141). This area was the area of highest fission density in a typical water-reflected SNAP 10A/2 reactor. An autoradiograph was taken of this area. Analysis of the autoradiograph showed it to be in close agreement with the radial fission density profiles obtained in the SNAPTRAN-3 calibration. Photomacrographs taken of this area also showed extensive cracking of the fuel material due to rapid evolution of hydrogen.

2. Electrical Pulse-Heating Experiments⁽²³⁾

a. Scope and Objective

The specific heat of ZrUH_x fuel material, thermal expansion, and hydrogen evolution characteristics were to be determined, using electrically induced heating rates. Composition of the fuel was to vary from an H/Zr ratio of 0.50 to 1.81. Heating rates to be studied were to range from 7000 to 100,000°C/sec.

b. Test Apparatus

The test apparatus for the ZrUH_x electrical pulse-heating experiments consisted of a three-bank, 12-v nickel-cadmium battery power supply; circuit breaker; sample mounting system; and associated instrumentation. Each test specimen was mounted in a sample-holding piston, which was enclosed in a vacuum-rated test chamber. Electrical feedthroughs were water-cooled to prevent heating of test chamber gas. The test chamber was designed to facilitate Fastax photography and pyrometry measurements through optical-grade Pyrex windows.

Maximum power transfer from the energy source to the sample during pulse heating was accomplished by impedance-matching sample and internal battery resistance. Heating rates were easily controlled by changing the number of trays

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Figure 140. Photomicrograph of Damaged Central Section of Intact Fuel Element

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Figure 141. Fuel Element from Position 37 Showing Areas of Blistering on
Outer Surface of Lower Section

of batteries connected in parallel. Three trays (72 v/tray) provided heating rates in excess of 80,000°C/sec; two trays provided rates between 20,000 and 80,000°C; and one tray yielded rates up to 18,000°C/sec. Figure 142 shows a schematic of the pulse-heating test apparatus.

c. Discussion of Test Results

The electrical resistivity (ρ) of each ZrUH_x specimen was determined prior to pulse heating. Standard potentiometric methods were used with an overall system precision of $\pm 2\%$. Electrical resistivity was used as a thermometer up to the material phase change and as a measure of the temperature coefficient of resistance ($d\rho/dT$). The specific heat of each ZrUH_x sample was determined during pulse heating. As the high-current pulse flowed through a sample of known density and cross-section area, the current flowing through the sample and the voltage drop between two voltage probes were measured as a function of time. These were recorded by means of a dual-beam oscilloscope.

Specimens contained modified zirconium - 10 wt % uranium hydride; and were fabricated by means of the consumable electrode technique, extruded to hydriding size, hydrided, and then finish-machined to a length of approximately 2.70 in. by 1/8-in. in diameter. Hydrogen compositions were calculated from weight gains after hydriding. Figures 143 through 146 are photomicrographs of samples before pulse heating, with H/Zr ratios 0.50, 1.20, 1.74, and 1.81, respectively.

Twenty-one specimens were pulse heated during these studies. The Fastax film showed that all of the specimens heated uniformly. The experiments were grouped according to the initial heating rate. Initial heating rates greater than 80,000°C/sec are in Group I, 20,000 to 80,000°C/sec in Group II and below 20,000°C/sec in Group III. Specimens in Group I exploded between 20 and 30 msec without evolving hydrogen; the specimens in Group II exploded prior to 40 msec and began evolving hydrogen just before failure; specimens in Group III evolved hydrogen over a longer period of time and exhibited characteristics in three time regions due to two phase changes. Tables 19, 20, and 21 present a summary of Groups I, II, and III experiment results, respectively.

LEGEND:

I = SAMPLE CURRENT	P = SYSTEM PRESSURE
E ₁ = VOLTAGE DROP ACROSS SAMPLE LENGTH, l_1	T _F = FINAL SAMPLE TEMPERATURE
E ₂ = VOLTAGE DROP ACROSS SAMPLE LENGTH, l_2	T _G = EVOLVED GAS TEMPERATURE

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Figure 142. Schematic Diagram of Pulse-Heating Experiment Apparatus

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Figure 143. Photomicrograph of an H/Zr 0.50 Sample
Prior to Pulse Heating (250X)

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Figure 144. Photomicrograph of an H/Zr 1.20 Sample
Prior to Pulse Heating (250X)

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Figure 145. Photomicrograph of an H/Zr 1.74 Sample
Prior to Pulse Heating (250X)

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Figure 146. Photomicrograph of an H/Zr 1.81 Sample
Prior to Pulse Heating (250X)

TABLE 19
SUMMARY OF GROUP I EXPERIMENTS

Sample Number	Initial Heating Rate (°C/sec)	Weight (gm)	Moles of H ₂ Available (calc)	H/Zr Atom Ratio	Maximum Pressure Possible* (atm)	Maximum Pressure Observed (atm)	H ₂ Evolved (%)
246A-23	105,800	3.3139	0.0299	1.81	0.157	0.0400	25.5
246A-24	84,400	3.312	0.0300	1.81	0.189	0.0633	33.6
246A-25	-	3.3354	0.0300	1.81	0.189	0.0734	38.7
262A-4-(1)	83,800	3.3195	0.0290	1.74	0.183	0.0853	46.4
246A-26	-	3.3502	0.0302	1.81	0.192	0.0543	28.3
246A-30	82,000	3.3639	0.0304	1.81	0.192	0.0510	26.6
248D-5	87,800	3.6668	0.022	1.20	0.139	0.0324	23.3
248D-3-(B)	144,000	2.8678	0.0172	1.20	0.107	0.0340	31.8
248D-7	164,000	3.6548	0.0091	0.50	0.057	0.0281	49.5
248D-8	-	3.7566	0.0094	0.50	0.058	0.0309	52.9

*Assuming that all H₂ in the specimen is evolved into the calibrated volume of the test chamber at 300°K.

TABLE 20
SUMMARY OF GROUP II EXPERIMENTS

Sample Number	Initial Heating Rate (°C/sec)	Weight (gm)	Moles of H ₂ Available (calc)	H/Zr Atom Ratio	Maximum Pressure Possible* (atm)	Maximum Pressure Observed (atm)	H ₂ Evolved (%)
246A-22	70,000	3.3287	0.0299	1.81	0.1209	0.042	34.8
246A-28	65,000	3.0385	0.0275	1.81	0.1739	0.041	24.2
248D-6	69,300	3.7441	0.0094	0.50	0.0582	0.036	61.8
262A-4(11)	57,000	3.1480	0.0274	1.74	0.1703	0.046	26.8

*Assuming that all H₂ in the specimen is evolved into the calibrated volume of the test chamber at 300°K.

TABLE 21
SUMMARY OF GROUP III EXPERIMENTS

Sample Number	Initial Heating Rate (°C/sec)	Weight (gm)	Moles of H ₂ Available (calc)	H/Zr Atom Ratio	Maximum Pressure Possible* (atm)	Maximum Pressure Observed (atm)	H ₂ Evolved (%)
246A-27	7,800	3.3362	0.0301	1.81	0.1904	0.1175	61.8
246A-31	15,800	3.3444	0.0303	1.81	0.1917	0.1511	78.7
262A-4-6	19,700	3.3233	0.0289	1.74	0.1829	0.1692	92.5
262A-4-13	15,800	3.3213	0.0289	1.74	0.1797	0.0756	42.2
248D-9	14,500	3.7475	0.0094	0.50	0.0583	0.0270	46.3
264B-4-27		3.1361	0.0281	1.79	0.1745	0.1076	61.7

*Assuming that all H₂ in the specimen is evolved into the calibrated volume of the test chamber at 300°K.

d. Analysis of Results and Conclusions

The electrical resistivity was determined for five different H/Zr compositions of the fuel. The resistivity of the samples in the epsilon phase (H/Zr = 1.81, 1.79, 1.74) increased monotonically with temperature from 23 to 700°C. The resistivity of the specimens of H/Zr - 0.50 decreased with temperature above 550°C. This is due to the phase shift from the alpha-plus-delta structure to the beta structure at this temperature. Results of electrical resistivity are shown in Figure 147.

Figure 147. Electrical Resistivity vs Temperature for Various H/Zr Samples

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The specific heats were determined at various temperature intervals for each H/Zr ratio. The results of these analyses are represented by the equations listed in Table 22. The results of the least-squares fits to the data are summarized and compared to previous and SNAPTRAN-3 test data in Figure 148.

TABLE 22
SUMMARY OF SPECIFIC HEAT EQUATIONS

H/Zr Ratio	Accuracy	Specific Heat Equation	Temperature Range (°C)
0.50	±7%	$4.312 \times 10^{-2} + 4.330 \times 10^{-4}T - 1.496 \times 10^{-6}T^2 + 2.38 \times 10^{-9}T^3$	$100 < T < 500$
1.20	±7%	$6.745 \times 10^{-2} + 2.279 \times 10^{-4}T - 4.018 \times 10^{-6}T^2 + 4.561 \times 10^{-9}T^3$	$100 < T \leq 500$
1.74	±5%	$6.587 \times 10^{-2} + 1.452 \times 10^{-4}T$	$100 < T < 650$
1.81	±5%	$8.043 \times 10^{-2} + 1.088 \times 10^{-4}T$	$100 < T < 650$

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Figure 148. Summary of Least-Squares Specific Heat Values vs Temperature

The percent of expansion was determined to be approximately 2% for 11 of the 21 samples at the point of failure for the higher heating rates. The lower heating rates showed the same approximate expansion, but decreased to zero at the transformation to the beta-phase material and as hydrogen evolved.

As the initial heating rate for specimens increased toward 80,000°/sec, less hydrogen evolution occurred before catastrophic failure, until a point was reached where no hydrogen was evolved until failure occurred. This was consistent with earlier predictions and with the results of the SNAPTRAN-3 experiment. It was predicted theoretically that the fuel would disintegrate as the internal gas pressure in the microscopic voids of the structure exceeded the

limits for integrity of the fuel. No detectable hydrogen migration occurred in the fuel samples tested.

3. TREAT Nuclear Test Capsule Experiment

a. Scope and Objective

The purpose of the Transient Reactor Test (TREAT) nuclear test capsule experiment was to measure the hydrogen diffusion coefficient, thermal expansion, and burnup of ZrUH_x fuel at high temperatures. These objectives were to be accomplished by a series of neutron bursts obtained in the TREAT reactor at the National Reactor Testing Station (NRTS).

b. Test Apparatus

A total of 17 capsules were prepared for this test series. Each capsule consisted of a 3-in. OD by 5-in.-long by 0.188-in.-thick stainless steel shell with an internal tantalum foil used as a heat shield. These capsules were instrumented for temperature and pressure. Suspended inside each capsule was a 1.212-in.-diameter by 0.100-in.-thick wafer of ZrUH_x which contained 10 wt % U^{235} . Mounted against the edge of the fuel wafer was a linear displacement transducer for measuring thermal expansion of the fuel. These prepared capsules were sent to NRTS, where each received from 1 to 5 bursts of the TREAT reactor.

c. Discussion of Results

The fuel varied from intact to powdered and granulated form after irradiation. Not all capsules yielded temperature data, but those that did recorded sample temperatures from 1100 to $1500^{\pm}$ °C. Post-irradiation hydrogen analysis was made on each of the fuel wafers. Final values ranged from 0.14 to 1.50 wt % for all samples originally containing 1.8 wt %. The burnup analysis was obtained by counting and integrating over the 1.60-Mev Ba-La 140 photo peak. Samples receiving a single TREAT burst showed $6.7 (\pm 0.6) \times 10^{-6}$ at. % burnup, while those samples receiving 5 bursts indicated as much as $3.0 (\pm 0.1) \times 10^{-5}$ at. % burnup. No hydrogen diffusion coefficient measurements were made.

4. Fission Product Release Tests⁽²⁴⁾

A series of experiments was performed to determine the fission product release from SNAP 10A/2 uranium-zirconium hydride fuel samples. The

experiments were needed to evaluate the radiation hazards from nuclear incidents when the reactor was near the Earth's surface. No prior experimental data were available for this material, and extrapolation from other materials was not possible because of the complex solid-state processes involved. Otherwise, overly conservative assumptions would have to have been made.

Twelve tests were run, using six samples with standard SNAP 10A/2 Hastelloy-N cladding and six unclad samples. The samples were 1-1/4 in. diameter by 4 in. long.

Samples were irradiated in the Materials Testing Reactor (MTR) of the National Reactor Testing Station (NRTS) in Idaho for the equivalent of about 1-yr operation at 60 kw. Later, each sample was heated in a closed induction furnace to temperatures from 2000 to 3500° F. Temperatures were maintained for from a few minutes for melts to 8 hr for nonmelts. All fission products were collected in specially designed particulate collectors, iodine traps, and gas traps. The entire system was closed to the atmosphere and contained helium gas as a carrier. The apparatus is shown schematically in Figures 149 and 150. The facility is pictured in Figure 151. General Dynamics-Fort Worth conducted the release tests under a subcontract.

Activity of isotopes of interest was determined for the fuel residue and evolved material through radiochemical analyses. Since decay times between irradiation and testing were of the order of 3 to 8 weeks, short-lived isotopes were not studied. Gross fission product release for various temperature-time conditions is summarized in Table 23. Some of the conditions were analogous to postulated accidents (i.e., prompt criticality followed by a rocket fuel fire was simulated by raising the fuel temperature of 3500° F then dropping it to 2000° F for 15 min). The melting temperature of unclad SNAP 10A/2 fuel is 3450° F, but is only $2550 \pm 100^\circ \text{F}$ for SNAP 10A/2 fuel with standard 0.015-in. Hastelloy-N cladding. This seeming anomaly occurs because the zirconium hydride forms a eutectic with the Hastelloy-N somewhat above the melting point of the Hastelloy when they are maintained in contact.

At all of the test temperatures, hydrogen dissociates at a very rapid rate from zirconium hydride. The process of dehydriding would therefore occur at the beginning of each test, as the temperature was being raised to the test temperature.

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Figure 149. Cross-Section Diagram of Furnace and Particulate Collector

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Figure 150. Fission Product Gas Collection System Schematic

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Figure 151. General View of Test Site — Fission Product Release Tests

TABLE 23
FISSION PRODUCT RELEASE FROM URANIUM-ZIRCONIUM HYDRIDE FUEL
RODS IN HELIUM ATMOSPHERE*

Fission Product	Unclad Samples	Clad Samples [†]
Barium-140	10% evolved at 3200°F; less than 1% evolved at 2000°F	70 to 95% evolved from melts; 40% evolved at 2450°F; less than 0.01% evolved at 2000°F
Cesium-137	12% evolved at 3200°F; 2% evolved at 2800°F; 0.04% evolved at 2000°F	70 to 97% evolved from melts; 25% evolved at 2450°F; less than 0.01% evolved at 2000°F
Ruthenium-103, -106	Approximately 0.001% evolved	20% evolved from melts; less than 0.0001% evolved at 2000 to 2450°F
Cerium-141, -144	0.01% evolved at 3200°F; less than 0.001% evolved at 2000 to 2800°F	25 to 35% evolved from melts; 0.004% or less evolved at 2000 to 2450°F
Strontium-89, -90	3 to 5% evolved at 2800 to 3200°F; 0.2% evolved at 2500°F; 0.003% evolved at 2000°F	80 to 100% evolved from melts; 25% evolved at 2450°F; 0.002% evolved at 2000°F
Iodine-131	10% evolved at 3200°F; 0.2% evolved at 2000°F	100% evolved from melts; 0.2% evolved at 2000°F

*Nonmelt temperatures were maintained 4 to 8 hr; melts were maintained approximately 10 min.

†At 2450°F, there was evidently partial melting of a clad sample. Following the test, the cladding appeared discolored, but there were no large cracks. Minute cracks or holes must have allowed expulsion of hydrogen and fission products. At 2000°F, the sample was not discolored, but minute openings must have occurred in the cladding.

Fission product activities were measured with a scintillation detector whose output was fed to a 256-channel analyzer. By using multiple traps during a single run, the time dependence of gas release was determined. A typical set of data is shown in Figure 152. These data indicated that product gas release activity was relatively constant during a run; it was not evolved in a large burst when the temperature was raised.

The test data were checked by comparisons with calculated total activities, monitor sample activities, etc. A safety factor of two was recommended for the release fractions presented in Table 23 that are of the order of 1% or larger, and a safety factor of 10 for the very small release fractions of 10^{-2} to $10^{-4}\%$.

SNAPTRAN-3 tests had indicated that, during a maximum excursion, the maximum fuel temperature in a SNAP 10A/2 reactor is 2000° F. If this were followed by a rocket fuel fire during launch, the temperature could be maintained for about 10 min. Table 23 shows the small release for clad samples at 2000° F (for 6 hr). Had the release experiments and the SNAPTRAN-3 tests not been performed, the radiation hazard from fission product release, based on conservative assumptions, would have been overestimated by several orders of magnitude.

5. Supporting Experiments

a. Boiling Channel Mockup

(1) Introduction

An experimental study of the boiling of water in a SNAP 10A reactor coolant channel was made to improve the analysis of the behavior of the reactor should it achieve criticality due to water reflection by immersion, or should it flood internally. The power from the critical reactor would cause the water to boil in the core, and this in turn would affect the dynamics of the reactor. Depending on its dynamic characteristics, the reactor would either operate in a stable manner or destroy itself. Boiling reduces the hydrogen atom density with a subsequent loss of neutron moderation.

To study the boiling phenomena, a mockup of a single SNAP 10A reactor coolant channel was designed, fabricated, and operated. Realizing the limitations of a single-channel mockup compared to the multi-channel reactor, the

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7611-0358

Figure 152. Fission Product Gas Release for Unclad Sample at 3200° F for 7 hr (Test No. 10)

AI-AEC-13100

experiments were performed to provide an insight into boiling stability, the mechanism of boiling, and information that could be used in a simple description of reactor behavior.

Experiments were performed with the channel mockup for two types of situations. In one, the channel was completely submerged in water; and in the other, the channel was only partially filled with water.

(2) Analytical Models for Boiling

An analytical model was developed to predict the nature of boiling in a partially filled channel. The following assumptions were made with regard to this model:

- 1) There is no axial heat conduction in the water.
- 2) All of the heat is transferred uniformly to the liquid water.
- 3) The boiling temperature is 212° F.
- 4) Boiling takes place at a specific channel height, and the steam formed forces the water above it out of the channel.

Under the conditions of the fourth assumption, it was postulated that the system would behave in a cyclic manner. The water in the channel would be heated to the boiling temperature, then steam would be formed which would expel the water above the boiling region. Concurrently, cool water would enter through the bottom of the channel and mix with the water remaining in the channel. Then, the cycle of events would begin again with the reheating of the water to the boiling temperature.

The study of the completely filled channel was considerably more complex than the study of a partially filled channel. In a completely filled channel, there was a threshold power, below which boiling would not occur. Below this power, all of the heat produced is removed from the channel by natural convection. The flowrate through the channel was mainly dependent on pressure drops and density variations, but each of these were complex functions of geometry (entrance and exit effects in tricuspid channel, etc.), inlet temperatures, and heat generation distributions. The formation of vapor affects the flow through the channel. Because of these complications, no analytical model of the boiling

behavior of a completely filled channel was developed. Based on an assumed similarity in the mechanism of water boiling in the partially filled channel and the completely filled channel, it was estimated that as the power increases or as the inlet temperature increases, the frequency of the boiling cycle increases.

(3) Experimental Equipment

Different heating elements were arranged along the length of the mockup channel to produce a heat generation distribution that was similar to the cosine power distribution of the reactor. The maximum heat rate that could be applied to the channel was 6 kw. Power was supplied to the heater through a powerstat. Thirteen thermocouples were used to measure axial temperatures, and one of these was free to slide along the length of the channel.

(4) Results

The experimental results obtained in these tests were grouped into three categories: (1) measurements of axial temperatures, (2) frequencies of boiling, and (3) the effect of angle on boiling.

Axial temperature distributions were obtained for both the completely filled and the partially filled cases. The data obtained at 3.1 kwt power input for the completely filled channel are presented in Figure 153. Surface temperatures vs power input are given in Table 24.

Figure 154 shows the axial temperature distribution in a partially filled channel. In this case, the water level within the channel is 11 in. Here it is seen that for each higher power, higher temperatures are obtained. Figure 155 shows the maximum temperature as a function of power. The deviations from the curve are probably due to the effect of differences in inlet temperature.

As power increased, the frequency of boiling increased, and there appeared to be some power below which no boiling occurs. These are the same conclusions drawn in the theoretical analysis. Attempts to express the frequency of chugging as an empirical relationship that was a function of either power or inlet temperatures were not successful.

The power required for incipient boiling as a function of the angle of inclination was determined. This power was approximately the same for angles of 0,

Figure 153. Coolant Channel
Mockup Axial Temperature
Distribution

4-14-65

TABLE 24
SURFACE TEMPERATURE VERSUS POWER FOR
AN IMMERSED COOLANT CHANNEL

Power (kw)	Water Inlet Temperature (° F)	Maximum Surface Temperature (° F)	Remarks
0.10	72	195	-
1.02	72	212	-
1.30	72	220	-
1.86	72	228	-
2.61	72	238	-
3.48	77	250	Chugging, boiling
3.96	80	258	Chugging, boiling
4.46	82	268	Chugging, boiling
5.12	83	390*	Chugging, boiling

*Maximum temperature recorded before power was reduced to prevent damage.

6531-4518

Figure 154. Axial Temperature Distribution in a Partially Filled Channel (11 in. of water)

6531-4519

Figure 155. Maximum Temperature as a Function of
Power in a Partially Filled Channel
(11 in. of water)

30, and 60 deg (1.36 to 1.66 kw). However, for an angle of inclination of 90 deg, the power required was much less (0.58 kw). This was because less heat was lost by natural convection when the channel was horizontal.

(5) Conclusions

In this study it was shown that steady boiling could be achieved in a single-channel mockup of the reactor for channel powers up to 6 kw, which corresponds to a total reactor power of 440 kw. The boiling in a completely filled channel and a partially filled channel was qualitatively described, and progress was made toward a quantitative description.

The time behavior of the hydrogen atom density, necessary for reactor kinetics studies, and the effect of cross flow between channels on the parameters that have been measured required further study.

b. Transient Nuclear Test (TNT) Capsule Experiment⁽²⁵⁾

Capsules containing SNAP fuel were placed outside a Kiwi reactor core which was destroyed in a nuclear excursion. The experimental capsules were designed and fabricated primarily to accomplish four objectives:

- 1) A measurement of the integral thermal expansion of U-ZrH 1.82 fuel up to the temperature of disintegration.
- 2) A measurement of peak pressure that occurs during U-ZrH 1.82 fuel disintegration.
- 3) A measurement of the impulse that occurs during U-ZrH 1.82 fuel disintegration.
- 4) A determination if well constrained U-ZrH 1.82 fuel disintegrates at high temperatures.

To cover the four objectives, four types of experiments or measurements were designed and eight capsules for each experiment were fabricated. The fuel used for the experiments was obtained from 10 wt % U - (93% enriched) uranium-zirconium-hydrided (H/Zr ratio of 1.82) fuel rods, which had originally been manufactured for use in SNAP 10A reactors. The pressure containers used for the capsule bodies were identically constructed from heat-treated, Vega tool steel. The four types of capsule inserts were assembled into individual pressure containers.

In the constrained fuel experiment, the void volume was minimized by stressing the copper cylinders during final assembly so that copper would flow into and fill adjacent voids. In the peak-pressure experiment, fuel samples were in chambers sealed with prestressed rupture disks calibrated to burst within 5% of specific pressure values. In the impulse experiment, hydrogen gas released from fuel acted on a free piston. After travel of the piston for a predetermined distance, porting of the gas reduced the driving pressure. The piston then impacted on a copper anvil, producing an indentation that was related to piston energy (and therefore to impact velocity) by means of calibration tests. From the experimental values of impact velocity, distance over which gas pressure accelerated the piston, and piston mass, it was possible to construct an approximate curve of gas pressure as a function of time. In the thermal expansion experiment, the fuel rod pushed on a mechanical leverage system in which a follower advanced against the surface of an inclined plane inside a quartered spline.

In order to obtain a range of temperatures needed for the four measurements, the capsules were stacked in eight containers (4 capsules each) and placed at four different positions outside the test reactor. During the test, heating was obtained in the form of fissioning in the U-ZrH 1.82 fuel from the neutrons leaking from the Kiwi test reactor. Figure 156 shows the capsule loading order and their locations in relation to the test reactor.

The Kiwi reactor transient test occurred on January 12, 1965 and lasted for 10 to 20 msec. The test resulted in a nuclear excursion which destroyed the test reactor. Extensive post-irradiation examinations were conducted.

Twenty-five recovered fuel capsules were identified. Fifteen of these were in excellent condition, six had lids blown off (otherwise in good condition), and four were severely damaged. Also examined were six longitudinal halves of unidentified capsule bodies and portions of the aluminum capsule containers, which were in various stages of melting or damage.

It appeared that in some of the experiments, the pressure created by the fissioning fuel exceeded the strength of the capsule bodies. Therefore, the internal pressure was greater than 200,000 psi. However, under certain conditions, the capsule bodies could have split with a dynamic load of 80,000 psi.

6531-4520

Figure 156. Capsule Loading and Location of Capsule Containers in Relation to Reactor During Test

The control experiment consisted of a single fuel specimen, sandwiched between two copper plugs. Four of the eight control capsules were recovered. The fuel was intact, and showed no evidence of disintegration. The fuel specimen from position 2B showed radial cracking starting from the center of the specimens. The two copper plugs on either end of the fuel specimen provided rapid heat transfer from the hot fuel (after the nuclear transient), causing internal cracking from thermal stresses within the fuel. After disassembly, the fuel specimen and capsule components were measured and weighed, and the densities were determined. Percent changes in the physical measurements are presented in Table 25 for the control fuel specimens.

Only one capsule from the peak pressure experiments showed any indication of a measurement. This capsule was from the second position and showed a partially ruptured 50,000-psi diaphragm. A fuel specimen with a 100,000-psi diaphragm showed the specimen to be intact and the diaphragm unruptured. Conclusions based on these two capsules indicated that the damage to the capsules probably occurred before or during the peak pressure rise, thereby relieving the pressure before the diaphragm could rupture. Probably, no peak pressure was measured. However, the peak pressure experiments did lend support to the fuel stability at high temperatures.

The impulse experiments showed that three pistons had discharged in the second position leaving a record on a copper anvil, and that two pistons in the third position had indicated that they had attempted to discharge. No measurements were indicated in the fourth position. The first-position capsules were badly damaged and offered no information. The anvil measurements indicated that one capsule from the second position had an impact energy of 8.7×10^5 ergs, which corresponded to an average release of 510 kcal/g-mol H_2 . The other capsule from the second position showed an impact energy of 4.6×10^6 ergs, which corresponded to an average of 573 kcal/g-mol H_2 .

None of the other impulse experiments from the other three positions offered any measurements. A hydrogen loss and a diameter increase was observed on the fuel specimens that actually released a piston. The hydrogen loss indicated temperatures above 1000° F for the fuel specimens from these two capsules. These observations are supportive of the postulated mechanism of the fuel disintegrations, in which high internal pressure caused by hydrogen gas collecting in

TABLE 25

CONTROL (CONSTRAINED) EXPERIMENTS -
MEASUREMENTS AND GENERAL CAPSULE CONDITION

Position of Capsule and Identification	% ΔD Fuel Diameter	% ΔL Fuel Length	% $\Delta \rho$ Fuel Density	Hydrogen Change in Fuel (wt %)	Burnup Analysis Uranium (at. %)	Condition of Fuel Specimen	Condition of Capsule
First							
No. 17 A	*	*	*	*	*	*	Missing
No. 15 B	*	*	*	*	*	*	Missing
Second							
No. 13 A	*	*	*	*	*	*	Missing
No. 5 B	-0.06	+2.34	-0.80	-0.04	1.23×10^{-5}	Radially cracked internally	Intact
Third							
No. 6 A	*	*	*	*	*	*	Intact
No. 31 B	No change	-0.16	-0.01	*	1.72×10^{-5}	Longitudinally cracked	Intact
Fourth							
No. 32 A	-0.01	*	+0.13	*	9.03×10^{-6}	Light cracking, some chipping	Intact
No. 24 B	-0.09	+0.09	+0.14	No change	4.79×10^{-6}	No cracks	Intact

*Not measured

microvoids lead to stresses that exceed the yield stress of the material. However, the fuel data showed for the most part that the fuel remained in good condition, even though it was not constrained, and was subjected to temperatures above those needed for fuel disintegration.

Three of the capsules used in the expansion experiments were recovered undamaged; two had damaged heads (otherwise were in good condition), and two were badly damaged. Disassembly showed the fuel to be broken in 3 to 8 pieces. Both capsules from the first position were recovered, but only one contained fuel, which had been melted. Table 26 shows the measurements and the general condition of the capsules after the test. The three "good" readings (capsules 7, 10, and 20) seem to decrease with the hypothesized neutron exposure; i.e., position 2 received $2/3$, position 3 received $1/2$, and position 4 received $1/10$ the neutrons received by position 1. The plot in Figure 157 is based on this assumption. The three questionable readings are also shown in this plot and show that an unreasonable extrapolation would be required for the value for coefficient of thermal expansion if they were made to fit the hypothesized trend in neutron exposure.

The energy released from the fuel in the capsule during the Kiwi test reactor excursion was determined. A linear correlation existed between the energy released in terms of kilocalories/gram-mole of hydrogen and the fuel burnup in terms of percent U^{235} fissioned. The fuel energy release for each capsule and its position relative to the reactor is presented in Table 27. A total of 18 kcal/g-mol of H_2 are required to raise the U-ZrH fuel temperature from ambient to $1100^\circ C$, assuming adiabatic conditions. If greater than 18 kcal/g-mol of H_2 are generated, the energy is used to dissociate the hydrogen, perform physical and chemical changes in the fuel, and perhaps produce fuel melting and disintegration.

Figure 158 shows energy release vs distance from the reactor. When plotted on semilogarithmic coordinates, linear relationships were obtained.

The TNT experiment capsules provided data on thermal expansion of U-ZrH fuel, which showed it to be a function of neutron exposure. Peak-pressure and impulse measurements indicated that the fuel had high stability and maintained integrity at high temperatures.

TABLE 26
EXPANSION EXPERIMENTS –
MEASUREMENTS AND GENERAL CAPSULE CONDITION

Position of Capsule and Identification	% Δ D Fuel Diameter	% Δ L Fuel Length	% $\Delta\rho$ Fuel Density	Hydrogen Change in Fuel (wt %)	Burnup Analysis Uranium (at. %)	Condition of Fuel Specimen	Capsule Movement (in.)	Condition of Capsule
First								
No. 27 A	-	-	-	-	-	No fuel	-	Neck fracture, signs of surface melting
No. 10 B	-	-	-	-	1.08×10^{-3}	Melted fuel	0.216	Neck fracture, signs of surface melting
Second								
No. 20 A	No change	-	+0.39		1.61×10^{-4}	5 pieces	0.199	Neck fracture, some surface melting
No. 1 B	+1.12	-	+0.52	-0.13	1.98×10^{-4}	8 pieces	0.345	Intact, some surface melting
Third								
No. 14 A	No change	-	+0.64		9.83×10^{-5}	4 pieces	0.389	Cracked lid, good condition
No. 7 B	+0.05	-	+0.82		1.26×10^{-4}	6 pieces	0.098	Intact, good condition
Fourth								
No. 26 A	-	-	-	-	-	-	-	Missing
No. 30 B	-0.10	-	+0.33	No change	3.02×10^{-5}	3 pieces	2.285	Intact, good condition

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Figure 157. Fractional Thermal Expansion as a Function of Fuel Position

TABLE 27

AVERAGE ENERGY RELEASED (kcal/g-mol H₂) FOR EACH EXPERIMENT, CALCULATED FROM BURNUP ANALYSIS

Experiment	Position 1		Position 2		Position 3		Position 4	
A							15	
Control							32	
B			20*	5	26	31	9	24
A	12*		24*		83		33	
Peak pressure		18		9		23		29
B					62	28	28	33
A			575		196		56	
Impulse				25		4		8
B			510	21	155	16	46	11
A			258		160			
Expansion				20		14		
B	1638	10	319	1	198	7	49	30

☐ Capsule identification *Denotes burnup data not used for calculations

Figure 158. TNT Fuel Energy Release for Each Experiment vs Distance from Reactor

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Figure 159. Diagram of SNAPTRAN-1 Reactor

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6. SNAPTRAN-1 Experiment⁽²⁶⁻²⁸⁾

a. Scope and Purpose

The SNAPTRAN-1 test series included a number of nondestructive power transients, the purpose of which was to enable a detailed study to be made of the prompt neutron kinetics of the SNAP core with a beryllium reflector. Extensive analyses of these tests were performed to extract basic kinetics data over a wide range of variables.

b. Test Apparatus

The SNAPTRAN-1 reactor consisted of a SNAP 10A/2-type core and reflector. Control of the four reflector control drums was by means of special drive assemblies. Two diametrically opposed drive assemblies were provided with pneumatically driven, rack-and-pinion operation through a 45-deg arc, allowing step reactivity insertions (\$14/sec/single drum) to be made. The other two control drums were provided with impulse control; i.e., 450-deg/drum rotation from the least reactive position through the most reactive position, then coming to rest at its least reactive position. The impulse mode of operation was provided with rack-and-pinion movement of the control drums. Reactivity insertion rates of \$185/sec/single drum were attainable. Either single- or coupled-drum step operations impulse operation were provided. All four control drums were equipped with variable speed drive motors for reactivity insertions up to \$0.06/sec/drum in the normal mode.

Fabrication and mechanical checkout of the SNAPTRAN-1 reactor was completed at AI. The unit was transported to the National Reactor Testing Station (NRTS) where the test program was conducted by Phillips Petroleum Co. (PPCo) at the Initial Engineering Test (IET) Facility.

The SNAPTRAN-1 reactor and support structure is shown diagrammatically in Figure 159. A layout of the reactor, showing fuel element, drum positions, and drum travel diagrams, is shown in Figure 160.

Instrumentation for the SNAPTRAN-1 reactor included power, temperature, pressure, strain, and drum position sensors. The power level of the reactor was determined by means of neutron and gamma detectors. A total of 20 fast neutron, and gamma ionization chambers and fast neutron scintillation detectors

IMPULSE DRUM TRAVEL

STEPPER DRUM TRAVEL

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Figure 160. Cross Section of Core-Reflector Assembly and Drum Travel Diagrams

were mounted either in-core or externally to monitor reactor power levels from source level to 4×10^{11} w. Calibration of these detectors was made at a known reactor power level of 0.91 kw.

Two methods of temperature measurements were used in the nondestructive test series: (1) direct fuel-temperature measurements with welded and diaphragm-ended thermocouples and interstitial temperature sensors, and (2) energy probes. A total of 35 thermocouples were used to measure fuel element temperatures and grid plate and vessel wall temperatures. Many energy probes were used during the test series to provide radial and axial temperature profiles. Five types of energy probes were used to provide these profiles: 10 wt % U-hydrided (10H), 5 wt % U-hydrided (5H), 10 wt % U-unhydrided (10N), 5 wt % U-unhydrided (5N), and no uranium-hydrided (0H).

Six fuel elements were fitted with pressure transducers to monitor hydrogen release from the fuel moderator. Sensitivity of these transducers ranged from 50 to 3000 psig. Dummy transducers were also monitored to determine radiation sensitivity.

Axial and circumferential strain gages were attached to the cladding of two fuel elements to monitor cladding strain. Radiation sensitivity was measured on dummy strain gages. A circumferential strain gage was placed on the reactor vessel at its centerline to measure vessel expansion.

For purposes of data analysis, the position of all control drums (static and transient) were monitored during each test. Both servo-synchro digital indication systems and individually mounted potentiometers provided these data.

As previously discussed in Section III-D-1, selected sensors were recorded on FM magnetic tape recorders. The SNAPTRAN-1 test series involved eight tape transports, each consisting of 14 data channels.

c. Discussion of Test Results

Independent analysis of these test data were made by both PPCo and AI site personnel. From these data, preliminary "working plots" were made to predict results for succeeding tests and to ascertain that threshold limits would not be exceeded that would result in premature damage to the reactor core or components.

A list of transient test limits established for both step and impulse transient series are summarized in Table 28.

TABLE 28
SNAPTRAN-1 TRANSIENT TEST SERIES LIMITS

Test Series	Hotspot Fuel Temperature (° F)	Fuel Element Pressure (psig)	Fuel Element Elongation (%)	Circumferential Vessel Elongation (%)
Step tests, ambient	1500	800	1	0.1
Step tests, elevated	1700	800	1.2	0.5
Impulse, single drum	1500	800	1	0.1
Impulse, coupled drum	1500	800	1	0.1

A total of 29 step transients were initiated from ambient starting temperatures. Reactivity inputs ranging from \$0.27 to \$1.50 produced reactor periods ranging from 6670 to 1.7 msec. During the initial tests where the reactor period was greater than 500 msec, the transient was terminated with a manual scram. All other transients were terminated by a programmed scram. A signal started drum movement an equivalent of 1.5 to 3 down-side periods after the peak of the power burst.

After a transient had been completed, the oscillographic records were analyzed to determine peak power, temperature at peak power, maximum core temperatures, maximum fuel and vessel strain, and maximum fuel element pressure. Values of peak power were obtained from those nuclear detectors biased for the peak. The stable reactor period was determined from higher-sensitivity nuclear detectors. Energy release, both to peak power and total, were determined by integration of a power trace, then normalized to a nuclear detector which described the average peak power. Fuel element hotspot temperatures were determined directly from energy probe and thermocouple data. Average core temperatures were determined from volume-weighted radial and axial energy probe and thermocouple temperature profiles. The maximum values of fuel element pressure and axial strain were determined from direct and remote transducers.

Plots of the ambient temperature step-transient preliminary results are presented to illustrate the progression of the test series. Each of the variables monitored during the test series are plotted as a function of reciprocal period. These include peak power, energy release, temperature, pressure, and strain. Figure 161 shows the relationship of peak power, energy release to peak power, and temperature rise (hotspot) to peak power observed during the series. Maximum energy release and core temperature rise plots are shown in Figure 162. The respective fuel element pressure and axial strain values are noted on Figure 163. This moderate hydrogen release observed during the ambient temperature step-transient test series provided the basis for extending the transient test limits (see Table 28) and initiated the elevated temperature test series. Figure 164 shows the inhour relationship obtained during the ambient temperature step-test series.

As a part of the SNAPTRAN objectives, it was desired to extend the average core temperature as high as possible to determine the magnitude of temperature feedback mechanisms. The previous test series was extended until maximum core temperatures were raised to approximately 1300° F as a result of a single \$1.70 coupled drum transient. To accomplish this, a series of transients were run where the average core temperature was raised above ambient temperature, using nuclear heat. These tests were termed the "cascade step-test series." The first transient of a cascade series involved higher reactivity inputs (\$1.50 to \$1.68). After thermal equilibrium in the core had been established, the reactor was subjected to a second transient, then a third and fourth of lesser reactivity inputs (\$1.40 to \$1.10). As a result of the cascaded transients, the maximum core temperatures were approximately the same value as the previous transient, while the average core temperature at peak power increased significantly over that obtained from a single-step transient. A total of five cascade step-test series were run and temperature feedback mechanisms and reactor characteristics determined over that elevated temperature range. Figure 165 shows the results of a typical cascade step series. Plots of transient behavior are shown for this series in Figures 166 and 167.

The single-drum impulse transient test series extended reactivity inputs from the maximum obtained in the step-transient series (\$1.70) to \$2.34. The

Figure 161. Preliminary Peak Power, Energy Release, Temperature Rise Plots of the SNAPTRAN-1 Ambient Temperature Step Tests

Figure 162. Preliminary Total Energy Release and Maximum Temperature Plots for the SNAPTRAN-1 Ambient Temperature Step Tests

Figure 163. Preliminary Fuel Element Pressure and Strain Plots of the SNAPTRAN-1 Ambient Temperature Step Tests

Figure 164. Preliminary Inhour Plot of the SNAPTRAN-1 Ambient Temperature Step Tests

Figure 165. Cascade Step Test Series No. 5

Figure 166. Preliminary Peak Power, Energy Release and Temperature Rise at Peak Power of the SNAPTRAN-1 Elevated Temperature Step Tests

Figure 167. Preliminary Peak Power and Maximum Temperature vs Total Energy Release of the SNAPTRAN-1 Elevated Temperature Step Tests

impulse mode of reactivity insertion provided a well defined 28-msec reactivity pulse from -135 to 0 to +135-deg drum rotation. Kinetic behavior of the reactor was determined, using a single reactivity insertion starting from various sub-critical and critical power levels.

The first eight of the 27 single-drum impulse tests were initiated from milliwatt power levels. Four tests were run with a reactivity input of \$2.16 and the peak-to-starting-power ratio was determined. These tests provided a basis for performing similar transients at \$2.34 input and higher starting power levels. The remaining 19 transients were performed with \$2.34 reactivity input. During this latter series, the starting power level was increased from 10 mw to 260 w.

The preliminary data plots used in the step-transient test series were expanded to include the ratio of peak to starting power, energy release and temperature rise as a function of starting power. Figure 168 presents preliminary values of peak power and energy release (normalized) as a function of inserted reactivity. The reactor power ratio, energy release and temperature rise as a function of starting power are shown in Figure 169. Reactor responses to \$2.34

Figure 168. Preliminary Peak Power vs Programmed Reactivity of the SNAPTRAN-1 Single-drum Impulse Tests

Figure 169. Preliminary Reactor Power Ratio, Energy Release, and Temperature Rise Data of the SNAPTRAN-1 Single-drum Impulse Tests

reactivity insertions were sufficiently fast that the starting power increased from 0.01 w to approximately 1.0 w before significant temperature rise resulted.

A series of transients were performed, using the highest reactivity insertion mode of the SNAPTRAN-1 test series. Reactivities ranging from \$2.00 to \$4.15 were inserted by means of coupled-drum impulse tests. The influence of reflector neutron delay on the dynamic behavior was observed, and the prompt-temperature coefficient of reactivity was measured in transients where significant nuclear heat was developed.

The initial coupled-drum impulse transients had reactivity insertion values close to the maximum of the single-drum impulse transients. The reactivity input was increased from \$2.00 to \$3.00 in a stepwise fashion to establish the initial effects of coupled-drum reactivity insertions. Plots of peak power vs the ratio of peak-to-starting power and reactivity were determined during these initial tests. The corresponding peak power and required starting power for succeeding higher reactivity inputs were determined by direct extrapolation of these plots. The reactivity inputs were then increased until the resulting energy release limits established for the series were approached. The maximum reactivity input attained was \$4.15. The final tests in the coupled-drum impulse series was conducted to increase the average and maximum core temperatures to the maximum (1500°F). This was accomplished by using a constant reactivity input of \$3.90 and increasing the starting power level to obtain much higher peak powers, energy releases, and temperatures. The maximum hotspot temperature rise observed during this series was 1485°F, which resulted from a \$3.90 transient initiated from a 1.67-w starting power level and an initial starting temperature of 80°F. The peak-to-starting power level ratio and reactivity insertion as a function of peak power is plotted in Figure 170. Figure 171 shows total energy release and the power ratio as a function of reciprocal period. A summary of the coupled-drum impulse test series results is shown in Figure 172, where power ratio, peak power, maximum temperature rise, and total energy release are plotted as a function of starting power.

d. Analysis of Results and Conclusions

The pertinent drum calibrations for the SNAPTRAN-1 transient test series are summarized in Figure 173. These curves give integral worth of the drums

Figure 170. Preliminary Peak Power vs Power Ratio and Programmed Reactivity of the SNAPTRAN-1 Coupled-drum Impulse Test

Figure 171. Preliminary Power Ratio and Energy Release vs Reciprocal Period of the SNAPTRAN-1 Coupled-drum Impulse Tests

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Figure 172. Preliminary Peak Power, Power Ratio
Energy Release, and Temperature Rise Data at
Constant Reactivity of the SNAPTRAN-1
Coupled-Drum Impulse Tests

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Figure 173. Integral Drum Worth Curves
for SNAPTRAN-1

used in the step transients, B and C, single impulse transients, drum A, and for the coupled impulse transients, drums A and D. The calibrations were made using "rod bump" techniques. Any interaction effects caused in calibration technique are significantly less than calibration error due to inherent experimental error.

A total of 33 step transients were run with reactivity insertions larger than \$1.10. These included ten transients initiated from elevated temperatures. A summary of these tests is presented in Table 29.

An inhour plot based on a set of results from the step tests above prompt critical is shown in Figure 174. A straight line has been fit to the data, using the method of least squares. The slope of this line, the ratio of the prompt neutron lifetime to the effective delayed neutron fraction, (ℓ/β) is $(8.3 \pm 0.1) \times 10^{-4}$ sec. From this, a prompt neutron lifetime of $6.6 \pm 1 \mu\text{sec}$ was found, based on a β_{eff} of 0.008. Both of these results are for 95% confidence intervals.

Figure 174. Inhour Data from Step Tests above Prompt Critical

All of the energy probes, 0H, 2.5N, 2.5H, 5N, 5H, 10N, and 10H, were used to determine radial and axial energy profiles. Examination of the integrated nuclear detector power data and volume-weighted energy profile data throughout the series increased confidence that the energy probe time response

TABLE 29
RESULTS FROM STEP TESTS ABOVE PROMPT CRITICAL

Transient No.	Date of Test	Peak Power (Mw)	Energy Release to Peak Power (Mw-sec)	Drum-Inserted Reactivity (β)	Period (msec)	Initial Temp. ($^{\circ}$ F)	Avg. Temp. Rise to Peak Power ($^{\circ}$ F)	Normalized Peak Power (Mw)	Prompt Temp. Coef. ($\beta/^{\circ}$ F)	Range of Temp. Coefficient Calculation ($^{\circ}$ F)	PCAL	Inverse Period (msec $^{-1}$)	Mean Temp. $\bar{T}$ ($^{\circ}$ F)
128	11-12-64	64.0	1.16	1.10	8.3	77.	80	42.0	-0.121	130 - 219	1.524	0.12048	174.5
131	11-18-64	151.3	1.57	1.18	5.15	72.	121	110.5	-0.125	124 - 286	1.369	0.19417	205.
133	11-20-64	375.8	2.55	1.26	3.2	82.	184	304.4	-0.136	132 - 410	1.235	0.31250	271.
136	12-11-64	656.0	2.58	1.32	2.4	73.	247	544.4	-0.151	123 - 500	1.205	0.41666	311.5
138	12-14-64	787.0	3.48	1.35	2.2	66.	266	617.7	-0.136	118 - 523	1.274	0.45454	320.5
140	12-16-64	982.0	3.96	1.39	2.0	70.	274	755.6	-0.149	125 - 395	1.300	0.50	260.
142	12-18-64	1280.0	4.65	1.50	1.7	52.	315	953.6	-0.150	103 - 510	1.342	0.58823	306.5
144	12-22-64	1267.0	4.51	1.48	1.8	62.	289	990.1	-0.144	115 - 604	1.280	0.55555	360.
159	01-14-65	663.0	3.30	1.31	2.55	82.	236	498.8	-0.148	136 - 412	1.329	0.39215	274.
200	04-22-65	650.0	3.61	1.38	2.2	66.	203	421.0	-0.172	118 - 406	1.544	0.45454	262.
201	04-23-65	1385.0	6.27	1.48	1.77	68.	296	888.9	-0.152	120 - 451	1.558	0.56497	285.5
203	04-28-65	1487.0	6.37	1.52	1.675	70.	317	981.4	-0.146	122 - 616	1.515	0.59701	369.
204	04-29-65	1480.0	5.52	1.52	1.65	75.	311	997.1	-0.145	126 - 667	1.484	0.60606	396.5
205	04-30-65	1727.0	6.42	1.53	1.57	75.	330	1140.1	-0.150	126 - 652	1.515	0.63694	389.0
206	05-03-65	2267.0	7.43	1.57	1.42	50.	366	1373.9	-0.155	102 - 702	1.650	0.70422	402.
207	05-04-65	2120.0	6.53	1.58	1.45	55.	351	1374.6	-0.155	107 - 701	1.542	0.68965	404.
209	05-05-65	1225.0	4.87	1.47	1.77	63.	292	838.9	-0.146	114 - 565	1.460	0.56497	339.5
210	05-05-65	63.0	1.1	1.10	9.17	432.1	122	-	-	486 - 549		0.10905	517.5
211	05-06-65	1587.0	11.02	1.50	1.59	82.5	311	1069.2	-0.159	135 - 623	1.484	0.62893	379.0
212	05-06-65	33.9	0.50	1.1	8.1	696.5	121	-	-	812 - 834		0.12345	823.
213	05-06-65	740.0	3.8	1.35	2.3	630.2	181	584.2	-0.164	708 - 969	1.267	0.43478	838.5
246	08-02-65	1550.0	5.27	1.52	1.69	75.	292	909.7	-0.173	150 - 563	1.704	0.59171	356.5
247	08-02-65	1258.0	4.99	1.42	1.94	598.8	202	797.0	-0.181	650 - 965	1.578	0.51546	807.5
248	08-02-65	330.0	2.77	1.20	4.18	900.	101	221.5	-0.193	956 - 1001	1.490	0.23923	978.5
249	08-03-65	1671.0	5.63	1.52	1.61	231.	276	1041.0	-0.182	288 - 660	1.605	0.62111	474.
250	08-03-65	958.0	4.87	1.36	2.25	693.	169	593.1	-0.181	745 - 1000	1.615	0.44444	872.5
251	08-03-65	376.0	3.03	1.20	4.0	911.	109	249.1	-0.189	963 - 1001	1.509	0.25	982.
252	08-03-65	253.0	2.62	1.15	4.9	1072.	84	164.1	-0.191	1123 - 1234	1.542	0.20408	1178.5
253	08-05-65	3200.0	8.1	1.68	1.21	95.	405	1995.5	-0.182	145 - 762	1.604	0.82644	453.5
254	08-05-65	855.0	3.24	1.36	2.27	642.	175	579.1	-0.175	694 - 956	1.476	0.44052	825.
255	08-05-65	315.0	2.50	1.19	4.18	900.	100	228.9	-0.196	955 - 1000	1.376	0.23923	977.5
256	08-05-65	241.0	2.46	1.14	5.29	1061.	79	153.8	-0.188	1114 - 1208	1.567	0.18903	1161.0

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was adequate and that heat losses were negligible. This was found to be untrue for the step transients initiated near or less than prompt critical. Large time-lag response was observed during these tests due to heat losses in each energy probe. A heat transfer model had to be developed to obtain meaningful energy probe data. Temperature coefficient data derived from the step-transient series is discussed below. Results from these step tests near or below prompt critical are presented in Table 30.

A total of 37 single-drum or coupled-drum impulse transients were run on the SNAPTRAN-1 reactor to determine reactor response from this mode of transient. Only seven single-drum and four coupled-drum impulse tests experienced sufficient rise in average core temperature to allow the possibility of temperature reactivity feedback calculations. The results of the impulse transients are presented in Table 31. The calculated generation times given in the table were found by plotting the drum-inserted reactivity vs the inverse period for individual tests. A 95% confidence interval mean of these data is 6.44 ± 0.6 μsec .

The magnitude of the temperature coefficient observed in the SNAPTRAN-1 experiments showed a tendency to increase as the test series progressed; i.e., -0.12 to -0.19¢/°F. Figure 175 shows the coefficient plotted vs the accumulated energy release by transients during the test series. Similar plots of the temperature coefficient data with inverse period and temperature suggests a dependence of the coefficient on these variables. A least-squares fit technique was used to determine existence of this correlation. Figures 176 and 177 show the dependence of the temperature coefficient on the inverse period and mean temperature, respectively. (Also shown on these figures are the results calculated by the TSN model.) Figure 178 shows the data of Figure 175 with both dependencies removed. From this it can be seen that the mean temperature and inverse period correlations are sufficient to remove the apparent increase during the test series.

The static coefficients due to fuel expansion, spectral changes, and Doppler broadening of resonances are evaluated separately, using experimental data for the fuel expansion, the spectrum-weighted cross sections, and calculated values of the resonance changes with temperature in the ANISN code. The results are shown in Figure 179.

TABLE 30
RESULTS FROM STEP TESTS NEAR OR BELOW PROMPT CRITICAL

Transient No.	Date of Test	Peak Power (Mw)	Energy Release to Peak Power (Mw-sec)	Inserted Reactivity (β)	Period (msec)	Initial Temp. ($^{\circ}$ F)	Avg. Temp. Rise to Peak Power ($^{\circ}$ F)	Normalized Peak Power (Mw)	Prompt Temp. Coef. ($\beta/^{\circ}$ F)	Range of Temp. Coefficient Calculation ($^{\circ}$ F)	PCAL	Mean Temp. $\bar{T}$ ($^{\circ}$ F)
111	09-28-64	0.124	2.24	0.485	6670.	72	186	0.0907	-0.13	111 - 420	1.367	265.5
112	09-30-64	0.333	2.44	0.66	2410.	69	210	0.243	-0.14	109 - 525	1.370	317.0
113	10-02-64	0.713	2.24	0.80	1020.	67	244	0.521	-0.12	113 - 328	1.369	220.5
115	10-09-64	1.31	1.87	0.91	340.	64	178	0.976	-0.14	111 - 431	1.342	271.
116	10-15-64	2.04	1.58	0.96	152.	65	130	1.59	-0.132	119 - 300	1.283	209.5
119	10-27-64	3.12	0.62	0.99	50.6	83	53	2.35	-0.141	120 - 334	1.328	227.
121	10-30-64	10.3	0.48	1.03	22.0	78	30	8.01	-0.117	112 - 174	1.286	143.
124	11-06-64	9.8	0.46	1.02	22.0	61	40	7.64	-0.112	99 - 199	1.283	149.
126	11-10-64	30.9	0.72	1.06	12.0	79	60	24.0	-0.122	114 - 192	1.288	153.
147	01-05-65	7.57	0.42	1.02	26.3	71	36	5.83	-0.120	107 - 186	1.298	146.5
149	01-06-65	6.15	4.41	1.02	31.4	84	35	4.51	-0.119	110 - 153	1.364	131.5
155	01-12-65	0.038	1.40	0.29	21,750.	75	163	0.028	-0.110	94 - 244	1.357	169
157	01-13-65	0.650	3.30	0.72	1490.	79	264	0.48	-0.125	109 - 371	1.354	24.0

TABLE 31
RESULTS FROM IMPULSE TESTS

Transient No.	Date of Test	Peak Power (Mw)	Energy Release to Peak Power (Mw-sec)	Drum-Inserted Reactivity (\$)	Period (msec)	Initial Temp. (°F)	Avg. Temp. Rise to Peak Power (°F)	Normalized Peak Power (Mw)	Prompt Temp. Coef. (d/°F)	Range of Temp. Coefficient Calculation (°F)	Generation Time (μsec)	PCAL	Inverse Period (msec ⁻¹)	Mean Temp. T (°F)
First Series of Single-Drum Impulse Tests:														
177	02-12-65	1623	2.78	2.37	0.59	95.8	245.	-	-	-	-			
178	02-15-65	1975	3.42	2.37	0.59	68.3	224.	-	-	-	6.6x10 ⁻⁶			
First Series of Coupled-Drum Impulse Tests:														
197	04-19-65	13	0.02	3.85	0.27	61.5	0	-	-	-	6.7x10 ⁻⁶			
198	04-19-65	500	0.83	3.99	0.27	61.8	23.3	-	-	-	6.5x10 ⁻⁶			
Second Series of Single-Drum Impulse Tests:														
215	06-11-65	750	1.42	2.37	0.61	70.0	92.5	-	-	-				
217	06-16-65	1048	1.89	2.37	0.60	60.0	114.4	-	-	-	6.3x10 ⁻⁶			
220	06-18-65	1873	3.16	2.37	0.61	61.5	149.5	-	-	-	6.25x10 ⁻⁶			
221	06-18-65	1850	3.18	2.37	0.61	82.0	190.	1163.	-0.187	134 - 415	6.42x10 ⁻⁶	1.591	1.64	274.5
222	06-21-65	2410	3.80	2.37	0.60	80.	224.	1547.	-0.196	135 - 479	6.3x10 ⁻⁶	1.558	1.67	307.
223	06-22-65	2543	4.05	2.37	0.60	78.	260.	1784.	-0.177	131 - 521	6.4x10 ⁻⁶	1.425	1.67	326.
224	06-22-65	2910	4.49	2.37	0.60	120.	259.	1917	-0.194	170 - 557	6.4x10 ⁻⁶	1.518	1.67	363.5
225	06-23-65	3307	5.10	2.37	0.63	85.	406.	4418	-0.176	145 - 639	6.4x10 ⁻⁶	0.7485	1.59	392.0
226	06-23-65	3783	5.65	2.37	0.62	120.	328.	2569	-0.172	192 - 685	6.6x10 ⁻⁶	1.473	1.61	438.5
227	06-24-65	4543	6.65	2.37	0.60	90.0	310.	1656	-0.172	139 - 630	6.3x10 ⁻⁶	2.743	1.67	384.5
Second Series of Coupled-Drum Impulse Tests:														
231	07-06-65	67	0.88	3.86	0.28	73.4	6.2	-	-	-	6.4x10 ⁻⁶			
232	07-07-65	141	1.90	3.97	0.27	70.8	11.2	-	-	-	6.4x10 ⁻⁶			
233	07-08-65	445	0.55	3.75	0.28	78.	24.2	-	-	-	-			
234	07-09-65	826	1.03	3.75	0.29	72.7	45.3	-	-	-	6.5x10 ⁻⁶			
235	07-12-65	1267	1.11	3.75	0.29	30.	100.0	-	-	-	6.4x10 ⁻⁶			
236	07-12-65	1463	1.83	3.75	0.29	100.	120.5	-	-	-	6.6x10 ⁻⁶			
237	07-13-65	2190	2.58	3.75	0.29	80.	153.	-	-	-	6.6x10 ⁻⁶			
238	07-13-65	2157	2.30	3.75	0.29	103.	280.	-	-	-	6.5x10 ⁻⁶			
240	07-15-65	3083	3.23	3.71	0.28	85.	203.	-	-	-	6.2x10 ⁻⁶			
241	07-16-65	3733	4.01	3.75	0.29	86.	250.	2500	-0.173	138 - 442	6.4x10 ⁻⁶	1.493	3.45	290.
242	07-16-65	3700	4.16	3.75	0.29	103.	193.	2302	-0.175	156 - 498	6.7x10 ⁻⁶	1.607	3.45	327.0
243	07-19-65	4267	4.74	3.75	0.29	78.	269	2787	-0.186	131 - 539	6.3x10 ⁻⁶	1.531	3.45	335.
244	07-20-65	5720	6.56	3.75	0.29	76.	376.	4133	-0.158	130 - 697	6.4x10 ⁻⁶	1.384	3.45	413.5

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Figure 175. Variation of Temperature Coefficient with Accumulated Energy

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Figure 176. Variation of Corrected Temperature Coefficient With Inverse Period (Experiment vs Calculation)

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Figure 177. Variation of Corrected Temperature Coefficient With Mean Temperature

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Figure 178. Variation of Corrected Temperature Coefficient
With Accumulated Energy

Figure 179. SNAPTRAN 10A/2 Temperature Coefficient—
Static Transport Calculation (ANISN)

The axially weighted data were used with the TSN code to simulate reactor step transients and calculate dynamic coefficients over a range of inverse period from 400 to 5000 sec^{-1} . The mean coefficient to each of several temperatures was evaluated, giving a set over a range of temperatures and inverse periods; the results are shown in Figure 179. As shown, the coefficient to a given temperature depends linearly on inverse period. The temperature dependence of the dynamic coefficient is not comparable to the static coefficient, shown in Figure 179, as the effective temperature coefficient increases with inverse period. Thus, correcting a transient for its "period effect" based on a correlation of mean temperature and inverse period assures that the first portion of the compensation will be treated correctly. For a transient covering a large ΔT , a wide range of periods are encountered. Toward the end of a transient, the inverse period may be zero or negative. The magnitude of the observed dynamic coefficient should decline with increasing ΔT , as observed. Using the calculated value of $\bar{\alpha}$, a variation of 12% in $\bar{\alpha}$ is due to this effect for an inverse period of 5000 sec^{-1} .

The generation time, Λ , was calculated from the single- and coupled-drum impulse tests. Using these data, it was found that the same coefficient results

for both tests if analyzed on the basis of Λ_r . This generation time was calculated on the basis of rotating drum effects on the overall reflector thickness.

The TSN calculations predict the magnitude of the SNAPTRAN temperature coefficient within 16% for the range of mean temperatures studied ($200^\circ\text{F} < \bar{T} < 1000^\circ\text{F}$). They predict the period dependence within 13% for the range of periods studied ($0 < \omega_o < 5000 \text{ sec}^{-1}$). The overall calculations show less variation with temperature than the correlation, although they show somewhat more variation for $\bar{T} < 400^\circ\text{F}$.

It was shown that the coefficient for transients involving large ΔT are not properly predicted by a single correlation in ω_o and $\bar{T}$. The spatially dependent effect, in this case, acts to give a coefficient smaller than the coefficient indicated by a series of small ΔT experiments at different initial temperatures. As a result, the coefficient for transients starting at ambient temperature appear nearly independent of temperature up to $\bar{T} \approx 500^\circ\text{F}$.

The study showed that the measured results are probably high by 2.6%, because of the generation time effect which results from changing drum positions to adjust reactivity. The TSN results have been adjusted to place them on the same basis as the experimental data.

Impulse tests were found to differ from corresponding step tests by about 5% due to the aforementioned error. They were also found to be more sensitive to errors in Λ_r determination and to drum calibration errors than step tests. The SNAPKIN-VI code was used to calculate inserted reactivities, including temperature feedback and power level effects. Comparison with experiment is shown in Figures 180 and 181. The TSN code was used to calculate the inverse periods. Comparison with experiments is shown in Figure 182. These comparisons were used in the preanalysis of the SNAPTRAN-2 destruct test.

Several fuel elements from the SNAPTRAN-1 reactor were analyzed to determine structural and composition changes. Analyses showed that only a minor degree of peripheral dehydriding occurred on those elements studied.

7. SNAPTRAN-2 Experiment⁽²⁶⁻²⁸⁾

a. Scope and Objective

The analytical model which was refined during the nondestructive SNAPTRAN-1 tests was used to predict the results of the planned SNAPTRAN-2 destruct test.

Figure 180. Comparison Between Calculated and Drum-Inserted Reactivities for Impulse Transients

Figure 181. Comparison of a SNAPKIN VI Forward Kinetics Calculation With the Experimental Results

Figure 182. Evaluation of Inhour Data From Entire SNAPTRAN Test Series

Input parameters to the analytical model were based on the stepper and impulse drive mechanism capabilities. It was desired to insert a maximum amount of reactivity in a stepwise fashion within a time period that would not cause significant temperature rises to occur. The mode of reactivity insertion for the destruct test was: (1) to insert as much reactivity as possible with the stepper drums, bringing the reactor slightly supercritical; then (2) to insert the remaining reactivity with the impulse drums. These reactivity values were programmed into the analytical model and results were computer analyzed. Analysis showed that the SNAPTRAN-2 reactor would undergo complete destruction with a stepwise reactivity insertion of approximately \$5. By performing a destructive transient of this magnitude, knowledge of neutronic behavior of the reactor was extended from the range observed during the nondestructive transients. Also, the mechanical and fission-product-release behavior of this type of reactor was determined. The foregoing results and consequences were then factored into the analytical model for determining maximum reactivity accidents in SNAP 10A/2 reactors.

b. Test Apparatus

The destruct test was performed at the same site as the SNAPTRAN-1 transient tests. The movable IET building was rolled back to expose the reactor during this test. A complete radiation monitoring grid was set up to measure and track the fission-product gases after the test.

The SNAPTRAN-2 destruct machine was similar in design to the SNAPTRAN-1 reactor previously described. The impulse drum drive mechanisms were modified to allow rapid (approximately 12-msec) step reactivity insertion. To accomplish this, the impulse drums were stopped at their full-in position, 0 deg, using pneumatic shock absorbers.

Although a new core was used for this test, its properties were the same as the SNAPTRAN-1 core, except for minor changes in the fuel composition. A summary of these properties is given in Table 32.

Instrumentation for the SNAPTRAN-2 reactor included: (1) reactor power, (2) temperature (energy probe); (3) radial and axial fuel element, vessel, and reflector strain; (4) acoustics; and (5) motion detectors.

TABLE 32
SNAPTRAN-2 CORE PROPERTIES

Number of fuel rods	37
Lattice spacing (in.)	1.26
Fuel rod OD (in.)	1.212
Fuel rod length (in.)	12.25
Weight of U ²³⁵ (gm)	4729.4
Total weight of fuel (gm)	51152.997
Average fuel density (gm/cm ³)	6.067
Average fuel composition:	
Fuel alloy (wt % U in Zr)	10
U ²³⁵ enrichment (%)	93
Atom densities (10 ²⁴ at./cm ³ of fuel):	
U ²³⁵	0.001438
U ²³⁸	0.0000886
Zr	0.03540
H	0.0653
H/Zr atom ratio	1.848

A total of 23 fast and thermal scintillation detectors and gamma scintillation detectors were used for this measurement. Several of these detectors had been calibrated in the SNAPTRAN-1 test series and were used as a basis for reactor power calibration. The composite range of these detectors was from source level to 3×10^{13} w.

Radial and axial temperature profiles through the reactor core and reflector were determined from 38 energy probes. Six of these energy probes either had the same composition as fuel material, ZrUH_x, or zirconium hydride (ZrH). The remainder of the energy probes were unhydrided ZrU, with 2.5 or 5 wt % uranium. By using 2.5 or 5% uranium content in these probes, an energy deposition of roughly one-fourth to one-half that of the adjacent fuel would be reached during the prompt burst. Further, since the probes were unhydrided, they would not exhibit pressure buildup due to dissociation as temperature increased.

The fuel elements, the reactor vessel, and the beryllium were fitted with strain gages to document the initial expansion. Acoustic detectors were installed on or in the area of the reactor.

Fourteen single- or multiple-pin closure or linear motion devices were installed on the top of fuel elements, the upper grid plate, beryllium reflector, and flatcar to document time and magnitude of motion during the test.

Extensive photographic coverage was made of the SNAPTRAN-2 destructive test. These included black and white, color, and infra-red motion pictures with framing speeds from 20 to 2000 frames/sec. These cameras were located in bunkers around the IET at distances from 110 ft to 1 mi.

Edgerton, Germeshausen, and Grier provided photographic coverage to determine component motion and mechanisms of disassembly during the destruct test. To accomplish this, certain portions of the reactor were selected so that their time expansion histories could be extrapolated to the reactor as a whole. Figure 183 shows the camera fields of view during the test. The four cameras used, CAM 1, 2, 3, and 4, were oriented to view the closing of interstitial spaces between individual fuel elements. Using back lighting, framing cameras 1 and 2 viewed interstitial spaces in the area designated CAM-2. The two streaking cameras viewed interstitial spaces in the areas designated CAM-3 and CAM-4.

c. Discussion of Test Results

Destruct test prerequisites required that several operational events be performed in sequence. The time of this sequence of events approaching the final test is termed the countdown period. A total of five partial or full-scale, dry-run countdown tests were successfully completed prior to conducting the destruct test. The transient initiated during each countdown sequence resulted from a step-reactivity insertion of $\$1.03$ to $\$1.05$.

During the calibrational step transients, the reactor power, energy release, and in-core temperatures were monitored. The preliminary results of these transients are presented in Table 33. Energy profiles were made, using the energy probe temperature data, and were found to agree well with calculated profiles. Prompt-temperature coefficients of $-0.13\%/^{\circ}\text{F}$ were in substantial agreement with SNAPTRAN-1 coefficients obtained in this temperature range.

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Figure 183. Camera Fields of View of SNAPTRAN-2 Core

TABLE 33
PRELIMINARY DATA OF THE SNAPTRAN-2 CALIBRATIONAL
STEP TRANSIENTS

Test No.	Starting Temperature (°F)	Reactivity Inserted (\$)	Peak Power (Mw)	Energy Release (Mw-sec)	Maximum Temperature Rise (°F)	Reactor Period (msec)
329	69	1.03	3.2	0.18*	84	23.3
330	70	1.037	12.0	1.0	34	19.3
332	91	1.03	10.6	1.07	190	22.4
334	50	1.036	13.0	1.54	260	18.9
340	82	1.056	26.1	3.0	347	13.4

*Minimum energy release resulted from premature scrambling of control drums during the transient.

The destruct test was initiated by the full insertion of the four reflector control drums. Examination of the drum position data and slow-motion pictures showed that all drums were fully inserted before the time interval considered in these analyses. The total drum-inserted reactivity was determined to be \$5.05. Preliminary analysis of the destruct test data indicated that as a result of the \$5.05 reactivity insertion, an asymptotic period of 206 μ sec developed, a peak power of 7.1×10^4 Mw was reached, and energy releases of 33 Mw-sec to peak power and 54 Mw-sec total had ensued.

High-speed photographs were taken of back lighting through core interstices. Figure 184 shows that fuel disintegration (closure of center interstices) began at the center of the reactor and proceeded outward. (Refer to Figure 183 for orientation.) The film in streak camera 4 was traveling at a known velocity in a direction perpendicular to the viewing slit. The time of fuel disintegration at each interstice location was determined from the streak photographs.

d. Analysis of Results and Conclusions

The ENRECO code was employed to calculate the power, energy, fuel disintegration, hydrogen pressure, and core disassembly for the conditions of the SNAPTRAN-2 destructive test. A comparison of calculations and the power

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Figure 184. Progression of Fuel Disintegration Recorded by Streak Camera No. 4

Figure 185. Theoretical and Experimental Power

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Figure 186. Time of Fuel Disintegration Determined From Streak Photographs with Theoretical Curve From ENRECO Calculation

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Figure 187. Variation of Temperature Coefficient With Temperature

traces from three nuclear detectors was made, and the results are shown in Figure 185. The time of fuel disintegration at each interstice location was measured and compared with the theoretical ENRECO calculation; these data are plotted in Figure 186.

Energy probe (EP) data were used to reconstruct power and energy release from the destructive test. The maximum reactor power calculated from EP154 and EP155 was 7.31×10^4 Mw released, compared to 7.24×10^4 Mw obtained from the nuclear detectors. The released energy total was 47.25 Mw-sec (EP) compared to 46.78 Mw-sec (nuclear detectors). The temperature coefficient calculated from the energy probes was constant over the entire range of calculations and was determined to be $-0.208\text{¢}/^\circ\text{F}$, compared to $-0.207\text{¢}/^\circ\text{F}$ from the nuclear detectors. This temperature coefficient is plotted with the overall SNAPTRAN-1 results of variation of temperature coefficient with temperature in Figure 187.

Analysis of the fuel debris found after the SNAPTRAN-2 destruct test showed that a significant portion of the fuel material had undergone sufficient heating to result in melting. Verification of the conclusion that melting had occurred was based on: (1) spheroidization of materials found, and (2) metallographic studies on fragments which found dendritic structures free of hydrogen, cracks, and voids. Electron microscopic data showed that the spheroids contained only uranium and zirconium. Other identifiable fuel fragments (fuel element end pieces) showed little more than mechanical damage and minor peripheral dehydriding.

E. MECHANICAL AND THERMOCHEMICAL INCIDENTS

1. Phase I Tests for SNAP 10A⁽²⁹⁾

A Phase I Mechanical and Thermochemical Test Series was conducted by Atomics International (AI) in conjunction with the Air Force Special Weapons Center at Holloman Air Force Base, New Mexico. The principal objective of the tests was to obtain data and information which could be analyzed for evaluating the hazards that may occur before, during, and after the flight of the SNAP 10A reactor.

During the factory-to-orbit sequence, the SNAP 10A reactor and its related systems were to be subjected to various handling, transportation, and launch conditions. As a result of these conditions, potential accidents, such as impacts, chemical interactions, explosions, and fires, could be postulated. In order to provide adequate nuclear safeguards, the potential radiological hazards that could result from the postulated thermochemical and mechanical incidents were evaluated.

To perform this evaluation, a series of tests was conducted at Holloman Air Force Base, New Mexico, in which full-scale mockup reactors and systems were subjected to conditions similar to the various postulated accidents.

The selection of the individual tests was quite critical because of the large number of accident conditions, attitudes, and geometries that could be postulated. Fourteen tests were selected which would give a statistically representative sampling of the credible conditions, and would develop the most meaningful information. Where possible, limiting conditions were selected in each test. These tests were divided into three categories: (1) chemical interaction, (2) fire and explosion, and (3) impact.

The Phase I Mechanical and Thermochemical Test Series was performed at Holloman Air Force Base, Alamogordo, New Mexico, from May to October 1962. Mockup SNAP 10A reactor assemblies (in some cases, including the shield and converter structure) were subjected to abort environments of thermal shock, chemical interaction, overpressure, and impact.

The tests performed are discussed in the following paragraphs.

a. Liquid Oxygen Spray Tests

The purpose of this test was to simulate the reactor being exposed to a liquid oxygen (LOX) spray, which could be expelled from an Atlas fuel tank. It was conducted to measure the effects of the thermal shock, created by such a LOX spray, upon the reactor vessel, grid plates, fuel element array, and to observe the general behavior of the reactor for evidence of structural failure.

For 40 sec, a dense spray of LOX enveloped the reactor. There was no external or internal physical damage to the reactor. A similar test was made with NaK, also exposed to the LOX spray. The NaK solidified at the nozzles and no damage resulted.

b. Explosion Test

The purpose of this test was to measure the geometric changes of the fuel element array and to observe structural failure of the reactor and fuel element dispersal resulting from an explosion. This test was planned to simulate a prelaunch or postlaunch Atlas abort and subsequent propellant explosion.

The suspended reactor was subjected to an overpressure of 400 psi for 2.8 msec caused by an explosion of 256 lb of TNT, 13 ft below it. The reflectors were blown off and the bottom of the reactor vessel deformed inwardly.

c. Sodium Potassium (NaK) - Water Interaction Immersion Test

The purpose of this test was to observe the NaK - H₂O chemical reaction and its effect on the reactor.

The reactor was placed in a 36-ft³ tank filled with water. Exposure of NaK caused an explosion which ruptured the tank. The core vessel was slightly ballooned by pressure buildup inside.

d. Drop Tests From 100 ft

The purpose of these tests was to determine the mode of failure of the reactor assembly when subjected to impact with concrete at an impact velocity of 70 ft/sec. These tests were intended to simulate a reactor being dropped from the top of the launch vehicle during the mating operation.

To simulate various modes of impact, test articles were dropped: (1) side-on, (2) tail-on, and (3) nose-on.

In the side-on drop test, the model impacted at 68.8 ft/sec. The reflectors were knocked off and the vessel sustained a small puncture. All the support legs failed. One reflector rotated inward before the reflectors separated. The maximum energy release was calculated to be only 230 watt-sec.

Upon tail-on impact, the reactor fell into the converter cone. All reflector drum brackets were severed and all top pins were bent outward. The core vessel buckled circumferentially. The reactor after impact is shown in Figure 188.

The velocity of the test article in the nose-on impact was 64.4 ft/sec. The impact caused complete failure of the pump radiators and severed all of the reflector drums. After the vessel support legs failed, the reflector blocks were pushed into the converter structure and were held around the reactor vessel by the structure. Examination indicated that no nuclear hazard would result, except for water immersion.

Figure 188. Reactor After 100-ft, Tail-On Drop Test

e. Fire Test

The purpose of this test was to measure differential expansion and to observe evidence of structural failure of the reactor when subjected to a high thermal flux environment. It was intended to simulate a prelaunch or postlaunch abort and subsequent propellant fire.

The reactor was surrounded by a burning cycle of 4000° F flames for 2.2 sec, and 1500° F flames for 15 min, based on launch-abort fire information. The reflector separated during the initial high-temperature burn. A local deflection inversion occurred in the lower vessel head.

f. Water Impact Tests

The purpose of these tests was to observe the reactor assembly during and after water impact. These tests were intended to simulate an abort after launch of 10,000 ft or above, including a free fall from the apogee.

Three reactor assemblies and one unit including the converter and shield were impacted, at different attitudes, into an 8 by 8 by 16-ft water tank.

A side-on water impact test was performed at 550 ft/sec; the reactor passed through the water tank. The fixed reflector blocks and the reflector drums were completely separated from the reactor vessel in the water. The vessel was found intact. No useful data were obtained from the onboard instrumentation, because the recording equipment failed to operate.

The nose-on water impact test was performed at 573 ft/sec; the reactor passed through the water tank. The reflector assembly separated completely from the reactor vessel in the water. Also, the NaK pump, the upper head, and the upper grid plate were severed from the test article. Fuel elements were displaced longitudinally in such a manner as to form a spherical pattern at the ends. Sufficient disassembly of the core did not occur to prevent its criticality in the water.

The tail-on water impact test was performed at 596 ft/sec. The reflector assembly was separated, and the core vessel split open axially. Fuel elements were displaced longitudinally through the bottom of the vessel. Two elements were completely ejected from the array. A fueled core would have gone critical in water.

The unit with converter and shield was impacted at 428 ft/sec. The reflector assembly, NaK pump, shield, converter structure, bottom grid plate, and bottom head separated from the core vessel. Fuel elements were displaced through the open end of the vessel. Core disassembly was not sufficient to prevent criticality in water.

g. Concrete Impact Test

The purpose of this test was to observe the reactor assembly during and after a high-speed concrete impact. This test was intended to simulate an abort after launch at 10,000 ft or higher, including a free fall from the apogee.

A concrete-faced monorail sled was impacted at 560 ft/sec onto the pump end of the test article, shown in Figure 189. The impact completely destroyed the reactor. The vessel was in three parts: the upper head, lower head, and the shell. The shell was completely flattened and had many local failures. Figure 190 shows the pieces of the test article which were gathered after the test.

h. Conclusions

It was concluded that thermal shock due to a LOX deluge had no appreciable effect on the reactor assembly. Chemical interactions, although violent in the case of the NaK-water reaction, did not significantly damage the core vessel. Fire, explosion, 70-ft/sec impacts on concrete, and 550-ft/sec (terminal velocity) impacts on water resulted in reflector ejection with little or no damage to the core vessel. Impact on concrete at 560 ft/sec, however, did produce complete disassembly of the reactor.

Extensive motion picture coverage was provided and many photographs were taken of the tests. The conclusions were based upon analyses of the documentary films. The significant information required from the tests, in most instances, was the ultimate condition of the test assembly. This made it possible to determine whether the core configuration was capable or incapable of being made critical.

When included, both the fixed reflector blocks and rotatable control drums separated from every test article as a consequence of the imposed test conditions. The final condition of the reactor vessel and fuel element array after every test, except for the 560-ft/sec concrete impact, was such that the disassembly was not sufficient to prevent criticality if the reactor were immersed in water.

This test series provided valuable information about the effects of the postulated mechanical and thermochemical accidents and constituted a foundation upon which the SNAP safety program was based in this area.

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Figure 189. Test Article Before Concrete Impact Test

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Figure 190. Pieces of Test Article Recovered After
Concrete Impact Test

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Careful evaluation of the Phase I tests and results revealed that, although many questions had been answered, additional tests were needed in order to investigate tumbling impact, impact at less than 70 ft/sec, post-reentry impact, and to simulate more closely the actual condition of the reactor and its environment.

2. Phase II Tests for SNAP 10A⁽³⁰⁾

a. NaK-Water Interaction Immersion Test

The Phase II test series was a rerun of the Phase I test series under improved conditions. An accident, where the reactor is dropped into water with subsequent rupture of the NaK nozzles, was successfully simulated.

Two explosive pulses were followed by a gradual pressure buildup ending at 15 sec. Strain measurements showed that the stresses were generally below the yield stress of the Type 316 stainless steel vessel. The core vessel was not damaged during the test. The measured time of initial pressure pulse was slower than the calculated time to peak power of a water-immersed core.

b. Fire Test on Squibs

A test was performed to evaluate the possibility of a missile abort fireball causing ignition of the SNAP 10A drum lockout pin puller squibs during a free fall of the reactor to the pad. To simulate the abort environment, the squibs were subjected to an incident heat flux of greater than 360 Btu/sec-ft^2 for 3 sec. Three sets of two squibs each were tested. The squibs were subjected to the desired incident heat flux in the Sandia Radiant Heat Facility.

At the end of the 3-sec period, the temperature of the actuator assembly had risen to approximately 600°F , but none of the squibs tested had ignited. Squib ignition took place at 8.7 sec.

The significance of this test is that it demonstrates that abort heat ignition of the squibs and release of the drum lockout pins cannot occur prior to impact of the reactor on the pad surface. Therefore, barring some other mechanism for squib firing, the reactor assembly will be in a configuration with all four control drums locked out at the moment of impact.

c. Soil Impact Tests

The terminal-velocity soil impact test was performed to establish the condition of the SNAP 10A core vessel and fuel element array after a terminal-velocity, head-on impact on compacted soil.

The test article, consisting of the core vessel with internals, reflector assembly, pump and fins, shield, and top part of the converter structure, was impacted into a rectangular parallelepiped of compact soil that was 16 ft wide, 8 ft high, and 8 ft deep.

The reflector assembly separated from the core vessel. Very little damage was incurred by the vessel, the most noticeable being a 1-in.-long opening in the vicinity of the top head weld. The fuel element array was not appreciably changed.

The test indicated that the core vessel would survive a terminal-velocity land impact. In order to more definitely determine the configuration and properties of a reactor and its ultimate condition after impact, tests with test articles that have been subjected to reentry conditions should be made.

3. Cylindrical Vessel Disassembly Tests

In the event of launch abort or following atmospheric reentry, an intact SNAP 10A reactor may be subject to a nuclear excursion if subsequently immersed in water. Impact disassembly of the reactor core will disrupt the potential critical configuration and prevent such excursions. The purpose of these tests was to determine the effect of various postulated impact disassembly features which would render the reactor sensitive to impact, yet maintain the strength required for operation.

For this study, no attempt was made to simulate any particular reactor vessel. Rather, cylindrical vessels, with loads of steel bars simulating fuel rods and water simulating NaK, were used to represent probable reactor vessels.

Since most launch towers place the reactor at a minimum launch height of about 100 ft, this was selected for the test drop height. Further, as most launch pads are made of concrete, the impact target was so selected. The vessels were dropped and fin-guided to produce side-on impact. This was considered to be the

least damaging to the vessel. Data on the impact dynamics were derived from transducer and photographic recordings taken from three series of impact tests.

The first series was performed at the Rocketdyne Vertical Test Stand, VTS-1, and at Atomics International Field Laboratory, both in Santa Susana.

The test vessels consisted of enclosed cylinders, 9.500 in. in diameter and 12.00 in. long. End enclosures consisted of flat disk plugs, 0.25 in. thick. Vessel material was Type 316 stainless steel. The vessel wall thickness and special machined stress concentration zones varied with the specimens. Each cylinder contained 31 simulated fuel elements, radially restrained by wood wedges, which were machined to simulate reflector structure geometry. No grid plates were used. The elements were made from cold-rolled steel bars, 1.25 in. in diameter and 11.5 in. long. Two stainless steel accelerometer blocks were welded on three of the specimens.

Six vessels were tested, and the results are summarized in Table 34. Of the six, the design used in vessel No. 5 resulted in destructive failure upon impact. This vessel, after impact, is shown in Figure 191.

From this series of tests, it was concluded that the design of a vessel definitely contributes to its impact strength properties. The judicious use of stress concentration grooves can so weaken a vessel structure that prompt disassembly

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Figure 191. Cylindrical Vessel Disassembly Test Series 1, Vessel No. 5 After Impact

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TABLE 34
CYLINDRICAL VESSEL DISASSEMBLY TEST SERIES 1,
VESSEL DESIGN AND RESULTS

Vessel No.	Wall Thickness (in.)	Stress Concentration Grooves*	Weld Material	Condition After Impact
1	0.032	None	None	Elliptic deformation. Local failure at end cap weld.
2	0.032	U-shape	None	Elliptic deformation. Failure in tension along length.
3	0.016	None	None	Severe elliptical deformation. Local failure at end cap welds.
4	0.010	None	None	Wall failed by tensile tearing.
5	0.032	90-deg V-groove by 0.016 in. deep	None	Destructively failed.
6 [†]	0.032	90-deg V-groove by 0.016 in. deep	Haynes No. 25	Elliptic deformation. Local failure at end cap weld.

*Grooved vessels contained three longitudinal and two circumferential grooves.

†Filled with water.

occurs upon impact. A pattern of three equally spaced longitudinal 90-deg V-grooves, 0.016 in. deep (0.032-in. wall thickness), produced the most pronounced disassembly.

A second series of tests was initiated to develop some means for enhancing impact disassembly through the use of the following features:

- 1) Minimum strength design
- 2) Brittle vessel material
- 3) Special effects like local embrittlement, load direction, and stress concentration.

One of the criteria applied for evaluating the potential disassembly features was that the modification of the basic design required to implement the feature be a minimum.

The objective of the second test series was to provide data about the relationship of stress concentration groove depth and root radius to the impact disassembly of 0.032-in.-thick wall, stainless steel, cylindrical vessels, containing mock reactor cores.

Improved testing methods, based on the first series of tests, were used. Nine vessels were tested, and the results are summarized in Table 35. None of these nine destructively failed upon impact.

TABLE 35
CYLINDRICAL VESSEL DISASSEMBLY TEST SERIES 2,
VESSEL DESIGN AND RESULTS

Vessel No.	Wall Thickness (in.)	Groove Depth (in.)	Groove Root Radius (in.)	Condition After Impact
1	0.032	0.005	0.006	No fracture
2	0.032	0.005	0.003	No fracture
3	0.032	0.005	0.001	No fracture
4	0.032	0.007	0.0008	Partial circumferential fracture
5	0.032	0.008	0.003	One longitudinal and partial circumferential fracture
6	0.032	0.010	0.005	One longitudinal fracture
7	0.032	0.011	0.005	One longitudinal and partial circumferential fracture
8	0.032	0.011	0.002	Partial circumferential fracture
9	0.032	0.013	0.002	One longitudinal and partial circumferential fracture

From this second series of tests, it was concluded that cleavage fracture can be initiated and made to propagate along selected paths (grooves) in normally ductile Type 316 stainless steel. The extent of fracture along the grooves is affected by the depth of the grooves. The value of the calculated elastic stress

concentration factor in the range investigated did not have a noticeable effect on the extent of rupture. It was found that a larger core void area within the vessel increased the damage to that vessel upon impact.

A third series of tests was made to study the effect of further modifications to the vessel and its internals to improve impact disassembly. Two modified designs were tested. Type 1 was a peripheral element modification, and Type 2 was an external end-ring modification. As in previous tests, one of the criteria was that modification of the basic design be a minimum. Test conditions were similar to previous tests.

Seven vessels were tested in this series, and the results are summarized in Table 36. Of the two types of vessels tested, the type with the external end rings was more completely disassembled for any groove depth than the type with the

TABLE 36
CYLINDRICAL VESSEL DISASSEMBLY TEST SERIES 3,
VESSEL DESIGN AND RESULTS

Vessel No.	Wall Thickness (in.)	Groove Depth (in.)	Groove Root Radius (in.)	Condition After Impact
1 (Type 1)	0.032	0.015 vee	0.002	Longitudinal groove fracture. Partial circumferential fracture.
2 (Type 1)	0.032	0.010 vee	0.002	Longitudinal groove fracture. Partial circumferential fracture.
3 (Type 1)	0.032	0.005 vee	0.002	Partial circumferential fracture.
4 (Type 2)	0.032	0.015 vee	0.002	Complete disassembly of core
5 (Type 2)	0.032	0.010 vee	0.002	Longitudinal groove fracture. Partial circumferential fracture.
6 (Type 2)	0.032	0.005 vee	0.002	Longitudinal groove fracture. Partial circumferential fracture.
7 (Type 2)	0.032	None	None	Partial fracture.

peripheral element modification. When compared to previous test series, both Type 1 and 2 vessels were more completely disassembled at impact. Vessel No. 4 (Type 2) was almost totally disassembled and is shown in Figure 192.

From this third series of tests, it was shown that it is difficult to cause disassembly of a SNAP-type reactor vessel made of Type 316 stainless steel without major modifications of the core elements and reactor vessel. A more promising approach for enhancing impact disassembly of compact reactor cores may be to make the reactor vessel of a less ductile material.

4. Impact Tests on SNAP Component Materials

In order to further study the effect of impact on the SNAP 10A reactor, two series of tests of its component materials were performed.

The first series of tests was conducted on solid cylindrical specimens, fabricated from stainless steel, lithium hydride, tungsten, tantalum-10 tungsten, and normal zirconium hydride fuel material. These are the principal materials from which the reference ZrH reactor system is fabricated. The objective of this test program was to determine how these materials would behave at impact velocities equivalent to those the reactor system would encounter at time of earth impact after its return from orbit. The test program was conducted to

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Figure 192. Cylindrical Vessel Disassembly Test
Series 3, Vessel No. 4 After Impact

confirm or deny the assumptions made in the analytical model that has been developed to predict the post-impact configuration of the reference ZrH reactor system.

The analytical model was based on the assumptions that the impact energy absorption ability of the reactor system was a function of the mass of the materials involved, their ability to deform plastically without rupture, and an assumed factor of K. The K factor of a material is the ratio of the energy absorbed per pound of the material dynamically to the energy per pound of material under the normal stress strain curve. The K values used in the analytical model were assumed values, based on pellet tests of similar materials, as it is virtually impossible to determine the K values of a material analytically. The objective of these tests were therefore to: (1) provide information as to the ductility of the materials tested over the range of impact velocities expected of the reactor system at earth impact; (2) determine the K values for the various materials if they do, in fact, deform plastically; and (3) determine if the size of the specimen affects the energy absorption capability per pound of the material. An added objective was to test different size specimens of several of the materials to help determine if model testing would be feasible for predicting the post-impact deformation of the reactor system.

The AI gas gun impact facility, located at the Santa Susana area, was used to conduct these tests. The specimens were impacted against a granite block at a known impact velocity. The kinetic energy of impact was calculated, based on the known specimen weight and impact velocity. The rebound and tumbling velocity for the ductile specimens was measured by means of motion picture cameras, from which the kinetic energy of rebound and tumbling could be determined. The energy absorbed by the granite target due to the compression wave and cratering was calculated. The energy absorbed by the ductile specimen is the impact kinetic energy minus the rebound, tumbling, cratering, and target compression wave energies. The K values for the ductile materials was then calculated, assuming that the energy absorbed by the specimen is all absorbed in the deformed portion of the specimen. The nonductile materials that fracture on impact do not absorb the energy by plastic deformation, but dissipate the energy in breaking up and in the kinetic energy of the flying particles. This test series was not set up to determine the energy dissipated in the breaking up of nonductile specimens.

a. First Series of Tests

The first series of tests yielded the results discussed in the following paragraphs for the various types of specimens.

(1) Type 316 Stainless Steel

The reactor vessel and a majority of the supporting structure of the reactor system are fabricated from Type 316 stainless steel. Two sizes of Type 316 stainless steel cylindrical specimens were tested at impact velocities of 100, 350, 600, 700, and 850 ft/sec. The larger specimen was 1.0 in. in diameter by 2.5 in. long. The smaller specimen was 0.495 in. in diameter by 2.00 in. long.

The K values which were determined varied from 0.4 at 350 ft/sec impact velocity to 1.8 for an impact velocity of 850 ft/sec. The difference in K values due to specimen size was not appreciable. Thus, the specimen size does not affect the energy absorption capability per pound of material and scale-model impact testing is feasible. It was recommended that impact tests be conducted on Type 316 stainless steel cylindrical shells to determine the energy-absorbing characteristics of a particular geometry.

It was also noted that the maximum neutron fluence on the reactor vessel after 40,000 hr of operation is 5×10^{19} n/cm². This irradiation will cause a small increase in the yielding stress and a small reduction in the ductility of Type 304 stainless steel. The energy absorption capacity of the vessel material will not be affected by this low level of neutron fluence.

(2) Lithium Hydride

Lithium hydride is one of the principal components of the ZrH reference reactor radiation shield. The LiH specimens were 1.97 in. in diameter and 9.0 in. long. As it has not been determined what, if any, reinforcement will be used in the LiH shield, specimens were tested with no reinforcement, steel wool reinforcement, 1-mil honeycomb with 1-in.-square cells oriented with the cell axis parallel to the axis of the cylinder reinforcement, and 1-mil honeycomb with 1-in.-square cells oriented with the cell axis perpendicular to the axis of the cylinder. Each type of specimen was impacted at velocities of 100, 350, and 600 ft/sec. One specimen with the steel wool reinforcement was also impacted at 850 ft/sec.

It was apparent from these tests that LiH does not absorb energy by plastic deformation, but that the impact energy is consumed by the pulverizing of the material. It was recommended that tests be made on samples of various diameters and thickness to obtain the effects of size, if any, for use in scale-model testing programs.

(3) Tungsten

Tungsten is another of the principal materials proposed for the reactor radiation shield. Cylindrical sintered tungsten specimens, 0.495 in. in diameter by 1.250 in. long, were tested at impact velocities of 100, 350, and 600 ft/sec.

At each velocity, the specimens failed in a brittle fashion with no evidence of plastic deformation. It was recommended that a suitable shield material that is more ductile than tungsten, such as Ta-10W or one of the Mallory alloys, be tested.

(4) Tantalum-10 Tungsten

This is one of the materials that constitute the makeup of the reactor control drums. Cylindrical Ta-10W tungsten specimens 0.500 in. in diameter by 1.25 in. long, were tested at impact velocities of 350, 600, 700, and 800 ft/sec.

At each velocity, the specimens exhibited a plastic deformation with no evidence of fracture. The K values varied from 1.75 at 600 ft/sec to 3.2 at 850 ft/sec. It was recommended that Ta-10W tungsten be considered as a replacement for the tungsten in the radiator shield.

(5) Zirconium Hydride Normal Fuel Material

Enriched ZrH is the fuel material used in the ZrH reactor. The dynamic properties of normal ZrH are very similar to those of the enriched ZrH. Cylindrical specimens of normal ZrH fuel material 0.500 in. in diameter by 3.00 in. long, were tested at impact velocities of 100 and 350 ft/sec.

At each velocity, the specimens broke up into several relatively large pieces and the rest of the specimen was reduced to chips and a fine powder. There was no evidence of plastic deformation in any of the specimens.

As a continued effort in the impact disassembly studies of the reference ZrH reactor system, the second series of impact tests were conducted. The specific objectives of this series of tests were to:

- 1) Verify modeling laws and determine the feasibility of predicting the post-impact configuration of the reactor system based on results of scale-model tests.
- 2) Determine the energy-absorbing capacity and failure modes of the reactor materials with geometries and loading conditions similar to the reactor system.
- 3) Verify the analytical model approach developed for predicting the reactor post-impact configuration.

To obtain these objectives, impact tests were conducted on: (1) stainless steel cylindrical shells with a radius-to-wall thickness (R/t) ratio and a length-to-diameter (L/D) ratio similar to the reactor vessel and the Ta-10W shield, (2) LiH pads impacted in a manner similar to the type of loading that would be encountered when the reactor system impacts the earth, (3) composite models composed of stainless steel and LiH, and (4) Inconel 800 cylinders.

To determine the feasibility of scale-model testing in predicting the post-impact deformation of a prototype, each type specimen was made in several different sizes with each size being a scale model of the others. The post-impact configurations of the scaled models were then compared.

b. Second Series of Tests

The second series of tests yielded the results discussed in the following paragraphs for the various types of specimens.

(1) Cylindrical Shells

Each type of shell was fired from the impact tester into a granite target at velocities of 350, 600, 700, and 850 ft/sec. Weights were added to some of the shells for further scaling, law verification, and for determining the ultimate amount of energy the shells could absorb. The post-impact deformations of the shells were then measured and plotted. The impact kinetic energy and the energy absorbed by the target were calculated; the difference was assumed to be the energy absorbed by the shell.

The cylindrical shells were chosen so that they would have approximately the same R/t and L/D ratios as the reactor gamma shield and the reactor vessel.

These tests verified that: (1) scale-model testing is indeed feasible for predicting the post-impact deformation of a prototype; (2) the R/t and L/D ratios of cylindrical shells do effect the energy-absorbing capabilities of a material with the larger R/t ratio shells (in the range tested), absorbing less energy per pound than the shells with the lower R/t ratios; (3) the shells lowered the effective impact velocity of the weights; (4) the failure mode of the shells changes with R/t ratio and concentrated weights; and (5) the K values of the shells change with the concentrated weights due to the different failure modes.

(2) Lithium Hydride

This test was conducted to determine the actual energy absorbed by the LiH when pulverized and the effect the thickness, diameter, and unit loading of the LiH pads had on its energy absorption capacity. In addition, this test was to determine if the modeling laws were applicable to materials such as LiH.

The LiH pads tested were cylindrical in shape and varied in diameter from 1/2 to 2 in. and varied in thickness from 1 to 6 in. The diameter of the pads used was dictated by the barrels available for the impact tester. The LiH specimens were tested by placing them between the target and an impacting projectile. The energy absorbed by the LiH was then determined by comparing the post-impact deformations of the projectiles with the LiH pads to the post-impact deformations of similar projectiles without the LiH pads. The difference in deformation was assumed to be due to the effect of the LiH.

The test verified that the modeling laws do apply to materials such as LiH and that LiH does absorb considerable energy on impact. The thicker pads absorbed considerably less energy per pound. The tests of the LiH samples did not model as closely as the cylindrical shells. It is believed that the reasons for this are the inconsistency in the granular structure of the LiH and the method of impacting the LiH.

(3) Composite Models

Tests were made on composite models to verify that the modeling laws hold true on complicated structures as well as simple geometries. In addition, it was hoped that the validity of the analytical model approach for predicting post-impact configurations could be verified from these tests as well as determining failure modes for some of the reactor components.

Composite Model No. 1 was a stainless steel rod, surrounded by an annulus of LiH with a thickness 1.4 times the rod diameter, encased in a stainless steel tube with a thickness 0.1 times the rod diameter. A stainless steel end cap, 1.5 diameters long, was at one end. Composite model No. 2 was an approximate scale model of the reactor shield, vessel, and control drums.

The post-impact deformations of the three sizes of Composite Model No. 1, at the four impact velocities tested, scaled very closely. The failure modes were all of the same type. Most of the impact energy for all the specimens was absorbed by the LiH-shell combination and the rod.

The post-impact deformations and the mode of failure of the two sizes of Composite Model No. 2 tested scaled very closely. At the higher velocities, both inner and outer shells failed in tension and tearing rather than crushing, and the rods failed in shearing.

(4) Inconel 800

Inconel 800 was tested to determine its impact characteristics. This material was being considered as a cladding material for the reactor fuel elements. If the Inconel 800 was superior to Type 316 stainless steel from an impact standpoint, it could also be considered for the reactor structure. The specimens tested were 1/2-in.-diameter by 2-in.-long cylindrical specimens.

There was no evidence of any fractures in any of the Inconel 800 specimens. The critical velocity of the Inconel 800 is apparently much higher than that of the Type 316 stainless steel. Thus, even though the K values are lower than the Type 316 stainless steel, it will, in all probability, absorb more energy before a rupture would occur.

F. END-OF-LIFE SHUTDOWN

1. Shutdown Device Evaluation⁽³¹⁾

It was shown that in many aerospace reactor missions, a reliable shutdown device was required to reduce the potential radiological hazards presented by a reactor that returns from orbit. Such a device would assure that the reactor was subcritical upon reentry into ground environments and that accumulated fission products would have decayed after shutdown to a low activity level before reentry.

The reactor would not shut down as a natural consequence of either power operation or the space environment. Power decline without shutdown is shown in Figure 193. The reliability of electronic, mechanical, and/or chemical shutdown devices was difficult to demonstrate. An inherently reliable shutdown mechanism was sought.

Such a mechanism, with many desirable features, was found in the tritium-helium (THE) system. This system appeared:

- 1) To be feasible with current technology
- 2) To be completely reliable
- 3) Not to reduce power system reliability
- 4) Not to create space trash
- 5) To cause no handling complications
- 6) To potentially prevent water impact criticality
- 7) To be effective without reflector ejection.

The tritium-helium system would shut the reactor down over a period of years by accelerating the reduction of reactivity. Tritium has a half-life of 12.26 yr, a very small neutron absorption cross section, and decays to He^3 by emitting a low-energy (0.018 Mev) beta particle. He^3 is stable and has a large (5400 b) neutron absorption cross section. He^3 absorption regenerates tritium plus hydrogen. A relatively small reactivity penalty is incurred during operation because of the burnup of He^3 , compared to its alternate worth when all tritium has decayed. As reactor power diminishes, He^3 builds in and thus accelerates power reduction. The resultant hazard from fission products for this gradual shutdown compares favorably to prompt shutdown for orbits lasting 5 yr, or more, as shown in Figure 194.

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Figure 193. Power Decline Without Shutdown

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Figure 194. Short-Lived Fission Product Inventory

Where to locate tritium in the core was studied. Tritium had to be incorporated in a manner which would withstand high-temperature conditions during fabrication and/or operation. Incorporation as a stable solid such as a tritide was desirable, but experimental evidence was lacking. Distributing tritium throughout the core was shown to have a significant reactivity advantages over more concentrated localized distributions.

Tritium decay during reactor storage prior to a mission was shown to reduce core reactivity at startup and to reduce the helium poisoning available for shutdown. The losses were small enough ($\sim 5\%/yr$), however, to be easily compensated by beryllium shim additions or increased loadings at fabrication. During operation, a reactivity penalty, which is about 5% of the potential poisoning, would be incurred from He^3 concentrations. This could be compensated by additional control and excess reactivity. In some reactors of sufficiently low power and containing prepoisons having sufficiently low absorption cross sections, the shutdown problem would be made more difficult by prepoison burnout after mission completion. A typical case is shown in Figure 195.

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Figure 195. Effect of Samarium-149 Prepoison on Power Operation with Tritium-Helium System in a SNAP 10A-Type Reactor

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No significant radiation hazards were found for the tritium-helium system. A later detailed study is reported in the following Section III-F-2.

Little was known about the mechanical effects of tritium-helium on reactor operation and fabrication techniques. The required concentrations for tritium distributed throughout the core were more than 1000 times less than hydrogen concentrations in SNAP hydride fuel. Experimental studies were recommended and later made as reported in Section III-F-3.

2. Preorbital Radiological Hazards⁽³²⁾

The tritium-helium (THE) end-of-life shutdown mechanism would require the addition of some 10^4 curies of tritium to a SNAP reactor core. The possibility that this radioactive material might increase radiological hazards in the interval between fabrication and startup in orbit was studied.

Helium is harmless, and the chemical toxicity of tritium is identical to hydrogen toxicity. Thus, any hazard introduced by THE would have to be due to tritium radiation.

Tritium emits no gamma radiation. It decays by emission of a low-energy (0.018 Mev) beta. When tritium is not inside the body, tritium is harmless to humans, because the betas are completely stopped by the dead outer layers of the skin. Tritium beta in zirconium would produce bremsstrahlung X-rays which could penetrate the outer skin. However, the bremsstrahlung would produce a dose rate of less than 0.35 rem/hr at the surface of an unclad fuel element. Further, this would be reduced to less than 0.2 mrem/hr by the fuel element cladding. Clearly, tritium in THE would constitute no external radiation hazard.

Even inside the human body, tritium is one of the least hazardous radioisotopes. This is due to tritium's low-energy radiation and favorable biological behavior. Specifically, the 10^4 curies of tritium in THE would be less radiotoxic than the $\sim 1/2$ curie of enriched uranium in a SNAP 8 reactor. THE would not significantly increase the radiological poison inventory.

Tritium released at ground level would rise, and tritium released at high altitude would not fall out. Tritium could not present a hazard unless it were

released from the core in quantity or in an enclosed space. No minor accident could release tritium, because SNAP reactors would be constructed to contain hydrogen through the shock, vibration, and acceleration of launch followed by extended operation at high temperature. The ruggedness of these reactors has been verified by tests. No significant tritium release would occur, unless there were a major accident, one which severely damaged the reactor.

Several types of major accidents are possible. First, a destructive nuclear explosion might release some 10% of the tritium; but the excursion would: (1) produce enough radioiodine to equal the tritium in hazard potential and (2) disperse the tritium enough to eliminate any significant hazard. Second, a large fire might rupture the cladding and drive out tritium; but such a fire would assure an immediate cloud rise and thus obviate any radiological hazard. Third, in an abort-reentry burnup accident, tritium would be driven off; but it would not constitute a hazard because it would not fall out. Finally, gross mechanical damage, such as 400-mph impact on rock, could release tritium; but two factors keep the "cold cloud" case from being hazardous. First, molecular tritium is much less hazardous than tritium oxide and the tritium oxidation rate in air is very low ($<1.5\%/day$). Second, tritium would escape from SNAP fuel very slowly (the diffusion coefficient is about $0.14 \text{ cm}^2/\text{yr}$) at room temperature. Actually, the low oxidation rate alone would prevent any hazard, as experiments and accidents have demonstrated. Tritium in THE would not constitute a hazard even if it were to be released by a major accident.

Hazards would not be materially increased by the addition of tritium to a SNAP reactor.

3. Mechanical Effects^(33,34)

Experiments were conducted to determine the feasibility of incorporating tritium into SNAP fuel. Specifically, the purpose of this work was to determine:

- 1) The effect of additions of tritium on the dissociation pressure of SNAP fuel
- 2) The amount of fuel swelling that occurs due to the presence of He^3 in the fuel

- 3) The permeation rates of hydrogen and tritium through a SNAP fuel-cladding assembly
- 4) The redistribution rate of tritium within SNAP fuel as a function of temperature
- 5) The rate of He^3 evolution from SNAP fuel as a function of temperature
- 6) The retention of tritium in the cladding
- 7) A practical process to incorporate tritium into SNAP fuel elements, to test this process, and to write a process specification.

It was necessary to measure the dissociation pressure of fuel with zirconium tritide because of lack of available data, and because results of measurements on related materials indicated that the values could not be predicted confidently. A modified Sieverts apparatus was used.

Comparisons of the dissociation pressures of ZrH_x and $\text{ZrH}_{(x-y)}\text{T}_y$ were made by first measuring the dissociation pressures of a ZrH_x sample over a range of temperatures and pressures. Next, a measured portion of hydrogen was removed from the sample and replaced with an equal portion of tritium. The dissociation pressures of the new sample, $\text{ZrH}_{x-y}\text{T}_y$, were measured over the same range of temperatures. Direct comparisons were then made of the pressure-vs-temperature curves of the data. Curves for tritium concentration at least 10 times that considered for use indicated no measurable difference in pressure (see Figure 196).

The extent of He^3 -induced fuel swelling was measured by obtaining densities before and after annealing inert gas-bearing metal at elevated temperatures. The dissociation pressure measuring apparatus was used to tritide and hydride 90 wt % zirconium - 10 wt % natural uranium, 0.15 wt % carbon-modified specimens. The He^3 concentrations contained in the specimen was up to 100 times that proposed. The amounts of swelling were insignificant compared to that of SNAP fuel after 12,000 hr of reactor operation. 840 to 930-hr anneals at 1300° F produced changes in density with average magnitudes less than $-0.003 \pm 0.006 \text{ g/cm}^3$. For 21- to 42-hr anneals at 1500° F, the changes were $-0.013 \pm 0.006 \text{ g/cm}^3$ or less (95% confidence level).

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Figure 196. Dissociation Pressure vs Temperature Curves for $\text{ZrH}_{1.64-1.83}$ and $\text{ZrH}_{1.62-1.81}\text{T}_{0.017}$

In order to facilitate nuclear calculations for tritium-loaded reactors, it was necessary to know the distribution of the poison, helium-3, between the fuel and the annulus between the fuel rod and the cladding. Measurements to determine the rate of release of helium from the fuel as functions of time, temperature, geometry, and tritium concentration were performed.

Measurement specifications and results are given in Table 37. The fractional release appeared to be independent of helium at these levels, but to very inversely with sample diameter. The increases in fractional release with time and with temperature apparently reflect the expected diffusion of helium in the fuel. It was concluded that the release of He^3 would be negligible.

TABLE 37
MEASUREMENT SPECIFICATIONS AND RESULTS OF
HELIUM-3 RELEASE

Specimen Dimensions			Initial Tritium (atoms/cm ³) ($\times 10^{-21}$)	Shelf Time (days)	Anneal Temperature ($^\circ\text{F}$)	Anneal Time (hr)	Helium-3 at End of Anneal		Fractional Release ($\pm 20\%$)
Diameter (in.)	Length (in.)	Volume* (cm ³)					Total (atoms) ($\times 10^{-19}$)	Concentration (atoms/cm ³) ($\times 10^{-19}$)	
0.318	0.423	0.534	3.66	144	1400	187	3.63	6.79	0.00127
0.317	0.426	0.533	3.66	144	1400	48	3.44	6.46	0.00051
0.317	0.418	0.525	3.66	144	1200	187	3.57	6.79	0.00022
0.152	0.400	0.116	7.09	153	1400	168	2.01	17.32	0.00263
0.319	0.420	0.540	7.11	153	1400	168	9.40	17.41	0.00135

*Calculated from H/Zr values and densities.

In order to reduce inhomogeneities in the reactivity profile of a reactor fuel element, the helium-3 (hence, tritium) must be distributed uniformly throughout the fuel rod. This distribution was obtained by redistribution of tritium and hydrogen between a normally hydrided rod and a small section of highly tritided fuel. It was the purpose of this study to determine the rate of this redistribution as a function of temperature.

The receptor rods were 0.53 in. in diameter, 6 in. long, and had an effective H/Zr = 1.76. After redistribution, the receptor rods were sectioned longitudinally and then transversely in one-half at the extremities and the center to obtain specimens. The specimens were dehydrided and a gas sample analyzed by mass spectroscopy to evaluate the percentage of each isotope present. The results are given in Table 38. "Near," "center," and "far" in the table refer to the position with respect to the highly tritided donor. The time for complete redistribution is seen to have been more than 24 hr, but less than 72 hr.

TABLE 38
TRITIUM REDISTRIBUTION

Donor Volume (cm ³)	Receptor Volume (cm ³)	Conditions		Total T ₂ + H ₂ (ℓ)	Total Tritium (cm ³)	Theoretical T/H Ratio* (x 10 ³)	Observed Ratio (x 10 ³)			
		Temperature (°F)	Time (hr)				Donor Specimen	Receptor Specimen		
								"Near"	"Center"	"Far"
0.534	21.4	1200	168	26.9	33.9	1.24	1.6 ± 0.4	1.2 ± 0.2	1.0 ± 0.2	1.1 ± 0.2
0.529	21.7	1300	168	27.1	34.4	1.26	1.7 ± 0.4	0.9 ± 0.4	0.7 ± 0.4	1.0 ± 0.4
0.527	21.5	1300	72	26.9	39.0	1.43	1.3 ± 0.2	1.3 ± 0.2	1.2 ± 0.3	0.4 ± 1.0
0.482	21.5	1300	24	27.0	35.7	1.30	3.7 ± 0.4	1.4 ± 0.2	1.1 ± 0.2	0.6 ± 0.2

*Assuming uniform distribution throughout all fuel.

Knowledge of the rate of permeation of tritium through fuel element cladding was needed to assure that the initial loading would be sufficient to provide the desired amount of tritium retained in the fuel. The permeation rate was determined relative to the rate for hydrogen, which would be known from acceptance tests of each fuel element.

A dummy element, modified by welding a pressure probe on one end cap, was fabricated for the test. Coated Hastelloy-N (SNAP 8) material was used for the cladding. Gas was admitted to the interior from a solid solution of known compaction. The permeated gas pressure was measured, and both interior and exterior compositions analyzed by mass spectroscopy. The results are given in Table 39.

TABLE 39
TRITIUM/HYDROGEN RELATIVE PERMEATION

Tritium Concentration (mole %)		Estimated Tritium Pressure (atm)	T/H Permeation Ratio (%)
Interior of Membrane	Exterior of Membrane		
0.78 ± 0.06	0.08 ± 0.02	0.0078	10.6 ± 2.7
33.82 ± 3.7	3.79 ± 0.34	0.50	7.7 ± 1.4

It was assumed that the permeation rates were directly proportional to the partial pressures. Within experimental error, the two measurements yielded the same permeation ratios with pressure differences of two orders of magnitude, apparently validating the assumption. The linear dependence on partial pressure indicated that permeation through the glass coating was the limiting step. The loss of tritium from fuel elements would be negligible according to these results.

Data on the retention of tritium by the fuel cladding was obtained from the cladding used in the permeation experiments. The data were used to indicate the concentration of helium in the cladding expected from tritium decay. Excessive helium at the grain boundaries of the cladding could have degraded its mechanical properties.

In one case, the retained tritium was determined from the apparent loss of gas during the experiment. In the other case, the cladding was outgassed. Considerable uncertainties were introduced from apparent deviations between expected solubilities and/or diffusion coefficients and those implied by the results. Using conservative assumptions, an upper limit of helium accumulation (~8 ppb) was determined. Since this was only one-half the concentration attained after a

10^{21} nvt irradiation of Hastelloy-N with a low (9 ppm) natural boron content, the addition of tritium should have no discernible effect on the mechanical properties of the cladding.

A process was sought by which tritium could be introduced into full-length SNAP 8-type fuel rods. Tritium exchange from a "highly" tritided ZrU donor to a previously hydrided and machined fuel rod was tested, with exchange to occur in a separate process prior to assembling the fuel into the element.

Three ZrU fuel cylinders, each containing $\sim 20 \text{ cm}^3$ of tritium, were prepared for use as donors, with overall $(\text{H} + \text{T})/\text{Zr}$ of ~ 1.7 . Receptors consisted of hydrided SNAP 8 ($\text{H}/\text{Zr} = 1.76$) fuel rods.

Donor and receptor pairs were placed within a high-density alumina retort (5/8 in. ID by 36 in. long) and evacuated to ~ 1 torr. This assembly was then centered within a 24-in. long Marshall furnace. Outgassing of the furnace-immersed components was accomplished under dynamic vacuum to a temperature of 600°F . A steady-state temperature of 1350°F was maintained for 72 hr, followed by furnace cooling of the assembly to promote reabsorption of the hydrogen and tritium.

Each donor was subsequently dehydrided to permit gas analysis by mass spectrometry. These analyzed values of tritium concentrations were compared with theoretical values, assuming 100% uniform redistribution. One of the receptor rods was longitudinally sectioned along a diameter and was autoradiographed. The soft β emissions from tritium showed the distribution of this isotope.

Bowing of the full-length fuel rods during the high-temperature redistributive process exceeded S8DR specifications in one of the three cases. This was felt to be correctable by controlling the straightness of the alumina retort used. No significant evidence of diametral distortion was observed.

The three tritium donor specimens were completely dehydrided following the tests. Mass spectroscopic analyses of the three gas samples showed very good agreement between theoretical and analytical T/H molecular ratios for complete redistribution. Autoradiographic results confirmed the uniformity of the tritium distribution.

The foregoing tests demonstrated the feasibility of a process for the incorporation of tritium into SNAP fuel. No significant losses of tritium or adverse mechanical effects from helium were found for unirradiated fuel tritided to proposed concentrations.

Figure 197. Solar Flux Time-Fit Working Curve

G. DISPOSAL MODE RISK ANALYSIS

1. Effects of Solar Flux on Orbital Decay

The longer a reactor remains in orbit after shutdown, the lower the risk on reentry, because of increased radioactive decay. A study survey was made to evaluate the effects of the solar flux cycle on the orbital decay of satellites with ballistic coefficients from 10 to 200 lb/ft² in circular orbits about the Earth's equator. The expected lifetime of a power system for a NASA space station with a ballistic coefficient of 18.0 lb/ft² was also evaluated.

The solar activity effect is the correlation between the density fluctuations in the altitude range between 200 to 1600 km and the solar flux in the decimeter wavelength. Solar flux is in units of watts/square meter-cycle per second at a wavelength of 10.7 cm. The solar flux was found to have a period of about 11.4 yr and its intensity varying greatly. The solar cycle used for this evaluation was the most recent and completely recorded cycle, and was of the largest amplitude which is expected to persist for upcoming cycles. The solar flux time-fit working curve used for these studies is presented in Figure 197.

Reentry times are based on the density of the upper atmosphere, which is dependent on solar activity. An expression for the incremental time period (ΔT) for a vehicle to sink from altitude Z_2 to altitude Z_1 , as a function of density, was derived.

Figures 198 and 199 illustrate the effect of the solar flux cycle on the reentry trajectories from various altitudes for ballistic coefficients of 16 and 200 lb/ft². The solar flux cycle (see Figure 197) is superimposed on each figure for reference. A body tends to orbit at nearly constant altitude during periods of low solar activity; but as the solar activity increases, orbital decay increases substantially. Depending on the starting year, altitude, and ballistic coefficient, a body may orbit for one or more solar cycles; but reentry usually occurs during or immediately following periods of high solar activity.

The foregoing techniques were also used to estimate the lifetime for the nuclear power system for a NASA space station. Using the expected ballistic coefficient of 18 lb/ft² and an initial altitude of 270 n.mi, the reentry time varies from 1.2 to 8.0 yr, depending on what year orbit stabilization is discontinued. Figures 200 and 201 illustrate the reentry times that may be expected from reentry from any altitude between 220 and 320 n.mi for 6 of the 11 yr of the solar cycle.

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Figure 198. Reentry Times for a Ballistic Coefficient of 16 lb/ft²

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Figure 199. Reentry Times for a Ballistic Coefficient of 200 lb/ft²

6531-4526

Figure 200. Reentry Time for a Ballistic Coefficient of 18 lb/ft^2 (Reentry Starting in 1971, 1975, and 1980)

6531-4527

Figure 201. Reentry Time for a Ballistic Coefficient of 18 lb/ft^2 (Reentry Starting in 1972, 1976, and 1978)

2. Radiation from Fallout of Ablation Particles

a. Analysis⁽³⁵⁾

The purpose of this study was to evaluate the hazards associated with the radiation field produced by large particles from the ablation of a SNAP 10A core reentering the atmosphere. Since the dose from fallout would be received over an extended period, the overexposure criterion was based on the equivalent residual dose. Maximum fallout concentrations were calculated for both polar and equatorial orbits.

The power history for a SNAP 10A flight was used. For dose calculations, the assumptions were as follows:

- 1) Uniform plane source geometry
- 2) A 1-m source-target distance
- 3) A 1-yr exposure time
- 4) Dose rate reduction by radiological decay.

Doses were obtained by transforming Curie-Dose code results from point source to plane source geometry. The overexposure criterion was taken as an equivalent residual dose (ERD) of 25 rem. Known variations in particle shape and density were neglected. An experimentally measured primary particle size distribution was used. For fallout calculations, the assumptions were as follows:

- 1) A 50-km burnup altitude
- 2) A 10-km burn path length
- 3) The wind speed profile that is expected half the time in the calmest part of North America in winter
- 4) Particles fall with terminal velocity
- 5) Turbulent eddy diffusion causes the cloud width (W, km) to increase with time (t, sec) as:

$$W = 2 \times 10^{-4} t.$$

The assumptions were conservative, so the results constituted an upper bound for the hazard.

It was shown that the criterion dose would not be recieved, unless the activity concentration was at least 1 core/in.². Fallout calculations show the maximum concentration to be 0.013 (core/mi²) from an equatorial orbit. It followed that the maximum equivalent residual doses were:

$$\text{ERD}_{\text{max}} = 0.4 \text{ rem.} \dots (\text{polar orbit})$$

$$\text{ERD}_{\text{max}} = 20 \text{ rem.} \dots (\text{equatorial orbit}).$$

Probability considerations indicated that even with worst possible fallout fields (i.e., if the radioactivity were deposited in the maximum area over which the criteria dose could be delivered), the hazard generally would be less than it would have been if intact fuel elements reached the biosphere.

It was concluded that reentry burnup would virtually eliminate SNAP 10A radiological hazards.

b. Experimental Requirements⁽³⁶⁾

An analysis of the factors affecting particle deposit concentrations at the Earth's surface was made. A need for experimental investigation of the cloud growth of large particles by turbulent eddy diffusion was identified. An experimental program for releasing selected particles at 150,000 to 250,000-ft altitudes, and observing the pattern of fallout at the surface, was described which would clearly define the hazard that can be expected from the fallout of reactor debris particulates.

Analysis confirmed that submicron particle fallout was well understood and constituted no hazard. However, lateral expansion of clouds of larger particles was found to be very poorly understood. Also, no data for cases with SNAP conditions were available, nor was the theoretical bases found adequate to admit confident extrapolations to the SNAP situation. Better knowledge of these phenomena was needed to demonstrate the safety of reentry burnup for reactors with powers greater than 40 kw.

Injection of particulate cloud sources into the atmosphere under known conditions and following of its growth in space and time were found necessary to fully describe cloud growth. A surface fallout collection scheme with supporting atmospheric cloud tracking was suggested as the most practical methods of

measurement. Particle sizes were identified which would be sufficiently affected by atmospheric motion during descent, but would not be spread over an impractically large area. Wind and weather requirements were defined. Design and distribution of surface collection devices were specified. A sample calculation under an actual wind condition was made. Statistical considerations were noted.

A proven and economical means of conducting atmospheric fallout experiments existed, using gun-launched probes. A 16-in. gun was available, which had potential capabilities of launching payloads up to 1000 lb, and having a volume of 500 in.³ to over 200,000 ft once an hour. Also, 5- and 7-in. guns with smaller payload capability, but more rapid firing, were available. Demonstrations of payload integrity, under firing g-loadings, and apogee release mechanisms and timers would be subjects of demonstration tests.

Support efforts were outlined. Only a small number of people were found necessary for experimental design and theoretical backup. Careful meteorological support was required.

The results of the experimental fallout measurements would permit accurate calculations of the radioactive hazards associated with the injection of reactor debris into the atmosphere.

c. Exposure Criteria⁽³⁷⁾

The purposes of this study were to review the factors involved in radiological consequences of reentry burnup and to recommend practical dose criteria which would ensure safety.

Historically, safety standards are much higher for nuclear than for other activities. Radiological hazards have been controlled with phenomenal success, based on nationally and internationally recommended criteria. There has never been any attempt to completely eliminate radiation doses. Public interest and emotional involvement in radiological hazards is entwined with the radiological safety tradition development. The emotional factor may affect reentry burnup considerations.

Radiation is by far the best understood environmental hazard. Biological responses to radiation and other toxic substances are statistical in nature. Therefore, threshold doses cannot be rigorously determined. Radiobiological

knowledge permits the choice of dose criteria on the basis of low probabilities of injury.

There are few such activities which produce a zero dose. A 1000-Mw, coal-burning power plant may release a curie of radium a year to the atmosphere. Radium deposits are measureable in the lungs of New Yorkers. Even normal breathing increases the atmospheric inventory of radioactive material by about 10^{-20} rem each year. A zero dose limit cannot be implemented.

Dose criteria based on ICRP recommendations have been applied in the nuclear industry. Thousands of radiation workers for decades have received doses on the order of the ICRP criteria for occupational exposure without detectable injury. Even so, to account for the possibility of gaps in radiobiological knowledge, highly radiosensitive individuals, and (primarily) genetic damage, ICRP-recommended dose limits for populations are almost a factor of 100 lower than limits for radiation workers. The probability of injury from doses of this (2 rem in 30 yr) magnitude is negligibly small. Populations have lived for generations in areas where the natural background radiation doses are many times the ICRP criterion; for example, Guarapari, Brazil, where the rates are 80 times the ICRP criterion.

It was concluded that the ICRP population dose limit was conservative and that it met safety requirements.

It was recommended that:

- 1) The ICRP population dose criterion (2 rem in 30 yr) be used as a guide in nuclear planning.
- 2) One-half the population dose (1 rem in 30 yr) be apportioned to aerospace activities.
- 3) For planning purposes, until space program requirements dictate change, 1/8 of the population dose (0.25 rem/30 yr) be apportioned to SNAP reactors.

This dose limitation would restrict the use of reentry burnup for disposal to about 100 SNAP 8-type reactors per year.

3. Disposal Methods⁽³⁸⁾

It had been shown that a reactor system, operated in an orbit with a lifetime of 50 yr or more, presented no safety problems, if a reliable end-of-life shutdown device were used. In attempting to apply the techniques of decay, recovery, and dilution, three basic ultimate disposal methods had evolved. These were: (1) orbital boost, (2) controlled reentry, and (3) destruct. A study of these methods was directed toward the problems associated with orbits of short duration (<50 yr).

Orbital boost may involve either injecting the reactor into a solar orbit or increasing altitude to extend orbit life. Clearly, safety objectives were achieved if the reactor was placed into a solar orbit, because it would not return to earth. Boost to a long-lived (parking) orbit was not sufficient in itself to satisfy safety objectives. To attain these objectives, shutdown of the reactor was required; otherwise, the problem was postponed. Even if shutdown was achieved, safety was assured only if the parking orbit was of sufficient duration to permit nearly complete elimination of fission products by decay, prior to reentry. Further, in-orbit elimination of fission products did not solve the problem, if the reactor could be made critical after reentry. Finally, the reliability of an orbit augmentation system could only approach one. If the system failed, safety depended on core characteristics and backup safety systems. Reentry burnup as a backup to an orbit augmentation system provided a large reduction in the probability of a hazard.

Controlled reentry included both: (1) bringing the reactor down to a predetermined, safe location for recovery and subsequent disposal in a conventional manner; and (2) directing the reactor to a safe, deep-ocean impact. The first approach was purely a recovery (containment) technique. The second approach, ocean disposal, was a combination of decay and dilution, since the slow escape of fission products would allow considerable decay before dilution is achieved in the ocean water. If controlled reentry for recovery was successful, safety was assured. However, for controlled reentry and subsequent recovery to succeed, the reactor had to be designed to withstand or be protected from reentry forces. Reentry disassembly would have caused the core to be widely scattered, and recovery would not have been practical. Ocean disposal was safe, if post-impact criticality was prevented by design or by reentry disassembly. With

either system, failure had to be considered; hence, backup systems became important.

Various destruct mechanisms were possible. For example, a destructive nuclear excursion could have been induced or the core might have been destroyed by a corrosive material. A simple explosive device might have proven to be the most practical. Its purpose would have been to separate the fuel elements from the core vessel, and thus enhance fuel ablation. Such a device might also have shattered fuel elements and/or have dispersed them before reentry. Each destruct system was a dilution technique. The destruct provided safety through facilitating dilution by the atmosphere. The explosive destruct device prevented post-impact criticality, but did not eliminate radiological problems, unless reentry burnup occurred and was accompanied by adequate atmospheric dispersal. A destruct system, like other safety systems, might have failed; therefore, reliable backup was desirable.

For orbits of short lifetime, the following eight combinations of the three basic methods (boost, controlled reentry, and destruct) were considered:

- 1) Orbital boost to parking orbit with reentry disassembly and burnup
- 2) Orbital boost to parking orbit with immediate destruct and reentry disassembly and burnup
- 3) Controlled reentry with intact impact in deep ocean
- 4) Controlled reentry with reentry disassembly and burnup and ocean impact
- 5) Controlled reentry with destruct and reentry disassembly, burnup, and ocean impact
- 6) Controlled reentry with intact impact on land, recovery, and storage
- 7) In-orbit destruct with reentry disassembly and burnup
- 8) Random reentry with reentry disassembly and burnup.

Each of these eight methods was examined in detail to determine the estimated probability of occurrence for each of the following nuclear accidents:

- 1) Atmospheric contamination
- 2) Overexposure due to personal contact with fuel elements
- 3) Overexposure due to post-impact criticality (either water or land).

The average number of overexposures for each of these accidents was determined, assuming that the accident had occurred. These overexposures-per-accident values were then multiplied by the probability of occurrence of each accident and were summed for each disposal method to produce the total average number of overexposures per mission. A summary of the results of these calculations is shown in Table 40.

TABLE 40
SUMMARY OF ULTIMATE DISPOSAL
METHODS FOR SNAP REACTORS
IN SHORT LIFETIME ORBITS

Disposal Method	Average Number of Overexposures per Mission
1. Orbital boost to parking orbit with reentry burnup	0.0036
2. Orbital boost to parking orbit with immediate destruct and reentry burnup	0.001
3. Controlled reentry with intact impact in deep ocean	0.36
4. Controlled reentry with reentry disassembly and burnup and ocean impact	0.0046
5. Controlled reentry with destruct and reentry burnup and ocean impact	0.001
6. Controlled reentry with intact impact on land; recovery and storage	0.26
7. In-orbit destruct (random reentry) with reentry burnup	0.14
8. Random reentry with reentry burnup	0.36

BOOST TO PARKING ORBIT

DESTRUCT

REENTRY BURNUP

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Figure 202. Orbital Boost to Parking Orbit With Immediate Destruct, End-of-Life Shutdown, Reentry Disassembly, and Burnup

A typical detailed study of a particular method is shown in Figure 202.

Orbital boost methods and controlled reentry methods that use reactors designed for reentry burnup resulted in at least a factor-of-100 improvement in safety over all random reentry methods and controlled reentry methods designed for intact reactor reentry. This study showed that reentry burnup, as a backup to orbital boost or controlled reentry, may provide a two-order-of-magnitude improvement over all other disposal methods, including intact reentry and recovery method.

4. Destruct Charge Development⁽³⁹⁾

A program required to evaluate the use of chemical explosives as a reliable technique for disassembly of a SNAP reactor after an operating mission in orbit was defined. To assure that the reactor fuel will burn up by aerodynamic heating upon reentry into the Earth's atmosphere at the end of a mission, an explosion which would rupture the reactor vessel and disperse the fuel elements may be needed. The chemical destruct device would have to be carried on the space vehicle for the life of the orbital mission.

Data were obtained from literature which indicated that a destruct charge firing train could be developed from existing materials without advancing the state-of-the-art of explosives. However, the effects of long-term storage at 160° F in combination with radiation at the SNAP levels had not been reported for the explosives in question. A design approach was adopted and used to plan the development of a SNAP reactor destruct device. Tasks were outlined which were required to evaluate the thermal and radiation resistance of explosives materials, to develop a technique for reactor disassembly, and to provide a suitable method of transporting and initiating the destruct charge.

High-explosive organic chemicals were selected because of their great shattering power. The likely configuration involved a three-part "explosive train" of detonator, booster, and main charge. The organic materials cannot survive high radiation levels, so they would have to be stored in a low radiation zone and transported to the reactor vessel just prior to the destruct event.

The first phase of the development program would be thermal storage at 160° F for long periods with irradiation to expected total doses. Possible candidate materials were selected by extrapolating thermal and radiation stability

data found in the literature, and through consultation with experts. A need to determine the composition of gas evolved during storage was identified. Analysis would indicate the extent of loss of explosive power compared to radiation-induced changes which do not affect power.

A development plan was detailed which included optimizing core disassembly, investigating problem areas associated with mechanisms for transporting the charge from the storage position to the reactor vessel, and controlling launch pad hazards. A system for measuring evolved gas volumes as small as 0.2 ml was described. Spectroscopic analysis would be used. Five types of measurements for describing the performance of explosives in self-destructive tests were selected and test apparatus was described. These tests would measure detonation velocity (shattering power), initiation sensitivity, initiation efficiency of detonators, auto-ignition conditions, and ignition and burning rates of transporter propellants.

Design options were planned for obtaining maximum fuel dispersal by optimizing the destruct charge size, method of core penetration, and point of detonation. Fragmentation studies would be made to determine fuel particle sizes and distributions. The final task would be a demonstration of the resultant explosive destruct system on a typical SNAP reactor.

A development schedule was made for the major tasks involved, and costs were estimated to be spread over a 4-yr period. The results would be applicable to the design of destruct systems for a wide range of SNAP reactor configurations.

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