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PROGRESS REPORT
SNAP AEROSPACE SAFETY PROGRAM
APRIL-JUNE 1965
(Title Unclassified)

AEC Research and Development Report

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SNAP AEROSPACE SAFETY PROGRAM
APRIL-JUNE 1965
(Title Unclassified)

R. L. DETTERMAN
Chief Project Engineer

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CONTENTS

	Page
I. Program Summary for FY 1965	7
A. Reentry and Abort Studies	7
1. Reactor Disintegration Projects	7
2. Fuel Reentry Burnup Projects	8
3. Fuel Properties Projects	8
4. Abort and Post-Reentry Projects	9
B. Nuclear Studies	9
1. Reactor Transient Projects	9
2. Critical Configuration Projects	10
3. End-of-Life Shutdown Project	10
II. Reactor Disintegration Projects	11
A. Experiments	11
1. SNAP 10A-Agena Aerostability Studies	11
2. Reactor Ablation-Disintegration Experiment (RADE)	17
3. Self-Welding Tests	17
B. Analysis	19
1. Review of RFD-1 Flight-Test Data	19
2. Development of Basic Methods	19
III. Fuel Reentry Burnup Projects	20
A. Experiments	20
1. Fuel Arc-Jet Tests	20
2. Fuel Particle Experiments	20
B. Analysis	20
1. Fuel Element Parametric Study	20
2. Supporting Analysis	22
IV. Fuel Properties Project	23
A. Thermophysical Properties	23
1. Electrical Resistivity	23
2. Specific Heat	23
3. Thermal Diffusivity	25
4. Data Comparison Program	25
B. Kinetic Studies	26
V. Reactor Transient Projects	27
A. SNAPTRAN-1, -2	27
1. Experiment	27
2. Analysis	27
B. Supporting Methods Development	32
C. Supporting Experiments	34

NAA-SR-11497

~~CONFIDENTIAL~~

	Page
VI. Critical Configuration Projects	35
A. Experiments	35
B. Analysis	35
VII. End-of-Life Shutdown Project	39
References	40
Bibliography of Reports Published in FY 1965	40

TABLES

1. Hypervelocity Wind Tunnel Force and Moment Data	12
2. Hypervelocity Wind Tunnel Dynamic Stability Data	15
3. Values of Integrated Aerodynamic Heat Available and Heat Required to Initiate Fuel Ablation at Various Release Altitudes	21
4. Electrical Resistivity of Tantalum	23
5. Electrical Resistivity of H/Zr 0.5 Fuel Samples	24
6. Thermal Diffusivity of H/Zr 1.90 _{eff} Fuel (Sample 247-D5)	25
7. Thermal Diffusivity of H/Zr 1.59 _{eff} Fuel (Sample 247-D6)	26
8. Parameters Used in Analysis of SNAPTRAN-1 Experiment Data	28
9. Dissociation Pressure vs Temperature (Run No. 1)	38
10. Dissociation Pressure vs Temperature (Run No. 2)	38

FIGURES

1. SNAP 10A-Agena Models Used in LTV Hypervelocity Wind Tunnel Force Tests	14
2. SNAP 10A-Agena Dynamic Stability Model Used in LTV Hypervelocity Wind Tunnel Tests	15
3. SNAP 10A RADE Model Components	17
4. SNAP 10A RADE Model (Assembled)	18
5. Integrated Aerodynamic Heat Available and Heat Required to Initiate Fuel Ablation as a Function of Release Altitude	21
6. Material Response of Induction-Heated Fuel Sample (5/8 in. diameter by 5/8 in. long)	22
7. Electrical Resistivity of Tantalum	23
8. Electrical Resistivity of H/Zr 0.5 Fuel Samples	24
9. Thermal Diffusivity of H/Zr 1.90 and 1.59 Fuel Samples	25
10. Compensated Reactivity vs Core-Averaged Temperature Rise (SNAPTRAN-1 Experiments)	27
11. Super-Prompt Critical Reactivity vs Reciprocal Period (SNAPTRAN-1 Experiments)	32

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FIGURES

	Page
12. Minimum Weight Penalty as a Function of Reduced Reactivity in a Water Environment	37
13. Dissociation Pressure vs Temperature (Run No. 1)	38
14. Dissociation Pressure vs Temperature (Run No. 2)	38

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I. PROGRAM SUMMARY FOR FY 1965

This section summarizes the significant accomplishments in the Aerospace Nuclear Safety Program at Atomics International for FY 1965. The reports published in FY 1965 are listed in the bibliography at the end of this report.

A. REENTRY AND ABORT STUDIES

1. Reactor Disintegration Projects

The RFD-1 flight-test data were reanalyzed early in FY 1965. The aerodynamic heating rates to the vessel wall and coolant outlet pipe were recalculated by considering that the thermocouples had only a limited contact with, rather than being completely welded to, the internal surfaces and, therefore, received heat principally by thermal radiation. The recalculated heat rates more nearly agreed with aerodynamic theory for equilibrium, continuum flow and led to predictions of reactor disintegration in orbital decay at altitudes higher than those previously reported. An experiment was completed to determine the thermal response characteristics of the thermocouples installed in the coolant pipe of the RFD-1 flight-test model. The results of this experiment were compared with those obtained from the aforementioned recalculations and showed good agreement.

Two mathematical models were developed in conjunction with the SNAP 10A vessel wall study. One represented the reactor without NaK, while the other represented the reactor containing NaK during reentry. Variable parameters affecting the time and altitude of vessel wall melting were studied. These parameters included emissivity, wall thickness, initial temperature, sky condition (full sun or dark sky),

and the presence of NaK in the reactor. Emissivity was found to strongly affect the time and altitude of wall ablation. Wall thickness, initial temperature, and sky condition have a less significant effect on the time of wall ablation.

Emittance (total normal) measurements were completed on 316 stainless steel to provide information on the behavior of the SNAP core vessel during reentry, since this parameter strongly affected the vessel melting altitude. Actual SNAP 10A reactor vessel samples were subjected to the oxidation-heating cycles predicted for reentry. The oxidized samples were then tested and the emittance values were compared to those obtained on clean, unoxidized control samples.

SNAP 10A-Agena aerostability tests were conducted by Ling-Temco-Vought (LTV) in their wind tunnel facility. Lockheed Missiles and Space Company (LMSC) was awarded a study contract to perform aerodynamic consultation for the experiments, to determine failure modes of SNAP 10A-Agena systems, to define a range of initial conditions and determine the attitude history and trajectories of reentering system configurations, and to provide aerodynamic characteristics of the system about the complete angle-of-attack range. These activities were completed, firmly establishing the aerodynamic characteristics of the SNAP 10A-Agena configuration. A topical report^{(1)*} will be issued early in FY 1966.

The results of calculations on the dissociated flow in the hotshot wind tunnel nozzle (wind tunnel tests conducted the latter part of FY 1964) were analyzed. The analyses indicated that an appreciable amount of recombination occurred

*Superscripts refer to the References listed at the end of this report.

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in the expansion cone. The effects of this dissociation on the wind tunnel test data were determined and the final test results were evaluated.

A Reactor Ablation-Disintegration Experiment (RADE) was in progress at the close of FY 1965. Half-scale reactor models will be subjected to a hyperthermal wind tunnel environment to develop reentry burnup data. The objectives of the RADE tests will be: (1) to experimentally verify the predicted behavior of a SNAP reactor exposed to a simulated reentry environment and (2) to observe the disassembly mode of the reactor in this environment.

Verification of the predicted behavior will be accomplished by first measuring the heat flux distribution on the reactor vessel for a catalytic wall, then exposing an instrumented model of a reactor vessel and simulated core (inert) to an arc-heated air stream, and finally comparing the temperature response of the vessel to analytical predictions of the same. The catalytic effect of the reactor vessel stainless-steel wall on heat flux was independently evaluated by AI in a separate test program, thus assuring proper application of the measured heat flux distributions.

2. Fuel Reentry Burnup Projects

In the analytical study of the transient hydrogen redistribution and heat transfer in a reentering SNAP fuel element, calculations were performed (for the arc-jet test conditions developed in the FY 1964 test series) in which the heat transport, due to the hydrogen redistribution, was evaluated. The most recent calculations made use of the multiregion model, which included the effect of the center hole in the arc-jet fuel specimen that was provided for a tungsten rod holder.

Calculations were also performed to account for the reduction in the fuel thermal conductivity during phase transition. The basic effect on ablation is an increased surface-to-average-

temperature ratio and a resulting radiation heat loss from the surface. The increase in radiation heat loss is reflected in a corresponding increase (of a few per cent) in the total necessary heat for ablation. An analytical description of the heating of a fuel rod up to melting temperature was completed. A topical report⁽²⁾ will be issued early in FY 1966.

A study of the release of hydrogen through cracks in a fuel rod during reentry was completed. The ablation process of a uranium zirconium-hydride fuel rod was analyzed by using the conservation equations for transient and quasi-steady flow conditions.

An arc-jet test of fuel material was undertaken during FY 1965 to complete the study of the reentry heating to a SNAP fuel rod. A gross investigation of the particle formation phenomenon was included in the experiment. The objectives of the test series encompassed the determination of: (1) surface catalytic effect, (2) radiant losses, (3) rod radius effect, (4) clearer understanding of the previously observed ignition phenomena (initial violation of material surface), and (5) use of a laser-shadowgraph apparatus to investigate hydrogen release from the rod and the effects of this release on aeroheating to the rod. The actual experiments were scheduled to begin early in FY 1966.

3. Fuel Properties Projects

Thermophysical property studies on SNAP fuel were completed during FY 1965. These studies covered the full range of hydride compositions up to the melting temperature. The data were reduced, evaluated, and plotted as a function of both temperature and H/Zr ratio. An extensive effort was required to modify equipment to obtain values of resistivity and diffusivity at temperatures higher than 900°F.

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The oxidation of nonhydrided SNAP fuel alloy was investigated and rate constants were obtained for various temperature ranges up to 2170°F. When the necessary equipment has been procured, oxidation studies of the fuel alloy during rapid temperature transients to 3000°F will be undertaken. Studies will also be made on the rate of hydrogen evolution from the fuel in both vacuum and air.

Equipment to simulate the thermal energy imparted to fuel elements during reentry was assembled and underwent checkout.

4. Abort and Post-Reentry Projects

Analysis of the accelerometer data from the first series of drop tests (FY 1964) indicated that recorded vertical shock-wave amplitudes ranging from 3,000 to 10,000 g were developed at impact. The instrumentation photography from this earlier series showed that a maximum load of 13,000 g was produced at the time of the side-on impact.

The Series II, 100-ft drop tests of nine test reactor vessels, which incorporated grooves of various depths, radii, and shapes, were completed. The purpose of this series of drop tests was to determine the effect of the various groove shapes and depths on vessel disassembly.

The merits of various methods of inducing vessel disassembly were evaluated. Two of the proposed methods were utilized in the design of test vessels for the Series III drop tests. This test series demonstrated that external end rings, in conjunction with grooves in the vessel wall, are effective for propagating fracture of the vessel wall and disassembly of the internal fuel rods. In addition to the aforementioned impact tests, a modified "Goer" vehicle was made operational near the end of FY 1965 in preparation for future Aerospace Safety impact testing requirements. This project was suspended prior to complete development of the mobile impact tester.

B. NUCLEAR STUDIES

1. Reactor Transient Projects

a. SNAPTRAN

The SNAPTRAN-3 experiment conducted late in FY 1964 was a destructive excursion with a water-immersed reactor of the SNAP 10A/2 type. The analysis of the results from this experiment was completed in the first half of 1965 and a report was issued. The test was highly successful from several points of view. Probably of greatest significance was the verification of the postulated modes of fuel disintegration, arising from hydrogen release in uranium-zirconium hydride, which prevents melting of fuel. This has the effect of reducing the release of fission products to very low amounts. For example, less than about 1% of noble gas fission products, and none of the less volatile fission products, were released in the SNAPTRAN-3 test. This greatly reduces the area which may be contaminated as a result of a destructive excursion.

The SNAPTRAN-1 reactor was being tested at the NRTS in Idaho. Its prime function is to provide data which will resolve questions relating to reactivity feedback mechanisms in SNAP reactors. This information is needed to predict transient behavior for various postulated accident situations.

The SNAPTRAN-2 reactor, which in most respects is a duplicate of the SNAPTRAN-1 system, was scheduled to undergo a destructive transient in the first half of FY 1966. This will provide data on reactivity feedback mechanisms for such systems at higher temperatures and shorter reactor periods than it is possible to attain with the nondestructive SNAPTRAN-1 experiments.

b. Supporting Experiments

The electrical heating, TREAT, and TNT capsule experiments provided the means for

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studying the behavior of uranium-zirconium hydride fuel exposed to rapid heating. These experiments have better defined the conditions under which disintegration will occur. They verified the postulated mechanisms of disintegration and measured quantities of importance for the analysis of destructive reactor transients.

The construction and operation of a simple mockup of a typical SNAP reactor coolant channel has shown that there are no large hydrodynamic instabilities which would prevent pseudo-steady-state operation at power levels as high as 3 to 6 kw per channel. (Note: There are 74 channels in the SNAP 10A core). This finding increases the need for a satisfactory solution of the problem of such operation occurring in an uncontrolled area.

2. Critical Configuration Projects

Initially, the work of the Critical Configuration Projects was concerned with the measurement of the reactivities of various potential accident configurations, such as the water-reflected situation, and with the development of devices, such as shipping sleeves and void filler blocks, which would keep the reactor subcritical during transportation and handling. The results of this approach have been quite satisfactory. The emphasis at the time of this report was, however, being placed on the more general problem of designing SNAP reactors which, although able to function normally in an operational sense, are intrinsically subcritical when flooded with water. Such a solution would automatically solve the problem of transportation and handling and, what is even more significant, would eliminate problems of post-launch criticality.

Several approaches to these designs will be taken. One such technique is the selective poisoning of the reactor with gadolinium, which is an excellent absorber of near-thermal neutrons and thus has a greater negative reactivity

worth in a water environment than in a zirconium hydride environment. A second approach concerns changes in reactor geometry. A third technique involves the use of beryllium oxide end reflectors. The purpose of the latter two approaches is to decrease the reactivity worth of water reflection. It may be that a combination of these techniques will be required to give the desired reactivity effect with minimum weight penalty. The work necessary to evaluate these possibilities will be undertaken in FY 1966.

3. End-of-Life Shutdown Project

Initially, the End-of-Life Shutdown Project studied the use of mechanical and explosive devices to render the reactor subcritical at the end of its useful life. All of these devices had a prime disadvantage in that their functioning was not inherent. The problem of ensuring high reliability of operation after more than a year in orbit and after exposure to a significant radiation dose level is a difficult one. Data are not yet available which would allow a quantitative evaluation of this reliability problem.

In view of these considerations, the work was redirected toward a new approach. This technique relies upon the natural decay of tritium to helium-3, which is a neutron poison of high absorption cross section. The operation of this technique is of course inherent; if there is tritium in the reactor, it will decay, and if the helium-3 daughter product remains in the reactor, it will poison the system. The questions that remain pertain to certain physical properties of tritium and helium in association with the reactor fuel and cladding. This project will attempt to answer such questions during FY 1966.

The studies of the potential hazards arising from the incorporation of tritium in SNAP reactor fuel have shown that there is no significant hazard involved, even in all conceivable accident situations.

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10

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A shutdown scheme in itself is not satisfactory for the solution of problems of short-lived orbit missions because many years are required for the decay of the fission products that have been produced from the normal operation of the reactor. For this category of missions, several schemes and combinations of schemes were studied. These studies led to the conclusion that, for certain missions and disposal schemes, nuclear or mechanical destructive devices may contribute to safety. In addition to rendering the reactor subcritical, a nuclear disassembly scheme, if activated during the early part of

reentry, has the further advantage of exposing fragmented fuel to the air stream at a high initial temperature. This reduces the amount of aerodynamic heating required for complete ablation and dispersal and increases the margin of safety incident to this disposal technique. This is important, since a conclusion of the studies previously referred to is that a destruct charge, to be effective, must contribute to the ablation of fuel in some manner.

An evaluation of the nuclear disassembly schemes and some testing of components will be conducted in FY 1966.

II. REACTOR DISINTEGRATION PROJECTS

A. EXPERIMENTS

1. SNAP 10A-Agena Aerostability Studies

a. Hypervelocity Wind Tunnel Tests (LTV)

The analysis of the results from the tests conducted by Ling-Temco-Vought (LTV) in their hypervelocity wind tunnel facility was completed. The objective of this test program was to obtain experimental force and moment data on the SNAP 10A-Agena configuration over a -10- to +190-degree angle-of-attack range and to obtain dynamic stability data on the same configuration over a 0- to 20-degree angle-of-attack range.

The models of the SNAP 10A-Agena used during this test program were machined from magnesium bar stock.

A total of five model configurations were used during the force test to provide an angle-of-attack range from -10 to 190 degrees. Because of the slender shape of the SNAP 10A-Agena and the tunnel test section dimensions,

two model sizes, 1/60 scale and 1/90 scale, were employed. The 1/60-scale models, used for angle-of-attack ranges of -10 to 20 degrees and 170 to 190 degrees, were sufficiently large to encompass the force balance, however, they were restricted to model centerline deviations of 20 degrees or less with respect to the tunnel centerline. A deviation of more than 20 degrees resulted in a portion of the model being outside the boundary of the core. The rocket nozzle was deleted in the model used for the -10- to 20-degree angle-of-attack range because of the presence of the force balance sting extension in this region. For the same reason, the forward section of the model used for the 170- to 190-degree angle-of-attack range was not included.

The angle-of-attack range from 20 to 170 degrees was covered by three 1/90-scale models having provisions for attachment to the force balance with the model centerline at 40, 90, and 150 degrees with respect to the balance

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TABLE 1
HYPERVELOCITY WIND TUNNEL FORCE AND MOMENT DATA

Model No.	Run No.	Angle of Attack, α (degrees)	Mach No., M	Reynolds No., $R \times 10^{-6}/ft$	C_A^*	C_N	C_m	C_D	C_L	C_l	C_Y	C_n	Comments
1	7	-10	16.58	0.462	0.386	-0.568	-0.434	0.479	-0.492	-0.0021	-0.0011	0.0025	
1	8	-10	16.63	0.488	0.399	-0.556	-0.427	0.490	-0.478	-0.0008	-0.0044	0.0054	
1	1	0	16.72	0.517	0.279	0.0066	0.018	0.279	0.007	-0.0068	0.0015	-0.0010	
1	2	0	16.64	0.474	0.262	0	0.005	0.262	0	-0.0025	0.0053	0.0060	
1	3	10	16.60	0.467	0.424	0.559	0.431	0.515	0.477	-0.0054	0.0123	0.0178	
1	6	10	16.61	0.477	0.401	0.516	0.403	0.484	0.438	-0.0089	0.0091	0.0109	
1	4	20	16.66	0.494	0.619	1.312	0.530	1.030	1.021	-0.0114	0.0140	0.0329	
1	5	20	16.65	0.501	0.607	1.246	0.499	0.997	0.963	-0.0269	0.0111	0.0202	Increasing R
1	52	20	16.94	1.146	0.595	1.415	0.542	1.043	1.126	-0.0212	0.0304	0.0397	High R
2	29	20.2	16.94	0.588	-	-	-	-	-	-	-	-	Schlieren only
2	45	20.2	17.05	0.644	0.624	1.332	0.535	1.046	1.034	-0.0608	-0.0070	0.0537	
2	44	30.2	17.03	0.633	0.784	2.524	0.095	1.948	1.786	-0.0821	-0.0370	0.1193	
2	46	34.2	16.97	0.600	0.896	3.130	-0.134	2.502	2.084	-0.0852	-0.0412	0.1182	
2	13	40.2	16.90	0.586	0.960	4.083	-0.579	3.370	2.496	-0.0226	0.1688	-0.0141	
2	14	50.2	16.88	0.574	0.980	5.633	-1.677	4.957	2.851	-0.0440	0.2359	-0.0023	
2	15	60.2	16.86	0.578	0.891	6.948	-2.711	6.473	2.676	-0.0358	0.2642	-0.0287	
3	47	59.9	16.96	0.602	0.995	6.540	-2.174	6.157	2.418	-0.2205	-0.2371	0.2670	
3	41	69.9	17.06	0.650	0.755	7.890	-3.370	7.669	2.002	-0.1768	-0.2176	0.2315	
3	42	69.9	16.99	0.615	0.799	7.804	-3.575	7.603	1.931	-0.1825	-0.2356	0.2904	
3	43	79.9	17.05	0.665	0.624	8.695	-4.503	8.670	0.910	-0.1969	-0.2687	0.2919	
3	24	89.9	16.94	0.590	0.301	8.279	-5.030	8.280	-0.286	-0.2011	0.1019	0.0431	
3	25	89.9	16.90	0.576	0.309	8.207	-5.052	8.208	-0.294	-0.2015	0.1027	0.0402	
3	26	89.9	16.99	0.614	0.186	8.137	-4.933	8.137	-0.172	-0.3419	-0.0737	0.0137	Fairings removed
3	23	99.9	16.97	0.602	0.098	7.930	-5.132	7.795	-1.460	-0.1674	0.1042	0.0149	
3	16	109.9	16.91	0.601	-0.222	7.941	-6.278	7.542	-2.494	-0.0068	0.1829	0.0253	
3	48	119.9	17.01	0.637	-0.864	6.525	-6.058	6.087	-2.504	-0.1397	-0.1866	0.2369	
3	50	129.9	16.99	0.618	-1.221	5.127	-5.331	4.717	-2.352	-0.1122	-0.0795	0.1007	
4	40	130	17.00	0.621	-1.045	5.466	-5.700	4.859	-2.713	-0.0297	-0.1460	0.1228	
4	39	140	17.10	0.665	-1.291	4.014	-4.475	3.569	-2.245	-0.0157	-0.0893	0.0579	
4	21	150	16.95	0.596	-1.288	2.099	-2.702	2.165	-1.174	-0.0166	-0.0754	0.0163	
4	22	150	16.94	0.594	-1.166	2.043	-2.647	2.032	-1.186	-0.0117	-0.0675	-0.0242	
4	18	160	16.87	0.583	-0.988	0.948	-1.269	1.253	-0.553	-0.0057	-0.0217	-0.0190	
4	19	160	16.94	0.591	-1.007	0.964	-1.268	1.276	-0.561	-0.0104	-0.0240	-0.0325	
4	20	170	16.92	0.580	-0.779	0.239	-0.387	0.809	-0.100	-0.0030	0.0043	-0.0291	
5	9	170	16.57	0.462	-0.800	0.307	-0.504	0.841	-0.163	0.0036	-0.0030	-0.0037	
5	10	180	16.55	0.456	-0.707	-0.003	0.009	0.707	0.003	0.0012	0.0013	-0.0127	
5	11	190	16.58	0.466	-0.781	-0.265	0.402	0.815	0.125	0.0086	0.0044	-0.0137	
	28	31	16.58	0.430	2.009	1.670	0.556	-	-	-	-	-	AI model
	12	61	16.62	0.473	1.267	6.195	0.640	-	-	-	-	-	AI model
3	17	99.9	16.91	0.602	0.065	6.538	1.007	6.429	-1.189	-0.2744	-0.0459	-0.0132	Rear sec off
3	36	119.9	16.9	0.6	-0.551	4.768	0.089	-	-	-	-	-	Rear sec off
3	49	129.9	17.01	0.624	-1.131	3.670	-0.134	3.541	-1.486	-0.0420	-0.1405	0.1858	Rear sec off

C_A - Axial force coefficient referenced to body axes
 C_N - Normal force coefficient referenced to body axes
 C_m - Pitching moment coefficient about CG referenced to body axes
 C_D - Drag coefficient
 C_L - Lift coefficient
 C_l - Rolling moment coefficient about CG referenced to body axes
 C_Y - Side force coefficient referenced to body axes
 C_n - Yawing moment coefficient about CG referenced to body axes

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centerline. Since only a very small portion of the balance was enclosed by the 1/90-scale models, a cylindrical balance shield supported by the tunnel sting was provided to preclude impingement of flow on the balance.

The models were constructed with interchangeable sections in order to minimize the number of components required for the five force model configurations. Photographs of the force balance, model components, and each of the force models mounted on the tunnel sting are shown in Figure 1.

The dynamic stability model, from which data were obtained at angles of attack of 0, 10, and 20 degrees, consisted of the 1/60-scale force model shell bonded to the dynamic stability balance. The rocket nozzle was deleted because of interference with the balance sting. A balance sting small enough in diameter to include the nozzle would not have provided adequate support for the model which was designed to oscillate about the CG location at a nominal frequency of 119 cps. Prior to making a run, the model was deflected to an amplitude of approximately 2 degrees and held in this position by a cocking bar. Release was accomplished during each run by means of a pneumatically driven piston which was attached by wire to the cocking bar. Photographs of the model and balance appear in Figure 2.

Results of the force test are presented in tabular form in Table 1. The static trim angle is 32.5° and the static stability derivative, C_{m_α} , at the static trim angle is -0.0614 per degree. The normal static derivative, C_{N_α} , at the static trim angle is 0.1468 per degree. The aerodynamic loads from which C_Y , C_n , and C_l were determined were very low and it is probable that interference effects from the tunnel sting may be of the same order of magnitude making it difficult to correlate the side force data from the several models, each with a different sting configuration.

Run No. 26F was made with all the fairings removed from the model configuration in order to determine the contribution of the fairings to the overall aerodynamic characteristics at a 90-degree angle of attack. The difference in the force and moment coefficients obtained with and without fairings may be observed in Table 1.

During run numbers 17, 36, and 49, the rear section (from station 382.2 aft) became disengaged at the beginning of the runs and the data obtained from these runs reflect the unique situations of the SNAP 10A-Agena minus the rear section. The runs were repeated with the model intact; however, the data obtained with the rear section off are valid for the altered configuration and are included in Table 1.

Force and moment data were obtained for three Reynolds number conditions at $\alpha = 20^\circ$. Run No. 4F was made at the same Reynolds number as that of the other 1/60-scale models. The Reynolds number was increased during Run No. 5F, and Run No. 52F was made with a higher initial Reynolds number. No significant Reynolds number sensitivity was noted.

In compliance with a request to demonstrate data repeatability in the LTV hypervelocity wind tunnel, two force runs were made with a model supplied by Atomics International. This model had been previously tested in the hypervelocity wind tunnel in 1964. Run numbers 12 and 28 were made using the model supplied by Atomics International and the data are tabulated in Table 1.

Results of the dynamic stability test are presented in Table 2. Four runs were made at each angle of attack in order to increase the confidence level of these data.

b. Reentry Dynamics Study (LMSC)

The analysis of data obtained from the aerostability study conducted by Lockheed Missiles and Space Company (LMSC) was completed. The objectives of this study were: (1) to analyze the

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FORCE BALANCE AND MODEL COMPONENTS

MODEL NO. 1 MOUNTED ON TUNNEL STING, $\alpha = 0^\circ$

MODEL NO. 2, $\alpha = 40^\circ$

MODEL NO. 3, $\alpha = 90^\circ$

MODEL NO. 4, $\alpha = 150^\circ$

MODEL NO. 5, $\alpha = 180^\circ$

7-21-65

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Figure 1. SNAP 10A-Agena Models Used in LTV Hypervelocity Wind Tunnel Force Tests

NAA-SR-11497

14

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DYNAMIC BALANCE AND MODEL

7-21-65

MODEL INSTALLED IN TUNNEL, $\alpha = 0^\circ$

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Figure 2. SNAP 10A-Agena Dynamic Stability Model Used in LTV Hypervelocity Wind Tunnel Tests

TABLE 2
HYPERVELOCITY WIND TUNNEL DYNAMIC STABILITY DATA

Run No.	Angle of Attack, α (degrees)	Mach No., M	Reynolds No., $R \times 10^{-6}/ft$	$(C_{m_q} + C_{m_{\dot{\alpha}}})^*$ (per rad)	$C_{m_{\alpha}}$ (per deg)	f_{RUN} (cps)	ω_R (rad)	Comments
3	-0.3	16.72	0.517	-28.74	0.04065	119.130	0.03113	
4	-0.3	16.71	0.507	-28.71	0.03786	119.180	0.03118	
5	-0.3	16.85	0.579	-30.80	0.04184	119.180	0.03312	
6	-0.3	16.81	0.555	-31.11	0.03304	119.050	0.03224	
7	10.3	16.77	0.534	-11.08	0.02843	119.270	0.03174	
8	10.3	16.71	0.512	-17.99	0.02712	119.270	0.03100	
9	10.3	16.72	0.515	-8.24	0.03283	119.270	0.03116	
10	10.3	16.71	0.501	-7.00	0.03283	119.270	0.03116	
11	19.8	16.75	0.516	-34.87	-0.00380	119.61	0.03187	
12	19.8	16.70	0.495	-38.89	0.00091	119.71	0.03107	
14	19.8	16.70	0.494	-39.95	0	119.640	0.03144	
16	19.8	16.74	0.510	-31.86	-0.00384	119.570	0.03184	
17	0	18.44	1.089	-	-	-	-	Fastax Schlieren only
22	0	18.40	1.048	-	-	-	-	Schlieren only

* $(C_{m_q} + C_{m_{\dot{\alpha}}})$ - Longitudinal dynamic stability derivative

$C_{m_{\alpha}}$ - Static stability derivative $(\partial C_m / \partial \alpha)$

f_{RUN} - Frequency of oscillation, run condition

ω_R - Reduced frequency parameter

NAA-SR-11497

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failure modes of the SNAP 10A-Agena systems to establish the non-nominal orbits and define the vehicle angular rates, (2) to define the range of initial conditions for the reentering SNAP 10A-Agena, and (3) to determine the attitude history and trajectory of the reentering SNAP 10A-Agena.

In performing the above analysis, the aerodynamic characteristics of the SNAP 10A-Agena were determined for the continuum hypersonic flow regime and the free-molecule regime. The continuum hypersonic coefficients were compared with the LTV wind tunnel test data and the sensitivity of the trajectory analysis to aerodynamic data was also investigated.

The following paragraphs present a summary of and conclusions from the LMSC study. Further details may be found in NAA-SR-11103.⁽¹⁾

Reentry trajectories and the body angular motions during reentry of the SNAP 10A-Agena were analytically determined. Probable failures during ascent were considered to determine initial orbits and body angular rates. It was determined that the wide range of possible eccentric orbits circularized to an eccentricity of 0.006 or less by the time the perigee decayed to 70 n.mi, which then resulted in a small band of velocities and flight path angles at 400,000 ft.

The effects of small velocity and flight path angle variations at 400,000 ft were studied along with the effect of drag coefficients and initial latitude. Initial entry latitudes caused a range increase of 100% for a 40-degree change in initial latitude; this results from the geometric effect of the earth's oblateness. This trajectory variation was much greater than that caused by small changes in velocity, flight path angle, and drag coefficient.

Further, large variations in the trajectory are caused by the lift force acting on the vehicle if the vehicle rates are small enough so that the lifting forces have time to act in a preferred

direction. For small initial rates, the motion will be a slow coning around the velocity vector. Planar motion is not possible, since the products of inertia and center-of-gravity offsets cause a coupling between the vehicle axes, and a rate about one axis will induce rates about the other two axes.

Failures during launch can cause probable body angular rates as large as 2 rad/sec about any axis. Much smaller pitch rates, in the order of 0.1 rad/sec, for example, will cause the vehicle to tumble to an altitude of 340,000 ft before aerodynamic moments stop the tumbling; and at 300,000 ft, the angle of attack will still be as large as 90 degrees. Larger initial pitch rates will cause tumbling to continue to proportionately lower altitudes. These motions will become a coning about the velocity vector when the tumbling stops, and will eventually converge to cone at the trim angle. Coning rates will be large enough to prevent significant trajectory changes above 160,000 ft.

Initial roll rates will also cause the vehicle to have a coning motion about the velocity vector. For initial roll rates on the order of 0.5 rad/sec, the coning rates will be high enough to distribute the lift forces and prevent them from significantly altering the trajectory.

Since only theoretical aerodynamic coefficients were known at the initiation of this study, an analysis was made to determine the effect of different aero coefficients. No effect in final eccentricity of the decaying orbit or the initial conditions at 400,000 ft can be attributed to reasonable uncertainties in aerodynamic coefficients. Only below about 340,000 ft can any change be noted, and this is due largely to drag differences. Angle-of-attack convergence was shown to be insensitive to changes in magnitude of the static stability margin. It was, therefore, concluded that changes in the aerodynamic coefficients will not change the initial conditions

NAA-SR-11497

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nor will they alter the basic motion of the body. For the aero data used in this study, static trim occurs at 22 degrees; however, from the recently available LTV wind tunnel data, it was found that static trim would occur at approximately 32 degrees.

The SNAP 10A-Agena malfunctions were investigated to determine the probability that relatively high angular rates would be placed on the vehicle as a result of the malfunction. The most probable causes of high rates at reentry are guidance and control system malfunctions and hydraulic engine gimbaling system failures that result in a tumbling vehicle at the time of the malfunction. The probability per launch of attaining a high angular rate range (0.1 to 2.0 rad/sec) is 2×10^{-3} . The probability per launch of attaining a nominal or non-nominal orbit with low reentry angular rates (0 to 0.1 rad/sec) is 0.825.

An estimate of the altitude at which the SNAP 10A-Agena will break up was obtained through an investigation of the approximate aerodynamic heating and structural loading of the intact vehicle. Three representative trajectories were analyzed. Initial breakup due to aerodynamic heating will occur in the altitude range of 275,000 to 290,000 ft, while structural forces do not become significant until the altitude range of 200,000 to 250,000 ft is attained.

2. Reactor Ablation-Disintegration Experiment (RADE)

Fabrication of the models to be used in the RADE tests was completed; the SNAP 8 models were assembled. Figure 3 shows components for the SNAP 10A model before assembly, and Figure 4 shows the model after assembly.

Fabrication of the calibration assembly components was completed except for the support arm assemblies. These will not be released for manufacture until a definite date has been set for the tests.

The shipping containers for the test models were being fabricated. The flexible thermocouple leads were being attached to the SNAP 8 models. The thermocouples for the SNAP 10A models were being furnace calibrated. Fabrication of the SNAP 10A calorimeter model was completed. A jig fixture was being made to facilitate furnace brazing of the calorimeter components.

3. Self-Welding Tests

An ultrahigh-vacuum, 5000-hr adhesion test of 316 stainless steel and beryllium was begun. These materials are under direct contact loads of about 25 lb per 0.25-in.² area and will be held at 1000°F for the test period. The pressure at this time was in the 10^{-8} -torr range. A second 5000-hr test with 316 stainless steel

6-28-65

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Figure 3. SNAP 10A RADE Model Components

NAA-SR-11497

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Figure 4. SNAP 10A RADE Model
(Assembled)

NAA-SR-11497

18

~~CONFIDENTIAL~~

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and beryllium coated with colloidal graphite to prevent or reduce adhesion will also be conducted. Four other tests of the same material combinations were in operation in the 10^{-9} -torr range at 1000°F. These tests are scheduled to operate for 10,000 hr. At the close of the reporting period, one of these tests had operated for 6000 hr.

B. ANALYSIS

1. Review of RFD-1 Flight-Test Data

The test data for the RFD-1 thermocouple tests were received from the Sandia Corporation. These data consist of the results of heatlamp tests on the prototype reactor model, with step-function heating inputs. An electrical analog simulating the vessel wall and the thermocouple inside was set up in such a way that the thermal conductance across the gap can be varied at will. It is assumed that the conductance varies with the mean temperature of the wall and the thermocouple according to a polynomial expression which has four coefficients to be determined.

Constants in the polynomial expressions for the transfer function between the RFD-1 vessel wall and thermocouples TC-19 and TC-20 were determined by means of an analog computer. The data from the step-function inputs run by Sandia were used in obtaining these constants. The analog was revised to include a feedback loop, which is expected to give true wall tem-

peratures corresponding to the measured thermocouple temperatures from the flight-test data.

2. Development of Basic Methods

Work was continued on the adaptation of the Thermal Analyzer Program (TAP) to the solution of trajectory integration problems. The integration subroutine used in RESTORE will be put into the TAP code without modifications. A subroutine will be written for calculating the aerodynamic forces and the resultant accelerations of the vehicle. The equations were taken from the NASA P-6200 trajectory code and modified to eliminate the terms for fin deflections, jet damping, radar ranges, and local winds. The reduced equations were ready for coding.

Work was initiated on the preparation of methods for calculating aerodynamic forces on reactors and vehicles by means of the Prandtl and Busemann theory of characteristics. These methods, which have been highly developed for nondissociated flow, will require modification for use with the dissociated flow that exists during reentry at hypersonic velocities.

Work on the RADE thermal model of the SNAP 8 reactor piping, head, fuel rods, reflectors, grid plates, and core vessel was in progress. Ablation and provision for variable local heat flux inputs will be incorporated into the model.

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III. FUEL REENTRY BURNUP PROJECTS

A. EXPERIMENTS

1. Fuel Arc-Jet Tests

Preparations for the fuel arc-jet test series to be conducted at NAA-LAD were continuing. The design of fuel specimen configurations was reviewed prior to final fabrication. The design of Configuration III-A-2 (solid fuel sample for cross-axial testing; 0.562-in.-diameter) was modified to increase the strength of the shank to be held in the chuck device of the rotator. The shank diameter was increased from 0.25 to 0.375 in. and a 0.125-in. hole will be drilled axially to within 1 in. of the main fuel sample for inserting a tungsten rod. All other configurations remain the same.

The fabrication of the fuel and materials was in process. The design of a particle catcher for the test series was considered. It was decided that a square, concave aluminum plate mounted to a copper block would be designed for eccentric mounting to the lower sting of the tunnel (the lower sting is normally used for the pitot probe). The catcher will be mounted in such a way as to be placed above or below and to the side of an ablating fuel specimen, during a predetermined time interval.

A visit was made to Hy-Cal Engineering Co. to discuss the application of the coatings to the calorimeters. Difficulties were encountered with several of the coatings during the calibration. These were to be reapplied and recalibrated. An additional calorimeter was planned which will provide a noncatalytic coating; several coatings were under consideration. The calorimeter holders were fabricated.

A laser system was specified and was ordered. The system was scheduled to be delivered early in the next quarter. A mating camera to be used in the shadowgraph system was selected

by NAA-LAD and the remainder of the shadowgraph system was available. The shadowgraph system was expected to be operable within one week after delivery of the laser.

2. Fuel Particle Experiments

A visit was made to Astropower to discuss their method of suspending particles by "levitation" (a technique which suspends particles by means of a magnetic field acting on eddy currents induced in the particle). This is one of the methods of particulate suspension that was being considered for the fuel particle experiments in FY 1966. A dynamic method of suspension (by the flowing gas) was also being considered. The possibility of using electrostatic forces to assist in particulate suspension was investigated. It was determined that the potential gradients necessary to overcome gravity for a 100-micron-radius particle are of a reasonable magnitude.

B. ANALYSIS

1. Fuel Element Parametric Study

Using the SNAP fuel element analytical model, the influence of various parameters upon the integrated heat necessary to initiate ablation, Q_{req} , of a SNAP fuel element was determined. Influence coefficients were developed for specific heat, hydrogen heat of dissociation, emissivity, initial temperature, initial hydrogen concentration, hydrogen diffusion coefficient, heat of oxidation, thermal conductivity, aerodynamic heating, outside fuel element radius, and air stream stagnation enthalpy. A topical report⁽²⁾ was being prepared.

The second phase of this study was to determine the influence of certain trajectory parameters on the integrated aerodynamic heat available, Q_{avail} , to a reentering SNAP 10A

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fuel element. Heating rates were integrated from the instant of release to the time when the net heating rate, corrected for wall temperature, is balanced by the radiation loss from the surface of the fuel element. The trajectory parameters found to have significant influence were the ballistic coefficient, velocity, and flight-path angle. Influence coefficients were calculated for these parameters for various fuel element release altitudes from 200,000 to 400,000 ft.

The ranges of error in Q_{avail} and Q_{req} were evaluated from estimated uncertainties in the corresponding individual parameters and the calculated influence coefficients of the parameters. A 2σ confidence limit, which was assumed to encompass 95.4% of all probable values, was chosen for each parameter involved in Q_{avail} and Q_{req} . The confidence limits on Q_{avail} will vary with release altitude, while those on Q_{req} are constant, since they are independent of heat input rate.

Aerodynamic heating rates from the trajectories investigated were substituted into the reference fuel element model to determine the value of Q_{req} for each release altitude. Values of $Q_{avail} \pm \sigma_{Q_{avail}}$ and $Q_{req} \pm \sigma_{Q_{req}}$ for each release altitude are given in Table 3. Curves A and B of Figure 5 show Q_{avail} and Q_{req} as a function of release altitude.

By extrapolating the Q_{req} curve from 255,000 ft to lower release altitudes, Q_{req} was found to equal Q_{avail} at approximately 253,000 ft. Given the values of $Q_{avail} \pm \sigma_{Q_{avail}}$ and $Q_{req} \pm \sigma_{Q_{req}}$, the probabilities of initial ablation for release altitudes from 253,000 to 400,000 ft were calculated.

At the altitude where $Q_{avail} = Q_{req}$, the probability of initial ablation is 0.50. The probability of initial ablation increases for higher release altitudes, reaching a maximum

7-21-65

7611-0970

Figure 5 Integrated Aerodynamic Heat Available and Heat Required to Initiate Fuel Ablation as a Function of Release Altitude

TABLE 3
VALUES OF INTEGRATED AERODYNAMIC HEAT AVAILABLE AND HEAT REQUIRED TO INITIATE FUEL ABLATION AT VARIOUS RELEASE ALTITUDES

Release Altitude (kiloft)	Q_{avail} (Btu/ft ²)	$2\sigma_{Q_{avail}}$ (Btu/ft ²)	Q_{req} (Btu/ft ²)	$2\sigma_{Q_{req}}$ (Btu/ft ²)
200	5,951	$\pm 1,263$	-	$\pm 2,510$
225	9,145	$\pm 1,412$	-	$\pm 2,510$
250	13,065	$\pm 2,127$	-	$\pm 2,510$
253	13,625	$\pm 2,150$	13,625	$\pm 2,510$
275	17,025	$\pm 2,174$	12,447	$\pm 2,510$
300	19,525	$\pm 2,138$	13,204	$\pm 2,510$
350	21,200	$\pm 2,172$	15,424	$\pm 2,510$
400	20,950	$\pm 2,127$	16,000	$\pm 2,510$

value of 0.99993 at 300,000 ft. Above approximately 300,000 ft, the probability decreases due to lower total available heat. For the highest release altitude considered (400,000 ft), the probability of initial ablation was found to be 0.985. Below 253,000 ft, it is impossible to calculate a probability of initial ablation, since the heat required to initiate ablation cannot be obtained. Probabilities of initial ablation were

NAA-SR-11497

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graphically approximated for release altitudes from 200,000 to 253,000 ft.

2. Supporting Analysis

A study on fuel element oxidation heating was completed. The role of the oxide film in controlling the oxidation appears to be overshadowed by other processes that occur. Calculations using recent rate constant data on unhydrided fuel show that oxygen diffusion into the metal lattice and the oxygen supply are the prime factors at temperatures where oxidation becomes significant. Consequently, oxidation calculations can become more involved unless recourse can be made to transient oxidation data for actual fuel samples. The amount of oxidation calculated for the recent, static-condition data obtained at AI is about 2-1/2 times greater than that used in the standard set of reentry burnup parameters.

Analytical calculations were being performed in support of the kinetics studies induction heating experiments. Material response calculations were performed for different constant heat inputs to the 5/8-in.-diameter, 5/8-in.-long fuel samples. A typical solution is shown in Figure 6 for a constant power input of 0.3 Btu/sec to the sample. The experimental heating conditions are to be guided in such a manner that similar temperature response patterns will be produced. In this manner, oxidation weight gains and hydrogen loss rates will be determined for representative reentry temperature histories.

7-21-65

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Figure 6. Material Response of Induction-Heated Fuel Sample (5/8 in. diameter by 5/8 in. long)

A brief study was made of possible causes of the change in the model floating potential noted by Plasmadyne during the ablation of SNAP fuel material. The model floating potential is the electrical potential difference between a model in a plasma jet and the ground. Two likely causes of the model floating potential are related to the oxidation of the fuel during ablation and to change in the thermionic emission at ablation.

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IV. FUEL PROPERTIES PROJECT

A. THERMOPHYSICAL PROPERTIES

Studies on the thermophysical properties of SNAP fuel were being conducted to provide information to develop an accurate model for ablation of the fuel during orbital reentry. The properties of particular interest are electrical resistivity, specific heat, thermal diffusivity, and thermal conductivity. The efforts in obtaining measurements of these properties are summarized in the following paragraphs.

1. Electrical Resistivity

To check the accuracy of the resistivity apparatus, measurements were taken on four samples of tantalum in the temperature range of 295 to 1310°K. Tantalum was chosen since reliable resistivity values are reported in the literature and measurements have previously been made on tantalum in this laboratory. The results tabulated in Table 4 and shown in Figure 7 are in excellent agreement with the data of Langmuir⁽³⁾ and Taylor.⁽⁴⁾

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Figure 7. Electrical Resistivity of Tantalum

TABLE 4
ELECTRICAL RESISTIVITY OF TANTALUM

Temperature (°K)	Resistivity, ρ (μ-ohm-cm)				
	Sample 1	Sample 2	Sample 3	Sample 4	Average
295	13.69	13.71	13.78	13.47	13.66
375	17.04	17.24	17.34	17.04	17.16
452	20.68	20.81	20.90	20.45	20.71
520	23.78	23.88	23.89	23.77	23.83
630	28.77	28.78	29.00	28.49	28.51
749	34.06	34.08	34.46	33.51	34.02
838	37.57	37.51	37.82	37.10	37.50
942	41.78	41.70	42.12	41.16	41.69
937	41.73	41.63	42.01	41.11	41.62
1021	44.97	44.92	45.33	44.35	44.89
1134	49.31	49.23	49.73	-	49.42
1225	52.79	52.69	53.14	51.99	52.65
1311	56.18	56.09	56.57	55.33	56.04
1051	46.32	46.19	46.65	45.47	46.16
803	36.51	36.34	36.62	35.88	36.34
799	36.34	36.14	36.68	35.75	36.23
502	23.49	23.51	23.77	23.63	23.60
298	14.18	14.13	14.29	13.98	14.14

Measurements were also made on four samples of H/Zr 0.5 fuel. The results are tabulated in Table 5 and are shown graphically in Figure 8. The data obtained during the heating cycle are plotted as a continuous curve up to about 500°C. At 550°C, the hydride goes through a eutectoid phase transformation which produces a discontinuity in the curve above this temperature.

2. Specific Heat

Specific heat measurements were made on three samples of H/Zr 1.59 fuel by means of the pulse-heating technique. The resulting specific heat data can be represented by the equation

$$C_p = 1.76 \times 10^{-4} T (^\circ\text{C}) + 0.054$$

in the temperature range of 100 to 1200°C. The scatter in data points is $\pm 6\%$ at 100°C and $\pm 9\%$ at 1200°C. In these pulse-heating runs, three

NAA-SR-11497

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TABLE 5
ELECTRICAL RESISTIVITY OF
H/Zr 0.5 FUEL SAMPLES

Sample	Temperature (°C)	Resistivity, ρ (μ -ohm-cm)
246-D3A	23.3	69.2
	102.8	82.7
	174.7	93.8
	241.0	105.2
	323.8	116.0
	396.0	125.0
	474.6	134.0
	545.6	127.7
247-D3A	23.3	67.8
	103.4	80.6
	174.6	92.2
	241.0	103.4
	323.9	114.8
	396.4	122.7
	475.2	132.1
	545.7	127.7
246-D8A	23.2	69.0
	103.7	82.0
	174.5	93.3
	241.14	104.7
	323.8	115.5
	396.6	124.4
	475.4	133.2
	545.0	128.0
246-D8B	23.2	69.1
	104.0	82.3
	174.4	93.3
	241.6	104.6
	323.6	115.0
	396.4	123.9

Figure 8 Electrical Resistivity of
H/Zr 0.5 Fuel Samples

inflections were observed in the oscilloscope traces at 465 ± 26 , 762 ± 33 , and 1149 ± 45 °C, indicating possible phase transitions. Analyses of these events with respect to the available Zr-H phase diagrams were being made.

The precision in results is not commensurate with that normally obtainable in the pulse-heating technique. In order to check the apparatus, therefore, tantalum samples were pulse heated and the resulting specific heat values were compared with literature values. The tantalum runs were made using both spring-loaded and spot-welded probes as well as several different heating rates. On the runs using spring-loaded probes, the specific heat values were within $\pm 6\%$ of the averaged literature values in the range of room temperature to 2000 °C. In the second series of runs using spot-welded probes, the measured values were within 6% of the literature values with a greater deviation (-15%) below about 200 °C. A critical analysis was being made of all phases of the experimental technique to improve the precision and accuracy of the measurements.

The manufacturer of the drop calorimeter to be used in obtaining specific heat measurements in the beta hydride phase region indicated that the drop calorimeter was 65% complete. There had been a delay in their fabrication schedule because of the delayed shipment of purchased

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pressure vessel parts. Delivery of the calorimeter was rescheduled for about the end of August 1965. Work was in progress on the preparation of AI laboratory facilities for accepting the calorimeter.

3. Thermal Diffusivity

The thermal diffusivity values on SNAP fuel are used to calculate thermal conductivity according to the equation

$$K = \alpha C_p d$$

where

K = thermal conductivity

α = thermal diffusivity

C_p = specific heat

d = density.

Thermal diffusivity measurements were made on one sample of H/Zr 1.90 fuel. The results are tabulated in Table 6 and are shown graphically in Figure 9. They agree very well with data previously obtained on other H/Zr 1.90 samples. Measurements were also made on a H/Zr 1.59 sample and the data are tabulated in Table 7 and plotted in Figure 6. The values near room temperature are about 15% greater than previous measurements on other H/Zr 1.59 samples, but agreement is good above 200°C. Small changes in thermocouple calibration due to the hydrogen atmosphere are believed to have caused this difference. Confirmatory measurements were planned whereby a hydrogen-stable thermocouple material (Geminiol) will be used.

4. Data Comparison Program

A subcontract was initiated with Battelle Memorial Institute to resolve significant differences which exist between the thermophysical

TABLE 6
THERMAL DIFFUSIVITY OF
H/Zr 1.90_{eff} FUEL
(Sample 247-D5)

Temperature (°C)	Thermal Diffusivity, α (cm ² /sec)	Temperature (°C)	Thermal Diffusivity, α (cm ² /sec)
611	0.070	238	0.097
611	0.069	353	0.084
612	0.070	355	0.082
611	0.070	355	0.083
611	0.068	355	0.083
611	0.069	355	0.085
14	0.147	356	0.085
16	0.147	427	0.080
16	0.146	415	0.080
114	0.116	414	0.078
116	0.118	415	0.080
118	0.120	417	0.078
244	0.096	553	0.071
239	0.097	557	0.072
238	0.096	558	0.072
238	0.097	560	0.070

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Figure 9 Thermal Diffusivity of H/Zr 1.90 and 1.59 Fuel Samples

NAA-SR-11497

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TABLE 7
THERMAL DIFFUSIVITY OF
H/Zr 1.59_{eff} FUEL
(Sample 247-D6)

Temper- ature (°C)	Thermal Diffusivity, α (cm ² /sec)
26	0.089
27	0.091
25	0.089
107	0.083
114	0.077
124	0.077
198	0.067
200	0.066
348	0.057
404	0.056
477	0.051
490	0.051
503	0.049
501	0.049
18	0.090
28	0.088
77	0.085

properties data on SNAP fuels generated by AI and BMI. Fuel for this program was fabricated and characterized. Four hydrided fuel rods (H/Zr 1.81) and seven rods of the nonhydrided alloy were delivered to BMI for these studies. Identical material will be tested at AI.

Thermal diffusivity values measured at both institutions by identical techniques will be compared. Specific heat values on the same materials will be compared between BMI's drop

calorimeter and the pulse-heating technique of AI. A steady-state thermal conductivity measurement on unhydrided fuel will be made by AI and compared with the calculated value generated with thermal diffusivity, specific heat, and density data of both BMI and AI.

B. KINETIC STUDIES

Twenty preliminary oxidation experiments on SNAP fuel alloy were performed to determine the optimum positions required for the two samples within the induction heating field. One sample is to be used for the oxidation kinetics evaluation, while the other, of identical composition and mass, contains a "black-body" hole for temperature measurements. Experiments were conducted in vacuums, in flowing helium ($\sim 71 \text{ ft}^3/\text{hr}$), and in flowing air at the same flow rate.

During the course of these experiments, involving the prescribed reentry thermal transients, it was observed that the fuel alloy melted at a temperature about 200°C lower than literature values. Investigation revealed that the barrel on the automatic recording pyrometer (Infrascope) had been oriented so that it could not be focused on the small black-body hole in the sample. This condition did not show up during the calibration of the instrument, since a large black-body source was used. The problem was corrected with the procurement of the correct barrel for the Infrascope. The instrument was recalibrated, and the accuracy of the sample temperature was about 15°C. The oxidation studies were then resumed.

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V. REACTOR TRANSIENT PROJECTS

A. SNAPTRAN-1, -2

1. Experiment

In the early part of the reporting period, eleven coupled drum impulse tests were performed with the SNAPTRAN-1 machine at a maximum reactivity input of \$3.45 for which the fuel temperature change was only 0.5°F. Later tests were performed with reactivity inputs up to \$4.20. This series was temporarily discontinued in order to proceed with the cascaded step transients.

All of the hardware for the SNAPTRAN-2 destructive test was shipped to the NRTS. A 1000-psi test of the destruct drums was performed. External pins on the drum cushions, used to indicate the position of the cushions,

were damaged during the test because of interference between the pins and the machine structure. Pins from a spare cushion assembly were used to replace the damaged pins. A minor modification to the structure provided the necessary clearance, therefore, no repetition of the damage will occur.

2. Analysis

a. Step Transients

A review of the SNAPTRAN-1 step transient results was conducted. A plot of compensated reactivity vs average core temperature change is shown in Figure 10. A prompt fuel temperature coefficient between -0.11 and -0.15¢/°F was determined for average core temperatures up to 500°F. These data were being correlated with those obtained from later transients.

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Figure 10. Compensated Reactivity vs Core-Averaged Temperature Rise (SNAPTRAN-1 Experiments)

NAA-SR-11497

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Additional temperature data were received for the thermal instrumentation in this step series. These data were obtained from the thermocouples and extend the temperature transients past peak temperature. The data used previously in determining power calibrations did not cover a broad enough time scale to show the maximum temperatures indicated by the thermocouples with the total energy determined from integrating the nuclear detector output.

Summary plots consisting of cross-plotting data at peak power were prepared in order to combine data from the various transients. A peak power analysis for the superprompt critical steps indicates $l/\beta = 8.25 \times 10^{-4}$ sec and a prompt temperature coefficient of $-0.14\text{¢}/^\circ\text{F}$. This temperature coefficient was obtained by least-squares-fitting the compensated reactivity and fuel temperature increase to the time of peak power.

A computer program was written which is designed to provide supplementary graphs and calculations from SNAPTRAN experimental data tapes which are not provided by the normal COMPOST analysis. The program features a means of plotting experimental temperature distribution, both axial and radial, at several times during the transient and a calculation of asymptotic period from each power channel, whether it was used in the power reconstruction or not.

New drum tables were prepared for use in the analysis of the SNAPTRAN-1 experiment data. The configurations presented in Table 8 were being used.

There were no experimental data for Case 4 in the range from 0 to 47.5 degrees. As a result, the differential and integral worth curves for Case 4 are approximate in this region. The Case 4 tables are used only during the scram of the step tests and will not have a large effect in determining temperature coefficients.

TABLE 8
PARAMETERS USED IN ANALYSIS OF
SNAPTRAN-1 EXPERIMENT DATA

Case	Calibration	Use in Experiments	Total Worth (\$)
1	A vs BCD	Single drum impulse tests	2 372
2	A & D vs B & C	Coupled impulse tests	4 26
3	B & C vs A & D	Step tests	4 273
4	A & D vs B & C	Scram drums for step tests	4 732

b. Single-Drum Impulse Transients

As discussed in the report for the previous period, considerable difficulty was experienced with the drum position data from this series of tests. It was found, however, that the time shifts originally observed in the drum data could be eliminated by changing the electronic noise filter being used with the analog-to-digital converter. Another problem was that the data contained a large amount of noise which seemed to be strongly biased in the zero direction.

The drum-inserted reactivity for transient No. 178 was analyzed in detail using X-Y plots of the analog drum pot voltage obtained from Phillips Petroleum Company (PPCo). This transient was chosen for detailed analysis because the power data were excellent over the major portion of the drum rotation and, therefore, provided an accurate and complete trace of calculated reactivity for comparison. The amplitude of the noise on the X-Y plots was on the order of 30 to 40 mv (19 to 25¢ reactivity) with some noise spikes as large as 60 mv. By visually drawing a line enclosing the uppermost extremes of the data, agreement between the calculated reactivity and the drum-inserted reactivity within about 5 to 10¢ was obtained.

New data tapes that were digitized after the corrections were made to the electronic noise filter were also analyzed. The drum-inserted reactivity from these data can be made to agree with the calculated reactivity within about 15¢ by again visually drawing a line enclosing the

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uppermost data points as with the analog data. The larger error associated with the digitized data is apparently due to the fact that these data are not identical to the analog data, but instead are related to the analog data by a proportional constant of 1.025.

The digitized data, when analyzed numerically, show errors as large as 20%. This is apparently due to the influence of the noise spikes which appear to be predominately in the downward direction. This results in a downward shift of the numerically smoothed data.

It should be noted that all of the foregoing analyses yield drum-inserted reactivities that agree within about 2% with the calculated reactivity at the full-in position of the drum. This verifies that the calibrated drum worth and the calculated reactivity are consistent. Since the lifetime used in the calculation was deduced from a separate set of experiments and has also been predicted by static $1/v$ poison calculations, this agreement indicates that both the drum calibration and the lifetime are accurate.

A technique was developed which allowed the above described hand interpretation of analog drum data plots to be used in conjunction with the regular computer analysis of the data. This technique was successfully employed on several transients in this series. Agreement between the reactivity calculated from the power and drum-inserted reactivity determined as described above was within 10% throughout the regions of no temperature feedback.

A temperature coefficient of $-0.13\%/^{\circ}\text{F}$ was determined from transients 177 and 178. These transients had average core temperature changes of about 300°F . The prompt neutron lifetimes determined from all the transients in this series fall between 6.5 and 6.7 μsec .

c. Double-Drum Impulse Transients

Due to delays in transmitting the digitized data from the double-drum impulse tests, the

data from test 198 were initially received from PPCo in oscillogram form and analyzed by hand. An apparent discrepancy between the timing blips from the zero reference time to peak power was different by 3 from one transport to the other. This amounts to a 0.3-msec discrepancy and could point to the source of some of the shifting difficulties which have been experienced with the data.

Drum D was found to lead Drum A by about 13 degrees near the full-in position. Peak reactivity was found to be \$4.13, as compared to the nominal \$4.20 based on synchronized drums. The "half-width" of the reactivity pulse above prompt critical was 6.8 msec. Similarly, the half-width above delayed critical was 8.0 msec. This is a somewhat faster pulse than was expected, based on earlier data. The shortest period reached was 0.260 msec and was essentially constant for 1.8 msec while the drums were passing through the full-in position. Comparing the period with peak reactivity results in a measured generation time of 6.5 μsec , which is in good agreement with the 23-group DTF-II calculation (6.47 μsec) and with the results of the step tests (6.6 μsec).

The time-dependent reactivity was also calculated from the power data and compared with the reactivity calculated from drum positions and calibration tables. The error in each reactivity curve was estimated at $\pm 10\%$. The two curves were in agreement everywhere by 10%, well within the estimated error bands. It was thus established that the present kinetics model and data are adequate to explain this transient within the limit of validity of the data.

A forward kinetics calculation was run using the reactivity pulse indicated by the oscillogram data. The results were in reasonable agreement with the experimental data, but a complete evaluation cannot be made until more calibration factors are received from PPCo.

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The digital data from this series of tests was subsequently received and a complete computer analysis of several selected transients were completed. A detailed review of the power data received from transients 196, 197, and 198 was made. Because of its significance with respect to the analysis of this series of tests, a brief summary of this review follows

(1) Transient 196

The only data channel to show peak power was 0606. This channel designation appeared on two similar, but not identical, data channels. These channels are from a gamma detector and are of extremely poor quality. Further, the reference time given them is about 30 msec in error. In summary, no suitable peak power was supplied for this transient.

The only clipped power channel which has good quality data to the end of the digitizing range is 0613. This channel is from a gamma detector and is therefore unsuitable.

Two channels from nuclear detectors (0511 and 0602) are marginal. Number 0511 looks suspicious just after it unclips, and 0602 has a downward spike about 5 msec before the end of data. Six power channels which were in the range to show back-side detail had to be rejected because they were negative after scaling

(2) Transient 198

The following clipped channels were in the range to show the back side of the power pulse

Transient No.	Channel No.	Comment on Quality of Data
5	12	
5	11	Did not unclip properly. Cannot be used.
6	13	Data OK, but from a gamma detector.
6	12	
6	11	Did not unclip properly. Cannot be used.
6	9	
6	4	
6	3	
6	2	

Channel cannot be used because a portion of the data was significantly negative.

Thus, the only data channel to show the back side of the pulse was from a gamma detector. Such a channel is of little value.

(3) Transient 197

The power data from this transient have the same difficulties as for transient 198. The back side of the power pulse can only be followed accurately for about 5 msec beyond peak power

The digital drum position voltage trace from transient 198 was compared with an X-Y plot of the analog drum position data. Good agreement was observed throughout the trace. This agreement would indicate that the problem associated with obtaining valid digital drum position data had been resolved. This was confirmed by the computer analysis of transients 198, 197, and 190. Good agreement was obtained for all three transients between power-calculated and drum-inserted reactivity throughout the ranges of usable power data

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The complete computer analysis of transients 190, 197, and 198 was completed. The prompt neutron lifetimes determined were all in the range from 6.5 to 6.8 μ sec.

A detailed analysis was completed on the double-drum impulse tests in an attempt to obtain some temperature coefficient information from them. Effort was concentrated on transient 198, since it had the largest temperature increase. Since, however, the average core temperature change was only about 110°F, the compensated reactivity was found to be about the same magnitude as the uncertainty in the reactivity calculations. Thus, it has been confirmed that temperature coefficient information cannot be obtained from this particular series of tests.

d. Second-Stage Step Tests

An analysis of transients 205 (\$1.55 step) and 206 (\$1.59 step) was completed. Both transients had average core temperature changes of about 600°F and yielded temperature coefficients of -0.17¢/°F. The prompt neutron lifetime determined from both transients was near 6.6 μ sec. The remainder of the transients in this series were being analyzed as usable digitized data were obtained.

Examination of scaled data plots from several transients indicates that there is considerable oscillation in the stepper drum position after stepping to the "full-in" position. This appears not to have too large an effect in the analysis, however, since the differential drum worth is quite small near the full-in position.

e. Cascaded Step Tests

The only step tests initiated at elevated temperatures which experienced significant temperature rise were transients 212 and 213. These were \$1.10 and \$1.37 step insertions, respectively. Transient 212 was initiated at a temperature of 800°F, while transient 213 was initiated

at about 680°F. Oscillations in the drum position after the step insertion were observed in both transients. The oscillations appear to be only a few cents in magnitude, but persist nearly to the time of scram.

Transient 212 was observed to have a temperature rise of only about 35°F. This was too small to produce significantly useful temperature feedback information. Transient 213, on the other hand, had a temperature rise of about 350°F. The temperature coefficient determined from this transient was -0.17¢/°F.

f. Summary

A plot of superprompt critical reactivity vs reciprocal period is shown in Figure 11. This plot uses data from both step and impulse transients. It was anticipated earlier in the planning of the experiments that a reflector thermalization delay might be seen as a curvature in this plot and might be evaluated. Later calculations, based on the final reactor configuration, showed that this effect would be quite small, less than \$0.15.

The data shown in Figure 11 have a slight curvature at very short periods, but the magnitude is within the range of uncertainty in the drum calibration. Thus, the presence of a reflector thermalization effect is suggested by the data but it appears to be too small to be conclusively proven or quantitatively evaluated. This being the case, ordinary bare-reactor kinetics can be used in analysis of this reactor configuration without adding significantly to the uncertainty of such analysis.

Further correlation of the temperature coefficient data from both transients already run and higher temperature transients planned for the future will be accomplished.

Oscillogram data on the tests of the destruct drum mechanism for SNAPTRAN-2 were received. These data were being analyzed to determine the corresponding reactivity insertion.

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Figure 11. Super-Prompt Critical Reactivity vs Reciprocal Period (SNAPTRAN-1 Experiments)

Computations of hydrogen loss from a fuel rod at high constant temperatures were made to set safe temperature limits for the remainder of the SNAPTRAN-1 test series. The pressure in the hydrogen gap was found to approach the equilibrium dissociation pressure in very short time. These calculations were made in order to determine the effect of allowing short time (~ 10 sec) fuel temperature overshoot above the clad burst pressure equilibrium temperature. For burst pressure equilibrium temperatures of 1400 to 1500°F, the effect of an additional 50°F fuel temperature would be to cause clad rupture within 0.5 sec. In the SNAPTRAN experiments, it is expected that axial fuel temperature would become flat about 10 sec after the energy deposition from the power burst. It was postulated that perhaps this temperature flattening would occur fast enough to allow the hot-spot temperatures in the fuel to exceed the clad-burst equilibrium temperature without damaging the clad. If this were true, the range of non-damaging reactivity insertions and mean core temperatures could be extended somewhat. Since the hydrogen pressure in the gas gap is coupled most strongly to the hottest temperature in the

fuel rod, and an excess temperature of 50°F can be allowed for only 0.5 sec (at most), it does not appear that the hot-spot temperatures should be allowed to overshoot the burst temperature by even a few °F.

An evaluation of the current status of the SNAPTRAN-1 data analysis was completed. Each class of transients (i.e., ambient temperature steps, elevated temperature steps, single- and double-drum impulses) was examined with respect to the information that further analysis could produce. This was correlated with the project objectives to produce a plan for the completion of the data analysis.

B. SUPPORTING METHODS DEVELOPMENT

Two methods that were under consideration for treating space-dependent kinetics for super-prompt transients are: (1) the multinode kinetics model and (2) space-time synthesis. Both methods would use DTF-II calculations at several steps during the transient to obtain detailed flux shapes.

The multinode kinetics model uses these shapes to obtain a set of transfer lifetimes between adjacent nodes. With the two-node model,

NAA-SR-11497

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it was observed that the lifetimes were fairly dependent on the flux shapes within the node. Since small errors in the lifetimes (about 5%) lead to large errors (20%) in the predicted dynamic period, an external estimate of the dynamic period is required. Although DTF gives good values of the flux shapes, it does not do so well in estimating the period under the conditions of interest, so that the two-node model cannot be used to predict a transient unless empirical values arise with a few-node model. To make the nodal model insensitive to the flux shapes within the node, so many nodes would be needed that one might as well integrate the time-dependent transport equations directly. Such an effort does not seem feasible at this time.

Handling temperature-dependence in the multinode formulation could require a full space-dependent calculation after every several time steps. This was proposed by Cohen in an earlier multinode method that used diffusion theory.

The synthesis method requires only the flux shapes $R_1(r)$ and adjoint flux shapes $R_1^+(r)$ at several times during the transient, and approximates the space- and time-dependent flux $N(r,t)$ and adjoint $N^+(r,t)$ as follows

$$N(r,t) = \sum_1 R_1(r) T_1(t) \quad ,$$

$$N^+(r,t) = \sum_1 R_1^+(r) T_1^+(t) T_1^+(t) \quad .$$

$R_1(r)$ and $R_1^+(r)$ can be calculated using the one-dimensional transport code DTF-II. The time-dependent functions, T_1 , needed to calculate the flux, $N(r,t)$, are determined from the matrix equation,

$$\frac{dT_1}{dt} = \sum_1 A_{1j} T_j \quad . \quad \dots (1)$$

The matrix coefficients, A_{1j} , sums are appropriate integrals over space, angle, and energy of products of the form

$$R_1^+ \Sigma R_j \text{ and } R_1^+ \vec{\Omega} \cdot \vec{\nabla} R_j \quad , \quad \dots (2)$$

where Σ is a (temperature-dependent) macroscopic cross section. Equation 1 has the same form as the multinode kinetics equations. In the multinode equations, the T_1 represent neutron densities averaged over the i -th zone of the reactor. In the synthesis method, the T_1 are weighting functions of the various flux shapes, R_1 , that apply over the whole reactor, so that Equation 1 may be described as a "modal" equation.

Experience with previous applications of synthesis techniques to burnout problems indicates that good results will be obtained if (1) the R_1 and R_1^+ are reasonable, and (2) an appropriate scheme is used to integrate Equation 1. Temperature dependence in Σ leads to a temperature dependence in the A_{1j} , which needs to be evaluated only once. The temperature distributions assumed in the calculation of the R_1 , R_j^+ are only important to the extent that the flux shapes obtained envelope the actual flux shape at all times during the usual manner by assuming that the temperature dependence of the A_{1j} may be neglected while integrating Equation 1 over small time steps.

The second component of the A_{1j} matrix shown in Equation 2 involves integrals of the form

$$\int dx R_1^+(x) \frac{d}{dx} R_j(x) f(x) \quad , \quad \dots (3)$$

where $f(x)$ is an analytic function of the direction cosine, x . Numerical quadratures have been found for Equation 3 which reflect the conservation forms of the flux and adjoint given by Lee.⁽⁵⁾

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The dynamic periods are obtained directly from Equation 1. If, as anticipated, A_{ij} is no more than a 5×5 matrix, it may be worthwhile to find its eigenvalues and eigenvectors and solve Equation 1 exactly.

Work was begun in modifying the DTF program to have angular fluxes (or adjoints), cross sections, and geometry parameters written on a binary tape in a format suitable for use with synthesis or multinode transient codes.

FORTTRAN-IV subroutines were written to calculate the Model A matrix from the asymptotic fluxes, N_1 , and adjoints, N_1^+ . The differential equation,

$$d\mathbf{T}/dt = \mathbf{AT} \quad ,$$

has a formal solution,

$$\mathbf{T}(t) = e^{-\mathbf{At}} \mathbf{T}(0) \quad ,$$

where $\mathbf{T}$ is a vector containing the time-dependent parts of the expansion of the time-dependent flux,

$$\Phi(r, E, \Omega, t) = \mathbf{N}(r, E, \Omega) \cdot \mathbf{T}(t) \quad ,$$

and $\mathbf{N}$ is a vector containing the N_1 . Since only a few (about 5) trial functions N_1 and N_1^+ will be used, it was planned to expand $e^{\mathbf{At}}$ in terms of $e^{K_1 t}$ with Sylvester's formula. The eigenvalues, K_1 , of the nonsymmetric A matrix may be found with a SHARE routine EIG₃, which only requires conversion from FORTRAN-II to FORTRAN-IV. Direct expansion, using the eigenvectors of A, may be more satisfactory, and SHARE routines for calculating eigenvectors of nonsymmetric matrixes were being investigated. If energy dependence is retained in the $\mathbf{T}$ vector,

$$\Phi(r, E, \Omega, t) = \mathbf{N}(r, E, \Omega) \cdot \mathbf{T}(E, t) \quad ,$$

there is no integration over energy in calculating A. The length of $\mathbf{T}$ is multiplied by the number of energy groups, G, and the size of A is increased by a factor of G^2 . In this case, expansions using all eigenvalues will not be practical, and only the first few dominant eigenvalues may be found.

C. SUPPORTING EXPERIMENTS

The experiments for the coolant channel mockup water boiling study were begun during the previous reporting period. The results of these experiments are described in the progress report for that period (NAA-SR-11097). During the course of one of these experiments, one of the three heating elements failed. The heating elements were removed and replaced and the balance of the experiments were then completed during this reporting period.

The power for incipient boiling was measured as a function of channel inclination and water depth. The angles of the channel were 0, 30, 60, and 90 degrees from the vertical. The water depths were 6, 12, 24, and 36 in. above the top outlet of the channel. It appears that the power for incipient boiling in the channel is independent of the depth of the water. The angle dependency is as follows:

Angle From the Vertical (degrees)	Power at Incipient Boiling (kw)
0	1.36 - 1.66
30	1.36 - 1.66
60	1.31 - 1.572
90	0.576

The maximum operating power of the coolant channel mockup assembly was limited by a temperature of 500°F within the heaters. Higher temperatures would have led to heater failure. The power required to produce a heater

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temperature of 500°F was measured. This power was measured at the time when the power for incipient boiling was being measured. These maximum powers were obtained at angles of 0, 30, and 60 degrees from the vertical and were as follows:

Angle From the Vertical (degrees)	Maximum Power (kw)
0	5.5
30	5.5
60	5.2

It should be remembered that these limits apply to the heaters in the coolant channel mockup, not to limits on the power of an actual SNAP channel.

In this study, it was shown that steady boiling can be achieved in a mockup of a single channel of the SNAP 10A reactor for powers per channel up to 6 kw. This corresponds to a total SNAP reactor power of 440 kw. The boiling in a completely submerged channel and a partially filled channel were qualitatively described, and progress was made toward a quantitative description.

VI. CRITICAL CONFIGURATION PROJECTS

A. EXPERIMENTS

Measurements of the reactivity effect of gadolinium poison in MYLAR tape on the SCA-4A core vessel were completed. The SCA-4A configuration was a simulation of the SNAP 10A design. The poison optical thickness was varied from 0.1 to 10 in $\Sigma_a t$. Measurements were made to determine the reactivity effect of the MYLAR tape. The data show the expected reduced incremental reactivity effect as the gadolinium poison becomes "black."

Reduction of data from the foregoing experiments was completed. The minimum measured poison worths were approximately 15% lower than those calculated using the DTF-II code for optical thicknesses above 2.0 in $\Sigma_a t$.

The worth of gadolinium on the fuel cladding was measured using SCA-4 fuel in the SCA-4A beryllium-reflected assembly. Poison concentrations of optical thicknesses of 0.1, 0.2, and 0.3 were studied. These measurements show that calculated predictions of the gadolinium poison worth on the fuel cladding are ~30%

lower than that measured. This contrasts with the results of calculations of gadolinium poison on the core vessel.

Checkout of the SCA-4B water-reflected assembly was completed in preparation for measuring the reactivity effects of gadolinium poison on the fuel elements and core vessel. Fuel loading was completed and the experiments were started. The first experiment will determine the base reactivity of the unpoisoned, water-flooded-and-reflected SCA-4 core.

The preliminary design of the hardware for studying the effect of geometry changes on water environment reactivities was completed. The SCA-5 fuel pellets will be used with the SCA-4B machine. The major hardware to be obtained are fuel tubes, grid plates, beryllium axial reflector, and a reflecting tank.

B. ANALYSIS

The nuclear hazards of placing gadolinium-poisoned MYLAR tape on the SCA-4B fuel elements were evaluated. Calculations for studying

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the power transient energy release caused by the step flaking of poisoned MYLAR tape from cladding were completed. The worth of the poison was taken to be \$4.35. The flaking could occur in the event the cladding is heated to a temperature of 800°F. It was postulated that the heat could be supplied by reactor power responding to an 8¢/sec reactivity insertion.

Calculations using the SNAPKIN code hydrogen loss feedback model indicate that the energy release is about 80 Mw-sec and is relatively independent of the time during the ramp transient at which the \$4.35 step occurs. The energy release was essentially equivalent to that from a \$4.35 step insertion. The energy generation from a \$4.35 step using the BOOMER hydrogen loss feedback model also indicated 80 Mw-sec. The energy release was reduced to 57 Mw-sec when the power transient was modified with a shock velocity disassembly calculation. An energy release of 51 Mw-sec was obtained with the reactor expansion disassembly model that was used for predicting the SNAPTRAN-3 destruct transient. The 51 Mw-sec number is felt to be most realistic, as it does not appear that the H₂ gas can leave the core fast enough to prevent the buildup of large pressures which will disassemble the core.

The calculation of reactor and shadow-shield weight for all of the SNAP 10A configurations previously studied was completed. An intrinsically subcritical configuration was obtained with no increase in system weight. This configuration would change the present design as follows:

- 1) 1.08-in. decrease in core diameter
- 2) 1.25-in. decrease in active core length
- 3) 1.5 in. of BeO placed on each end of the fuel
- 4) 1.75-in. increase in total core length
- 5) 1.125 in. of Be added to the radial reflector.

This design, without the radial reflector, is about \$9 less reactive when immersed in water than the present core in a water environment. Because end reflectors are included, the reactor weight decreases. At the same time, the additional radial reflector increases the envelope diameter, causing the increase in shadow-shield weight. For the foregoing configuration, these weights are equal in magnitude. The results up to this point showed that the minimum weight design for a \$5 to \$10 reactivity decrease (from the present design in water) had a total core length (fuel + BeO) of about 14 in.

Calculations of reactor and shadow-shield weight for SNAP 10A configurations which result in subcriticality in water environments were completed for BeO end-reflector thicknesses up to 4 in. On the basis of these and previous calculations, estimates of system weight were also made for 5-in. end reflectors. The results show that, for a \$9 reactivity decrease from the reference core in water, there are several designs which give weight savings of up to 8 to 10 lb from the reference design. These are characterized by approximately the same active fuel length and diameter, but differ in the thickness of the BeO end reflector. For a fuel length of about 10 in. and homogenized fuel diameter of about 7.05 in. (reference core = 12.25 by 8.05 in.), end reflectors from 2.5 to 3.5 in. thick result in about the same system weight.

The weight study of SNAP 10A core designs which provide decreased reactivity in a water environment was completed. The summary plot of the weight penalty as a function of reactivity change in a water environment is shown in Figure 12.

The calculation of reactor and shield weight penalties resulting from addition of gadolinium in the fuel cladding or core vessel wall of the reference (SNAP 10A) design were completed. For a \$9 reduction of reactivity in a water environment, the weight penalties were: (1) 33 lb

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for core vessel poisoning and (2) 80 lb for fuel cladding poisoning. These weight penalties are greater than those resulting from changes in core geometry.

The effect of hydrogen depletion from the central element of the SNAPTRAN-1 core on

core reactivity and power distribution was studied. Hydrogen concentrations (N_H) ranging from 6.5 to 5.0×10^{22} atoms/cc were studied. A maximum reactivity effect of $\$0.65$ was calculated ($\$0.043$ per 0.1 change in N_H). Previous measurements of minor perturbations in the SCA-4A assembly showed a greater worth ($\sim \$0.14$ per 0.1 change in N_H).

Figure 12 Minimum Weight Penalty as a Function of Reduced Reactivity in a Water Environment

NAA-SR-11497

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Figure 13. Dissociation Pressure vs Temperature (Run No. 1)

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Figure 14. Dissociation Pressure vs Temperature (Run No. 2)

TABLE 9

DISSOCIATION PRESSURE VS TEMPERATURE
(Run No. 1: ZrH_x and ZrH_x with 0.017 T/Zr)

Temperature (°C)	Pressure (mm Hg)	Hydrogen	(Hydrogen + Tritium)
		Zirconium (ratio)	Zirconium (ratio)
543	26.1	1.837	-
559	37.1	-	1.836
596	83.3	-	1.821
598	92.2	1.819	-
641	193.1	1.787	-
649	208.8	-	1.781
687	304.8	1.751	-
706	348.0	-	1.736
727	427.0	1.711	-
744	454.8	-	1.701
792	620.4	1.647	-
796	618.4	-	1.647

TABLE 10

DISSOCIATION PRESSURE VS TEMPERATURE
(Run No. 2: ZrH_x and ZrH_x with 0.023 T/Zr)

Temperature (°C)	Pressure (mm Hg)	Hydrogen	(Hydrogen + Tritium)
		Zirconium (ratio)	Zirconium (ratio)
557	38.8	-	1.846
567	49.1	1.842	-
585	91.5	-	1.829
608	114.1	1.822	-
628	160.5	-	1.808
670	278.7	1.771	-
680	302.7	-	1.764
711	389.2	1.736	-
720	414.2	-	1.728
751	508.8	1.697	-
755	522.3	-	1.693
785	613.9	-	1.661
791	638.2	1.654	-

NAA-SR-11497

38

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VII. END-OF-LIFE SHUTDOWN PROJECT

As the first step in the program outlined in the SNAP Aerospace Safety Program Quarterly Progress Report for the period of October through December 1964 (NAA-SR-10797), comparative dissociation pressure measurements were determined for zirconium hydride and zirconium hydride tritide. This was accomplished by determining the dissociation pressures for a zirconium-hydride standard sample over the temperature range of 550 to 800°C, then extracting from this sample a known quantity of hydrogen and replacing it with an equal quantity of tritium. The dissociation pressure of this sample, now containing a mixture of hydrogen and tritium, was again determined over the same temperature range. Since a fixed quantity of hydrogen or hydrogen and tritium is contained within the sample and the system manifold (~8 gm), the composition of the specimen will vary with temperature, and the total hydrogen-plus-tritium content can be determined from the known volume of the system manifold at any temperature and the measured equilibrium pressure. The equilibrium pressures obtained in this manner are used to plot curves of pressure (mm Hg) vs temperature ($10^4/T$ °K) for each run. The smooth curves obtained are characteristic of the particular sample (quantities of zirconium, hydrogen, and tritium), the void volume of the apparatus, and the equilibrium decomposition pressure rela-

tionships. Prior to an experimental run, the isotopic purity of the tritium gas, contained on a uranium bed, was checked with a mass spectrometer.

The results obtained to date are summarized in Tables 9 and 10 and Figures 13 and 14.

Runs 1 and 2 contained considerably more tritium (T/Zr ratios equal to 0.017 and 0.023, respectively, at ambient temperature) than was being considered for SNAP reactors at this time. Since the zirconium-hydride-tritide points fall on the same curves at the zirconium-hydride points, the graphs in Figures 13 and 14 indicate that there is no measurable difference in dissociation pressure between zirconium hydride and zirconium hydride tritide.

This result was not wholly unexpected, since the isotherm data reported in the previous reporting period showed zirconium deuteride to have a dissociation pressure approximately 10% greater than zirconium hydride over a range of hydrogen (or deuterium)-to-zirconium ratios varying from 1.647 to 1.782. Assuming that zirconium tritide has a dissociation pressure greater than zirconium deuteride by about the same order, the difference in dissociation pressure between $ZrH_{1.60-1.85}$ and $ZrH_{1.58-1.83}^{T_{0.02}}$ sought in this investigation would only be about 0.2%, which is not within the limits of the measurements.

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