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IRRADIATION PERFORMANCE OF
A FULL-SCALE PROTOTYPE
SNAP 2 REACTOR FUEL ELEMENT
(NAA 82-1 Irradiation Experiment)
(Title Unclassified)

AEC Research and Development Report

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By
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ABSTRACT

The NAA 82-1 irradiation experiment was designed to demonstrate the performance of a full-scale reference SNAP 2* fuel element when subjected to an environment which simulated the most severe conditions (i. e. , peak temperature, burnup, and burnup rate) encountered in a SNAP 2 reactor core upgraded to produce 125 kwt. This experiment was irradiated for twenty-three reactor cycles in the Materials Testing Reactor and was discharged on August 16, 1965.

The fuel element achieved its design peak burnup of 0.08 metal at. % in an equivalent of one year at full power. The design peak cladding temperature of 1300°F (which is nominally equivalent to a peak fuel temperature of 1507°F) was maintained within $\pm 50^\circ\text{F}$ during 93.4% of the time that the reactor was at full power.

The post-irradiation condition of the fuel element and the irradiation assembly was excellent. Both the uranium-zirconium hydride fuel rod and the Hastelloy-N cladding were intact and in excellent condition. The performance of the fuel element, in terms of suitability for use under 125 kwt SNAP 2 reactor conditions, was completely satisfactory.

This report describes the design, analysis, fabrication, in-pile operation, and post-irradiation examination and evaluation of the NAA 82-1 experiment.

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I. INTRODUCTION

The NAA 82-1 irradiation experiment was designed to evaluate the performance of a reference SNAP 2 fuel element under conditions encountered during 125 kwt operation of a SNAP 2 reactor. This design objective imposed the criteria listed below on the operation of the experiment.

- 1) Fuel centerline temperature of 1500 to 1525°F
- 2) Peak burnup of 0.08 metal at. %* to be attained in one year
- 3) Constant fission density around the surface of the fuel for a given axial location

The fuel rod linear power rating was such that a binary gas temperature control technique could be employed; the requirement for close control of temperatures made this technique desirable. The requirements for a large-diameter capsule and low fission rate were prime considerations in an extensive analysis of possible irradiation sites, culminating with selection of the Material Testing Reactor (MTR), NRTS, Idaho. Several reasons led to this choice; the MTR axial flux profile shifted very little (~4 in.) during a reactor cycle, so that hydrogen redistribution effects in the fuel were minimized; the test positions in MTR were such that the large-diameter capsule and flux depressor could be accommodated; the MTR plant factor was high (~0.75); test positions that provided the low fission rates required were available; and an AI site representative was available to assist in maintenance, control and operation of the experiment. (However, the neutron flux gradient existing in the suitable test facilities—MTR reflector test positions—was such that a radially eccentric flux depressor would have to be employed in order to achieve a circumferentially constant fission density around the fuel rod as required above.)

*Metal atom percent burnup is computed by the formula:

$$\text{Metal at. \%} = \frac{\text{U}^{235} \text{ atoms fissioned}}{\text{total metal atoms present}} \times 100.$$

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Figure 1. SNAP 2/10A Fuel Rod and Fuel Element Assembly

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II. FUEL ELEMENT DESCRIPTION

The fuel element used in the NAA 82-1 irradiation experiment was a reference, SNAP 2/10A fuel element which consisted of an integral fuel-moderator rod* of zirconium-uranium alloy hydride enclosed in a Hastelloy-N cladding tube. Figure 1 shows the dimensions of the prototype fuel element, while Table 1 presents selected dimensional, compositional and performance data concerning the fuel element employed in the experiment. A total of three fuel elements were fabricated according to production standards. One element was irradiated in this experiment, one retained as an archive, and one retained for use in a possible future irradiation experiment.

The cladding tubes were prepared from vacuum-melted Hastelloy-N, Heat No. 281-128, by rolling 0.015-in. sheet to a 1.25-in. outside diameter tube, seam-welding, and drawing. End caps, cups and end plugs were machined from Hastelloy-N round bar stock, Heat No. D-1-0408. Chemical analyses of these materials are listed in Table 2.

An end cap was welded to the cladding tube by the tungsten inert gas process (TIG). This assembly, the other end cap, and the cup were chromized prior to application of the ceramic coating. The ceramic coating improves hydrogen retention by acting as a barrier to hydrogen permeation through the Hastelloy-N. The composition of the ceramic used, a boron-free barium silicate glass designated AI8763D, is given in Table 3. Each coat is applied as an aqueous slip, allowed to air-dry, and fired at 1900°F for 15 minutes. Application of three coats resulted in a ceramic layer 0.0020 to 0.003-in. thick.

The fuel rod was a right circular cylinder, 12.250-in. long by 1.210-in. diameter. The lower 0.78 in. of the fuel rod had a reduced diameter of 1.152 in., to accommodate the cup (see Figure 1). The fuel rod was fabricated by consumable-arc melting to form a zirconium - 10 wt % uranium alloy, machining the ingot to billet form, extruding the billet to rod, remelting and reextruding, hydriding by introducing a known volume of hydrogen into a 1650°F furnace, and machining to final dimensions.⁽¹⁾ Fuel material, before irradiation, consisted of a fine dispersion of alpha uranium particles precipitated in a matrix of epsilon

*Fuel-moderator rods are referred to as fuel rods in this report.

TABLE 1
FUEL ELEMENT DATA (NAA 82-1)

<u>Fuel Element E2004</u>	
Length (in.)	12.449
Average OD (in.)	1.253
Weight (gm)	1542.66
Cladding Material	Hastelloy N (Heat 281-128)
Coating Material	AI 8763D
Hydrogen Permeation Rate [cc/hr (STP) at 1200°F]	0.33
Diametral Gap (in.)	0.004
Axial Gap (fuel to ceramic)* (in.)	0.000
<u>Fuel Rod 674-6</u>	
Length (in.)	12.250
OD of Major Section (in.)	1.210
OD of Reduced Section (in.)	1.152
Uranium Content (wt %)	10.29
U ²³⁵ Content (wt %)	9.58
Hydrogen Content (wt %)	1.77
Calculated H/Zr (effective)	1.84
N _H (hydrogen atom density x 10 ⁻²²)	6.40
Zirconium Content (wt %)	87.54
Carbon Content (wt %)	0.19
Total Impurities (wt %)	0.21
Density (gm/cc)	6.065
Weight (gm)	1381.62

*Fabrication tolerances were such that an axial gap of 0.004 in. could occur between the cup and the end cap. However, since the cup is force-fitted against the fuel rod during assembly, the axial gap seen by the fuel rod is zero (see Figure 1.)

TABLE 2
CHEMICAL COMPOSITION OF HASTELLOY-N

Element	Weight Percent in Heat	
	Heat No. 281-128 (Cladding Tube)	Heat No. D1-0408 (End Caps, Cup, End Plugs)
Aluminum	0.20	0.07
Boron	0.005	0.005
Carbon	0.08	0.04
Chromium	7.00	7.16
Cobalt	0.09	-
Copper	0.01	0.01
Hydrogen	0.0017	-
Iron	3.82	4.24
Manganese	0.52	0.50
Molybdenum	16.37	16.78
Nickel	Balance	Balance
Nitrogen	0.002	-
Oxygen	0.007	-
Phosphorus	0.001	0.001
Silicon	0.35	0.28
Sulfur	0.008	0.008
Titanium	0.08	0.01
Tungsten	0.04	0.03

phase zirconium hydride (see Section V, Figure 29.) The light particles are those of zirconium carbide, which acted as grain refiners and served to prevent cracking during hydriding.⁽²⁾ A more complete description of the SNAP fuel system can be found elsewhere.^(3, 4)

The fuel rod was inserted into the assembled tube, the coated end cup fused to the coating of the cladding tube, and the other Hastelloy-N end plug TIG welded to the cladding. The finished length of the fuel element was 12.450 in. exclusive of the end pins, which were intended to locate the element in the SNAP 2 reactor grid plates. In order to facilitate assembly of the irradiation capsule, special locating sleeves were electron beam welded to these end pins.⁽¹⁾

TABLE 3
COMPOSITION OF CERAMIC
COATING

AI8763D Frit	
Component	Weight Percent
SiO ₂	47.2
BaO	31.6
TiO ₂	9.88
ZrO ₂	5.01
Li ₂ O	2.26
CaO	1.2
SiO	1.05
MnO	1.0
MgO	0.66

Slip*

Component	Amount
AI8763D Frit (gm)	1000
Ferro Green Label Clay (gm)	60
Chrome Oxide (gm)	20
Potassium Nitrate (gm)	1.25
Bentonite (gm)	2.50
Distilled Water (ml)	580
Sm ₂ O ₃ (added to 2nd and 3rd coat only) (gm)	40

*"Slip" is a suspension of the frit and other constituents of the ceramic in water. The amounts given are for batch lots and do not reflect the total quantities present in the coating of a single tube.

III. EXPERIMENT DESIGN

A. MECHANICAL DESIGN

1. Irradiation Capsule

The prime considerations for the irradiation capsule design were containment integrity, differential thermal expansion, temperature control and neutron flux shaping.⁽⁵⁾ Containment integrity was critical both in terms of fission products and the NaK surrounding the fuel element, since the MTR is a water-cooled reactor. NaK was used to thermally bond the fuel element to the inner capsule tube and to simulate the SNAP reactor environment. A schematic drawing of the experiment is shown in Figure 2. The NaK was doubly encapsulated in two concentric Type 304 stainless steel tubes. These two tubes were separated by mechanical spacers located beyond each end of the fuel element and outside the region of high temperature to minimize possible thermal expansion interferences. The annular gap between the tubes formed a thermal barrier to heat flowing from the fuel element to the reactor water, which cooled the outer surface of the capsule. A binary gas mixture of helium and nitrogen passed through the thermal barrier annulus. The thermal resistance of the gas annulus would vary with the mixture ratio of the two gases, providing an effective means

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Figure 2. NAA 82-1 Capsule Schematic

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of controlling experiment temperatures (see Section III-F). The fuel element was held at each end by sleeve-centering fixtures which allowed unrestricted thermal expansion or irradiation swelling of the element. Sixteen thermocouples were banded to the fuel element cladding to monitor experiment temperatures. (See Section III-F and Figure 20.) The thermocouples were routed along and around the fuel element so that they could easily flex to accommodate differential thermal expansion during temperature transients. The thermocouples penetrated the capsule through two brazed bulkheads and were conducted to the reactor head in a 0.875-in. diameter x 0.065-in. wall lead tube. The lead tube also carried the gas lines. The capsule was of all-welded construction except for the bulkheads, which were brazed with Nicrobraz 30. The welds and brazes were inspected using x-ray, Zyglo, and helium leak tests to insure integrity. Except for the fuel element and the thermocouples, all other capsule parts were made of Type 304 stainless steel. Figure 3 shows the capsule components prior to assembly.

2. Flux Depressor

The irradiation capsule was large enough to require an entire "A" position in an MTR reflector. The horizontal neutron flux gradient in the irradiation

Figure 3. NAA 82-1 Capsule Components Prior to Assembly

facility (MTR position A-17) required an eccentric flux depressor tube in order to produce a radially symmetrical flux across the experiment (see Section III-C). The flux depressor and the special "A" piece used in this experiment are shown in Figure 4. The flux depressor was made of Haynes-25 (a high cobalt alloy), and the "A" piece was made of aluminum. The experiment was irradiated in a vertical position within the flux depressor tube and "A" piece, with the flux depressor oriented so that the thickest part was toward the reactor core.

Figure 4. NAA 82-1 Flux Depressor/A-Piece

B. HEAT TRANSFER ANALYSIS

A detailed heat transfer analysis⁽⁵⁾ was performed in order to, (1) optimize the gas annulus size to maintain the desired fuel temperature for a wide range of heat generation rates, (2) determine absolute temperatures and temperature gradients in the fuel element and the experiment, (3) predict stress levels and thermal expansion effects, and (4) provide a basis for measuring experiment heat generation rates and thereby predicting fuel burnup.

The design peak heat generation rate was fixed by the design criteria for fuel burnup and irradiation time and was calculated to be 1453 Btu/hr-in.³ for

the fuel. Axial variations in heat generation followed the MTR flux profile (truncated cosine shape) except for end peaking due to nuclear end effects (see the gamma scan, Figure 30a, Section V-D). The gas annulus size was determined using a simple one-dimensional cylindrical symmetric heat transfer model. The annulus was 0.026-in. wide at ambient temperatures, but thermal expansion of the inner tube reduced the gap to 0.019-in. at design temperature. The analysis showed that the design temperature could be maintained for heat generation rates varying from 46 to 154% of the design value.

A multidimensional heat transfer analysis was performed using an IBM 7094 computing system together with the HEATING Code.⁽⁶⁾ An R-Z heat transfer model was used to analyze the radial and axial temperature distributions, and an R- θ model was used to analyze the possible effects of an off-center fuel rod due to fuel element bending. The effects of the maximum possible off-center conditions are shown in Figure 5. It can be seen that, although the peak fuel temperature increased only 6°F, the fuel surface would have a circumferential temperature variation of 53°F. It should be noted that the location of peak fuel temperature would shift from the centerline to a radial location 0.015-in. above

Figure 5. Radial Temperature for Off-Center Fuel Rod

the centerline of the fuel. Since fuel swelling due to irradiation would decrease the fuel clad gap, the maximum possible off-center condition and corresponding thermal effects would tend to become less pronounced as the irradiation continued.

The thermal analysis was performed based on an anticipated peak fuel temperature of 1450°F, which corresponds to maximum 100 kw SNAP 2 reactor conditions. When the SNAP 2 reactor was upgraded to 125 kw, the peak temperature of interest became 1500 to 1525°F. The stress analysis (Section III-E) indicated that the peak fuel temperature should be limited to 1507°F (corresponding to a peak cladding temperature of 1300°F) for design heat generation rate conditions. The critical portions of the heat transfer analysis were recalculated based on that value. Figure 6 shows the design axial temperature profile for the fuel element, together with actual thermocouple readings from the experiment, which effectively verifies the validity of the analysis. The "startup curves," which were used to measure fuel heat generation, are explained in Section III-F.

a. Temperature Profiles in MTR (April 1965)

b. Experiment Histogram

Figure 6. NAA-82-1 Operating Data

C. NUCLEAR ANALYSIS

The nuclear analysis had three principal objectives, to: (1) determine the unperturbed thermal flux level, and hence the test position, required to give the specified fission rate in the fuel; (2) determine the geometry of nuclear poison required to give a flat fission rate profile around the circumference of the fuel; and (3) determine the neutron spectrum in the experiment.

Since the test position thermal flux levels were confirmed by dosimeter wire measurements immediately prior to experiment insertion, and since the experiment operated at 98% of the design heat generation rate after startup, it is known that objectives (1) and (3) were correctly determined. The four highest temperature thermocouples which were placed so as to measure the circumferential temperature profile around the center of the fuel element (Thermocouples 7, 8, 9, and 10) operated in a $\pm 10\%$ band and thereby indicated that the second objective was fulfilled.

The eccentric Haynes No. 25 poison (50% cobalt) tube that was used to flatten the flux is shown in Figure 4. The tube was sized after inspection of the thermal flux gradients in the test position areas, and measurement by MTR personnel showed that the reactor radial flux level decreases by a factor of approximately 2.4 across the south test positions.

Because the flux depressor tube was so poisonous, the experiment perturbation factor* was unusually large (13.7). Values obtained from computer code calculations were very sensitive to changes in the assumed values of experiment nuclear cell size and thermal neutron temperatures. For this reason, it was necessary to determine the neutron spectrum shape and average thermal energy at the test position location before the perturbation factor could be accurately calculated.

The detailed nuclear analysis⁽⁵⁾ employed the computer codes ULCER⁽⁷⁾ and TEMPEST⁽⁸⁾ for MTR core spectrum calculations; nuclear codes S_n ⁽⁹⁾ and

*This factor is the ratio of unperturbed thermal flux (flux with no experiment in the irradiation facility) to the perturbed thermal flux (flux with experiment in the irradiation facility). Perturbed flux refers to the volumetric average thermal neutron flux in the fuel rod. The perturbation factor is defined at the axial location of peak neutron flux.

TEMPEST were used for the experiment flux perturbation calculations. The version of ULCER used has 23 energy groups total, with 8 groups in the thermal neutron energy range (0.001 to 0.7 ev), and 15 in the fast energy range (0.7 ev to 10 Mev). Groups 15 through 23 include transfer cross-sections for energy gain, i. e., upscatter, as well as energy loss, i. e., downscatter, while Groups 1 through 14 include only downscatter cross-sections. ULCER is a diffusion theory code. TEMPEST is a code which takes the weight percentages of an element in a cell, weighs the neutron spectrum according to absorption cross-sections, and then gives effective absorption cross-sections for each element for the "hardened" spectrum. S_n is a cylindrical geometry transport theory cell code. Hardened cross-sections and neutron energies from ULCER and TEMPEST were input to S_n , which then calculated experiment flux profiles, perturbation factors, etc. The S_n -TEMPEST cell calculations were checked, after the experiment had been inserted in the reactor, by THERMOS.⁽¹⁰⁾ THERMOS is a 30-group (0.001 to 0.625 ev) transport theory cell code which hardens the spectrum and then uses the hardened spectrum in thermal flux perturbation calculations. The THERMOS answers agreed within 4% with the S_n -TEMPEST answers.

The MTR was mocked up in slab geometry for ULCER, as shown in Figure 7. A combination of homogenous blacknesses (boron mixtures) was used in the core test and the reflector test positions to give (1) the correct core thermal flux profile, with its albedo humps, and (2) the correct shape and thermal flux gradient in the reflector test positions. These blacknesses were used in cases simulating a reactor cross-section indicated by line B of Figure 7. Cases simulating a reactor cross-section indicated by line A used the core test position blackness; reflector test positions were not included.

The results of the MTR spectrum analysis are shown in Figures 8 and 9. Figure 8 compares the calculated flux profiles with values measured by MTR personnel. The A and B lines correspond to those shown in the core diagram (Figure 7). The fast fluxes (>1 Mev) measured in the test positions correspond closely to the cases that include the aluminum-boron reflector test positions, thus indicating, as expected, that the fast neutrons stream through the aluminum. The measured thermal fluxes fall between lines A and B, but are considerably closer to line B. This is reasonable since the test position "island" is not

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a. MTR Cross-Section

b. NAA 82-1 Experiment in Reactor Position
(photo provided by NRTS)

Figure 7. MTR Cross-Section and
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Reactor Position

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Figure 8. MTR Thermal and Fast Flux

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Figure 9. MTR Effective Neutron Temperatures

infinitely wide as assumed by a slab geometry mockup, but is surrounded by a "sea" of reflector beryllium, which has higher thermal flux levels.

The thermal neutron spectrums at the north and south test positions and in the reactor core are shown in Figure 9. The ULCER results shown were used to determine the effective temperatures for input to TEMPEST, which in turn gives cross-sections for S_n calculations. The amount of spectrum hardening caused by the shroud tube is calculated by use of the following equations in conjunction with Figure 9.

$$\Sigma_a \cdot t(\text{Aluminum-Boron}) = \Sigma_a \cdot t(\text{Shroud tube}) \quad \dots(1)$$

and

$$\epsilon_{\text{eff}} = \frac{4kT}{\pi} \quad \dots(2)$$

where

t = Thickness (cm)

Σ_a = Macroscopic absorption cross-section (cm^{-1})

k = Boltzman constant (ev/ $^{\circ}\text{K}$)

T = Moderator temperature ($^{\circ}\text{K}$)

ϵ_{eff} = Energy, for a Maxwell-Boltzman thermal neutron flux distribution, that corresponds to Σ_a for the distribution for a moderator at temperature T .

The ϵ_{eff} values for the north and south test positions are 0.059 and 0.044 ev, respectively. These energies correspond to temperature of 540 and 400 $^{\circ}\text{K}$, respectively.

The experiment was inserted in a south reflector position (A-17), so the temperature for the beryllium surrounding the test position was 400 $^{\circ}\text{K}$, compared to a 325 $^{\circ}\text{K}$ MTR physical moderator temperature. The neutron temperature in the flux depressor tube is about 480 $^{\circ}\text{K}$ and the neutron temperature in the fuel rod is about 560 $^{\circ}\text{K}$. These temperatures correspond to effective energies of about 0.053 and 0.061 ev, respectively.

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The S_n code, using hardened cross-sections, gave the 13.7 perturbation factor used to specify the unperturbed flux required. An S_n case using the MTR physical moderator temperature of 125° F (325° K) indicated a perturbation factor of 20.8. This again indicates the sensitivity of the calculations to neutron spectrum hardening. The thermal flux profile in the fuel is shown in Figure 10.

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Figure 10. Results of MTR Test Position and Flux Perturbation

The unperturbed thermal flux level required was calculated from the following three equations:

$$\frac{N_{25}}{N_{25}^0} = \left[1 - \frac{\beta N_m}{N_{25}^0} \right] \quad \dots (3)$$

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and

$$\frac{N_{25}}{N_{25}^0} = \exp(-\phi_p \cdot \sigma \cdot t) \quad \dots (4)$$

and

$$\phi_{up} = (\phi_p)(P) \quad \dots (5)$$

where

N_{25} = U-235 atom density at time t_0 (at./cm³)

N_{25}^0 = U-235 original atom density (at./cm³)

N_{28} = U-238 atom density (at./cm³)

N_{Zr} = Zirconium atom density (at./cm³)

$N_m = N_{25}^0 + N_{28}^0 + N_{Zr}$ (at./cm³)

β = Metal atom fraction burnup desired

ϕ_p = Average thermal flux required in fuel (n/cm²-sec)

ϕ_{up} = Unperturbed reactor thermal flux (n/cm²-sec)

t = Time irradiated at flux ϕ_p (sec)

σ = Microscopic fission cross section (cm²) for U-235

P = Perturbation factor

Perturbed and unperturbed fluxes of 1.2×10^{12} nv and 1.6×10^{13} nv, respectively, were calculated. It was on this basis that test position A-17 was chosen for the test.

D. HYDRAULIC ANALYSIS

The analysis was performed using standard hydraulic equations on the experiment "A" piece flow channel. The 39-psi test position pressure drop allows a flow rate of 88 gpm, and a velocity past the experiment of 43 ft/sec.

Because of the relatively low fuel linear power rating (6 kw/ft) and the high flow rate, the bulk temperature rise was less than 1°F. The peak film

temperature differential from the capsule to the coolant water was calculated to be less than 10°F based on a heat transfer coefficient of 7600 Btu/ t²-hr-°F.

The Mirshak burnout correlation equation⁽⁵⁾ indicates a burnout heat flux of about 2.7×10^7 Btu/hr-ft², a factor of 60 over the design peak heat flux of 4.4×10^4 Btu/hr-ft².

E. STRESS AND SAFETY ANALYSIS

1. Fuel Rod Stress

The maximum thermal stress in the fuel rod was a biaxial tensile stress, occurring at the surface of the rod, with magnitude given by the equation:

$$\sigma_{\max} = \frac{\alpha \epsilon \Delta T}{2(1 - \nu)} \quad \dots (6)$$

where

ϵ = Modulus of elasticity (psi)

α = Coefficient of thermal expansion (in. /in. - °F)

ν = Poisson's ratio

ΔT = Fuel temperature differential (centerline temperature - surface temperature)(°F)

The fuel rod stress varies strongly with the heat generation rate, due to the ΔT dependency in Equation 6. For the purpose of calculating maximum and minimum possible stresses, the maximum and minimum heat generation rates were taken to be, respectively, 46 and 154% of the design value of 1453 Btu/hr-in.³ These power levels define the range of heat generation rates for which design temperature could be maintained in the experiment. The maximum thermal stresses in the fuel rod are given in Table 4 for various heat generation rate conditions. Tensile data⁽¹¹⁾ indicate that the ultimate tensile strength of zirconium - 10 wt % uranium hydride fuel is approximately 10,000 psi, indicating that the fuel has sufficient strength to withstand the stresses due to thermal gradients.⁽¹²⁾ In addition, some irradiation annealing of stresses would be expected to occur.

TABLE 4
FUEL ROD THERMAL STRESSES

Normalized Heat Generation Rate (Q/Q_{Design})	Radial Fuel Temperature Differential (ΔT , °F)	Maximum Thermal Stress (σ_{max} , psi)
0.46	60	2,200
0.80	100	3,700
1.00	120	4,440
1.20	145	5,370
1.54	190	7,040

2. Cladding Stresses

Stresses in the cladding were due to thermal stress and internal pressurization. The thermal stress was calculated using Equation 6, with the ΔT term representing the temperature differential across the cladding tube (inside surface temperature - outside surface temperature). Hoop stresses were also derived from pressure differentials due to (1) hydrogen dissociation pressure, (2) fission gas release, and (3) NaK pressurization due to thermal expansion of the NaK and experiment components.

The calculation of hydrogen dissociation pressure was performed using the HYREP⁽¹³⁾ code (a Fortran code for IBM 7094 computing system) together with predicted fuel temperatures. HYREP calculates the equilibrium axial hydrogen distribution due to the temperature gradient over the length of the fuel rod. For these calculations, the temperature of the fuel at any axial location was taken to be the cross-sectional average fuel temperature* at that location. Figure 11

*If the heat generation in the fuel rod is assumed to be uniform, the radial temperature distribution is given by

$$T(r) = T_o - \frac{Q r^2}{4K}$$

where

Q = Uniform volumetric heat generation rate

K = Thermal conductivity

T_o = Centerline temperature

r = Fuel rod radius

The cross sectional average fuel rod temperature is found by performing the indicated integration,

$$T = \frac{\int_0^R r T(r) dr}{\int_0^R r dr} = 1/2(T_o + T_R)$$

where the upper limit of the integration is the radius of the fuel rod, and the fuel surface temperature is

$$T_R = T_o - \frac{Q R^2}{4K}$$

Figure 11. Dissociation Pressure as a Function of Fuel Element Effective H/Zr

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presents the calculated hydrogen pressure as a function of fuel heat generation rate and fuel rod average hydrogen content (H/Zr effective). Since the operation of the experiment was to be adjusted to maintain a constant peak fuel temperature for any heat generation rate, it is clear that the minimum heat generation would produce a minimum radial temperature gradient and would therefore have the highest bulk fuel temperature and hydrogen pressure.

After the experiment had been completed and inserted in the MTR, hydrogen pressures were again calculated using the FUSAK⁽¹⁴⁾ code together with measured in-pile operating temperatures. FUSAK incorporates HYREP as a subroutine and also computes hydrogen loss due to permeation. The end-of-life hydrogen distribution predicted by FUSAK is shown in Figure 12.

Cladding stress levels are summarized in Table 5. Total stresses were taken to be the summation of the thermal stress and the hoop stress due to pressure differentials.

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Figure 12. Predicted Hydrogen Distribution

TABLE 5
CLADDING STRESSES

Normalized Heat Generation Rate (Q/Q_{Design})	Thermal Stress (psi)	Hoop Stress (psi)	Total Stress (psi)
0.46	1150	2990	4140
1.00	1440	2510	3950
1.54	1820	1950	3770

A value of 420 psi was included in the hoop stress to allow for fission gas pressurization. This allowance represents a condition more severe than that expected. As an additional safety factor, external NaK pressure and relief of thermal stresses by high temperature annealing were disregarded. Stress rupture and minimum creep rate data obtained for Oak Ridge alloy INOR-8 (Hastelloy-N)⁽¹⁵⁾ indicated that the calculated stress levels in the cladding represented safe conditions.

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3. Fission Gas Release

It was assumed that fission gas escaped from the fuel rod only by recoil at the surface, which assumes an uncracked fuel rod and no diffusion of fission gases from the interior to the surface. The total fuel volume within one recoil length of the surface was calculated to be 0.315 cm^3 . The design fission density at the end of the irradiation, corresponding to 0.08 metal at. % burnup, was $2.95 \times 10^{19} \text{ fissions/cm}^3$. The fission product yield of the gases bromine, krypton, iodine, and xenon was taken to be 0.28 atoms per fission. A geometric factor of 0.25 was applied, and all the above factors were multiplied to obtain the predicted total fission gas release of 6.5×10^{17} atoms. The void within the cladding was calculated to be 0.00785 in.^3 after irradiation swelling of the fuel; the fission gases would therefore produce a pressure of 10 psia resulting in a cladding stress of 420 psi. However, a large fraction of the recoil particles would have sufficient energy to penetrate into the ceramic barrier coating and therefore would be unavailable to contribute to pressure within the cladding. The 420 psi was taken as an upper limit to the cladding hoop stress due to fission gas pressure.

4. Fuel Swelling

A preliminary empirical relationship⁽¹²⁾ predicting irradiation swelling of SNAP fuel was derived by Miller⁽¹⁶⁾ based on the observed swelling of zirconium-uranium hydride specimens in the NAA-53 irradiation experiments:

$$\frac{\Delta V}{V} = 3770 B^{1.5} \exp \left(- \frac{12,000}{T} \right) , \quad \dots (7)$$

where

$\frac{\Delta V}{V}$ = volume percent change

B = burnup (at. %)

T = central fuel temperature ($^{\circ}\text{R}$)

Assuming an average central temperature of 1450°F and the design peak burnup of 0.08 metal at. %, the predicted fuel volume increase would be 0.2%.

If isotropic fuel swelling is assumed, the diametral increase would be 0.07%, or 0.0008-in., and the length increase would be 0.009-in.*

5. Irradiation Capsule Stresses

The inner capsule tube contained the NaK at a pressure no greater than 35 psi. The NaK was loaded into the capsule at room temperatures under a helium cover gas at one atmosphere pressure. Thermal expansion of the NaK compressed the cover gas and raised the pressure. The associated hoop stress in the inner tube would be 394 psi in the maximum case. In addition, thermal stresses would be present in proportion to the heat generation rate. In the event of defective fuel element cladding, the hydrogen pressure would also have to be contained. However, the hydrogen would tend to permeate at a rapid rate through the stainless steel at operating temperatures, considerably decreasing the pressure. In any event, the resulting stresses would not cause any problem for either long or short time conditions. The outer capsule tube would have only thermal stresses because the gas annulus was vented to the atmosphere. The capsule stresses are summarized in Table 6.

TABLE 6
CAPSULE STRESSES

INNER TUBE							
Normalized Heat Generation Rate (Q/Q_{Design})	Temperature ($^{\circ}\text{F}$)	Thermal Stress (psi)	NaK Hoop Stress (psi)	Total Stress (psi)	Stress to Cause Rupture 10,000 hr (psi) ⁽¹⁸⁾	Ultimate Tensile Strength (psi) ⁽¹⁸⁾	Hoop* Stress (Hydrogen) (psi)
0.46	1387	1330	394	1724	4000	28,000	1750
1.00	1311	3210	394	3604	5600	35,000	2020
1.54	1256	5150	394	5544	7600	35,000	2430

OUTER TUBE			
Normalized Heat Generation Rate (Q/Q_{Design})	Temperature ($^{\circ}\text{F}$)	Thermal Stress (psi)	Stress-to-Rupture in 10,000 hr (psi) ⁽¹⁸⁾
0.46	120	3,470	60,000
1.00	137	7,470	60,000
1.54	153	11,400	60,000

*Hydrogen pressure hoop stresses will be present only in the event of fuel element cladding failures.

*The above empirical relationship was the best available at the time this analysis was done and has since been superseded.

6. Stresses and Experiment Safety

The stresses calculated for both the design and extreme conditions would not cause a failure of either the fuel element or the capsule except for the case of a structural defect. Quality control was sufficiently effective to minimize the possibility of defects, and a particular effort was made to insure the integrity of welds and brazes. The NaK was double-encapsulated to isolate it from the reactor cooling water, and the fission products were triply-contained by virtue of the fuel element cladding. Provisions were made for monitoring the helium/nitrogen gas mixture at MTR to detect gaseous fission products, which would indicate loss of capsule and element integrity.

7. Operation Limits

The upgrading of the SNAP 2 reactors from 100 to 125 kwt changed the peak fuel temperature of interest in the NAA 82-1 experiment from the initial design value of 1450°F to a value in the range of 1500 to 1525°F. A study of the possible modes of failure for this test indicated that cladding creep in excess of 0.2%, with subsequent loss of coating adherence, represented the limiting condition. In this regard, 0.2% creep was taken as an arbitrary, conservative limit. A parametric study identified those conditions of temperature and heat generation which would produce 0.2% cladding creep in 10,000 hr, which allows a modest safety factor in time over the intended 8760 hr of operation. This study made use of the INOR-8 data⁽¹⁵⁾ with a safety factor of 2 applied to minimum creep rate. Figure 13 shows the recommended operating temperature limits (corresponding to 0.2% cladding creep in 10,000 hr) as a function of heat generation rate. At the design power level, the peak fuel temperature limit would be 1507°F, corresponding to a cladding temperature of 1300°F. These temperatures include a 50°F allowance for possible thermocouple errors due to irradiation effects, loss of calibration, or other unknown effects. As it turned out, the 1507°F peak temperature limit was employed, since the experiment operated at, or very near, its design power level.

While the experiment was in-pile, experimental evidence indicated that ceramic coating adherence was reliable for cladding creep up to 0.75%. However, it was also reported⁽²⁰⁾ that the minimum creep rate of the cladding

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Figure 13. Fuel Operating Temperature Limits as a Function of Heat Generation Rate

could be expected to be higher than indicated by the original INOR-8 data. A reanalysis incorporating the latest data indicated that the operation of the NAA 82-1 experiment should not be altered.

8. Predicted Performance

The stress, fuel swelling, and fission gas release calculations performed in the course of the designs of this experiment indicated that the fuel elements could be expected to perform satisfactorily. Where simplifying assumptions were made, they were generally conservative, i. e., tending to overestimate the severity of the result. However, in many cases it was not clear as to how the interaction of effects might affect the performance of the experiment.

During the time that NAA 82-1 was operating in MTR, the FUSAK⁽¹⁴⁾ computer code was developed at AI to predict the performance of SNAP fuel elements. This code performed an iterative calculation based on a relatively sophisticated model which took into account, (1) neutron flux variations in the fuel, (2) hydrogen redistribution along the fuel rod, (3) changes in hydrogen gas gap dimensions due to hydrogen loss and redistribution and fuel swelling (4) changes in fuel thermal conductivity due to hydrogen loss and redistribution and fuel swelling, (5) fuel swelling based on burnup and temperature, and (6) hydrogen loss based on a calculated equilibrium hydrogen pressure and an

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integration over the surface of the hydrogen barrier and cladding, using calculated temperature profiles. The code described conditions in the fuel rod at each of several time steps from the beginning to the end of the irradiation. The fuel element was represented by twenty-one axial nodes in the calculation of axial hydrogen redistribution. Radial hydrogen distribution calculations were made only when beta phase zirconium hydride was indicated in the axial case.

Another interesting feature of the code allowed the hydrogen barrier quality to change during the irradiation. The NAA 82-1 fuel element at appropriate conditions of temperature and neutron flux was analyzed using the FUSAK code. Three cases were treated based on variation in possible performance of the hydrogen barrier. Since furnace tested fuel elements show a tendency toward improved barrier quality as a function of time-at-temperature, this was taken to represent an optimistic condition. The nominal case assumed no change in barrier quality with irradiation. The worst case was based on the observed performance of S8ER which, at that time, was nearing the end of its operation. Reactivity losses from the S8ER reactor which could not be assigned to other causes were taken to be due to hydrogen loss. A study of S8ER operation equated hydrogen loss rate to decreasing hydrogen barrier quality for the average fuel element. (The results of the optimistic, the pessimistic, and the nominal case FUSAK studies are shown in Table 15, Section VI.) In general, the peak fuel temperatures calculated in FUSAK are higher than those indicated in the heat transfer analysis. This is because (1) FUSAK is basically a one-dimensional heat transfer code and does not allow for axial heat flow, and (2) FUSAK allows for hydrogen redistribution and fuel swelling effects. In the pessimistic case, hydrogen loss would be sufficient to cause the formation of a beta phase material in the high temperature region of the fuel rod. A comparison of observed irradiation effects to predicted conditions is made in Section VI.

F. INSTRUMENTATION

1. Description

The instrumentation system for this experiment served to: (1) control the fuel element temperature within the design operating range, and (2) maintain a continuous record of the temperature history of the experiment.

Figure 14. NAA-82-1 Instrumentation Console

Figure 15. Logic Diagram for Data Acquisition System

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The fuel element was instrumented with sixteen chromel-alumel thermocouples banded to the outside of the cladding with 0.003-in. thick stainless steel bands 0.250-in. wide (see Figure 20, Section V-A). Eight thermocouples were stainless steel-sheathed and the remaining eight thermocouples had Inconel sheaths. Stainless steel-sheathed thermocouples were employed because previous experience indicated that they would operate reliably. However, the coefficient of thermal expansion for Inconel more nearly matches that of the chromel-alumel thermocouple wires than does stainless steel, so that Inconel-sheathed thermocouples should have a higher tolerance for thermal shocks.⁽²¹⁾ In the absence of corroborative data, it would have been imprudent to rely entirely on the supposedly superior Inconel-sheathed thermocouples, so a 50-50 mixture was employed. As it turned out, all sixteen thermocouples operated satisfactorily for the duration of the irradiation period and no relative evaluation of Inconel to stainless steel sheaths could be made.

The sixteen thermocouples were located on the fuel element cladding as follows: Nine thermocouples were located at equal intervals along the length of the fuel element at 0° circumferential orientation; two thermocouples were attached to the end caps, one at each end of the elements; two thermocouples were located at 180° orientation, 0.5-in. from either end; and three thermocouples were located at the center of the element, at 90, 180 and 270° orientations. This thermocouple location scheme placed thermocouples at representative locations, which delineated both the axial and circumferential temperature profiles and provided redundant thermocouples in the high temperature regions.

The instrumentation console (shown in Figure 14) monitored each of the sixteen thermocouples in turn and recorded the temperatures both on strip charts and on a digital punched paper tape. A logic diagram for this system is shown in Figure 15. The heart of the system is a multipoint strip chart recorder (20 points). A Friden paper tape punch, in series with the multipoint recorder, recorded the thermocouple readings together with the date and time of reading as supplied by a digital clock. The analog-to-digital data conversion was performed by a shaft angle encoder mounted on the back of the multipoint recorder. The punched paper tape data format was made compatible with IBM computing equipment to facilitate data evaluation via high speed computers. A single point strip chart recorder operated in parallel with the multipoint recorder and

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provided a backup data logging system. The single-point recorder could be manually switched to any one of the sixteen experiment thermocouples.

The instrumentation console also housed the temperature control system, a helium/nitrogen gas mixing network known as the gas flow module (Figure 16.)

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Figure 16. Gas Flow Module

Manually operated needle valves controlled the helium/nitrogen ratio of the gas mixture supplied to the experiment. The gas passed through the annular thermal barrier region which insulated the fuel from the reactor coolant. Since the thermal conductivity of the gas mixture varies strongly with the mixture ratio, the temperature rise across the gas annulus, and therefore the fuel operating temperature, could be adjusted externally at the gas flow module. Helium, which has the higher thermal conductivity, produced the lowest (i. e., safest) experiment temperature; temperatures were raised by adding nitrogen. Solenoid-actuated valves acted to short-circuit the gas mixing network and supplied high pressure helium to the experiment in the event of a high temperature alarm signal from the data recording system. Alarm signals were generated by cam-actuated limit switches on the two strip chart recorders. The gas flow module was also monitored for abnormal pressure conditions by means of a differential

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pressure switch which would trigger an alarm and helium purge. Alarm conditions also triggered a flashing red light and energized a loud buzzer to summon reactor operating personnel. Low temperature limit switches actuated a flashing amber light.

The scan rate of the multipoint strip chart recorder was set at six seconds per point. The scan sequence alternated high and low temperature thermocouples so that the high temperature region of the fuel element was monitored every twelve seconds. The two highest temperature thermocouples were each monitored twice during the data scan cycle. In addition, a millivolt signal from one of the MTR reactor power level repeaters stations in the reactor bay area was monitored once during the cycle. The complete data scan took two minutes, and this cycle was repeated continuously for the duration of the irradiation.

2. Operation

The instrumentation system was designed to provide experiment safety (that is, to avoid excessively high temperatures) and to maximize experiment time at design temperature. Although the safety features were automatic and incorporated the "fail-safe" principle, the experiment operating temperatures were set and maintained manually. The design peak fuel temperature was 1507°F; however, since there was no in-fuel thermocouple, the peak temperature had to be calculated using cladding thermocouple readings.

Figure 17 presents the "startup curve" which was used at the test reactor to determine experiment heat generation rates and to establish the appropriate cladding operating temperature. The lower curve represents the calculated relationship between cladding temperature and fuel specific heat generation rate. It is obvious that the temperature difference between the fuel and the reactor coolant will vary with the heat flux through the experiment. However, since the MTR coolant temperature remains at a relatively constant temperature during operation, a relationship between heat generation rate and cladding temperature only can be used.

At each reactor startup, the experiment was operated on pure helium (minimum temperature) until full stable reactor power had been achieved. The peak cladding temperature was measured and the corresponding heat generation

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Figure 17. Fuel Element Temperature for Pure Helium and He/N Gas Mixtures as a Function of Fuel Heat Generation Rate

rate was obtained using the startup curve. The cladding operating temperature (and corresponding peak fuel temperature) appropriate for that heat generation rate was obtained from the upper curves in Figure 17. After the appropriate cladding operating temperature was determined, from the curve in the figure, the limit switches were set at 25°F above and below the operating point which established the operating range. The differential pressure switch and the high temperature limit switches were proof-tested during this time. This procedure was performed at initial reactor startup for each reactor cycle and following mid-cycle refueling. Figure 18 shows the temperature record for the initial startup of NAA 82-1. The sequence of events shown is typical for all startups. The experiment was brought to operating temperature by increasing the nitrogen flow rate in very small increments. This operation was a very delicate one, due to the sensitivity of the experiment to small changes in needle valve settings and because of the long response time of the system, caused by the long gas lines and low flow rates. The peak temperature experienced during the irradiation was associated with normal changes in needle valve settings. The safety system limited the most severe temperature excursion to a peak cladding over

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Figure 18. Cladding Temperature of NAA-82-1 vs Time at Initial Startup

temperature of 75°F; that is, the temperature was 50°F above the alarm set point and 75°F above the operating temperature.

3. Data Output and Processing

NAA 82-1 experiment used a dual data logging scheme: the primary record was made on the multipoint strip chart, and the secondary record was logged onto punched paper tapes (an innovation for SNAP irradiation tests). Routine data evaluation was made at the reactor by MTR operating personnel and by the AI on-site engineer. The strip chart and paper tapes were then shipped to Canoga Park for detailed analysis at AI. Figure 19 shows the data flow diagram for punched paper tape information. First, the paper tapes were read, edited, and reproduced on magnetic tape using an IBM 1401 computer. An intermediate data printout at this stage allowed an early evaluation of current experiment operation. In the second step of data flow, the magnetic tapes were processed using an IBM 7094 computer and a specially written data reduction code. The bulk of the data was reduced by testing each data point and discarding those temperature readings which did not deviate more than 7°F from the previously retained data point. The importance of reducing the bulk of the data can be

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Figure 19. Logic Diagram for Data Processing

appreciated when one considers that approximately 39 miles of punched paper tape were generated by this experiment. Reducing the data also served to identify those periods when the experiment was not stable. Time at temperature data was accumulated for each thermocouple for operating temperatures from 500 to 1400°F in 25°F intervals. Figure 6 shows a typical histogram plot of these data. The histogram was plotted using a cathode ray tube unit associated with the computer. Approximately 85% of the experiment operating data was processed from paper tapes; the rest of the data were taken from strip charts. Gaps in the punched tape record were due both to down time in the data logging system and to interfacing problems associated with advanced computing systems.

In retrospect it would seem to be more desirable to reduce the bulk of data as it is generated at the reactor, instead of afterwards using the computers. This might be done by separating the data scan function from the data logging function. A fast scan rate would insure experiment safety and a slow logging rate would serve the purpose of data reduction. Alarm conditions could initiate rapid data logging to provide a record of temperature transients. It would also seem desirable to use a servo-operated gas flow module for temperature control. This would reduce the probability of temperature overshoots, maximize experiment time at temperatures and minimize the operating range of the experiment.

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IV. IRRADIATION PERFORMANCE

The irradiation of NAA 82-1 was initiated on April 9, 1964 at the beginning of MTR cycle 207 and continued for 23 reactor cycles. The experiment was discharged on August 16, 1965 at the end of MTR cycle 229. During this time, the reactor was in operation 9,350 hours and was at full power (40 Mwt) for 9,088 hours. The experiment operated within 50°F of its design cladding temperature (1300°F) for 8,479 hours. The operating goal was 8,760 hours (one year) at design temperature and power.

During the initial startup, the fuel heat generation rate was determined to be within 3% of the design value. Throughout the irradiation, the heat generation rate, as determined by temperature measurements, was very stable, varying about 5% from the design value. During the first reactor cycle of irradiation, the operating temperature was maintained at a lower than normal level in order to allow the hydrogen distribution in the fuel rod to adjust to the gradient temperature conditions and thereby avoid excessively high internal hydrogen pressures and subsequent detrimental effects.

Early in the operation of this experiment, it was observed that the thermocouples on the top end of the fuel element indicated an oscillating temperature phenomenon while those at the center and lower end of the fuel element were well behaved. In general, the thermocouples at 0° orientation would indicate step change increases in temperature of up to 120°F while the thermocouple at 180° orientation would simultaneously indicate a corresponding step decrease in temperature. Eventually, a reverse step change in temperature would occur and the process would repeat. The period of oscillation was not constant or predictable (it sometimes ceased for several months), and the magnitude of the temperature steps also varied. This phenomenon was ascribed to thermal convection currents in the NaK surrounding the fuel element. The NaK surrounding the center of the vertically aligned fuel element was approximately 750°F hotter than the NaK above or below the ends of the fuel element. Having cold NaK above the hot NaK near the fuel element center would provide a strong driving force for convection, while the hot NaK near the center of the element over the cold NaK below the element would promote a stagnate condition. Furthermore, the thermocouples which ran longitudinally along the fuel element (see Figure 20,

Section V-A) acted as baffles and channeled the NaK flow. Once a convection flow pattern was established, the thermocouple exposed to rising NaK would see higher than normal temperatures, while those thermocouples exposed to descending NaK would see lower than normal temperature. A change in the NaK convection flow pattern would produce step change temperature reversals on the affected thermocouples. The convection flow pattern might be interrupted by a variety of causes; a broken thermocouple band was suggested as one. The post-irradiation examination showed that several thermocouple bands were broken (see Figure 20), but it is possible that this damage occurred during experiment disassembly.

The in-pile operation of this experiment was routine and uneventful except for the temperature fluctuations described above. The irradiation capsule performed safely and satisfactorily. Some difficulties were experienced with the data logging function of the control console but normal trouble-shooting techniques provided the remedy. Table 7 presents a summary of pertinent operating data.

TABLE 7
SUMMARY OF REACTOR OPERATIONS

Design Fuel Centerline Temperature (°F)	1507
Design Cladding Temperature (°F)	1300
Design Time at Temperature and Power (hr)	8760
Hours of Reactor Operation	9350
Hours of Full Power (40 Mwt) Operation	9088
Hours Experiment Operated at $\pm 50^{\circ}\text{F}$ of Design Cladding Temperature	8479
Hours Experiment Operated at Less Than 1250°F	609*
Peak Cladding Temperature During Temperature Transients (°F)	1375
Peak Fuel Temperature During Temperature Transients (°F)	1600†
Reactor Scrams	101
Thermal Transients Greater Than 200°F	205

*This time excludes reactor down time

†Includes estimate for hydrogen redistribution, expected fuel swelling

V. POST-IRRADIATION EXAMINATION

A. EXPERIMENT DISASSEMBLY

The experiment was discharged from the reactor August 16, 1965. The flux depressor/A-piece assembly was removed at the reactor site and the capsule shipped to Atomics International Hot Laboratory (AIHL) for examination.⁽²²⁾ After the capsule was removed from the shipping cask, both ends were cut off and the inner tube pushed from the outer tube. Visual examination of both inner and outer tube revealed no abnormalities or defects, however the inner shell showed discoloration in the high temperature region of the fuel element. A puncture made through the inner tube and into the NaK annulus did not release any fission gases, indicating the fuel element cladding was intact and disassembly could proceed normally. A circumferential cut at one end of the inner tube freed the element. All NaK present was reacted with Dowanal EB* under an inert atmosphere.

Figure 20 shows the fuel element with cladding thermocouples restraining bands still in place. Two bands were broken, the other six were loose. However, it cannot be determined whether this was due to handling during and after fabrication, reactor operations, or disassembly at AIHL. The zero orientation for the AIHL examination — the line of nine thermocouple hot junctions (see Figure 3) — was 76° away from directly facing the reactor core.

B. FUEL ELEMENT

Figure 20 shows the fuel element after removal of the thermocouples and bands. The outline of each thermocouple and band was clearly evident on the cladding. A detailed visual examination of the cladding surface at magnifications up to 30X failed to reveal any defects. The oxidation layer produced during pre-irradiation permeation testing was removed by NaK in the high temperature regions of the cladding, but remained near the ends, which were at lower temperatures.

Post-irradiation diametral measurements were taken with a profilometer. This device is a lathe with end stocks modified to support standards and test

*Trade name for ethylene glycol n-butyl ether, Dow Chemical Co., Midlands, Michigan

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a. Thermocouples Attached

b. Bare, at Zero (Note outline of each TC and band on cladding)

Figure 20. Fuel Element With Thermocouples and Thermocouple Restraining Bands Attached, and After Removal of Thermocouples and Bands

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pieces, and two linearly variable displacement transducers mounted in opposition on the tool carriage. The transducers traverse the surface of a standard and a test piece producing a signal that is a continuous trace of the test piece topography. Thus Figure 21 presents a trace of the fuel element at 90° orientation, and is considered accurate to ± 0.0003 -in. Profilometer technology was acquired subsequent to obtaining pre-irradiation data, therefore pre-irradiation diametral measurements were taken with a micrometer (accuracy ± 0.0005 -in.).¹ Pre-measurements were reported at specific locations along the element, not in a continuous profile. Table 8 presents a comparison of pre- and post-irradiation diameters, showing a maximum change of 0.005-in. and an average change of 0.002-in.

TABLE 8
FUEL ELEMENT DIAMETERS

Location From Top of Fuel Element (in.)	Pre-irradiated Diameter, D_o (in.)	Post-irradiated Diameter, D_f (in.)	$\Delta D = D_f - D_o$ (in.)	$\% \Delta D/D = (D_f - D_o)/D_o \times 100$
1/4	1.251	*	*	*
1-1/4	1.250	1.252	0.002	0.16
2-1/4	1.250	1.252	0.002	0.16
3-1/4	1.251	1.253	0.002	0.16
4-1/4	1.250	1.253	0.003	0.24
5-1/4	1.250	1.253	0.003	0.24
6-1/4	1.250	1.255	0.005	0.40
7-1/4	1.251	1.254	0.003	0.24
8-1/4	1.251	1.253	0.002	0.16
9-1/4	1.251	1.253	0.002	0.16
10-1/4	1.251	1.251	0.000	0.00
11-1/4	1.251	1.251	0.000	0.00
12-1/4	1.253	*	*	*
Average	1.251	1.253	0.003	0.16

*Measurements could not be taken due to end pieces supporting the fuel element

Pre- and post-irradiation length measurements were taken with gages accurate to ± 0.001 -in. Multiple determinations revealed a post-irradiation length of 12.457 in., as compared to a pre-irradiation value of 12.449 in., yielding a change of 0.008-in., or 0.06%.

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a. FUEL ELEMENT AT 90° ORIENTATION

b. FUEL ROD AT 90° ORIENTATION

REDUCED SECTION
HELD BY PROFILOMETER

6-3-67 CRD (GP-1,

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Figure 21. Profilometer Trace of
Fuel Element and Fuel Rod
at 90° Orientation

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Fission gases were released, by puncturing near the end cap, and were collected on activated charcoal in liquid-nitrogen-cooled gas traps. The traps were removed from the apparatus and analyzed by gamma spectroscopy. This analysis indicated a release of Kr^{85} amounting to $5.99 \times 10^{-3}\%$ based on a peak burnup of 0.08 metal at. %.

A tubing cutter was used to remove both end caps. The cladding was longitudinally slit at 0 and 180° with an Elox milling machine and the cladding halves easily separated from the fuel rod. Figure 22 shows the bare fuel rod and the inner surfaces of the cladding tube. The fuel rod was in excellent condition, showing no cracks, blisters or other evidence of gross irradiation damage. Machining marks imparted to the fuel rod during fabrication were still clearly visible. The fuel rod was in intimate contact with the cladding tube and end caps, as shown by the impressions of the fuel rod machining marks in the ceramic coating of the cladding tube and the transfer of ceramic coating from the inside of both end caps to the ends of the fuel rod, (Figure 23). Ceramic adherence to the cladding appeared good, as evidenced by the lack of chipping along the cuts in the cladding.

C. CLADDING AND COATING

Cladding samples were taken for metallography, hardness testing and bend tests. Half-circles of cladding, 1/4-in. wide, were cut from the split cladding halves at 3, 6-1/4 and 9-3/4 in. from the top of the element and mounted transversely so that the entire cladding cross-section could be observed. A mixture of nitric and hydrochloric acids was used for attack polishing and electrolytic etching. Figure 24 illustrates the condition of the cladding material following irradiation. No evidence of a crack or NaK penetration was observed, agreeing with previous conclusions that the cladding was intact. Both intra- and inter-granular precipitation were observed, while grain boundary precipitates and the chromized layer showed preferential attack by the etchant. Twinning was pronounced in all three samples examined.

The ASTM grain size of 5.6 was similar to that shown by SNAP 10FS-3 samples,⁽²³⁾ but larger than the 6.5-7.5 ASTM grain size shown by S8ER⁽²⁴⁾ and NAA 115-1.⁽²⁵⁾ The ceramic was intact and exhibited good adherence to the

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CRD (GP-1)

NAA-82-1-41, -42, -43, -44, -45

a. Intact Fuel Rod (Machine Marks From Pre-Irradiation Turning and Grinding Operations. Scratches Caused by Slave Fingers)

CRD (GP-1)

NAA-82-1-54, -55, -56, -57, -58, -59

b. Inside of Cladding Tube (Machine Marks on Fuel Rod Impressed Into Surface of Ceramic, Indicating Intimate Fuel-Cladding Contact. Scratches Caused by Slave Fingers)

Figure 22. Fuel Rod and Inner Cladding Surface Following Disassembly

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CRD (GP-1)

CRD (GP-1)

NAA-82-1-46

a. Bottom End (Dark Ring is Ceramic Adhered to Fuel Rod and Pulled From Cladding. Pre-irradiation Number Visible)

CRD (GP-1)

b. Inside of Cap (Coating Off Circular Area. Very Light Area Bare Metal)

CRD (GP-1)

NAA-82-1-70

Figure 23. Bottom End of Fuel Rod and Inside of Bottom End Cap, Illustrating Ceramic Transfer

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a. Etched Cross-Section.
(Large-Grained With Pro-
nounced Twinning. Inter-
granular and Intragran-
ular Precipitation)

b. Closeup of Outside Edge Tube
(Reaction of Chromized Zone,
Showing Precipitates and
Scratches)

Figure 24. Hastelloy-N Cladding Material Following Irradiation

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cladding. The porosity found in the ceramic has also been observed in other irradiated and unirradiated samples.⁽²⁵⁾

Microhardness measurements were made by the Knoop method, using a 100-gm load and a 20-mm objective. Averaging fourteen measurements for each cladding sample yielded values of 234, 219 and 220 KHN for the samples at 3, 6-1/4 and 8-3/4 in. from the top of the fuel element. Considering the error band of ± 20 KHN values, these values are well inside the range of values for unirradiated cladding, 220 to 260 KHN.⁽²⁵⁾

Bend-test samples were 1/2-in. -wide half-circles cut from the split cladding halves. Tests were performed by bending the strips with the hot cell manipulators back and forth through an angle of 180° until failure. The results of these tests are shown in Table 9.

TABLE 9
CLADDING BEND TEST RESULTS

Location of Sample From Top of Fuel Element (in.)	Ceramic Cracked (Number of Bends)	Cladding Cracked (Number of Bends)
2-3/8	1	11
5-5/8	1	11
8-1/8	1	9

D. FUEL ROD

Diametral measurements of the bare fuel rod were taken with the profilometer previously described. A trace of the fuel rod at 90° orientation was shown in Figure 21. Table 10 presents a comparison of pre- and post-irradiation diameters, indicating a maximum change of 0.72% and an average change of 0.53%.

Caliper measurements indicated a post-irradiation length of 12.262-in., which is 0.012-in., or 0.09%, over the pre-irradiation value of 12.250 in. Fuel rod volume change of 1.15% was calculated from the formula,

$$\% \Delta V/V = 2(\% \Delta D/D) + (\% \Delta L/L) \quad , \quad \dots (8)$$

where

$$\Delta D/D = 0.53\% \text{ and } \Delta L/L = 0.09\%$$

The fuel rod was sectioned according to the plan shown in Figure 25. A diamond cutoff wheel was used to remove eleven wafers, each approximately 1/4-in. thick. A total of eight wagers were used for hydrogen analysis, three for mass-spectrographic burnup determinations. Density measurements were performed on the seven large pieces which were subsequently sectioned to provide three metallography samples and three samples for radiochemical burnup determination.

Fuel rod density measurements, reported in Table 11, were obtained by liquid displacement techniques using monobromobenzene as the liquid. The accuracy of this method has been determined to be $\pm 0.1\%$ of the cited value. Density decreases are to be expected for fuel that has swelled.

TABLE 10
FUEL ROD DIAMETERS

Location From Top of Fuel Rod (in.)	Pre-irradiation Diameter, D_o (in.)	Post-irradiation Diameter, D_f (in.)	$\Delta D = D_f - D_o$ (in.)	$\% \Delta D/D = [(D_f - D_o)/D_o] \times 100$
3/8	1.2101	*	*	*
1-3/8	1.2102	1.2132	0.0030	0.24
2-3/8	1.2102	1.2139	0.0037	0.30
3-3/8	1.2102	1.2160	0.0058	0.47
4-3/8	1.2102	1.2156	0.0054	0.44
5-3/8	1.2102	1.2167	0.0065	0.53
6-3/8	1.2102	1.2190	0.0088	0.72
7-3/8	1.2102	1.2186	0.0084	0.69
8-3/8	1.2102	1.2168	0.0066	0.54
9-3/8	1.2102	1.2172	0.0070	0.58
10 3/8	1.2101	1.2184	0.0063	0.51
11-3/4	1.1514	1.157†	0.0056	0.48
Average	1.2102	1.2167	0.0065	0.53

*Unable to measure due to interference of profilometer standards

†Measured with calipers, accuracy ± 0.001 -in.

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TABLE 11
FUEL DENSITY MEASUREMENTS

Sample*	Pre-Irradiation ρ_1 (gm/cc)	Post-Irradiation ρ_2 (gm/cc)	$\Delta\rho/\rho_2$ (%)
1	6.065	6.005	0.999
2	6.065	6.027	0.630
3	6.065	6.003	1.033
4	6.065	5.956	1.830
5	6.065	5.986	1.320
6	6.065	6.005	0.999
7	6.065	6.037	0.960
Average	6.065	6.003	1.04

*See fuel rod sampling plan, Figure 25, for location of these samples

Figure 25. Fuel Rod Sampling Plan

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Hydrogen analyses were performed on 20 small cores taken from eight fuel wafers (location shown in Figure 25). A summary of these data, presented in Table 12, shows the effects of the axial temperature gradient on the hydrogen

TABLE 12
FUEL HYDROGEN ANALYSIS

Hydrogen Wafer	Pre-Irradiation H/Zr*	Post-Irradiation Centerline	H/Zr Radial Average†	Percent Change
1	1.84	1.89	-	+3.00
2	1.84	1.85	-	+0.70
3	1.84	1.75	1.77	-3.86
4	1.84	1.68	1.71	-7.20
5	1.84	1.70	1.70	-7.75
6	1.84	1.71	-	-7.20
7	1.84	1.81	-	-1.50
8	1.84	1.81	-	-1.50
Vol. Avg	1.84	1.77	-	-4.07

*Calculated on the basis that zirconium added as ZrC does not hydride.

†Average of 5 samples on each wafer.

redistribution. The lowest hydrogen concentration (maximum loss) occurred at the center of the rod where the maximum temperature occurred. The top of the fuel rod, which operated at a relatively cool temperature, shows a gain in hydrogen concentration. This effect can be seen by comparing Figure 26 and Figure 6. Three fuel wafers were sampled at the center, twice at mid-radius and twice at the edge to determine radial hydrogen distribution. The center samples showed a slight decrease in hydrogen content over the edge samples, approximately 0.04 wt %. The maximum hydrogen loss, observed at the center, was 7.75%. Volumetric average hydrogen content was obtained by correcting the centerline hydrogen values to reflect the radial hydrogen distribution observed in the three fuel wafers tested for off-center hydrogen content. The post-irradiation volumetric hydrogen loss was 4.07%.

As illustrated in Figure 27, which shows density versus hydrogen content, hydrogen loss from the fuel will produce shrinking, with a resultant increase in fuel density. Theoretical values, that separate volume changes due to irradiation

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Figure 26. Post-Irradiation Hydrogen Distribution

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Figure 27. Variation of Zirconium Hydride Density With Hydrogen Content

and those due to hydrogen content changes, can be calculated by correcting measured values for post-irradiation hydrogen content. Hydrogen-corrected density changes are based on the relationship,

$$\left(\frac{\Delta\rho}{\rho}\right)_{\text{irradiation}} = \left(\frac{\Delta\rho}{\rho}\right)_{\text{measured}} + \left(\frac{\Delta\rho}{\rho}\right)_{\text{H}_2 \text{ correction}} \quad \dots (9)$$

where hydrogen corrections are taken from Figure 27. Diameter corrections are based on the equation,

$$\left(\frac{\Delta D}{D}\right)_{\text{H}_2 \text{ correction}} = \left(\frac{\Delta\rho}{\rho}\right)_{\text{H}_2 \text{ correction}}^{1/2.5}, \quad \dots (10)$$

which is patterned after hydriding data,⁽²⁵⁾ where $(\Delta D/D)/(\Delta L/L) = 2.0$. It is assumed that subsequent hydriding and dehydriding steps follow the original path where the effect on diameter is greater than the effect on length. Dimensional and density changes, uncorrected and corrected for post-irradiation hydrogen content, are presented in Table 13. Figure 28, a plot of $\Delta D/D$ versus $\Delta\rho/\rho_2$

6-7-67 CRD (GP-1)

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Figure 28. Fuel Directional Swelling Behavior

Distance From Top of Rod	Pre- Irradiation		Post-Irradiation (measured)						H/ Zr		Post-Irradiation (modified to eliminate hydrogen loss effects)							
			Diameter			Density			Irradiation		Diameter				Density			
(in.)	D ₁ (in.)	ρ ₁ (g/cc)	D ₂ (in.)	D ₂ -D ₁ (in.)	ΔD/D ₁ (%)	ρ ₂ (g/cc)	ρ ₁ -ρ ₂ (g/cc)	Δρ/ρ ₂ (%)	Pre-	Post-	Correction (in.)	D ₂ (in.)	D ₂ -D ₁ (in.)	ΔD/D ₁ (%)	Correction (gm/cc)	ρ ₂ (gm/cc)	ρ ₁ -ρ ₂ (gm/cc)	Δρ/ρ ₂ (%)
1.0	1.210	6.065	1.213	0.003	0.25	6.005	0.060	0.99	1.84	1.88	-0.0005	1.2125	0.0025	0.20	-0.007	6.012	0.053	0.90
2.5	1.210	6.065	1.215	0.005	0.41	6.027	0.038	0.63	1.84	1.79	0.0003	1.2153	0.0053	0.44	0.004	6.023	0.042	0.69
4.5	1.210	6.065	1.216	0.006	0.50	6.003	0.062	1.03	1.84	1.71	0.0010	1.2170	0.0070	0.58	0.013	5.990	0.075	1.41
7.0	1.210	6.065	1.219	0.009	0.74	5.956	0.109	1.83	1.84	1.69	0.0012	1.2202	0.0102	0.84	0.015	5.941	0.124	2.08
8.5	1.210	6.065	1.217	0.007	0.58	5.986	0.079	1.32	1.84	1.71	0.0010	1.2180	0.0080	0.66	0.013	5.973	0.092	1.54
10.0	1.210	6.065	1.218	0.008	0.66	6.005	0.060	0.99	1.84	1.75	0.0008	1.2188	0.0088	0.73	0.010	5.995	0.070	1.17
11.5	1.210	6.065	1.216	0.006	0.50	6.037	0.028	0.46	1.84	1.81	0.0002	1.2162	0.0062	0.51	0.002	6.035	0.030	0.50

Table 13. Fuel Rod Dimensional
Changes (Corrected for
Hydrogen Loss)

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using corrected data from Table 13, shows the directionality of fuel rod swelling, and that swelling was anisotropic with diametral growth greater than axial. One exception to this is indicated by the density sample at 1-in., which shows axial growth greater than diametral. Similar end effects have been noted in the NAA 115-1 irradiation experiment.⁽²⁵⁾ The fact that this behavior was not seen at the bottom of the fuel rod may be due to axial compression caused by the column loading (vertical alignment).

Fuel metallography specimens, obtained by cutting 1/4-in. -thick wafers from density specimens 3, 4 and 5 (see Figure 25), were mounted so that cross-sections 5-1/8, 6-5/8 and 10-1/8 in. from the top could be inspected. Metallographic preparation included attack-polishing with a Linde B-H₂O slurry and a HNO₃-HF-H₂O mixture followed by electro-etching in oxalic acid.⁽²⁶⁾

Samples at 5-1/8, 10-1/8 in. from the top of the fuel rod, with respective hydrogen contents of 1.73 and 1.75 H/Zr, showed epsilon phase zirconium hydride only.⁽²⁷⁾ The sample at 6-5/8 in. from the top, with hydrogen content of 1.70 H/Zr, showed delta plus epsilon phase zirconium hydride, as indicated by distortion in the twins that comprise the epsilon banding. Figure 29 presents a photomicrograph of the fuel at 5-1/8 in. No microcracking, structural damage, or increase in porosity over the unirradiated structure was noted.

Absolute burnup analyses were obtained by three methods: isotopic uranium content by mass spectrographic analysis, radiochemical determination of Cs¹³⁷ activity, and heat transfer calculations based on in-pile temperature measurements. Data so obtained are presented in Table 14, and show good agreement, except for one mass spectrographic value which is apparently in error. This value was not included in the average, determined to be 0.067 metal at. %, or 2.52×10^{19} fissions/cc.

Gamma scans and autoradiographs were taken to augment the absolute burnup determinations. Gamma scans were taken by counting the Zr⁹⁵ gamma peak (0.72 Mev) with a scintillation counter. An axial gamma scan of the entire fuel rod, Figure 30a, shows peak activity at the center of the rod decreasing in a smooth and continuous manner with increasing distance from the center. The increased activity at each end is due to nuclear end effects. Radial scans were

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CRD (GP-1)

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a. Etched Sample From End of Fuel Rod Before Machining (Epsilon Phase Hydride Throughout Sample. Relatively Small Size Probably Due to Hydriding Effects)

CRD (GP-1)

b. Etched Sample 5-1/8-in. From Top of Irradiated Fuel Rod (Epsilon Phase Hydride Throughout Sample. Grain Size Only Difference in Pre- and Post-irradiation Microstructure)

CRD (GP-1)

44-2-5

Figure 29. Microstructure of Unirradiated and Irradiated Uranium-Zirconium Alloy Hydride

a. Summary

b. Radial Gamma Scan 3-5/16 in. From Top of Rod

c. Autoradiograph at 4-3/16 in. From Top of Fuel Rod (Numbers Represent Normalized Densitometer Readings)

Figure 30. Gamma Scans and Autoradiographs of Burnup Data

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TABLE 14
FUEL BURNUP ANALYSIS

Method	Location From Top of Rod (in.)	Burnup (metal at. %)
Heat Transfer Analysis	6	0.080
Mass-Spectrographic	1-7/8	0.071
	5-1/2	0.140*
	8-7/8	0.058
Radiochemical	2-1/8	0.063
	5-1/8	0.068
	8-3/4	0.064
Average	-	0.067

*Value apparently in error; not included in average

taken across the face of samples intended for radiochemical burnup analysis. These scans, typified by Figure 30b, show activity peaking at the surface of the wafers, followed by a rapid drop-off to a level area in the center of the wafer.

Autoradiographs were taken by placing the metallographic samples on a glass photographic plate which was affected in proportion to the activity across the sample. A typical autoradiograph, shown in Figure 30c, shows lighter in areas of increased burnup, and verifies the burnup distribution indicated by the radial gamma scans.

All of the relative burnup data were normalized to a peak burnup of 0.08 metal at. % and are shown in Figure 30a with the absolute burnup data. All of the data show very good agreement, both in form and magnitude, except for the single high mass spectrographic data point, which must be assumed to be erroneous.

VI. DISCUSSION

The NAA-82-1 irradiation experiment was intended to be a proof test of a reference SNAP 2 reactor fuel element under simulated 125 kw_t SNAP reactor conditions. The range of peak fuel temperatures representing these conditions would be 1500 to 1525°F. The recommended peak fuel operating temperature for NAA 82-1 was 1507°F, based on cladding creep and ceramic coating adherence considerations. Peak fuel temperatures could not be directly verified because in-fuel thermocouples were not used, since inclusion of an in-fuel thermocouple would result in a non-reference fuel element. However, the heat transfer analysis showed that the desired peak fuel temperature would correspond to a cladding temperature of 1300°F during nominal power operation; therefore, cladding thermocouples were used to control fuel element temperature. The experiment operated in the MTR for 23 full reactor cycles, during which the reactor was at full power for 9088 hours. Cladding temperatures were maintained within $\pm 50^\circ\text{F}$ of the 1300°F design value for 8479 hours, representing 93.5% of the time when the reactor was at full power. Both the time of full reactor power and the time within $\pm 50^\circ\text{F}$ of the design temperature fell within 4% of the design operating time of one year (8760 hours).

The predicted temperature profile along the length of the fuel element was verified in-pile by the cladding thermocouple readings (see Figure 6). The temperature gradient along the top half of the fuel element was moderated by thermal mixing of the NaK due to convection currents.

Peak fuel temperatures were predicted using a heat transfer model based on the as-built condition of the fuel rod, and irradiation-induced changes could not be strictly accounted for until the post-irradiation examination was made. In addition, the complex interaction of effects due to temperature gradients, hydrogen redistribution, fuel swelling and other factors represented an area of uncertainty. The FUSAK⁽¹⁴⁾ computer code was developed while this experiment was in operation in the MTR and was used to predict the effects of these factors, as described in Section III-E-8. FUSAK incorporated the best available models and equations, including a revised correlation for fuel swelling derived by Miller;⁽¹⁶⁾

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$$\frac{\Delta V}{V} = B + 1.2 \times 10^6 B^2 \exp\left(\frac{-22,600}{T}\right) \quad \dots (11)$$

where

B = burnup (metal at. %)

T = average peak operating temperature (°R)

Table 15 presents selected data calculated using FUSAK and some of the post-irradiation measurements. The assumption of no degradation in hydrogen barrier quality most nearly predicts the observed hydrogen loss from the fuel rod.

TABLE 15
COMPARISON OF FUSAK PREDICTIONS AND FUEL PERFORMANCE

	FUSAK Values			Measured Values	
	Hydrogen Loss (%)	Peak Fuel Temperature Initial/Final (°F)	Peak Diametral Swelling (mils)	Peak Fuel Swelling (mils)	Hydrogen Loss (%)
Nominal Case (No change in hydrogen barrier quality)	2.31	1549/1556	0.65	9.0	4.07
Optimistic Case (Improved hydrogen barrier quality)	1.46	1548/1550	0.64	-	-
Pessimistic Case (S8ER type hydrogen barrier damage)	14.6	1549/1690*	0.79	-	-

*Beta phase zirconium hydride was calculated to be present in this case after 5747 hours of operation due to excessive hydrogen loss.

This assumption is supported by the apparent good ceramic condition observed in the hot cell. However, some damage to the coating was sustained (see Figure 23) due to fuel rod swelling and mechanical interference between fuel and cladding (the fuel element was fabricated with no axial fuel-to-cladding gap). The close agreement between predicted and observed hydrogen loss indicates that the coating damage was not great. Hydrogen loss from this fuel element

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was less than would be expected for an element operated in a SNAP reactor, because the operating temperatures within ~ 1 in. of each end of the element were lower than the reactor design conditions. On the other hand, the thermal treatment of this element was substantially more severe than would be expected for a SNAP reactor fuel element, due to the large temperature gradients at the ends of the element, and particularly due to the large number of reactor scrams (101) and thermal transients (205) endured.

The Miller equation (Equation 11), as used in FUSAK, underestimated the peak radial fuel swelling in this experiment by a factor of 13. Therefore, the end-of-life temperatures calculated by FUSAK were unrealistically high because the fuel had, in fact, sustained irradiation swelling sufficient to permanently deform the cladding. It is clear that fuel swelling would decrease the fuel/cladding gap and thereby reduce the fuel operating temperature. If the fuel had been in perfect contact with the ceramic coating and there were no thermal resistance between the fuel and cladding, the peak fuel temperature would have been reduced to 1442°F . It appears, however, that the fuel was most probably not in intimate contact with the ceramic coating while operating at design temperatures, although it certainly was in contact when the reactor was shutdown (after fuel swelling consumed the 0.004 in. design gap). At operating temperatures, the diametral thermal expansion of the cladding was calculated to be 0.0026 in. greater than the corresponding expansion of the fuel rod. The maximum elastic increase in cladding diameter due to fuel rod interference at room temperature was calculated as follows,

$$e_d = d \times \epsilon_{\max} = d \times \frac{\sigma_{\text{yield}}}{E} = 0.0018 \text{ in.} \quad \dots (12)$$

where

$$\sigma_{\text{yield}} = \text{yield strength} = 45,000 \text{ psi}$$

$$E = \text{modulus of elasticity} = 31.7 \times 10^6 \text{ psi}$$

$$d = \text{cladding diameter} = 1.25 \text{ in.}$$

$$\epsilon_{\max} = \text{maximum strain}$$

During a reactor scram, the cladding would tend to shrink onto the fuel rod and, if the interference due to fuel swelling were great enough (i. e., greater than

0.0018 in.), permanent deformation of the cladding would occur. When the fuel element was returned to its operating temperature, the differential thermal expansion would relieve the cladding-to-fuel-rod interference (due to elastic strain) and the hydrogen gap would open to 0.0008 in. Fuel swelling at temperature would then tend to close this gap. If the peak diametral fuel swelling occurred linearly with time over the 8479 hours at operating temperature, the fuel diameter increase produced during the longest uninterrupted reactor run of 450 hours would be 0.0005 in. , closing the hydrogen gap to 0.0003 in. Although the temperature rise across such a small gap would be minimal, it is significant that the fuel swelling was not radially restrained by the cladding. A similar argument shows that the fuel rod was not axially restrained while at operating temperature (i. e. , while fuel swelling was taking place), although axial fuel-to-cladding interference existed at room temperature.

The temperatures predicted by FUSAK for beginning-of-life are more realistic and indicate that a temperature correction to the original heat transfer analysis should be made to account for hydrogen redistribution and other effects. However, the FUSAK calculations overestimate fuel temperatures because axial heat flow is ignored (FUSAK is a one-dimensional code). When the appropriate correction from FUSAK is factored into the calculations from the two-dimensional analysis, the initial peak fuel temperature corresponding to a clad temperature of 1300°F is 1522°F. Figure 31 illustrates the probable variation in peak fuel temperature during operation. Starting at 1522°F, the peak temperature decreased uniformly as fuel swelling reduced the hydrogen gap until the cladding was contacted during a scram. Thereafter, fuel temperatures remained essentially constant at a minimum value of 1450°F (0.0008 in. gap). The time of fuel/cladding contact was calculated assuming linear peak fuel swelling with time at temperature. Peak fuel swelling was 0.009 in. , the as-built diametral fuel/cladding clearance was 0.004 in. , and the total time at operating temperature was 8479 hours. Calculated cladding contact occurred after 3768 hours at temperature.

The peak cladding deformation was 0.005 in. or 0.4% $\Delta D/D$. This measured value can be compared to calculations of 0.2% strain, using early INOR8 creep

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Figure 31. Probable Peak Fuel Temperature vs Operating Time for Constant Cladding Temperature of 1300°F

Figure 32. End-of-Life Hydrogen Content and Operating Temperature

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rate data, and 0.75% strain using more recent data (see Section III-E-7). The observed cladding deformation corresponded to the fuel swelling profile of the fuel rod (see Figure 21) along the length of the element and indicated that fuel swelling, not thermal and hoop stresses, caused the cladding deformation. Although fuel swelling was greater than expected, cladding stresses were much less than allowed for in the stress analysis. Evidently, some annealing of thermal stresses occurred. In addition, fission gas release and the associated pressurization stresses were lower than allowed (an overallowance was made for safety). Hydrogen pressure was less than expected because of lower-than-expected fuel temperatures due to fuel swelling (see Figure 32). The peak cladding deformation of 0.005 in., together with the observed peak fuel swelling and the as-built axial fuel/clad gap, verified that fuel and clad were in intimate contact at room temperature. The axial cladding elongation of 0.008 in. (0.064% $\Delta L/L$) was small and could be due to creep or to elastic strain resulting from fuel rod interference. The fuel rod axial growth was 0.012 in. and, since it appears that axial fuel/cladding interference occurred (Figure 23), the axial gap in the two-piece end closure, between the cup and the end cap, may have been as much as 0.004 in. (see Figure 1).

All the fission gases released from the fuel rod were contained by the cladding. The measured quantity of the fission gas Kr^{85} released — $5.99 \times 10^{-3}\%$ — was substantially less than the predicted value of $3.42 \times 10^{-2}\%$. It would seem that either the recoil model for fission product release overestimates actual release or, more probably, that a significant amount of the recoil atoms pass through the hydrogen and penetrate into the ceramic coating.

Absolute fuel burnup measurements were determined by heat transfer calculations, mass spectrographic analysis, and radiochemical analysis of irradiated fuel. Relative burnup determinations were obtained from radial and axial gamma scans and autoradiographs. All indications from the various methods of burnup measurement show that the three mass-spectrographic data points are not mutually consistent but, since two of the mass-spectrograph measurements are in good agreement with the heat transfer and radiochemical determinations, it can be assumed that the third mass-spectrograph measurement taken at the location of peak burnup is erroneous, and is high by a factor of ~ 2 . Figure 30a compares all the burnup data with relative data normalized

to a peak burnup of 0.08 metal at. %. This normalization is somewhat arbitrary, and acceptable consistency among this data could be obtained by normalizing to slightly higher or lower burnups. However, it appears that the actual peak burnup was within 10% of the design value of 0.08 metal at. %. The axial burnup profile reflects the axial neutron flux distribution in the MTR, except for peaking at top and bottom due to nuclear end effects. The radial gamma scans show the effects of self shielding in the fuel. The highest fission rates occurred near the surface of the fuel, while the minimum burnup was along the centerline of the fuel rod. This distribution is a consequence of employing an external source of neutrons (the MTR) while the fuel rod acts as a poison. In a SNAP reactor, the fuel rod acts both as fuel and moderator; and the radial flux distribution is more nearly uniform, with some flux peaking at the centerline of the fuel rod. Circumferential variations in burnup were effectively minimized by the eccentric flux depressor.

A trace of the fuel rod surface following irradiation (see Figure 21) shows three distinct bulges. The central and largest bulge corresponded to the location of peak temperature and burnup. The observed peak swelling of 1.83 vol % exceeds the 0.16 vol % swelling predicted by FUSAK.⁽¹⁴⁾

The fuel swelling in this test appears to be more consistent with the behavior of the S8ER⁽²⁴⁾ and the NAA 115-1 irradiation experiment⁽²⁵⁾ than with the Miller correlation⁽¹⁶⁾ (Equation 11) which was programmed in the FUSAK code. The two satellite bulges at 3-1/2 and 10-1/2 in. from the top of the fuel rod represent an amplified fuel swelling effect (1 to 3 vol % increase) associated with zirconium hydride fuel, which operates with an effective H/Zr of 1.75 (near the $\Delta + \epsilon/\epsilon$ phase boundary.) Figure 32 shows the end-of-life hydrogen content and operating temperature of the seven fuel density samples plotted on an enlarged segment of the zirconium hydride phase diagram, and shows that four of these samples operated with an average H/Zr equal to or less than 1.75. Fuel swelling at the extreme ends of the fuel rod was enhanced by increased burnup due to nuclear end effects.

VII. CONCLUSIONS

The NAA 82-1 experiment was intended to expose a full-scale, reference SNAP 2 fuel element to 125 kw SNAP reactor conditions and thereby determine the performance of the fuel element in terms of suitability for use in SNAP reactors. The irradiation capsule met all of its design goals, since the fuel heat generation rate, peak burnup, neutron flux profile, cladding operating temperature, and irradiation time were all very near the design values. The 93.4% of operation time within $\pm 50^\circ\text{F}$ of design temperature was excellent, considering the frequency of reactor scrams. The experiment safety provisions limited the maximum overtemperature to 50°F above the operating range (75°F above design temperature). Both time-at-temperature and overtemperature conditions might be improved by employing an automatic servo-operated temperature control system.

The fuel element performance was excellent. Both the fuel rod and the cladding were intact and showed no gross irradiation damage effects. The ceramic coating was in very good condition in spite of fuel-rod-to-cladding interference, although some coating adhered to the ends of the fuel rod. The observed hydrogen loss of 4.0% was acceptable for a SNAP 2 reactor fuel element. The thermal treatment, including 101 reactor scrams, twice that many thermal shocks, and large temperature gradients over the ends of the fuel element, were more severe than would have been experienced in a SNAP reactor, although the relatively cool end temperature may have reduced the hydrogen loss. Fuel swelling was sufficient to deform the cladding 0.4%. Cladding strain occurred during reactor scrams and fuel-to-clad interference probably did not exist at operating temperature due to differential thermal expansion. Cladding strain was insufficient to cause fuel element interference in a SNAP 2 reactor. Fuel swelling was substantially greater than predicted on the basis of NAA 53⁽¹⁶⁾ performance, but was similar to fuel swelling behavior observed in S8ER fuel elements⁽¹⁷⁾ containing moving $\delta \rightleftharpoons (\delta + \epsilon) \rightleftharpoons \epsilon$ phase boundaries.

The performance of fuel rod and fuel element assembly was completely satisfactory for use under peak 125 kw SNAP reactor conditions.

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