ANCR-1015

Reactor Technology
TID-4500

ATR EXTENDED BURNUP PROGRAM

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AEROJET NUCLEAR COMPANY

Date Published -- November 1971

PREPARED FOR THE U. S. ATOMIC ENERGY COMMISSION
IDAHO OPERATIONS OFFICE
UNDER CONTRACT NO. AT(10-1)-1375

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ACKNOWLEDGMENT

This document covering the initial phases of a program with such a large potential savings could not have been completed in the allotted time without the complete cooperation and extra effort put forth by personnel in the Applied Metallurgy, Critical Facilities, Nuclear Research and Reactor Analyses Sections of the Nuclear Technology Division, the Thermal and Fluid Dynamics and Applied Structural Mechanics Sections of the Engineering Division, Special Services Reproduction Facilities, and the Engineering Document Section.

The long nights and weekends of work by the following people are gratefully acknowledged.

E. F. Aber
R. G. Ambrosek
A. W. Brown
T. J. Conrads
B. E. Ellingford
G. N. Fillmore
W. C. Francis
M. J. Graber
G. W. Gibson
G. O. Hayner
R. R. Hobbins
H. L. Magleby
H. L. McMurry
L. G. Miller
B. L. Sherry
A. C. Smith

ABSTRACT

The objective of the ATR extended burnup program is to minimize the fuel cost per MWd within the constraints provided by the existing fuel element design, and the current inventory and procurement schedule. Fuel element structural considerations currently limit the maximum allowable fuel burnup to 1.5×10^{21} fissions/cc. Scoping calculations indicate that extension of the burnup limit to 1.8×10^{21} fissions/cc and proper blending of standard and graded fuel (7F) elements would minimize fuel costs by allowing the ATR to operate to the reactivity limit in either balanced or unbalanced mode with the presently envisioned experimental programs. This document contains all supportive information relative to the feasibility and safety of operating to 1.8×10^{21} fissions/cc and proposes the extended irradiation of two fuel elements, fabricated by National Lead Corporation, during Cycles 8A, 8B, and 8C.

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I. PROGRAM SUMMARY

The ATR fuel design basis was a 17 day, 4250 MWd cycle. On the basis of ex-reactor corrosion tests, this was the anticipated maximum cycle life as limited by thermal stresses resulting from the thermal insulating effects of the retained corrosion product. The fuel element development program demonstrated the fuel stability to 1.5×10^{21} fissions/cc (approximately twice the design requirements). The Core I corrosion test program determined that the thermal effects of the corrosion processes were approximately half of the predicted value. As a result of the low corrosion rate and an extension of the ATR SAR⁽¹⁾, the balanced power demonstration core and the 7 subsequent operating cycles were operated to a limiting burnup of 1.5×10^{21} fissions/cc. This results in 6830 - 8750 MWd cycles, depending on utilization of the 7F, graded fuel elements, with an annual fuels fabrication* savings of 1.5 to \$2,000,000.

With a burnup limit of 1.8×10^{21} fissions/cc and proper blending of standard and 7F, graded fuel elements, the ATR could be operated to the reactivity limit in either the balanced or unbalanced mode. With current sponsor requirements, this would result in an additional \$500,000* annual savings in fuel fabrication costs.

The ATR extended burnup program^(2,3,4,5) was developed to demonstrate the feasibility of operating to elevated burnups. As part of the program, it is proposed to obtain initial, full size ATR fuel element irradiation data up to 1.8×10^{21} fissions/cc by allowing two test elements to operate in cycles 8A, 8B, and 8C. The remainder of the 8A fuel charge will be replaced prior to cycle 8C. The test elements will be 7F, graded fuel elements, fabricated by National Lead Corporation. These elements were selected because:

* This does not include fuel charges or increased productivity resulting from the reduction in refueling shutdowns.

1. The National Lead elements contain 4 to 10% voids⁽⁶⁾ distributed throughout the fuel core and are better able to retain the fission gases and, therefore, less apt to blister.

2. The graded fuel geometry causes the maximum fuel burnup to occur where calculated burnup values are more precise.

To demonstrate the feasibility and safety of this irradiation program, the following supporting information has been provided:

1. Fuels Stability with Extended Burnup

The blister resistance of the commercially fabricated fuel plates in the high burnup (1.5×10^{21} fissions/cc) elements from Cores II and III was similar to the sample fuel platelets that have been tested in the ETR. The minimum blister temperature was 750°F in a U_3O_8 fueled element and 900°F in a UAl_x fueled element (Section III.A.3.4 and Table III-6). Detailed metallurgical examination of these same elements revealed nothing to suggest that the UAl_x fuel had reached its limit (Section III.A.3.3). Extrapolated performance evaluation of nonprototypical platelets irradiated to burnups up to 2.56×10^{21} fissions/cc indicates desired safety margins for the ATR fuels at similar burnup levels⁽⁷⁾. Canal examination of prototypical platelets irradiated to burnups up to 2.3×10^{21} fissions/cc revealed no plate warping, buckling, gross swelling or blistering (Section III.B). Hot cell examination of these first prototypical platelets (irradiated to 1.8×10^{21} fissions/cc) will begin the week of April 12, 1971.

2. Test Element Quality

The two test elements were selected from the 7F, graded fuel elements fabricated by National Lead Corporation because of the higher void content [4 - 10 %⁽⁶⁾] in these elements. The elements have a minimum clad thickness in the dogbone region of 0.0075 in. (Section VII.A.3). The dimensional tolerances, fuel content and fuel homogeneity are better than minimum specification requirements (Section VII.A.1 & 2).

3. Burnup Prediction Tolerances

The reactor operating mode during the test program (Section VI), as evaluated by the procedures detailed in Section II.A, was selected to produce a maximum nominal burnup of 1.8×10^{21} fissions/cc, in each of the test elements, at the end of cycle 8C. Because of localized perturbations caused by the side plate vents, an area approximately 1/2 by 2 in. will exceed this burnup value by an average of 5%, i.e., the burnup will be about 1.89×10^{21} fissions/cc in this area. With an estimated 95% confidence, the maximum burnup at any point will be 2.1×10^{21} fissions/cc (Section II.E). The largest component in the burnup uncertainty (9%) is associated with post-irradiation determination of the actual burnup achieved. Since the same analytical techniques were used in determining burnup in the experimental irradiation demonstrating the fuel stability, this error component will be self cancelling as the number of determinations is increased.

4. Safety

The two failure modes identified with extended burnup are (1) buckling from axial compressive loads developed either from thermal stresses or irradiation growth stresses, and (2) blistering due to excessive fission gas buildup. The thermal stresses in the experimental elements will be within the current stress criteria (Section IV.C) and the axial compressive stresses resulting from irradiation growth will not differ significantly from stresses developed at 1.5×10^{21} fissions/cc (Section V.B). The failure hazards analysis (Section V.C) has identified no mechanisms whereby any localized failures (should they occur) can propagate. The maximum credible fission product release associated with the extended burnup results in insignificant direct personnel exposure. The fission gas removal via the degassing system can be conducted with favorable environmental conditions. Degassing at 10^5 curies/day, the maximum rate allowable within the operating limit, under fumigation conditions will result in maximum TRA exposures of 40 mrem per 8 hour day. Under these same conditions, maximum off-site dosages will be within the AECM-0524 guidelines (Section V.C).

References:

- (1) W. L. Ginkel to C. M. Rice, September 12, 1969, private communication.
- (2) C. M. Rice to W. L. Ginkel, November 9, 1970, private communication.
- (3) W. L. Ginkel to C. M. Rice, January 8, 1971, private communication.
- (4) C. M. Rice to W. L. Ginkel, February 3, 1971, private communication.
- (5) C. M. Rice to W. L. Ginkel, February 26, 1971, private communication.
- (6) C. M. Rice to W. L. Ginkel, June 29, 1970, private communication.
- (7) C. M. Rice to W. L. Ginkel, September 22, 1969, private communication.

II. CALCULATIONAL PROCEDURES FOR PREDICTING POWER DISTRIBUTION AND BURNUP

A. Models and Procedures

The power distribution and burnup calculations are made in two spatial dimensions and four energy groups. The IBM 360/75 version of PDQ-7⁽¹⁾, a neutron diffusion depletion code, is currently used for these calculations. The Core II calculations utilized an earlier version of this code, the TURBO-44 code⁽²⁾, written for the IBM 7044 computer. The two codes produce essentially identical results for identical problems. Cross section input to these codes is generated by the PHROG⁽³⁾ fast spectrum code and the TAMP⁽⁴⁾ and INCITE⁽⁵⁾ thermal spectrum codes. The three fast groups generated by PHROG have the following energy structure: Group 1 from 0.821 to 10 MeV, Group 2 from 5.53 KeV, and Group 3 from 0.532 eV to 5.53 KeV. The thermal group constants generated by TAMP cover the energy range from 0.532 eV down to zero. The PHROG cross section library as used for the ATR calculation is based on the GAM-1⁽⁶⁾ and RBU⁽⁷⁾ libraries and modifications to the library produced internally by Phillips Petroleum Company⁽⁸⁾. The cross section library used by the TAMP code is based on the TEMPEST⁽⁹⁾ library as modified internally by Phillips Petroleum Company. The INCITE library is based on the GATHER-II⁽¹⁰⁾ library with modifications.

The PDQ calculations are made using either 1/4 or 1/2 core descriptions with reflective boundaries according to the symmetry present in the core for the particular calculation. Thus the Core II and Core III calculations were made utilizing 1/4 core geometry with 19600 mesh points. These core calculations are in two dimensions with the vertical dimension accounted for by taking suitable averages of vertical composition variations, adjusting the critical configuration multiplication factor as fuel burnup proceeds to account for non-uniform vertical burnup and, finally, applying suitable vertical maximum to

average factors to the resulting power densities and fuel distributions in order to arrive at the reported midplane values. The diffusion theory constants used in the core region were obtained from PHROG and TAMP by averaging the microscopic cross sections over spectra obtained from buckled infinite medium calculations utilizing the homogenized fuel element concentrations. These microscopic cross sections are then mixed in PDQ to obtain fuel region constants according to the isotopes present in each mesh rectangle. Self shielding factors for the thermal group are determined by the S_n portion of the TAMP code. The side plate constants are obtained as nonburning macroscopic constants and are averaged over the spectrum determined for the homogenized fuel element. Materials in the flux traps and in the reflector are averaged over spectra which are most appropriate to the surroundings. For all compositions the Mixed Number Density (MND)⁽¹¹⁾ model was used for the thermal group constants. Hafnium in the drums, neck shims, and experiment shrouds is treated in a special manner.

The usual methods of treating heavy absorbers in diffusion theory require either a logarithmic boundary condition to be applied at the surface of the absorber, which results in a method which is absorber surface area dependent (and in fact is not available in PDQ-7), or in a blackness theory type of calculation which again turns out to be either surface area dependent or requires an inordinate number of mesh lines within the absorber region. Neither of these limitations can be tolerated in ATR calculations. Therefore, the method adopted involves matching a few group diffusion theory calculation to a multigroup transport calculation in a manner which gives absorber constants which are primarily volume dependent and depend only slightly upon surface area and number of mesh lines. Briefly this method involves establishing a one-dimensional model

around the absorber region then making a multigroup transport calculation for the model. A few group diffusion calculation is then set up for the model and the diffusion coefficient in the absorber region set to a large value. The absorption coefficient is then adjusted until the total absorption is the same as in the transport calculation. This method is used for groups three and four in the hafnium regions. There has been considerable burnup of the hafnium in the ATR since initial startup, and this burnup is not accounted for in the calculations except by adjusting the desired critical multiplication factor.

The above basic calculational tools are used in the following manner. First, when the experiment description is received, the experiment is mocked up in as close a manner as possible in an R-Z description where the experiment is surrounded by its shroud, pressure tube, flux trap filler, and then the ATR fuel.

The hafnium constants used for the shroud are dependent upon the fraction of hafnium in the shroud at each vertical section, however, no adjustment is made for the type of experiment present. It is felt that this lack of adjusting hafnium and experiment constants for the type of experiment present may have relatively large effects on experiments but only minor effects upon the fuel elements. However, this point has not been investigated thoroughly. Also, no adjustments are made in the hafnium shroud constants to account for burnup of hafnium from cycle to cycle.

The start of life R-Z problem is then run, and flux averaged microscopic cross sections are obtained over the experiment and the shroud for inclusion in the ATR core problem. If experiment or portions of the experiment are to be included in the following cycle and burnable materials are present, a one step depletion is run to obtain the approximate depleted atomic densities for the start of the next cycle.

An R-Z model representative of the core is then run for the full cycle using the power densities representative of the cycle and utilizing the same time steps to be employed in the cycle calculations. A one-dimensional radial calculation is then run on the R-Z model using an average vertical power and average experiment constants. The mismatch in multiplication factor of these two problems is then used to adjust the desired critical multiplication factor as a function of burnup. Also, the calculations do not include directly the hafnium burnup or the build up of poison in the beryllium reflector. As hafnium has burned up in the neck shims it has been necessary from time to time to add fixed hafnium shims to the central H holes in order to hold the power down in the center lobe. This has resulted in a net shift in reactivity hold-down to the center and an increase in total hold-down due to hafnium. To partly compensate for this, corrections are made to the critical multiplication factor at each time step to account for the extra hafnium poison. This correction along with the initial critical configuration is determined by the ATR critical facility and is used to adjust the initial multiplication factor. The hafnium burnup correction results in correct drum positions at the start of a cycle, but an increasing mismatch in position as the cycle progresses.

After the experiment constants have been computed and the desired critical multiplication factor for each time step determined, the burnup calculations for the core are begun. The calculations are run at a power corresponding to an average plane in the reactor and the neck shims and drums are adjusted to obtain the required power split and the desired multiplication factor. A further adjustment, as described below, is made to the desired critical multiplication factor to obtain the desired factor at each step. The flux distribution is computed at the start of each time step and this distribution is held constant during the time step and is used to deplete the fuel in the core. In

actuality, the flux will be changing during the time step because of the movement of the drums and neck shims. To compensate for the flux change during a time step, an intermediate multiplication factor is determined from the critical multiplication factors computed previously. This intermediate factor is chosen in order to give control positions intermediate to those which would be required at the start and at the end of the time step.

At each time step, the midplane power distribution is computed from the PDQ power distribution results by applying the vertical midplane to average factor determined by the initial R-Z time dependent calculations. The midplane distribution of total fissions is also determined at each time step by applying a ratio of time-integrated midplane power to time integrated average power, obtained from the R-Z calculation, to the fission distribution computed by the X-Y problem.

When the calculations are complete, the power distributions are examined to determine the controlling fuel plate from a heat transfer standpoint and the plate which has experienced the maximum fission density.

B. Precision of Power Distribution Predictions with New Core Conditions

To assess the precision of the calculated power distribution, ATR critical facility data^(12, 13) were compared to calculated data where the calculations utilize the same control configuration as the ATRC. Since normally calculations are not made on the ATRC, the conditions of the calculations were slightly different than those in the ATRC. Thus, the core operating temperature was used rather than the ATRC canal temperature and all loop experiments were at operating temperature in the calculations. Also, the control span of the ATRC hafnium drums is

slightly different than the ATR. Four sets of comparisons were made, two on Core II and two on Core III. On each core, the comparison was made at clean critical and at xenon equilibrium with the desired power split. The xenon equilibrium conditions were simulated in both cores by adding borated plastic tapes to the ATRC core in a uniform distribution, and a uniform boron concentration was added to the fuel regions of the calculations where the total boron was the same as that added in the critical facility. The calculations for this comparison were made by PDQ-7 in 1/2 core geometry for the SW 1/2 core since more detailed specific power data were measured in the ATRC in this half core than in the rest of the core. The comparison of lobe powers for the four cases is shown in Table II.B.1 along with the control configuration used. In each case, the total core power is normalized to 250 watts. Since the drum positions in the calculations must be varied in five-degree increments, the exact ATRC positioning could not be accomplished.

The measured versus calculated element power is shown in Figures II.B.1 through II.B.4 for the four cases. The percent difference shown in the above table and figures is computed as:

$$\frac{\text{Calculated Value} - \text{Measured Value}}{\text{Measured value}} \times 100$$

The gross power distribution for these cases duplicated the measured distribution within 3% for lobe power and within 7% for element power.

Detailed midplane distribution of specific power was measured in core positions 26 through 35 for each of these 4 cases. In each of these core positions, specific power distributions were measured in channels 2, 6, 11, 15, and 19. The comparison of calculated and measured specific power is shown in Figures II.B.5 through II.B.14 for channel 15 in the clean, Core III case. For these figures, the power in each lobe has been normalized to 50 watts and the

units of specific power are 10^{-3} watts/gram of U^{235} . The ordinate values are the percent distance from the left side of the fuel where the left and right side of the fuel are taken to be the lines extending through the ends of the meat on the left and right sides of plates 2 through 18. The comparison for channels 2, 6, 11, and 19 of position 33 in the clean, Core III case is shown in Figures II.B.15 through II.B.18. The comparison for channels 2, 6, 11, 15, and 19 of position 33 in the clean Core II case is shown in Figures II.B.19 through II.B.23. Table II.B.2 shows the ATRC measurement position for each channel and the corresponding value of percent distance from the left side. The large wands are used in channel 19 of the standard elements and the small wands are used in channel 19 of the 7F elements.

As is noted on all of the detailed comparisons, the agreement between calculated and measured specific power is very good from about 20 to 80% of the distance from the edge of the channel. However, at the ends of the channels, the calculated power is much lower than the measured. To determine how well the calculated power distribution agrees with the measured conditions for the four cases under consideration, all of the measured data should be employed. However, due to the limited amount of time available for this study, only a few of the plotted comparisons were studied in detail. The positions chosen were 28, 30, 31, 33 which are positions symmetric to positions 11 and 13 for which chemical burnup data are available.

The calculated points correspond to points of intersection of x and y mesh lines used to describe the core for the numerical solution. The points plotted were chosen to lie within the channel under consideration, however, it was necessary in many cases to pick points which were at a distance from the channel center line. Since there is a flux gradient from the outside to the

center of the fuel elements these off center points in the outer channels will lie in a higher or lower flux region than the channel center. Therefore, before drawing a curve through the calculated points these off center points were labeled by an "H" or "L" indicating that they probably lie in a higher or lower flux region. For the outer channels (2 and 19) this analysis should have been extended to account somewhat for the distance between the point and the outer edge of the fuel, however, this was not done and the points plotted for the outer channels show a larger amount of scatter than those for the inner channels. This scatter is caused by the "sawtooth" fuel edge used in the calculational model to describe the curved fuel boundary by the x and y mesh lines.

The curves through the calculated points were used to determine the ratio of measured to calculated specific power at each of the ATRC measurement points. These ratios are shown in Figures II.B.24 through II.B.27. These ratios show that the calculated specific power for the center portions of the channels are within 10% of the measured values but the calculated values are as much as 40% low at the channel edges. The ratio plots for positions 28 and 33 show a marked tilt across the elements. To obtain an over all measure of the variation between measurements and calculations, the ratios for each channel were plotted on a graph as shown in Figures II.B.28 and II.B.29. The variation of the points about the lines plotted is about $\pm 5\%$ for the center portions of the channels and up to $\pm 15\%$ at the channel edges.

The large discrepancy at the channel edges may be due to: (1) poor spectrum representation in these regions - a better representation of the thermal spectrum variation such as the overlapping group method may be needed to

adequately compute the flux variation into the side plate regions, (2) the use of water temperatures corresponding to the ATR rather than the ATRC - this results in a slightly lower moderator density and thus fewer neutron thermalized in the side plate regions than is actually present in the ATRC, and (3) inadequate side plate region description used in the calculations.

There are also several possibilities that could cause the tilt across the element: (1) The cross section sets used for the fast energy groups are based on libraries over 10 years old. These libraries may be inadequate to describe the distribution of neutrons in the above thermal range. The neutron source to the thermal group would then be incorrect and the resulting power distributions might then show this tilt. (2) The cross sections used in the control elements may not be quite correct. (3) The approximations made to represent the vertical variation of the experiments may be too coarse or the error in the experiment and hafnium shroud cross sections may be too great.

C. Precision of Burnup Predictions with New Core Conditions

The total fission distribution has been measured in two elements which were irradiated in Core II positions 11 and 13^(14, 15, 16) and one element irradiated in Core III in position 13⁽¹⁷⁾. The distribution was measured by first removing the side plates then scanning each plate of the element with a gamma scan device to get the fission distribution. Finally, a number of 1/4 inch diameter punchings were taken from several of the plates and analyzed for their isotopic content. The gamma scan data was then normalized to the absolute value of fission density from the sample analysis to give the absolute fission density distribution (Section III.A.3.6).

The Core II and III depletion calculations were made in 1/4 core geometry with the power split adjusted at each time step. Calculations assumed each core was depleted continuously, that is the calculational model was kept at full power while the actual cores experienced many scrams and shut down periods to wait the decay of xenon. Thus, the calculational model assumes xenon equilibrium conditions, while in the actual cores a substantial amount of operational time was under non-equilibrium xenon conditions. Also, the Core II calculations were depleted to only 31 days and it was necessary to extrapolate the depletion data to the 35 day operating life. However, the burnup is nearly linear during this period so that the extrapolation is accurate. The vertical maximum to average factor applied to the Core II and III burnup was based on a slightly different R-Z model than was used for later cycles, including Cycle 8 calculations. If this Cycle 8 model had been used for Cores II and III, the calculated burnup would be increased by about 3%.

The comparison between the gamma scan data and calculated data for the element from position 13 of Core II is shown in Figures II.C.1 through II.C.4, the comparison for position 11 of Core II is shown in Figures II.C.5 and II.C.6 and the comparison for position 13 of Core III is shown in Figures II.C.7 and II.C.8. The error bars (2σ corresponding to a 95% confidence limit) quoted for the gamma scan data is dependent on the fission density. However, representative values for the three elements at their respective peak burnup values are 0.12×10^{21} for position 13 Core II, 0.04×10^{21} for position 11 of Core II and 0.35×10^{21} fissions per cm^3 for position 13 of Core III. Because of the large experimental error bars assigned to the Core III data, no attempt was made to assign an uncertainty to the calculated data from Core III. For Core II calculations, the 2σ limit for

the fission densities calculated for position 13 is 0.24×10^{21} and for position 11 was 0.15×10^{21} . It should be noted, however, that for the peak plate, plate 15, in position 13, the calculated fission density is within the experimental error associated with the measured values at all points. Also, the tilt across the element in position 13 is very nearly the same as the tilt noted earlier between calculated and measured specific power distribution. If the assumption is made that the tilt noted in the specific power distribution holds throughout cycle life, then the specific power data from the core could be used to correct the burnup calculation. This correction would improve the overall comparison but would increase the computed burnup at plate edges and the result would be errors of the order found for position 11 of Core II. However, the cause of the tilt in specific power ratio is not clear at this time and the assumption that the tilt holds throughout cycle life may well be incorrect. Therefore, the value of 0.24×10^{21} fissions per cm^3 is the recommended 2σ error bar to apply overall to these Core II and Core III calculations.

D. Calculated Effects due to Errors in Drum Position

As discussed in Section II-A, the burnup in the hafnium control elements and poison build up in the beryllium reflector are only approximately accounted for in the calculations by the adjustment of the critical multiplication factor. With this method of correction, the calculated control movements is too great. During Cycle 7, the magnitude of this difference amounted to about 20 degrees on the drums after 10 days operation.

To assess the effect on power distribution of this difference, a similar control imbalance was impressed on a time step of the Cycle 6 calculation.

Hafnium rods were added to the H holes in the center of the core and the cross section of some of the neck shims was reduced to simulate hafnium burnup. The drums were then moved to regain the approximate power split. The drums in the NW lobe were moved from 100 to 130 degrees, the drums in the SW lobe were moved from 65 to 80 degrees and the drums in the SW lobe were moved from 80 to 95 degrees. Table II.D.1 summarizes the lobe power and control positions for these two calculations and Figure II.D.1 shows the power generated in each element for the two conditions. Values of specific power were also generated for both cases for channels 2, 6, 11, 15, and 19 of each element. Ratios of specific power along each channel were then calculated and are plotted in Figures II.D.2 through II.D.4 where the values plotted are $100 \times \frac{\text{perturbed case} - \text{base case}}{\text{base case}}$. The greatest variation occurs in the channel around the outside of the core where the effect of the drum movement is greatest. The largest difference occurs in the SW lobe where the drums were moved from 65 to 80 degrees. The effect is only half as large in the NW lobe where the drums were moved from 100 to 130 degrees. The effect on the innermost channel of inserting the number 6 neck shim in the NW and SE lobes is very clearly shown in Figure II.D.4 where a 20 and 15% depression in specific power was attained. Although the effect on control positioning is quite large in some elements, the core "hot spot" is located in position 33 and the special irradiation elements will be placed in position 33 and the position symmetric to position 18. As seen in Figures II.D.2 through II.D.4, positions 18 and 33 are only slightly affected by either the drum movement or neck shim removal.

E. Burnup Uncertainty for Irradiation to 1.8×10^{21} fissions/cm³ in Position 13 or 33

The errors discussed under Section II-C for the Core II burnup distribution must be amended to take into account the error in drum positioning. This factor

is quite large for positions affected by the drum or neck rod movement. However, in the present situation only positions 13 and 33 need be examined since these positions will be used for the special high burnup tests. The plots given in Section II-D indicate that the drum positioning error for channel 15 of these two positions is less than 6%. If the power in the SE lobe had been normalized to the same values for the two cases examined, these plots would have indicated an error of about 4%.

Since the power in these positions is only slightly affected by drum and neck movement, the burnup in these positions will be a direct function of the lobe power history. To verify this Figure II.E.1 shows the computed burnup, at the point of peak burnup in these elements, versus integrated lobe power. The calculated values from Cycles 6, 7, and 8 and Cores II and III are all plotted. The Core II and III points are plotted in two ways on this graph: First, the solid points were obtained in the same manner as reported previously in Section II.A and the open points were obtained using the Cycle 8 method. As is shown on the graph, all points computed by the Cycle 8 method lie very near to the same line. Since these sets of calculations represent a variety of conditions of core power splits, the completed burnup in positions 13 and 33 is a function only of the power generated in the corresponding lobe and is independent of core power split and nearly independent of control movement. The maximum burnups measured in the Core II and III elements operating in these positions are also shown (with analytical burnup error margin) in Figure II.E.1 for additional verification.

The flux trap filler conditions for Cycle 8 are the same as in Core II, and the lobe experiment loads for the two cycles are not significantly different. The "B" hole experiments in B.3, B.4, and B.7 are not significantly different from each other or from the experiment in B.3 of Core II. Therefore, the burnup distribution measured for Core II should be applicable to Cycle 8 calculations

at the comparable time point, and the burnup measured in position 13 of Core II can be used to adjust the burnup computed for Cycle 8 in positions 13 and 33. With the Cycle 8 method of computing burnup, the maximum burnup in Core II, position 13 is 1.49×10^{21} fissions/cc at the edge of plate 15. This is well within the quoted gamma scan error limits and for this point and no adjustments should be made to the Cycle 8 calculations for maximum burnup.

The side plate vents cause a slight localized perturbation in the fission rate⁽¹⁴⁾ and the integrated burnup. In the high burnup plate, this influence is limited to an area approximately 1/2 by 2 in. immediately adjacent to the vents. The average burnup within this area is increased by about 5% over the unperturbed value. The maximum increase is about 8%. This effect has not been included in the above analysis and because of the procedures utilized in the analytical burnup determination, this localized effect is not detected.

The 2σ uncertainty for the maximum burnup at this point should combine the 2σ uncertainty deduced from the calculated burnup shown in Figure II.E.1, and the 2σ uncertainty in the gamma scan correlated burnup determinations. The points plotted in Figure II.E.1 all lie within a band of about $\pm 0.04 \times 10^{21}$ fissions/cc and the gamma scan 2σ error bar extrapolates to about 0.16×10^{21} fissions/cc at a total of 1.8×10^{21} fissions/cc. Assuming a conservative value of 0.08×10^{21} fissions/cc for the 2σ limits on calculations and correlating the two errors by taking the square root by the sum of the squares gives an overall 2σ uncertainty of 0.18×10^{21} fissions/cc for this point of maximum burnup.

In conclusion, the point of maximum in Cycle 8 high burnup elements occurs at the same point as in Core II when core symmetry is taken into account. When a value of 1.8×10^{21} fissions/cc is computed for this point, the analysis indicates that at a 95% confidence limit, the maximum burnup will be less than 1.98×10^{21} fissions/cc. The neglect of the effect of incorrect rod movement due to the approximation of hafnium burnup results in a conservative answer for this particular point, since the correction would lower the computed burnup. Inclusion

of localized effects from the side plate vents results in an increase in the maximum burnup in this location at the 95% confidence limit to 2.1×10^{21} fissions/cc.

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III. MATERIALS ANALYSIS

A. Post-Irradiation Examination of Cores II and III Fuel Elements

1. Introduction

During the startup operations of the Advanced Test Reactor (ATR), four loadings of fuel elements were used. These were designated Cores 0, I, II, and III. Core 0 was used for zero power physics studies to establish the relationship between the Advanced Test Reactor Critical Facility (ATRC) and the ATR⁽¹⁾. No fuel elements from this core were destructively examined. Core I was used for intermediate power operation where fuel element performance was evaluated under a number of contrived thermal-hydraulic conditions⁽²⁾. One instrumented fuel element from this core was examined. This element had been used for an experiment designed to determine the rate at which corrosion product oxide builds up on the heat transfer surfaces of the fuel plates under maximum nominal heat flux conditions.

The last phase of the ATR startup program was designed to demonstrate the high-power operational capabilities of the reactor⁽³⁾. Two cores were used for this. During the balanced power demonstration tests, Core II was used. Three elements from this core, two of which had been operated in peak power positions, were destructively

examined. The fuel elements in this core were operated for 35 full power days. This was twice the design life since it was originally planned that the fuel elements would have a life of 17 days⁽⁴⁾.

Core III was used for the final phase of the startup program. This core was used during the operation of the reactor for 27 days at 250 MW maintaining an unbalanced power division. In the unbalanced power mode of operation used during this demonstration, 40 MW of power were generated in two lobes of the reactor core, 60 MW were generated in two other lobes, and 50 MW were generated in the center lobe. Two fuel elements were destructively examined following this test run. After the successful completion of the startup operations, the reactor was placed into routine operation. At this time, the fuel element loadings were designated by cycle numbers starting with cycle number 1.

For the operation of the reactor with Cores II and III, three types of fuel elements were used. Examples of each of these types were given post-irradiation examinations. The fuel loadings and materials of construction used in each of the types of fuel elements are given in Table III-1. Operating parameters are given in Table III-2.

The standard U_3O_8 type element was the reference fuel system for the reactor⁽⁵⁾, and the first elements procured for the ATR were this type. However, during the design and construction of the ATR, fuel elements using powdered uranium-aluminide compounds ($UA1_x$) were developed⁽⁶⁾. Irradiation testing of miniature fuel plates

demonstrated that the plates containing UAl_x would tend to be more stable than those fueled with U_3O_8 ^(7,8,). Consequently, powdered uranium-aluminide is now the standard fuel for the ATR.

Thermal-hydraulic studies made on the fuel elements while the reactor was under construction revealed that in the hot spots of the reactor core the localized stresses on the fuel plates of standard elements would exceed the yield strength of the plates. In order to overcome this, a new fuel loading prescription was developed. The zone loaded elements made to this prescription were designated "7F" type elements. As indicated in Table III-1, three loadings of ^{235}U and boron are used in the fuel plate cores of the "7F" type elements. In the standard fuel elements, the weight of fuel and boron in a given volume of core is the same in each of the 19 plates.

Of the four core loadings in the ATR approach to power and power demonstration tests, the last two designated Cores II and III, achieved maximum allowable burnup on some of the elements. Selected elements from each core were therefore examined in the hot cell. The post-irradiation examinations were conducted for the following purposes:

- (1) To determine the distribution of fuel burnup in the plates of the elements.

- (2) To assess the stability of the fuel elements under actual operation conditions.

- (3) To compare the performance of full size commercially produced fuel elements with that projected from the testing of miniature fuel plates.

- (4) To determine, if possible, the safe operating limits of the current fuel element designs for the ATR.

2. Examinations Performed

Upon reaching the hot cells, the fuel elements were visually inspected under normal and oblique lighting, optically inspected at macro and micro magnifications and microconstituent analyses were made with the electron microprobe. Measurements were made of total plate lengths before and after disassembly, of plate thicknesses before and after chemical stripping, of oxide thickness and of plate volumes before and after stripping. Gamma scans were made to complement burnup analysis and samples were blister-tested.

It should be pointed out that not all of the examinations were made on every element or on all the plates of any one element. The plates to be used for a certain examination were selected on the basis of providing the best compromise between the needs of thermohydraulics, physics calculations, metallurgy, and budget considerations.

The most thorough examination was made on XA3G. The effort was slightly reduced for XA20G and A16D. Certain of the plates had complete plate thickness and oxide thickness surveys while on others only maximums were determined. Certain of the plates had complete gamma scan surveys while others had only centerline scans taken. Some metallography samples were taken for the purpose of oxide evaluation, others for fuel stability and still others for post-anneal blister observations. Burnup samples were selected from locations which cover the range of gamma activities for correlation with the gamma scan. The various examinations which were performed on the plates from each of the five fuel elements are indicated in Table III-3.

3. Results of Post-Irradiation Examinations

3.1 Summary of Results

The results of the post-irradiation examinations of the fuel elements from Cores II and III are summarized in Table III-4. In this table, the worst case data for fuel elements A172C, XA3G, XA8G, and XA20G are shown.

Examination of the data in this table reveals that in the areas of peak burnup the fuel plates as received in the hot cells were still within the pre-irradiation thickness tolerances, and that the maximum oxide thickness measured by eddy current techniques was 1.3 mils. The data also show that the areas of maximum plate thickness were not necessarily in the same location as the peak burnups, but, that even in these areas the plates still were within the pre-irradiation thickness tolerances.

The core swelling data are based on the differences in the plate thickness measurements over the fuel plate picture frames and fuel plate cores. The data obtained by this rather crude method would indicate that the 7% $\Delta V/V^*$ limitation on core swelling may have been exceeded in some areas of the plates. However, the favorable results obtained by the blister testing indicate that there was no tendency for breakaway swelling to occur.

More detailed information on the results of the post-irradiation examinations are given in the sections which follow.

*Since no measurable change in width and length dimensions were observed, $\Delta V/V$ is $\Delta t/t$.

3.2 Oxide Thickness Measurements

As indicated in Section III.A.2, detailed oxide thickness measurements were made on plates 1, 3, 5, 10, 15, 17, and 19 from XA3G, XA8G, and XA20G. Detailed oxide data was also obtained on plates 1, 2, 10, 18, and 19 from Al72C. The remainder of the plates were generally scanned for maximum oxide thickness only. The maximum observed oxide thickness readings on the convex sides of all of the plates measured in the examination of the Core II and III elements are given in Table III-5. Some measurements were made of the oxide film thickness on the concave side of the plates. These measurements proved to be unreliable, and are not reported in Table III-5. The 1/2 in. diameter eddy current probe used for the measurements was believed to be the source of the problem and a new probe with a smaller diameter is being developed. It can be seen that the maximum accumulation of oxide generally was in the range from 0.6 to 1.7 mils on the convex side of the fuel plates. A typical plot of oxide thickness buildup after 35 days in the ATR versus fuel plate surface temperature for plate 5 from XA3G is given in Figure III-1. Here the lower temperatures occurred at the top of the fuel plate and the highest temperatures occurred between the center and the bottom of the fuel plate. It is seen that the oxide thicknesses measured with the eddy current instrument are considerably greater than the oxide buildup predicted according to the Griess⁽⁹⁾ equation. It will be seen in Section III.A.3.3 (Metallography), that the total oxide thickness can be

divided into an inner and outer layer. The inner layer appears to correlate with the predicted values according to the Griess equation, however, when the outer layer is considered, the total oxide film buildup follows a typical pattern as seen in Figure III-2. This figure compares oxide thickness measurements made on the convex side of plates from XA20G employing the eddy current measurement device (1/2 in. probe) and the true range of oxide thickness measured from photomicrographs of mounted fuel plate sections. Metallographic examinations of the oxide films showed no significant difference in the average film thickness on the convex and concave sides of the plates.

There does, however, seem to be more oxide on the surfaces of the plates which face the inside water channels of the elements than on the convex sides of the plates which faced the outside water channel (plate 19). The measurements made in the canal of the oxide films on the convex sides of number 19 plates in these elements are in good agreement with the values reported in Table III-5. It was, of course, impossible to make measurements in the canal of the films on the inside plates of the elements.

The elements from Cores II and III had been stored under water in the ATR canal for over six months prior to examination. It is believed that the outside layer of oxide may have formed during this long storage period.

3.3 Metallography

Metallography samples were taken from the fuel plates to show various behavior effects on the microconstituents. Samples were examined for fuel stability, oxide buildup, blister initiation, and pimple formation characteristics.

Figure III- 3 shows typical fuel areas at intermediate and high burnup at 50X and 200X. The fuel appears to be sound at both burnup levels. The fuel was examined for cracking from excessive gas bubble agglomeration or fuel particle growth, but this was not observed.

Figure III- 4 shows typical oxide which forms on the surface of the fuel plates. It was found that the oxide is present in two layers. The inner layer is typically 60% of the total thickness and is cracked intermittently. The outside layer accounting for the remaining 40% of the oxide has more porosity but does not exhibit cracking. On XA3G, the inner layer of oxide as observed on these metallography specimens more nearly approximates the Griess correlation than does the total thickness. (See Figure III-5.)

When blistering occurs in the UAl_x plates as a result of the post-irradiation anneal tests, (Figure III- 6), cracks form simultaneously throughout the core at temperatures where the aluminum matrix would have very little strength. Figure III- 7 at 200X shows crack initiation in the matrix after the blister test. This suggests that the pressure of the fission gas collected in the recoil zones could not be restrained by the aluminum matrix at the high temperature. Similar crack initiation sites were observed in annealed sample fuel plates which were tested in the G-12 loop of the ETR.

The extensive reporting previously made of the formation of small pimples at the dogbone areas precludes the necessity to include great detail in this report. The metallographic results of the effort can be summarized by noting that "pimple material" (Figure III-8) was found in a U_3O_8 fuel element fabricated by Sylcor as well as in the UAl_x elements fabricated by AI and was associated with an opening in the fuel cladding which could allow stagnant water to be entrapped in the fuel core during irradiation.* It is believed that the "pimple material" which is composed of oxides of the core materials resulted from the corrosiveness of the dissociated stagnant water. In the UAl_x fuel plates, the opening in the cladding was associated with thin cladding over the dogbone. Since the National Lead fuel elements had metallographic quality control of this problem, it is expected that these elements do not have the thin cladding problem. National Lead fabricated fuel element number A025F was disassembled in the hot cell after irradiation in order to examine a possible pimple on plate number 13. It was found that the minimum cladding in this area was 9 mils, and that there were no "pimples" on this element.

Nothing was found in the metallography to suggest that the UAl_x fuel has reached its limitation.

*The opening in the cladding of the U_3O_8 element was caused by lack of bond which resulted in a large blister rather than a pimple.

3.4 Blister Testing

Blister testing was limited to the Core II fuel elements XA3G and A172C.

Dogbone areas of element XA8G were also tested. Blister samples (see Table III-6) were selected to cover a range of burnup 6.0×10^{20} fiss/cc to 15.0×10^{20} fiss/cc for UAl_x fueled plates and 6.0 to 13.0 for those using U_3O_8 . For this, portions of fuel plates were cut into blister test samples about 7 inches long by plate width. These were heated in air at $50^\circ F$ temperature increments for 30 minutes each. The plates were inspected after each increment until blistering was found to have occurred. Since, when blistering occurred, it was always at the high burnup area of the sample, these values of burnup were plotted against blister temperature (see Figure III-9), and compared with previously established blister test data.

It was found that the behavior of these commercially fabricated fuel plates was similar to sample fuel plates tested in the G-12 loop of the ETR. The higher w/o UAl_x plates out-performed the intermediate and low w/o UAl_x and the UAl_x compositions out-performed the U_3O_8 fueled plates. In every case, the fuel blister resistance was better than the previously established minimums. Samples taken from the dogbone regions from element XA8G were tested to $1100^\circ F$ ($593^\circ C$) without the formation of a blister.

3.5 Fuel Plate Thickness Measurements

A detailed gross thickness survey (before stripping) was performed on plates 1, 3, 5, 10, 15, 17, and 19 from XA3G and XA8G and plates 1, 2, 10, 18, and 19 from A172C. Generally, the remaining plates from these elements were surveyed only in the area of maximum thickness (20 to 30 inches from the top of the fuel plate). All thickness measurements were made by use of a dial indicator. On the average, the cumulative error in measurement by this technique was about ± 0.5 mil.

The maximum gross thickness readings obtained on the plates examined are contained in Table III- 7. In this table, it can be seen that the maximum thickness readings for plate 1, plates 2 through 18, and plate 19 were 82.0 mils, 51.5 mils, and 100.4 mils, respectively. These maximum thicknesses were randomly distributed on the plates, did not necessarily occur in high temperature or peak burnup regions, and were not of a sufficiently high magnitude to restrict flow significantly.

Reliable core swelling data has not been obtained from these thickness measurements as yet mainly because of the difficulties involved in reliably estimating the total plate thickness over the core prior to irradiation for the specific plate under consideration. This data is very important because 1 mil change in total plate thickness amounts to 5% core swelling. The possibility of obtaining reliable swelling information from this data is still being investigated.

Table III- 8 gives both pre-irradiation and post-irradiation total plate length measurements for plates 1, 2, 3, 17, 18, and 19 from XA3G. These measurements indicate that very little plate lengthening took place and that any swelling which occurred during irradiation increased the plate thickness rather than the plate length.

3.6 Burnup Analysis of Cores II and III Elements

The two basic methods employed to obtain measured information concerning fuel plate burnup in the elements examined from ATR Cores II and III consisted of: (1) analyzing chemical burnup samples taken at known locations in the fuel plates, and (2) performing detailed gamma scanning on certain fuel plates to establish the distribution of gamma producing fission products in these fuel plates. The results of both of these methods were then combined to obtain detailed data concerning the number of fissions of uranium atoms which occurred per cubic centimeter of fuel plate core. These data are presented in terms of contour plots which will be developed in the following sections.

3.6.1 Chemical Burnup Sampling

During the post-irradiation examination of the Core II and III elements, 1/4 inch burnup samples were punched from known locations in selected plates. Figure III-10 shows the burnup sample location plan for each of the Core II and Core III elements examined except A16D and XA20G. The "A" and "C" planes noted in Figure III-10 were nominally 3/8 inch from the edge of the fuel plate (after the fuel plates were machined free from the side plates). Both plate outlines sketched in Figure III-10 represent a fuel plate in the concave position. It can be seen that nominally 120 burnup samples (from each element) were taken from plates 1, 3, 5, 10, 15, 17, and 19 from XA3G and XA8G. Approximately 86 burnup samples were taken from plates 1, 2, 10, 18, and 19 from A172C. Those punchings that were selected (from the Core II and III elements) and sent to the Analytical Chemistry Branch of Idaho Nuclear Corporation* for

* Now Aerojet Nuclear Company (7/1/71).

analysis are reported in Table III-9 . The remaining samples were held in reserve. The estimated ^{148}Nd fissioned values listed in Table III-9 were determined from Figure III-11.

This figure compares the ASTM 1 isotopic burnup results with the ^{148}Nd burnup results reported from the Analytical Chemistry Branch. It can be seen that this plot is a well defined linear relationship with only two slightly deviate points. This method of estimation was employed (rather than 100% ^{148}Nd burnup analysis) because of the much higher cost of the neodymium method compared with the isotopic methods.

The estimated random error ranges (from Analytical Chemistry Branch experience) in the isotopic values and ^{148}Nd values (as reported in Table III-9) are 4 - 6% and 3 - 4% relative standard deviation, respectively. It can be seen in Table III-9 that the isotopic methods also have systematic errors associated with the determinations. That is, these values are always less than the burnup calculated by the neodymium method. This deviation seems to occur (in the isotopic method) because ^{238}U and ^{236}U contents are assumed to remain constant during burnup of the fuel. These values, however, do change during irradiation. The ^{148}Nd method is not subject to this same type of systematic error because heavy atoms other than uranium are not present in ETR and ATR fuel and this method takes into consideration total uranium fissions (i.e., the change in atom percent of all uranium isotopes are calculated).

The fission density values calculated and listed in Table III-9 result from multiplying the ^{148}Nd burnup values by the nominal atoms U/cc for each ATR 7F plate type. The nominal values used for ^{235}U concentration in ATR 7F

cores are(10): 0.976g $^{235}\text{U}/\text{cc}$ for plates 1, 2, 18, and 19; 1.232g $^{235}\text{U}/\text{cc}$ for plates 3, 4, 16, and 17; and 1.488g $^{235}\text{U}/\text{cc}$ for plates 5-15. These values convert to (assuming 93% enrichment) $2.69 \times 10^{21} \frac{\text{atom U}}{\text{cc}}$ for plates 1, 2, 18, and 19; $3.39 \times 10^{21} \frac{\text{atoms U}}{\text{cc}}$ for plates 3, 4, 16, and 17; and $4.10 \times 10^{21} \frac{\text{atoms U}}{\text{cc}}$ for plates 5-15. The nominal values used for ^{235}U concentration in ATR regular cores are 1.22g $^{235}\text{U}/\text{cc}$ of core or 3.36×10^{21} atoms U/cc. All of these values are based on a nominal core thickness of 20 mils.

The least-squares plot of the centerline fission density data (with 95% confidence limits on either side) presented in Table III-9 versus normalized gamma amplitude for elements XA3G and XA20G is given in Figures III-12 and -13, respectively. The burnup samples taken from the "A" or "C" plane were not included in Figure III-11 because of the sharp peaking in burnup near the edge of the fuel plates. This burnup gradient over the 1/4 inch punchings was not desired for use in preparing Figures III-12 and -13 because the equations of these lines were used in the computer program CONTOR to convert gamma amplitude input data to fission density output in the form of contour plots. Therefore, burnup punchings, having relatively uniform burnup over the 1/4 inch, were desired. The standard deviations ($\hat{S}_y$) of the center line data points about the least-squares fit line are 7.23×10^{19} ($\hat{S}_y/\bar{y} = 8.05\%$) and 9.11×10^{19} ($\hat{S}_y/\bar{y} = 12.3\%$) for XA3G (Figure III-12) and XA20G (Figure III-13), respectively.

3.6.2 Data Obtained from Gamma Scanning

The experimental setup used in the hot cell for gamma scanning is seen in Figure III-14. Here it is seen that the fuel plate or subject is supported approximately 9 inches from the end of the collimator slit and moved at a constant rate during the scanning operation. The rate of speed employed when the plates were scanned in the length direction was 2.4 inches/min. and the speed during cross scanning was 1.75 inches/min. The collimator slit used during the Core II and III element examinations was 0.05 x 0.019 x 11.75 inches deep. With the type of geometry shown in Figure III-14 and using the dimensions above, it can be shown that the gamma signal emitted from the fuel plate during scanning comes from an area 1.25 inches long x 0.048 inches wide. During the center line scanning operations, the slit length (0.50 inches) was parallel with the ends of the fuel plate (top and bottom). During all other scanning operations ("A" and "C" planes and cross scans), the slit length was parallel with the sides of the fuel plate. The scanning was carried out in this manner because of the flux peaking previously mentioned along the sides of the plates. All plates were in the concave position with respect to the detector, when scanned. This peak burnup along the sides of the plate (because of scanning geometry shown above) is always averaged over at least 0.048 inches. This type of slit orientation is designated to provide the best possible resolution at the areas of maximum burnup.

3.6.3 Iso-Fission Density Contours

The computer program CONTOR receives values of amplitude from gamma scans as input. For each fuel plate, this input is generally about 140 - 150 points for each length scan and 30 - 35 points for each of nine cross scans. These values are then normalized by subtraction of a baseline amplitude. These values are then stored as a matrix and converted to fission density by the equation of a line such as those given in Figures III-12 and -13. Linear interpolation is then performed between the stored data sets to provide fission density contours at known intervals.

Iso-fission density plots of three plates from XA3G (plates 1, 3, and 15) and three plates from XA20G (plates 5, 15, and 19) are given in Figures III-19 through -25. In reading these contour plots, each line represents a constant fission density (atoms U fissioned per cubic centimeter of core). The contour interval (i.e., the amount of change in fission density between adjacent lines) is given at the top left hand side of each plot (such as $2.5 + 19$ which reads 2.5×10^{19} fissions/cc from one line to the next). The multiplication factor for the labeled values of fission density is given at the top near the center. Also, the approximate length and width of each fuel plate core is labeled in inches. Here, an example of reading a plate length value would be $1.6 + 01$. This is equal to 16 inches. All fission density plots seen are viewed in the concave direction, and the top end of each plate is at zero inches.

The areas of maximum flux peaking and hence maximum burnup generally occur between 20 and 30 inches along the plate length and on either edge of the plate.

This gradient can be seen by the closely spaced contour lines in these areas. It should be noted that those contour plots were primarily intended to provide a general visual indication of the burnup patterns which occur in these type of fuel plates.

3.6.4 Cummulative Accuracy of Analysis

A measure of the total accuracy of the burnup values given is approximately equal to the combined variance of the least-squares fit line which related gamma amplitude to fission density (Figures III-12 and III-13) and the variance of the points about the least squares fit line. The random error present in the ^{148}Nd burnup results should be present and part of the variance of the data points about the correlation line. A confidence interval about the correlation line may be calculated at any desired confidence. If a 95% confidence interval is desired, the band about the correlation line at the peak burnup in plates XA3G and XA20G would be $\pm 1.2 \times 10^{20}$ fissions/cc and $\pm 3.5 \times 10^{20}$ fissions/cc, respectively. Because CONTOR has predicted maximum fission density values in XA3G (plate 15) and XA20G (plate 15) on the "C" edge of the fuel core between 24 and 25 from the top of the fuel plate of 15.0×10^{20} fissions/cc and 15.0×10^{20} fissions/cc, respectively, it can be stated with 95% confidence that the true peak fission densities in these plates (assuming no instrument error) was not greater than 16.2×10^{20} fissions/cc and 18.5×10^{20} fissions/cc, respectively.

Detailed computer print outs of fission density values of plates 1, 3, 5, 10, 15, 17, and 19 (XA3G); plates 5, 15, and 19 (XA20G); and plates 2, 10, 18, and 19 (A172C) with corresponding 95% confidence interval data are available,

however, were not included because of the great volume of paper involved. Detailed burnup data from elements XA8G and A16D has not been obtained as yet because of lack of manpower.

3. 6.5 Plate-to-Plate Distribution of Burnup

The maximum longitudinal center line and maximum peak plate-to-plate burnup distributions in elements XA3G, XA20G, and A172C are given in Figures III-15 through III-17, respectively. It can be seen from these figures that the plate-to-plate burnup distribution is quite different when a regular ATR and ATR 7F elements are compared. Both of the 7F elements show large changes in plate-to-plate burnup in the regions of plates 1-5 and plates 15-19, maxima generally occur at plates 5 and 15, and a generally uniform burnup pattern is achieved between plates 8-13. The single ATR regular element which has been evaluated in detail shows steadily decreasing burnup values from plate 1 to 19. The distribution of peak burnup can also be seen in these figures. The actual data used to plot Figures III-15 through III-17 is given in Tables III-10, III-11, and III-12, respectively.

B. INC-16 Sample Irradiations

Irradiation of the INC-16-1 sample train in the G-12 position of the ETR has been completed. The irradiation took place during Cycles 109 and 110 for a total of 18,667 MWd. Ranges of estimated burnups and operating temperatures for the fuel compositions studied are presented in Table III-13. At the end of Cycle 109 (9,343 MWd), tiers 2 and 4 were interchanged in order to increase the burnup of the ATR 7F high loaded fuels originally in tier 2. Two sample fuel plates (one ATR regular and one ATR 7F high) were removed from the reactor at this point, having reached calculated peak burnups of 1.8×10^{21} fissions/cc. Hot cell examination of these two plates is expected to begin during the week of April 12, 1971.

The remaining plates are cooling in the ETR canal. Visual examination of these plates while still in their holders revealed no plate warping, buckling, gross swelling or blistering, and no cooling channel deformation or blockage. The plates were removed from their holders and photographed on both sides. Figure III-18 is a canal photograph of the plates from holder 4. These plates achieved the highest burnup, in terms of fissions/cc, of those in any holder. The oxides display patterns related to coolant flow. The white oxides are thickest, and on all plates the oxide over the fuel core is surrounded by a dark border of thin oxide over the cooler picture frame.

Oxide measurements were made on all sample fuel plates in the canal. The maximum oxide of each composition in each tier is presented in Table III-14. The oxides on the plates in tier 1 are surprisingly thick considering this is the coolest tier. It may be that material was deposited on these plates as the cooling water entered the sample train. This possibility will be checked by hot cell metallography.

C. ORNL Sample Irradiations

The objective of the irradiation program being conducted by ORNL is to determine those factors that influence the performance of Al-clad UAl_x and U_3O_8 dispersions in the ATR and HFIR fuel elements at fission densities of up to 2.5×10^{21} fissions/cm³(11). The irradiations for this program are being conducted in the reflector region of the HFIR. The experimental capsule consists of 18 uninstrumented plates and three instrumented plates. The fuel plate centerline temperatures are being monitored by means of thermocouples inside the instrumented plates. The test plates being used in this experiment are shown in Table III-15.

The two-number indexing scheme for the reference irradiation position shown in Table III-15 identifies the location of the plates in the VXF-17 reflector hole of the HFIR. The first number gives the radial column (0 to 3) with respect to the center of the active reactor core, and the second number gives the vertical orientation from top to bottom (1 to 6).

This experiment, which is scheduled to last for five reactor cycles, began operation in early January. The thermocouple readings have ranged between 88 to 98°C. It had been expected that these readings would be in the range of 99 to 134°C. It is believed that the low readings are due to lower than expected neutron fluxes. Thus, it is currently believed that when the experiment terminates in late April, the peak burnup which will be reached will be 2.1×10^{21} fissions/cc.

This will occur in the UAl_x plate which is installed in position 25. Two other plates will reach burnups over 2×10^{21} fissions/cc. These are the samples located in position 15 and 35. The samples in positions 14, 24, and 34 should have burnups of over 1.8×10^{21} fissions/cc, while the sample in position 32 should reach 1.5×10^{21} .

By April 7, 1971, the samples had been exposed for four 1/2 cycles. No indications had been received of fission breaks, and except for the low temperatures noted above, the experimental train appeared to be operating normally⁽¹²⁾.

The fuel plate compositions and irradiation conditions used in this experiment do not exactly duplicate those of the ATR. Nevertheless, this test does lend support to the other evidence which has been accumulated that indicates that it is safe to take ATR fuel elements, fueled with UAl_x powder, to peak burnups of over 1.8×10^{21} fissions/cc.

References:

- (1) J. W. Henscheid, N. C. Kaufman, J. L. Durney, J. B. Willis, ATR Startup, Zero-Power Experiments and Comparison with ATR Critical Facility, IN-1136 (December 1967).
- (2) V. A. Walker and J. W. Henscheid (eds.) internal report (February 1968).
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- (4) D. R. deBoisblanc and S. Cohen, Safety Analysis Report - Advanced Test Reactor Vol I, IDO-17021 (April 1965) p 11.
- (5) Ibid, p 143.
- (6) G. W. Gibson, The Development of Powdered Uranium-Aluminide Compounds for Use as Nuclear Reactor Fuels, IN-1133 (December 1967).
- (7) M. J. Graber, G. W. Gibson, V. A. Walker, W. C. Francis, Results of ATR Sample Fuel Plate Irradiation Experiment, IDO-16667 (March 1964).
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- (9) J. C. Griess et al, Effect of Heat Flux on the Corrosion of Aluminum by Water - Part IV, Tests Relative to the Advanced Test Reactor and Correlation with Previous Results, ORNL-3541 (February 1964).
- (10) IN-F-9-ATR, Section 3.5.1.3.
- (11) M. M. Martin, "Evaluation and Design of Cermet Fuel Plate for Irradiation Experiments in HFIR", Fuels and Materials Development Program Quarterly Progress Report for Period Ending June 30, 1970, ORNL-4600 pp 239-242.
- (12) W. R. Martin, Oak Ridge National Laboratory, private communication.

IV. THERMAL STRESS

A. Mechanisms

The ATR fuel element has nineteen 6061-0 aluminum clad fuel plates joined together with unfueled 6061-T6 side plates (Figure IV.A.1). The fabrication is completed with all components near the same temperature. Under initial irradiation, in the reactor environment, the fuel plates heat up to 300-350°F while the side plates, heated by gamma absorption only, operate at approximately 130°F. This temperature differential results in a compressive loading on the fuel plates which produces a potential for buckling. During prolonged irradiation, the relatively hot fuel plate cladding reacts with the primary coolant, thereby producing a hydrated aluminum oxide thermal barrier on the heat transfer surfaces. This thermal barrier causes the maximum plate temperature and thermal stresses to increase with cycle life until the decreased power generation in these regions, resulting from the greater fuel burnup, overrides the corrosion effect.

The fuel plate to side plate joint is formed by a mechanical roll swagging process. The mechanical joint (Figure IV.A.2) provides adequate fixity (150#/in) in the azimuthal direction but allows axial slippage. The axial load transmission characteristics are not specified in the fuel fabrication contract and tests have indicated that this parameter is highly variable. However, experience in the ETR with similarly fabricated elements and elements that were completely restrained from differential axial expansion, by pinning the fuel plate into the side plates, indicates that fuel plate slippage does allow some degree of stress relief.

B. Calculational Models

1. Stress

The ATR operating standard practices require that the thermal stress, in the center half-span of all fuel plates, be below the unirradiated yield stress of the fuel plate composite throughout the cycle. This criterion was

established to avoid buckling or warping that could be deleterious to heat transfer within the operating fuel assemblies. The criterion was based on out-of-pile tests that simulated both the thermal stresses and the resistance to buckling, provided by the side plates, by isothermal heating of actual fuel plates restrained in carbon steel or stainless steel simulated side plates. For conservative simplicity, the compressive thermal stresses on the hot fuel plates are calculated, on a routine basis, assuming all regions within the fuel element are elastic and that the fuel plates are completely restrained by the side plates.

The validity of the criteria restricting yielding to the edges of the plate was evaluated by calculating buckling temperatures using a digital computer code called BOSOR III which was developed by Lockheed Missiles and Space Corporation. BOSOR III employs a finite difference method of solution for the stress, stability, and analysis of layered bodies of revolution. The program has been verified by comparisons with other known solutions.

Although the BOSOR III code is incapable of an exact analysis of the ATR fuel plate configuration, it possesses more capabilities in the area of analysis of shell stability than any other commercially available code at this time. In order to analyze a shell segment such as the ATR fuel plates, a two-dimensional code with all the capabilities of BOSOR III plus plasticity is required. None is available.

The most logical fuel plate to investigate is No. 18, due to its large radius, small thickness, and high temperature. Since it possesses both a relatively large radius to thickness ratio ($r/t > 100$) and length to radius ratio ($l/r > 9.0$), it can be closely approximated by a cylinder, sandwich in make-up, having the same length, radius, thickness and physical properties as fuel plate 18. To obtain a model to simulate a curved fuel plate, a cylinder with a circumferential mode shape of 4 was used. This mode shape for a cylinder with a radius of 5.25 inches very closely simulates plate 18 of the ATR fuel element.

The lines of inflection are lines of zero moment and zero radial deflection and, therefore, simulate a plate with simply supported edges (i.e., this assumes that the side plate roll-swaged joint acts as a simple support). To study the thermal buckling problem the ends of the cylinder were completely restrained in the axial direction. This model conservatively simulates the restraint of the side plates. The temperatures computed for buckling are temperature differences above an equivalent side plate temperature. The equivalent side plate temperature is the temperature that produces the side plate elongation that is actually produced by the combination of side plate heating to the operating temperatures and tension loading from the rest of the fuel plates in the fuel assembly. The ends of the cylinder were also restrained in the radial direction to conservatively simulate the restraint of the fuel element combs.

Values of the physical constants of the core and clad material were taken from compression tests at a temperature of 400°F. The modulus of elasticity of both the core and clad was 7×10^6 psi and the coefficient of thermal expansion was theoretically calculated to be $11.7 \times 10^{-6}/^\circ\text{F}$ and $13.2 \times 10^{-6}/^\circ\text{F}$ for the core and clad materials, respectively. This was compared to the experimental value of $12.5 \times 10^{-6}/^\circ\text{F}$ obtained from the composite. Poisson's ratio was taken as 0.39 for both the core and the clad in the elastic analysis.

With this model, a delta temperature for buckling was computed using the linear or small deflection option and using the non-linear or large deflection option of the BOSOR program. The computed buckling temperatures (above the equivalent side plate temperature) are:

Linear Analysis	273°F
Non-Linear Analysis	334°F

The temperature difference (above the equivalent side plate temperature) that would produce yielding of the plate, is 125°F.

The results of the calculations show:

- (1) Plates will yield before buckling for loadings produced from differential thermal expansion.
- (2) The consideration of large deflections results in the computation of higher temperatures for thermal buckling (in contrast to results of computations of applied loads which would show lower buckling loads when large deflections are considered).

From these results, the conclusions that can be drawn are:

- (1) General yielding must occur before buckling. The criterion permitting the yielding near the edges of the fuel plate, where the buckling is restrained by side plate rolled swaged joint, is justified.
- (2) Since computed buckling temperatures are higher for large deflection computations, initial yielding would be expected to relieve thermal expansion stresses rather than produce buckling.

2. Oxide Buildup

Initially the oxide film on ATR fuel plates was calculated using a correlation based on out-of-pile work at ORNL⁽¹⁾. The correlation is:

$$X = a \quad \Theta^{0.778} \quad e^{-\left(\frac{8290}{\Theta R}\right)}$$

Where:

X = Corrosion-product thickness in mils

Θ = Time in hours

a = 443 for pH of 5.0

Corrosion tests conducted in the ATR during the startup program indicated that the actual temperature effect of the corrosion process (measured directly) was less than half that predicted by the ORNL correlation.

A design equation, based on one-half the ORNL buildup rate and ORNL determined thermal conductivity, correlated all of the temperatures observed, during the stable power operation of the corrosion test, within $\begin{smallmatrix} +8 \\ -6 \end{smallmatrix}$ °F. The scatter observed was associated primarily with reactor power variations, rather than variations in the corrosion process. Figure IV.B.2.1 compares the temperature measured at one location in the test element with the temperature predicted by the above model. The effects of local power fluctuations, caused by the N-16 calibration test, during the initial 100 hours of the test are clearly visible.

Since thermal stress is a function of temperature and not film thickness, per se, stress analysis based on the design equation (one-half of the ORNL buildup rate) are accurate as long as the corrosion mechanism remains unchanged.

Because temperature measurements are costly and impractical on a routine basis, surveillance of the corrosion process must be accomplished by periodically measuring the thickness and verifying the crystalline form of the corrosion film. It is, therefore, desirable to identify the film buildup rate experienced during the corrosion test and verify that this rate does not change throughout the life of the reactor.

To separate this temperature effect into (1) film buildup rate, and (2) film thermal conductivity, a precise film thickness measurement at the end of the test is needed. Idaho Nuclear Corporation* presently has no method for measuring corrosion film thickness on interior fuel plate surfaces, such as the instrumented plate in the corrosion test, other than disassembly and measurement in the hot cell. Prior to hot cell examination, the elements must be stored in the canal for a minimum of 90 days to allow the fission products to decay. For budgetary and programmatic reasons, the corrosion test element was stored in the canal nearly a year prior to hot cell examination. Experience with the behavior of fuel elements and corrosion films in stagnant pools in the ETRC and the ETR, indicates that both the crystalline form and the thickness of the corrosion film may be drastically altered during storage. Although investigative efforts are still in progress, it has been concluded that the hot cell measurements do not actually reflect the corrosion film buildup rate during the Core I corrosion test.

Subsequent to Core I, Idaho Nuclear Corporation* has developed the capability for underwater oxide film measurements. These measurements have an absolute precision of ± 0.0005 in. but because of physical size restrictions, they are limited to the external surfaces of plates 1 and 19. These surfaces bound the coolant channel with the poorest thickness tolerance control because the channel thickness relies on the mutual tolerances between (1) the fuel element,

* Now Aerojet Nuclear Company (7/1/71).

(2) the flux trap baffle, and (3) either the beryllium or the neck shim housing (Figure IV.A.1). This relatively poor tolerance control increases the uncertainty in the operating surface temperature of the external plates and thereby the calculated oxide thickness. However, cycle by cycle comparison between the calculated and the measured oxide thicknesses indicates the validity of using half the film buildup rate from the ORNL correlation with the ORNL thermal conductivity. Figure IV.B.2.2 compares the thickness predicted by the design equation (half of the ORNL equation) and the thickness measured at discharge. This figure, and all other ATR data not dependent on canal storage, indicates that the design correlation is reasonably accurate with respect to both film thickness and temperature, in the temperature range between 330 and 400°F. This is the ATR hot spot temperature range and, therefore, the temperature range in which most of the ORNL and Idaho Nuclear Corporation* data were collected. However, the precision of the correlation is not good at reduced temperatures, i.e., the correlation over-estimates the temperature dependence. Idaho Nuclear Corporation* is in the process of improving the corrosion correlation in this respect, but neither this improvement nor the conductivity-thickness separation are essential to predict maximum thermal stresses provided that the corrosion mechanism has not changed subsequent to Core I.

The data in Section III.A.3.2 are contradictory to the above conclusions, but it is believed that this results from the prolonged canal storage prior to examination.

* Now Aerojet Nuclear Corporation (7/1/71).

C. Calculated Thermal Stresses for Extended Burnup Element

The maximum thermal stresses for the primary experimental element (position 33 for Cycle 8A/B and position 13 for Cycle 8C) have been evaluated at the +2 σ temperature distribution. The calculation, based on power distributions predicted by PDQ analysis and the Core I corrosion rates, indicates that the thermal stress in the homogenized fuel plates will be below yield at all times and locations (Figure IV.C.1). The maximum stress in the principle experimental element will occur at the end of Cycle 8B, after which the maximum stresses will decrease because the element will be repositioned in a low power lobe.

Calculations, of maximum thermal stress in the secondary experimental element (position 13 for Cycle 8A/B and position 33 for Cycle 8C), will not be complete until April 21, 1971. The stresses in this element are not expected to exceed yield, but the stress will reach a maximum later in the irradiation program.

References:

- (1) J. C. Griess, H. C. Savage, J. L. English, Effect of Heat Flux on the Corrosion of Aluminum by Water - Part IV. Tests Relative to the Advanced Test Reactor and Correlation with Previous Results, ORNL-3541 (February 1964).

V. FISSION STRESSES

A. Mechanisms

The fissioning process results in an increase in the fuel core volume. Irradiation of unrestrained fuel pellets has shown that under these conditions the growth is isotropic. A 6% volumetric increase in the fuel core is expected to result from 1.8×10^{21} fission/cc in the test elements, fabricated by National Lead Corporation. Isotopic growth of this magnitude would result in a free growth of 2% in the axial direction or 0.94 inch. Axial growth is restrained by the cladding and results in compressive loading of the fuel core and tensile loading of the clad material. Irradiation experience with aluminum clad, UAl_x -aluminum core, fuel platelets and full size ATR fuel elements indicates that the growth is essentially all in the thickness (0.050 inch) dimension. Increases in the length dimension ($49\frac{1}{2}$ inches) are no more than 0.020 to 0.030 inch at volumetric increases of 3 to 5% (Table III-8). The contribution of this growth stress loading to fuel plate failure is analyzed in the following section.

B. Irradiation Growth Stresses

Fuel core volume increases from the fissioning process in the range of 6% would appear to have the potential to produce large fuel plate elongations and compressive loads. The potential for buckling from this source could be much greater than the potential produced by fuel plate thermal expansion. Measurements of free fuel plates after irradiation, however, have shown insignificant increases in length and width. All the increase in volume has been in the thickness direction.

The increase in thickness can be explained by the plastic flow of the fuel core. The clad material has a higher strength than the fuel core in the length and width directions. As growth occurs, the compressive yield stress of the core is reached while the clad, loaded in tension, is well below yield. The cladding produces very little restraint in the thickness direction, therefore, the plastic core flows only in the thickness direction.

For a fuel plate that has significant fuel core growth, the compressive loading from differential thermal expansion of the hot fuel plate and the cold side plate will produce compressive loading in the cladding and an increased compressive deflection in the core. The resistance to buckling from the clad is unchanged, but with the core plastic, small loads can produce large core deflections.

The increased compressive loading and reduced buckling resistance of the core are expected to produce only a small decrease in the buckling resistance of the composite plate. First because once the fuel core has

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The increased compressive loading and reduced buckling resistance of the core are expected to produce only a small decrease in the buckling resistance of the composite plate. First because once the fuel core has

reached yield stress, increased deflection can be produced with only a small increase in loading. Second, the resistance to buckling is produced by resistance to bending and the bending strength of the plate is derived primarily from the cladding.

A theoretical study was made which verified that the buckling resistance would be only slightly reduced by fuel core growth. The only program available for the study was the BOSOR-III program which handles only elastic solutions of axisymmetric layered shells. The program can be used to obtain a meaningful solution of the curved plates with fuel core yielding by using a cylinder with the proper mode shape as described in Section IV-B to represent the geometry and by using a reduced modulus of elasticity to represent the plasticity of the fuel core. The cladding was assumed to remain elastic for the study.

The method of selecting a reduced modulus of elasticity to represent the yielding of the fuel core was to approximate the stress strain curve for the fuel core by two linear segments, an elastic segment and a plastic segment. For a restrained plate the full elongation from both thermal expansion and fuel growth must be accommodated by strain deformation. The slope of a line drawn from the origin on a stress strain diagram to the intersection of the strain deformation and the stress strain curve gives the modulus which represents yielding of the fuel core. This method is shown in the following line drawing.

Since this model is for a completely restrained plate, the computed buckling temperatures must be interpreted as temperatures above an equivalent side plate temperature.

The equivalent buckling temperatures for a model of plate 18 for no growth and for a growth of 8%, which is the maximum value for the extended burnup test, are as follows:

<u>% Fuel Growth</u>	<u>Modulus of Clad</u>	<u>Pseudo Modulus of Core</u>	<u>Yield Stress of Core</u>	<u>Coefficient Expansion</u>	<u>Equivalent Buckling Temp.</u>
0%	7×10^6	7×10^6	Elastic	13.2×10^{-6}	350°F
8%	7×10^6	4.87×10^6	10,000 psi	13.2×10^{-6}	307°F

These results indicate the equivalent temperature at buckling is only slightly reduced and confirm the hypothesis that fuel growth with the accompanying plastic flow of the core produce only a small change in the buckling resistance of the plate. All component temperatures are above the 120°F equivalent temperature that would produce general yielding of the plates.

Some uncertainty exists as to the properties of the fuel core. Out-of-pile measurements indicate that the yield strength increases with irradiation on

the other hand some evidence exists that during the fissioning there is a large reduction in the yield strength. To evaluate the effect of yield strength, a problem was run in which the yield strength assumed to be essentially zero. A comparison with results for a best estimate of core yield stress is shown below:

<u>Core Yield Stress, psi</u>	<u>Modulus of Clad, psi</u>	<u>Pseudo Modulus of Core, psi</u>	<u>Coefficient of Expansion $^{\circ}\text{F}^{-1}$</u>	<u>% Fuel Growth</u>	<u>Equivalent Buckling Temperature</u>
0	7×10^6	3.5×10^4	13.2×10^{-6}	12%	410°F
10,000 psi	7×10^6	4.87×10^5	13.2×10^{-6}	8%	307°F
Elastic	7×10^6	7×10^6	13.2×10^{-6}	0	350°F

These results indicate that if the yield stress of the core is low, the reduction in loading from the core as a result of thermal expansion and growth is more effective than the small reduction in buckling resistance. The net result is that even higher equivalent buckling temperatures are computed. All computed temperatures are above the 120°F equivalent temperature that would produce general yielding of the fuel plates.

The fuel plates are required to resist a small lateral loading from differential pressure. For actual fuel plates the loading is localized by the side plate vents, which tend to equalize the pressure. To estimate the effect of a plastic core on the ability of the fuel plate to resist lateral pressure a very conservative model with lateral pressure over the full length of the plate was used. The high pressure was assumed to be on the convex side since pressure on this side reduces the stability of the plate. In addition, low values of fuel core yield strength were used to conservatively estimate the weakening of the plate due to a plastic fuel core. The results are:

<u>Lateral Pressure</u>	<u>Modulus of Elasticity of Cladding psi</u>	<u>Equivalent Modulus of Core to Simulate Plastic Behavior psi</u>	<u>Yield Stress of Fuel Core</u>	<u>Coefficient of Thermal Expansion $^{\circ}\text{F}^{-1}$</u>	<u>Growth of Core</u>	<u>Equivalent Buckling Temperature</u>
0 psi	7×10^6	7×10^5	Elastic	13.2×10^{-6}	0%	350°F
2 psi	7×10^6	3.5×10^5	7000 psi	13.2×10^{-6}	8%	305°F
4 psi	7×10^6	3.5×10^5	6000 psi	13.2×10^{-6}	7%	271°F

These results show the plates retain a good resistance to buckling with a plastic fuel core and lateral pressure. All computed temperatures are above the 120°F equivalent temperature that would produce general yielding of the fuel plates.

These results indicate increased irradiation beyond the 1.5×10^{22} fissions per cm^3 would not be expected to produce buckling if the center half span of the clad is not loaded to yield in compression from differential thermal expansion. This criterion is justified since computation considering growth, yielding of the core, and lateral pressure show the plates are still stable if the clad does not have general yielding.

C. Failure Hazards Analysis

A thorough analytical and experimental evaluation, of the effects of increasing the burnup in UAl_x fuels beyond 1.5×10^{21} fissions/cc, has identified two failure modes - blistering and warping. Blistering is assumed to include all cases of separation, whether the fracture is in the core region or at the core-clad interface, caused by volumetric increases associated with the fissioning process. Warping is the stress relief mechanism associated with restrained growth, whether the growth resulted from thermal expansion or the fission

produced volumetric increase. The preceding sections (IV.C and V.B) concluded that the probability of failure, by the warping mode, will not be altered by the increased fission density. Section III indicates that failure by the blistering mode, at reactor temperatures, is very unlikely at the upper limit of the burnup levels calculated for the proposed test. However, the extent of damage has been calculated assuming the potential failures do occur. Existing data, relative to these two failure modes, has been evaluated, in the following sections, to determine (1) the possibility of a hydraulic failure propagating from an initial deflection produced by the blistering or warping mechanisms and (2) the heat removal capabilities of the blistered or warped fuel plate(s) and the extent of damage that could result from melting in the blistered or warped areas. These calculations were used to determine the extent of fission product release that could be associated with the failure modes should they occur.

The following analysis of failure modes indicates that warping or blistering will not result in a propagating failure. The most severe failure, associated solely with the burnup extension, will not result in significant increases in personnel exposure and the activity in the primary coolant system can be reduced to operating levels with net costs equivalent to a xenon shutdown (about \$40,000).

1. Blistering

- a. Hydraulic Propagation

The probability that blistering or warping will cause a propagating failure of a fuel element was analytically evaluated by use of equations and relationships presented by Miller⁽¹⁾ and Johansson⁽²⁾. These equations were used to calculate the velocity, at the minimum cross-sectional area, which would cause failure of the element. The area change could occur due to blistering and warping. Because the axial bending strength of a fuel plate is increased when the length of the deformation is limited to less than a plate width, an

axial bending coefficient was calculated, using a relationship presented by Laura and Saffell⁽³⁾, to allow application to deformed areas with a deformation length less than the plate width (span). The equation used to calculate the critical velocity was:

$$V_{cr} = C(1 + \alpha) \sqrt{\frac{2H K_{\infty}}{\frac{16}{15} \rho \left\{ 4 + 3/2 \frac{f}{2h} - \frac{3}{1+R} \frac{\ell}{L} \left[1 + k_i + \frac{f(\ell_1 + 1/2\ell)}{2h} \right] \right\}}}$$

where

α - Parameter which corrects for axial bending stiffness of the plates

h - Channel thickness, in.

K_{∞} - Stiffness of plate considered as a wide beam subject to uniform pressure -
 $\frac{384D}{b^4}$

C - Parameter to correct for curved plates

ρ - Fluid mass density, lb-sec²/in⁴.

f - Friction factor

ℓ - Axial length of deflected region, in.

L - Plate length, in.

k_i - Inlet loss coefficient

k_e - Exit loss coefficient

ℓ_1 - Axial distance from inlet to start of deflected region

$R = (k_i + k_e) \frac{fL}{2h}$

δ - Plate deflection

b - Plate span, in.

$D = Et^3/12(1 - v^2)$

E - Modulus of elasticity, psi

t - Plate thickness, in.

v - Poisson's ratio

y - Axial distance along deflected region, in.

This equation is based on an assumed deflection curve, where

$$\delta = 1/2 \delta_m \left(1 - \cos \frac{2\pi y}{\ell}\right),$$

with the deflection and slope being zero at each end.

Values of V_{cr} vs ℓ/b are plotted in Figure V.C.1 for a flat plate assembly, where b is the plate span corresponding to the weakest fuel plate (plate 18) in the ATR elements. Values of C vs β are plotted in Figure V.C.2 where β is one-half of the curved plate arc between supports.

These values of C were then used with the calculated values of α for various values of ℓ/b , listed in Table V.C.1, to calculate the critical velocity for curved plates.

Calculations were made for several cases. It appears that the worst case would be a deformed region extending over three spans. Calculations made for this condition indicate that the critical velocity would be ~ 270 ft/sec. This would correspond to a minimum channel clearance of ~ 0.014 inch at the deformation midpoint (axial). The minimum is assumed to extend across the entire channel.

Available data on swelling and blistering indicate that this condition is very unlikely. The maximum blistered area typically is $\sim 10\%$ of the plate surface, with a mean blister height of ~ 0.020 inch. These data are from oven blister tests at 950°F after having been irradiated to 1.78×10^{21} fissions/cc.

b. Heat Removal

Two cases were analyzed for heat removal from an area of blister formation. These were:

(1) A blister forming in one matrix-clad interface with the assumption that all of the generated heat must be transferred out the opposite side of the plate.

(2) Blisters forming in both matrix clad interfaces with the assumption that all of the generated heat must be transferred by conduction through the fuel matrix to a bonded area, and then transferred to the coolant.

If a blister forms on only one side of the fuel plate, the maximum heat flux would be twice the operating heat flux at the time the blister formed.

Nominal worst case conditions (250 MW, 40-50-60 unbalanced operation after 17 days of oxide buildup) were used to calculate the most severe credible temperature profile.

The fuel center line temperature and the surface temperature were calculated to be 560°F and 403°F respectively. This surface temperature is below the saturation temperature of the water. These temperatures would decrease as the irradiation time (and burnup) was increased beyond 17 days.

If blisters form on both sides of the plate, the fuel matrix must conduct all of the generated heat to a bonded area. Since the matrix area available for axial and azimuthal conduction is very limited, the fuel matrix temperature at the center of the blister increases rapidly with blister diameter.

The lowest melting point of UAl_x compounds is 1346°F for UAl_4 . This corresponds to a minimum blister diameter of 0.367 inch formed on each side of the fuel matrix at the same location. The blisters which have been examined have not shown separation from both clad surfaces. A plot of the blister centerline fuel temperatures vs blister diameters is shown in Figure V.C.3 using the hot-spot heat flux.

c. Fission Product Release

Two methods of obtaining extended burnup data were analyzed for possible fission product releases. These were:

- (1) Irradiation of an entire core until the maximum burnup reaches 1.8×10^{21} fiss/cc, or
- (2) Extended irradiation of two elements until the maximum burnup reaches 1.8×10^{21} fiss/cc.

Fission product releases were then calculated for each possibility assuming (1) 100% fission gas and 10% particulate release from the areas covered by blisters, and (2) the more probable condition of 4% fission gas and 1% particulate release from the blistered areas.

1) Extended Irradiation of Full Core

For 45 days of operation (the time required to reach 1.8×10^{21} fissions/cc) at 250 MW, the total calculated fission product inventory is 1.0×10^9 curies. Of this total, 1.34×10^8 curies are contributed by the noble gases (xenon, krypton, and argon). These results were calculated by the computer code RSAC and verified by the computer code SINBAD.

From peaking factors calculated by the computer code TURBO for a full core of standard elements operating in the balanced* configuration, it was determined that 96 plates would have some portion exceed burnups of 1.5×10^{21} fission/cc.

a) 100% Fission Gas and 10% Particulate Release from Blistered Areas

The available data indicate that the maximum surface of a fuel plate that blistered during a blister test was ~10% of each plate side, i.e., a maximum of 20% of the total plate fuel core was affected. This plate had been irradiated to a burnup of 1.78×10^{21} fission/cc and blister tested at 950°F. Another plate irradiated to 2.07×10^{21} fission/cc and blister tested at 950°F had only 10% of its fuel core affected by blistering. All of the other plates tested had smaller portions of their surface blistered. Therefore, the assumption of 20% total fuel core affected by blistering should be a maximum and will be used where applicable.

* This configuration produces the largest number of plates reaching a given burnup.

The assumption that the total plate has the assumed blister distribution is conservative because of the cosine axial burnout distribution. The data available indicates that blisters form only on the areas where the burnup reaches the blister point. Other areas (such as the ends of the plates) with reduced burnup should be unaffected.

Assuming that 20% of the fuel core area is affected in any plate that exceeds 1.5×10^{21} fission/cc, calculations indicate that the maximum activity of the primary coolant system would be 1.36×10^4 $\mu\text{c/cc}$. The degassifier Design Manual⁽⁴⁾ indicates 23 hours would be required to cleanup the system such that the fission gas concentrations would be less than 6×10^{-3} $\mu\text{c/cc}$ with 600 gpm degassing flow. The particulate activity could be reduced to acceptable levels in 27 hours with 500 gpm bypass demineralizer flow. The calculations and isotopes evaluated are listed in Appendix V.C.A. If a continuous release rate is assumed with continued operation, one plate could release every 8 days.

b) 4% Fission Gas and 1% Particulate Release From Blistered Areas

Some experimental work has been done on the fission product release from UAl_x fuels. This work (References 6, 7, and 8) has indicated that the maximum measured release was 4% of the generated fission gases. This study has also shown there is no appreciable gas migration in the UAl_x fuels. On this basis, a more realistic estimate of the potential contamination was made.

If all of the plate blistered areas were to release 4% of the gaseous fission products and 1% of the particulate fission products instantaneously, the activity levels would be 7×10^2 $\mu\text{c/cc}$. The gaseous fission product activity could be decreased to 6×10^{-3} $\mu\text{c/cc}$ in 18 hours at flow rates of 600 gpm through the degassifier. The calculations in Appendix V.C.A indicate 21 hours at 500 gpm would decrease the specific particulate isotopes to acceptable levels.

If a uniform release rate is assumed, it is calculated that one plate could release its fission products on the assumed basis every 7 hours, with flow rates of 600 gpm.

2. Extended Burnup of Two Elements

If two 7F elements are irradiated in core positions 13 and 33, under the planned irradiation schedule, calculations indicate portions of 28 plates will exceed 1.5×10^{21} fissions/cc. In this case, 28 plates were used to predict cleanup times.

a. 100% Fission Gas and 10% Particulate Release from Blistered Areas

With instantaneous release, the activity level would be 4×10^3 $\mu\text{c/cc}$ and the estimated cleanup time to meet the particulate limits would be 23 hours. The uniform release rates would be the same as for the full core case.

b. 4% Fission Gas and 1% Particulate Release from Blistered Areas

Assuming these conditions, an instantaneous release would produce a calculated activity level of 205 $\mu\text{c/cc}$. Due to the conservatism of the calculations, it is not expected that the activity levels would exceed the current operating limit which states that the activity levels shall not exceed 200 $\mu\text{c/cc}$ in any 24 hour period. If desired, reactor shutdown would not be necessary as the levels could be decreased by use of the degassifier and bypass demineralizer to meet steady state operating limits. Again, the uniform release rates would be the same as the full core case.

3. Cleanup Costs

From the above calculated cleanup times, it appears the largest cost incurred by either extended burnup procedure would be the shutdown time required to discharge the elements and wait for xenon decay (about \$40,000, based on latest irradiation charges).

Based on SAR calculations, the increase in radiation levels outside the primary system shielding for the worst case (C.1.a) would be 5 mrem/hr. The noble gases can be released at a controlled rate. If these gases were released under the present operating limits, which limit the discharge to 1×10^5 curies/day, the maximum exposure to personnel in the TRA area under continuous fumigation conditions, would be 40 mrem/8 hour day.

Since the release is controllable, the rate of release could be coordinated with favorable meteorological conditions. For the worst case (C.1.a), approximately 5 days would be required to complete the noble gas discharge at 1×10^5 curies/day. The most severe off-site doses are obtained by assuming that the plume remains aloft and disperses under inversion conditions until it reaches a receptor, drops and immerses the receptor for 24 hours. For this case, the whole body dose at Arco, Idaho is less than 0.17 rem.

2. Warping

a. Hydraulic Propagation

Data on the warping of the fuel plates is very limited. The only observed warping occurred in an ETR (flat) fuel plate. This warpage occurred in a pinned assembly that could not relieve either the thermal stresses or any stresses that may have been produced by axial growth due to irradiation. The observed buckling had a maximum amplitude of 0.030 inch.

Significant axial growth has not been observed in irradiating unrestrained aluminum clad fuel platelets. Recent measurements taken on a sampling of ATR elements show that the fuel plates have slipped in the side plates and have elongated, thereby reducing the compression stresses, whether produced by

irradiation growth or thermal expansion. These measurements show that the fuel plates have elongated 0.021 - 0.031 inch, with the pre- and post-irradiation measurements taken at the same temperature.

Based on the analyses in IV.C & V.B substantiated by the fact that 0.030 inch of deflection did not cause plate failure in the ETR pinned flat plate assemblies and because slippage of the fuel plates in the side plates is partially relieving any stresses produced by either thermal or irradiation growth, no propagative failures from warping are anticipated. However, to experimentally verify the probability of propagation failure if warping should occur, a half-sine deformation was formed in plate 18 such that channel 18 was restricted. A deformation across the complete channel could not be obtained due to the support of the side plates. The deformation obtained is shown in Figures V.C.5, V.C.6, and V.C.7.

The element was instrumented with pitot tubes in channels 18 and 19 just prior to discharge to the lower plenum.

The plate position and the velocities in channels 17, 18, and 19 at the midpoint of the deflection were obtained by a single pressure tap which could be positioned to obtain the dynamic and static pressures by a micrometer.

The element was tested at element differentials ranging from 17 psid to 230 psid at water temperatures of 150 and 350°F.

The results obtained at bulk water temperatures of 150°F indicate that the plate spacing for channel 18 did not change appreciably. However, there did appear to be a general shifting of the entire element towards channel 20.

With the bulk water temperature at 250°F, the same phenomena was also observed. In addition, at 125 psid flow redistribution and/or secondary buckling below the initial deflection caused the direction of pressure difference to reverse (with the static pressure in channel 18 higher than that in channel

19). From 125 to 200 psid the lateral ΔP continued to increase, resulting in plate deflections which allowed an increase in the relative flow through channel 18. The channel static pressures and the velocity for channels 17, 18, and 19 are included as Table V.C.2.

The test results indicate there was flow redistribution through the vents and that flow redistributed such that critical velocities could not be obtained over the deformed region. The data indicate the peak velocity in channel 18 was 132 ft/sec.

Post-test examination of the element indicated that the original deformation was still present but with some change in shape and dimensions. Also present were secondary buckles above and below the original deformation. The channel traces showing this information are included as Figures V.C.8 & V.C.9. The secondary buckles may have been formed by the release of cold working stresses, placed in the plate during the original deformation, when the element was heated to 350°F, and/or by hydraulic forces. No experimental data is available to verify the mechanism.

Based on these results, it is concluded that because the flow redistributes the results from the theoretical equation are conservative, and that as previously stated propagative failures are unlikely.

b. Heat Removal

Because there is no separation of the fuel matrix from the clad and because the wave length associated with warping does not exceed twice the hydraulic entrance length, the heat removal characteristics do not degenerate unless blockage occurs.

c. Fission Product Leakage

It is not anticipated that warping of sufficient magnitude to cause blockage of a flow channel will occur. However, the releases of fission products which could occur from a single warped plate blocking a flow channel were calculated.

Two possible conditions for plate meltdown were considered. These were:

(1) Melting of the entire plate and release of all of the gaseous and particulate fission products.

(2) Melting of the longest segment of fuel plate between flow vents.

1) Meltdown of Fuel Plate

Because of the relationship between unsupported span and buckling strength, plate 18 is the most likely to buckle.

Based on plate 18, it is calculated that 5.6×10^4 $\mu\text{c/cc}$ would be the maximum process water activity level. The gaseous product concentrations could be reduced to less than 6×10^{-3} $\mu\text{c/cc}$ in ~ 22 hours with 600 gpm degassing flow, while 26 hours would be required to remove the particulates to acceptable limits with 500 gpm bypass demineralizer flow.

2) Partial Plate Meltdown

Based on the longest segment of plate 18 between flow vents, if is calculated that the maximum activity expected would be 1.68×10^4 $\mu\text{c/cc}$, with gaseous product cleanup time of ~ 18 hours, and particulate cleanup time of ~ 23 hours.

These two cases give calculated activity levels if the hypothesized failures should occur. Due to support of the side plates, it is not anticipated that a channel can be completely blocked by buckling. If a channel were blocked to the point that melting did occur, experience has shown that the loss of fuel and particulate fission products is small. During ETR Cycle 87A, some of the flow channels were sufficiently blocked to cause partial melting of 9 plates, with one plate having 40% of its surface melted. This resulted in the loss of approximately 7.3 grams of fuel.

The element involved E-018-D, had a UAl_3 - 1100 Al matrix. The cladding was 1100 aluminum. Thus, the cladding and core matrix would have identical melting points. Examination of the element revealed that the cladding had become molten and flowed away leaving the fuel matrix. Further examination showed that the fuel matrix had also been molten. However, due to the aluminum oxide inherent in the aluminum powders, the matrix material has a much higher viscosity, and did not flow as readily as did the cladding (Reference 8).

The elements presently being used in the ATR have a UAl_x - 1100 Al matrix and are clad with 6061- Al. The melting range for 1100 Al is 640 - 660°C, while that for 6061-0 Al is 580 - 650°C. So the clad will melt before the matrix, and the phenomena observed in the ETR incident should be even more marked.

Based on this result, it is expected that the cladding will melt and be removed, and the matrix will be displaced until flow access is provided for fuel matrix cooling. Based on these observations, it is concluded that the release of large quantities of non-gaseous fission products will not occur. The maximum anticipated activity levels, if melting did occur, would be less than those previously presented, with equivalent cleanup times.

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- (2) E. B. Johansson, "Hydraulic Instability of Reactor Parallel Plate Fuel Assemblies", Preprint Paper No. 57, Nuclear Engineering and Science Conference, (April 4-7, 1960).
- (3) P. A. Laura and B. F. Saffell, Jr., "Study of Small-Amplitude Vibration of Clamped Rectangular Plates Using Polynomial Approximations", Journal of the Acoustical Society of America, Vol 41, No. 4 836-839 (April 1967).
- (4) Design Manual for the Advanced Test Reactor, Vol No. 6, Contract No. AT(10-1)-1075.
- (5) R. R. Hammer, L. L. Dickerson, R. R. Hobbins, "Fission Product Release from Uranium Aluminide Fuels", Metallurgy and Materials Science Branch Annual Report Fiscal Year 1970, IN-1437 (November 1970).
- (6) W. F. Zelezny, G. W. Gibson, M. J. Graber, "A Microprobe Study of the Retention of the Fission Gas Xenon in Irradiated Uranium Fuels Dispersed in Aluminum Clad Nuclear Fuel Plates", National Symposium on Developments in Irradiation Testing Technology, Sandusky, Ohio, September 9-11, 1969, Report No. CONF-690910, pp 516-523.
- (7) M. J. Graber, M. Zukor, G. W. Gibson, "Fission Gas Release from Fuel Plate Meltdown", Annual Progress Report on Reactor Fuels and Materials Development for Fiscal Year 1966, IDO-17218 pp 52-58.
- (8) Reactor Engineering Branch Annual Report Fiscal Year 1968, IN-1228 pp 25-29.
- (9) Letter, H. K. Peterson to R. G. Ambrosek, ATR Degas Problem, private communication.

VI. CYCLE 8 IRRADIATION PROGRAM

A. Cycle 8A/B Operation

The two zone loaded 7-F elements selected for the extended burnup program (XA003F and XA004F) were fabricated by National Lead Corporation. During Cycles 8A/B XA003F will operate in position 13 and the primary experimental element (XA004F) will be in position 33 (Fig. 1.1). The remainder of the fuel loading will be elements fabricated by A.I. The A.I. elements have been examined by beta-autoradiographic techniques and all elements have a minimum of 0.006-inches of clad in the dogbone regions.

1. Reactor Power, Lobe Power, and Cycle Life

ATR Cycle 8A has been evaluated for 234 MWds of operation in the 52-44-50-44-44 unbalanced mode followed by 14 days of operation in the 220 MW balanced mode. Cycle 8B will operate at 242 MW in the 58-44-52-44-44 mode for 15 days. The maximum burn-up for the primary experimental element, XA004F, at the end of Cycle 8B will be 1.3×10^{21} fissions/cc (Fig. VI.A.3.1 and Table VI.A.3.1).

2. Power Density Distribution vs Cycle Time

PDQ-7 depletion calculations were made for the proposed ATR operating schedule for Cycles 8A and 8B. Radial-axial calculations were made to obtain diffusion theory constants for the loop experiments. These constants were flux weighted over the axial dimension for use in the PDQ-7 XY calculations. A one-half core 280 x 140 mesh PDQ-7 model was then prepared for the fine mesh XY calculations. The core was divided along the NW-C-SE axis. Only the SW one-half model was calculated, since this model contains the core hot spot (element 28). Also, this model is most suitable for the projected recycle of experimental fuel elements. The fuel loading for the calculational model at the start of Cycle 8A consisted of six (6) 7F fuel elements in the NW lobe

including the experimental element in core position 33. A second experimental element was placed in core position 13 (the 1/2 core model uses the symmetrical position 18). The balance of the fuel loading consisted of standard fuel elements. The U-235 and boron contents are given in Table VI.A.2.1. Tables VI.A.2.2 through VI.A.2.4 give the control positions and the lobe and element powers for the depletion of the SW one-half core model. The midplane azimuthal power distributions for the Cycle 8A and 8B calculations are plotted in Figures VI.A.2.1 through VI.A.2.72.

3. Burnup (in Experimental Elements) vs. Cycle Time

A tabulation of the peak fissions in the core hot spot and the experimental elements is given in Table VI.A.3.1. These data are plotted in Figure VI.A.3.1 which also contains the Cycle 8C recycle calculations discussed in VI.B.

B. Cycle 8C and 9A Operation

Cycle 8C will consist of the two experimental elements and 38 new elements fabricated by Atomics International. As in the 8A/B core loading, all AI elements will be inspected by beta-autoradiography and the minimum cladding will be 0.005-inch or greater. The two experimental elements will be interchanged, i.e., XA003F will operate in position 33 and XA004F in position 13. The two experimental elements will be replaced, at the start of Cycle 9A, with partially spent elements from Cycle 7A.

1. Reactor Power, Lobe Power, and Cycle Life

Cycle 8C will operate for 15 days at 242 MW in the 58-44-52-44-44-44 unbalanced mode.

2. Power Density Distribution vs. Time

Because PDQ-7 does not allow partially spent fuel parameters as input, the recycling of experimental elements had to be simulated by leaving the parameters of the experimental elements in place and interchanging the NW and SE experiments and flux traps, i.e., the Cycle 8A and 8B NW lobe became the

Cycle 8C SE lobe. This simulated recycle procedure with experimental elements in core positions 18 and 33 is then equivalent to exchanging the fuel elements in core positions 13 and 33 in the ATR. The U-235 and boron contents of the refueled core positions are the same as for Cycle 8A and are given in Table VI.A.2.1. Tables VI.A.2.2 through VI.A.2.4 give the control positions and the lobe and element powers for depletion of the SW one-half core PDQ-7 model. The midplane azimuthal power distributions are given in Figures VI.B.2.1 through VI.B.2.36.

3. Burnup (in Experimental Elements) vs. Cycle Time, Including Estimated Error Margins

A tabulation of the peak fissions in the experimental elements is given in Table VI.A.3.1. These data, including the estimated error margin, are plotted in Figure VI.A.3.1.

VII. TEST ELEMENT INSPECTION

A. Pre-Irradiation

1. Dimensional

A system was designed to measure changes in length and position of irradiated ATR fuel plates relative to their pre-irradiated configuration. The reference measurements for the experimental elements have been completed and the results are in the Idaho Nuclear Corporation* Engineering Division files. The thickness of the coolant channels in the experimental elements, as recorded on a continuous trace along the azimuthal center and at both sides of the channels, is also on file. The coolant channels are, in all cases, better than the minimum specification requirements.

2. Fuel Content and Homogeneity

Radiographs of the plates in both experimental elements have been reviewed and evaluated for fuel content and homogeneity. Both elements are better than the minimum specification requirements.

3. Beta Autoradiography

All elements in Cycle 8A/B have been examined by beta autoradiography and the minimum clad thickness in the dogbone region is 6 mils. The minimum clad thickness in the dogbone region in the two experimental elements are shown in Table VII.A.3.1.

All elements scheduled for the Cycle 8C/9A core loading will be examined by beta autoradiography and will have a minimum of 5 mils of cladding over the dogbone regions.

* Now Aerojet Nuclear Company (7/1/71).

B. Post-Irradiation

1. Canal Inspections

The post-irradiation canal inspection will consist of fuel plate length measurements, channel thickness measurements and a visual inspection for plate deformations or blistering. The visual inspection will follow the plate length and channel measurements since a good visual inspection requires removal of the end boxes (end boxes are required for the plate length measurements). Following end box removal, the elements will be moved to the inspection window to permit detailed examination.

2. Hot Cell Examinations

Sufficient examination data will be taken so that a performance evaluation can be made. Particular attention will be given to those areas which have attained burnups between 1.5×10^{21} fissions/cc and 1.8×10^{21} fissions/cc. It will, therefore, be necessary to determine burnup gamma scan correlations as with XA3G so that maximum burnup areas can be located. Blister testing will be necessary to locate any incipient failures which are not revealed during the visual and dimension checks or pre-blister metallography. The decrease in channel width as a function of cool down time in the canal, if it exists, will be checked for correlation with the second oxide layer (possibly a deposition layer) as detected by metallography. The weight per unit area as a function of plate temperature and burnup will be determined by density testing before and after stripping to help in the growth rate data reduction and to relate oxide buildup to amount of aluminum loss.

In summary, the examinations will be: (1) visual, (2) oxide survey, (3) thickness surveys stripped and unstripped, (4) burnup surveys, (5) metallography-stability, oxide and blister, (6) densities stripped and unstripped, and (7) blister tests.

APPENDIX V.C.A
REMOVAL OF PARTICULATE FISSION PRODUCTS

APPENDIX V.C.A

REMOVAL OF PARTICULATE FISSION PRODUCTS

The ATR primary system bypass demineralizer is used for cleanup of the particulate fission products from the primary water. This system can be depicted as in Figure V.C.A.1

Fig. V.C.A.1 The ATR primary coolant system.

g = Release rate - $\mu\text{c}/\text{min}$

v = Reactor volume - cc

f = Flow rate - cc/min

Q = Concentration - $\mu\text{c}/\text{cc}$

t = Time - min

n = Total activity - μc

The change in activity can be expressed by the equation: $\frac{dn}{dt} = g + fQ_2 - fQ_1$.

An instantaneous release is to be assumed, so $g = 0$.

The efficiency of the demineralizer is nearly 100% so $fQ_2 = 0$ will be assumed.

$$\frac{dn}{dt} = - \frac{fn}{v}$$

$$\frac{dn}{n} = - \frac{f}{v} dt$$

$$\int_{n_0}^n \frac{dn}{n} = - \frac{f}{v} \int_0^t dt$$

$$\ln n \Big|_{n_0}^n = - \frac{f}{v} t$$

$$\frac{n}{n_0} = e^{-\frac{ft}{v}}$$

$$n = n_0 e^{-\frac{ft}{v}}$$

The reactor volume is approximately 3.07×10^8 cc, and the maximum flow rate through the demineralizer is 1.89×10^6 cc/min.

Example 1

Sample calculation for full core with 10% particulate release from blistered areas. Certain isotopes are of primary interest for waste disposal, so the concentration of these isotopes will be calculated, with the initial inventory being taken from the RSAC computation.

Isotope	Curies/core	Amt. Released Curies	Initial Conc. $\mu\text{c/cc}$	Allowable* Conc. $\mu\text{c/cc}$
Ce-144	1.31×10^7	3.31×10^4	1.07×10^2	6×10^{-3}
Cs-137	3.68×10^4	9.30×10^1	3.03×10^{-1}	1×10^{-3}
I-129	1.21×10^{-3}	3.06×10^{-6}	1.0×10^{-8}	6×10^{-3}
I-131	1.23×10^6	3.11×10^3	1.01×10^1	6×10^{-2}
In-106	6.49×10^4	1.64×10^2	5.3×10^{-1}	2×10^{-3}

*ID Appendix 0510 Part I

Sr-89	4.61×10^6	1.16×10^4	3.78×10^1	1×10^{-2}
Sr-90	3.67×10^4	9.27×10^1	3.02×10^{-1}	4×10^{-5}
Zr-95	4.98×10^6	1.26×10^4	4.1×10^1	8×10^{-2}
Cd-115	3.44×10^3	8.69	2.83×10^{-2}	6×10^{-2}

With 500 gpm bypass demineralizer flow and a reactor volume of 3.066×10^8 cc.

$$n = n_o e^{-\frac{(1.89 \times 10^6)(t)}{3.066 \times 10^8}}$$

$$\text{or } \frac{n}{n_o} = e^{-\frac{(1.89 \times 10^6)(t)}{3.066 \times 10^8}}$$

<u>t, min</u>	<u>t, hrs</u>	<u>n/n_o</u>
600	10	0.0248
900	15	0.00390
1200	20	0.000613
1440	24	0.00013
1800	30	0.000015

These results were plotted and are included as Figure V.C.A.2

Based on the acceptable concentrations as listed in Part I of ID Appendix 510, the following cleanup times have been calculated.

	<u>n/n_o</u>	<u>Cleanup hours</u>
Ce-144	0.000056	~27
Cs-137	0.0033	~16
I-129	-	-
I-131	0.0059	~14
Ru-106	0.0038	~15
Sr-89	0.00026	~23
Sr-90	0.00015	~24
Zr-95	0.0020	~17
Cd-115	0.47	~9

These calculations indicate that 27 hours would be necessary to reduce the Ce-144 activity levels to 6×10^{-3} $\mu\text{c/cc}$. This is the limiting isotope.

The same procedure has been followed for other initial release quantities.

Fig. V.C.A.2 Bypass demineralizer cleanup ratio vs time (assumed no decay).

APPENDIX V.C.B

TABLES

TABLE II.B.1

ATRC VS. CALCULATED LOBE POWERS AND CONTROL POSITIONS

<u>Case</u>	<u>Lobe</u>	<u>Lobe Powers</u>		<u>Difference (%)</u>	<u>Drum Positions</u>		<u>Neck Shims Fully Inserted</u>	
		<u>ATRC</u>	<u>Calc</u>		<u>ATRC</u>	<u>Calc</u>	<u>ATRC</u>	<u>Calc</u>
Core II (clean)	NW	48.1	48.2	0.2	60.8	60.0	1-6	1-6
	C	53.7	53.0	-1.3	--	--	--	--
	SW	50.7	49.8	-1.7	65.7	65.0	1-3, 5, 6 ^[a]	1-3, 5, 6
	SE	49.1	49.3	0.4	63.4	65.0	1-6	1-6
Core II @ Xe Equil.	NW	49.5	49.6	0.2	93.7	90.0	1-4, 6	1-4, 6
	C	48.5	48.5	0.0	--	--	--	--
	SW	51.8	50.9	-1.7	98.3	95.0	1-3, 6	1-3, 6
	SE	50.5	50.1	-0.7	96.1	95.0	1-4, 6	1-4, 6
Core III (clean)	NW	59.2	58.9	-0.5	79.0	80.0	1-6	1-6
	C	54.2	55.0	1.4	--	--	--	--
	SW	38.7	39.0	0.7	26.2	25.0	1-3, 5, 6 ^[a]	1-3, 5, 6
	SE	59.8	58.2	-2.6	79.0	80.0	1-6	1-6
Core III Xe Equil.	NW	57.7	58.4	1.2	117.9	120.0	1-4, 6	1-5
	C	49.3	49.3	0.0	--	--	--	--
	SW	43.2	42.5	-1.6	69.0	70.0	1-3, 6	1-3, 6
	SE	58.1	57.4	-1.2	118.2	120.0	1-4, 6	1-5

[a] SW-4 neck shim @ 30 inches.

TABLE II.B.2

ATRC MEASUREMENT POSITIONS FOR SPECIFIC POWER

<u>Channel</u>	<u>Distance from Center (in.)</u>	<u>Percent from Left</u>
2	-0.87	2.6
2	-0.12	43.5
2	0.87	97.4
6	-1.08	1.72
6	-0.60	23.2
6	-0.12	44.6
6	0.60	76.8
6	1.08	98.3
11	-1.28	3.3
11	-0.70	24.4
11	-0.12	45.6
11	0.70	75.5
11	1.28	96.7
15	-1.60	-0.9
15	-0.86	22.6
15	-0.12	46.2
15	0.86	77.4
15	1.60	100.9
19 large	-1.84	-1.9
19 large	-0.99	22.1
19 large	-0.12	46.6
19 large	0.99	77.9
19 large	1.84	101.9
19 large	-1.57	5.7
19 large	-1.28	13.9
19 large	1.28	86.1
19 large	1.57	94.3
19 small	-1.68	2.6
19 small	-0.90	24.6
19 small	-0.12	46.6
19 small	0.90	75.4
19 small	1.68	97.4
19 small	-1.43	9.6
19 small	-1.16	17.3
19 small	1.16	82.7
19 small	1.43	90.4

TABLE II.D.1

BASE CASE VS. PERTURBED LOBE POWERS AND CONTROL POSITIONS

<u>Lobe</u>	<u>Lobe Powers</u>		<u>Difference (%)</u>	<u>Drum Positions</u>		<u>Neck Shims Inserted</u>	
	<u>Base Case</u>	<u>Perturbed</u>		<u>Base</u>	<u>Perturbed</u>	<u>Base</u>	<u>Perturbed</u>
NW	60.60	60.39	0.3	100	130	1-5	1-6
C	48.33	46.78	3.2	--	--	--	--
SW	42.66	44.18	-3.6	65	80	1-3, 5, 6	1-3, 5, 6
SE	43.88	42.10	-3.0	80	95	1-5	1-6

TABLE III-1

FUEL ELEMENT MATERIALS AND LOADINGS

Type Element	Standard U_3O_8	Standard $UA1_x$	"7F" $UA1_x$
Manufacturer	Sylcor	Atomics International	
Total Wt. ^{235}U - g	975	975	1075
Fuel Loading, g $^{235}U/cc$ Core			
Plates 1, 2, 18, 19	1.22	1.22	0.976
Plates 3, 4, 16, 17	1.22	1.22	1.232
Plates 5 through 15	1.22	1.22	1.488
Fuel Material	U_3O_8	$UA1_x$	$UA1_x$
Core Matrix Material	X8001	X8001	X8001
Burnable Poison Material	B_4C	B_4C	B_4C
Boron Loading, mg nat B/cc Core			
Plates 1, 2, 18, 19	5.2	5.2	14.2
Plates 3, 4, 16, 17	5.2	5.2	7.5
Plates 5 through 15	5.2	5.2	1.1
Plate Cladding Material	6061-OA1	6061-OA1	6061-OA1
Side Plate Material	6061-T6A1	6061-T6A1	6061-T6A1
End Fitting Material	356 A1	356 A1	356 A1

TABLE III-2

OPERATING PARAMETERS OF ATR STARTUP PROGRAM CORES II AND III
FUEL ELEMENTS WHICH WERE DESTRUCTIVELY EXAMINED

Fuel Element Number	Used in Core	Days of Operation	Mode of Operation	Type Fuel Element	Irradiation Position	Max. Power Generated in Fuel Plate Core MW/l
A172C	II	35	250 MW Balanced	U_3O_8	F11	15.9
XA3G	II	35	Balanced	"7F" $UA1_x$	F13	15.7
XA8G	II	35	Balanced	"7F" $UA1_x$	F14	13.2
A16D	III	27	250 MW 40-50-60 Unbalanced	Std. $UA1_x$	F11	15.4
XA20G	III	27	Unbalanced	"7F"	F13	18.6

TABLE III-3

POST-IRRADIATION EXAMINATION ON ATR CORES II AND III FUEL ELEMENTS

Core Position	Fuel Element	Plate Number	Measure Length	Visual Inspection	Oxide Thickness Survey	Oxide Thickness Max. Only	Detailed Gamma Scan	Gamma Scan Only	Thick. Measurements Survey	Thick. Measurements Max. Only	Oxide Met. Sample	Chem. Strip	Thickness Measurement	Determine Density	B.U. Samples	Met. Samples	Blister Samples
13	XA3G	1	X	X	X		X		X			X	X	X	X		
		2	X	X				X		X	X					X	X
		3	X	X	X		X		X			X	X	X	X		
		4		X				X		X							
		5	X	X	X		X		X			X	X	X	X		
		6		X		X		X		X							
		7		X				X		X							X
		8		X		X		X		X							
		9		X		X		X		X	X						
		10	X	X	X		X		X			X	X	X	X		
		11		X		X		X		X							
		12		X		X		X		X							
		13		X		X		X		X							
		14		X		X		X		X	X					X	
		15	X	X	X		X		X			X	X	X	X	X	X
		16		X				X		X	X					X	X
		17	X	X	X		X		X			X	X	X	X		
		18	X	X		X		X		X							
		19	X	X	X		X		X			X	X	X	X		

TABLE III-3 (Contd.)

Core Position	Fuel Element	Plate Number	Visual Inspection	Oxide Thickness Survey	Oxide Thickness Max. Only	Detailed Gamma Scan	Gamma Scan Only	Thick. Measurements Survey	Thick. Measurements Max. Only	Oxide Met. Sample	B.U. Samples	Met. Samples	Blister Samples
14	XA8G	1	X	X		X		X					
		2	X		X		X		X	X			
		3	X	X		X		X					
		4	X		X		X		X				
		5	X	X		X		X			X		
		6	X		X		X		X				
		7	X		X		X					X	X
		8	X		X		X		X				
		9	X		X		X		X	X			
		10	X	X		X		X			X		
		11	X		X		X					X	X
		12	X		X		X		X				
		13	X		X		X		X				
		14	X		X		X		X				
		15	X	X		X		X					
		16	X		X		X		X				
		17	X	X		X		X					
		18	X		X		X		X	X			
		19	X	X		X		X			X		

TABLE III-3 (Contd.)

Core Position	Fuel Element	Plate Number	Visual Inspection	Oxide Thickness Survey	Oxide Thickness Max. Only	Detailed Gamma Scan	Gamma Scan Only	Thick. Measurements Survey	Thick. Measurements Max. Only	Oxide Met. Sample	Thickness Measurement	B.U. Samples	Blister Samples
11 (Std. U_3O_8)	A172C	1	X	X		X		X			X	X	
		2	X	X		X		X		X	X	X	X
		3	X		X		X		X				
		4	X		X		X		X				
		5	X		X		X		X				
		6	X		X		X		X				
		7	X		X		X		X				
		8	X		X		X		X				
		9	X		X		X		X				
		10	X	X		X		X			X	X	
		11	X		X		X		X				
		12	X		X		X		X				
		13	X		X		X		X				
		14	X		X		X		X				
		15	X		X		X		X				
		16	X		X		X		X				
		17	X		X		X		X				
		18	X	X		X		X		X	X		X
		19	X	X		X		X			X		

TABLE III-3 (Contd.)

<u>Core Position</u>	<u>Fuel Element</u>	<u>Plate Number</u>	<u>Measure Length</u>	<u>Visual Inspection</u>	<u>Oxide Thickness Survey</u>	<u>Oxide Thickness Max. Only</u>	<u>Detailed Gamma Scan</u>	<u>Gamma Scan Only</u>	<u>Oxide Met. Sample</u>	<u>B.U. Samples</u>
14	XA20G	1	X	X		X				
		2	X	X		X		X		
		3	X	X	X		X		X	
		4		X		X		X		
		5	X	X	X		X		X	X
		6		X		X		X		
		7		X		X		X		
		8		X		X		X		
		9		X		X		X		
		10	X	X	X		X		X	X
		11		X		X		X		
		12		X		X		X		
		13		X		X		X		
		14		X		X		X		
		15	X	X	X		X		X	
		16		X		X		X		
		17	X	X	X		X			
		18	X	X		X		X		
		19	X	X	X		X		X	X

TABLE III-3 (Contd.)

<u>Core Position</u>	<u>Fuel Element</u>	<u>Plate Number</u>	<u>Measure Length</u>	<u>Visual Inspection</u>	<u>Oxide Thickness Survey</u>	<u>Oxide Thickness Max. Only</u>	<u>Detailed Gamma Scan</u>	<u>Gamma Scan</u>	<u>B. U. Samples</u>
11 (Std. UA1 _x)	A16D	1		X	X		X		X
		2		X	X		X		X
		3		X		X		X	
		4		X		X		X	
		5		X		X		X	
		6		X		X		X	
		7		X		X		X	
		8		X		X		X	
		9		X		X		X	
		10		X	X		X		X
		11		X		X		X	
		12		X		X		X	
		13		X		X		X	
		14		X		X		X	
		15		X		X		X	
		16		X		X		X	
		17		X		X		X	
		18		X	X		X		
		19		X	X		X		

TABLE III-4

WORST CASE DATA ON ATR CORES II AND III FUEL ELEMENTS

<u>Fuel Element Number</u>		<u>A172C</u>			<u>XA3G</u>		<u>XA8G</u>		<u>XA20G</u>	
Type element		U ₃ O ₈ Standard			UA1 _x - "7F"		UA1 _x - "7F"		UA1 _x - "7F"	
Hot Plate Number		1			15		15		15	
Peak B.U. in Hot Plate - F/cc		1.42 x 10 ²¹ (5)			1.5 x 10 ²¹		1.4 x 10 ²¹		1.5 x 10 ²¹	
Location of Peak										
Long. - in		24 3/4			24 3/4		24 3/4		24	
Trans (1)		A - C _L - C			C+		C		C	
Plate Thickness at Peak BU										
Before Strip - in		0.0795			0.0499		0.0489		Not Measured	
After Strip - in		N.A.			0.0480		Not Measured		Not Measured	
Change/2 - mils		N.A.			0.95		Not Measured		Not Measured	
Oxide Thickness(2) at Peak B.U.										
%	Convex - mils	1.32	1.18	1.21	1.02		0.61		0.65	
	Concave - mils	0.46	0.58	0.49	0.89		1.19		0.72	
	Ave - mils	0.89	0.88	0.85	0.96		0.90		0.68	
<u>Max. Plate Thickness</u>										
Before Strip - in.		0.0795	0.0795		0.0505	0.0505	0.0489	0.0489	Not Measured	
After Strip - in.		Not Measured			0.0477	0.0478	Not Measured		Not Measured	
Change/2 - mils		Not Measured			1.40	1.35	Not Measured		Not Measured	
Oxide Thickness										
Convex - mils		1.18	1.21		0.00	0.87	0.75	0.43	Not Measured	
Concave - mils		0.58	0.49		1.56	1.09	0.96	0.78	Not Measured	
Ave - mils		0.88	0.85		0.78	0.98	0.86	0.61	Not Measured	
Location										
Long		24 3/4	24 3/4		18 3/4	18 3/4	22 3/4	22 3/4	Not Measured	
Trans.		C _L	C		C _L	C	A	C	Not Measured	
B.U. - Fiss/cc		1.42 x 10 ²¹			1.2 x 10 ²¹		10 x 10 ²¹	14 x 10 ²¹	Not Measured	

TABLE III-4 (Contd.)

Fuel Element No.	A172C		XA3G		XA8G		XA20G
Plate Thickness Over PF							
Before Strip - in.	Not Measured	0.0784 ⁽⁶⁾	0.0480	0.0487 ⁽⁷⁾	0.0470 ⁽⁶⁾	0.0471 ⁽⁶⁾	Not Measured
After Strip - in.	Not Measured	N.A.	0.0475	0.0470			Not Measured
Apparent Core Swelling %							
Before Strip							
At Peak B.U.	5.5		6.0		9.5	9.0	Not Measured
At Max. Thick	5.5		8.5		9.5	9.0	Not Measured
After Strip							
At Peak B.U.	Not Measured		5.0		Not Measured		Not Measured
At Max. Thick.	Not measured		4.0		Not Measured		Not Measured
Min. Blister - Temp.	750 °F = 400°C ⁽³⁾		900°F = 480°C ⁽⁴⁾		No Blisters at 1100°F		
B.U. - Fiss/cc	(5)	(5)	1.0 to 1.5 x 10 ²¹		on plates 7 and 11		

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Notes:

- (1) Because of flux peaking the maximum B.U. occurs on plate edge - off of "C" plane.
- (2) Dermitron measurements.
- (3) On plate 18
- (4) This was minimum blister temperature.
- (5) These values are being verified.
- (6) Top
- (7) Bottom

TABLE III-5

MAXIMUM OXIDE THICKNESS OBSERVED ON CONVEX SIDES
OF FUEL PLATES - mils [a]

Plate Number	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>	<u>6</u>	<u>7</u>	<u>8</u>	<u>9</u>	<u>10</u>
XA3G	1.35		1.19		1.52	1.24		1.12	1.05	0.51
XA8G	1.13	0.92	0.95	0.94	1.17	1.00	1.05	0.97	0.85	1.05
XA20G	1.13	1.16	1.16	1.04	1.04	0.76	0.73	0.70	0.71	0.76
Al72C	1.48	1.75	1.68	1.35	1.40	1.28	1.43	0.97	1.28	1.08

Plate Number	<u>11</u>	<u>12</u>	<u>13</u>	<u>14</u>	<u>15</u>	<u>16</u>	<u>17</u>	<u>18</u>	<u>19</u>
XA3G	1.04	1.40	0.98	0.99	1.02		0.84	0.67	0.51
XA8G	0.63	0.92	0.88	0.78	0.95	0.60	0.84	0.60	0.49
XA20G	0.56	0.75	0.73	0.52	0.68	0.65	0.68	0.61	0.72
Al72C	1.12	1.08	0.83	0.62	0.87	0.62	0.83	1.03	1.32

[a] These measurements were made in the hot cell using an eddy current device and 1/2 inch diameter probe.

TABLE III-6

RESULTS OF POST-IRRADIATION BLISTER TESTS PERFORMED ON PLATES FROM ATR
FUEL ELEMENTS

Element No.	Plate No.	Sample Location Inches from Top of Plate	$^{\circ}\text{F}$ Temperature	$^{\circ}\text{C}$ Temperature	Burnup Fiss/cc x 10^{-20}	Remarks
XA3G	2	21-28	900	482	10.5	
"	7	0-7	1050	565	6.5	
"	7	7-14	1050	565	8.5	
"	7	14-21	1050	565	9.5	
"	7	21-28	1050	565	9.5	
"	7	28-35	1050	565	9.5	
"	7	35-42	1050	565	8.0	
"	7	42-48	1050	565	6.0	
"	15	25 3/4-32 3/4	1000	537	15.0	
"	16	21-28	900	482	11.0	
XA8G	7	top dogbone	>1100	598	~3.0	Test terminated at 1100 $^{\circ}\text{F}$ without blistering
"	11	top dogbone	>1100	593	~3.0	
"	11	bottom dogbone	>1100	593	~3.0	
Al72C	2	0-7	950	510	9.0	
"	2	7-14	900	482	11.0	
"	2	14-21	850	454	13.0	
"	2	21-28	850	454	13.0	
"	18	0-7	850	454	7.0	Edge of sample
"	18	0-7	900	482	6.0	Center of sample
"	18	7-14	900	482	8.0	
"	18	14-21	850	454	9.0	
"	18	21-28	750	398	9.2	

TABLE III-7

MAXIMUM THICKNESS READINGS OBTAINED BEFORE STRIPPING - mils⁽¹⁾

Plate No.	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>	<u>6</u>	<u>7</u>	<u>8</u>	<u>9</u>	<u>10</u>	<u>11</u>	<u>12</u>	<u>13</u>	<u>14</u>	<u>15</u>	<u>16</u>	<u>17</u>	<u>18</u>	<u>19</u>
XA3G	82.0	50.3	51.3	50.4	50.4	50.1	50.3	50.3	50.1	51.0	50.4	50.2	50.1	50.2	50.5	50.2	51.5	50.1	100.4
XA8G	81.6	52.0	50.2	50.4	49.8	50.4		50.1	49.8	49.3		49.5	49.2	49.3	48.9	48.3	49.3	48.9	99.8
Al72C	79.5	50.4	50.3	49.9	50.5	50.3	49.9	50.8	49.8	50.4	50.4	50.1	49.5	49.9	50.7	50.7	51.1	49.7	100.4

Note: (1) The specified total plate thickness (IN-F-9-ATR) prior to irradiation for plate 1, plates 2 through 18 and plate 19 are 80 +0 mils, 50 +0 mils, and 100 + 0 mils, respectively.

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-2

-2

TABLE III-8

LENGTH MEASUREMENTS FOR FUEL PLATES IN ATR
 "7F" ELEMENT NUMBER XA3G

Plate Number	<u>1</u>	<u>2</u>	<u>3</u>	<u>17</u>	<u>18</u>	<u>19</u>
As Assembled Pre-Irradiation - in.	49.490	49.498	49.496	49.498	49.493	49.501
As Assembled Post-Irradiation - in.	49.492	49.498	49.497	49.488	49.491	49.496
Disassembled Post-Irradiation - in.	49.473	49.490	49.489	49.492	49.492	49.488
Length Changes:						
As Assembled Pre to Post - in.	+0.002	0	+0.001	-0.010	-0.002	-0.005
% Δ l/l	+0.004	0	0.002	-0.020	-0.004	-0.010
After removing side plates - in.	-0.019	-0.008	-0.008	+0.004	+0.001	-0.008
% Δ l/l	-0.038	-0.016	0.016	+0.008	+0.002	-0.016

TABLE III-9

BURNUP ANALYSIS RESULTS FOR ELEMENTS EXAMINED FROM CORES II AND III

Sample Number ⁽¹⁾		²³⁵ U Atoms-% Fissioned ASTM 1 Isotopic Method	U Atoms-% Fissioned ¹⁴⁸ Nd Method	Fission Density ⁽³⁾ Calculated from ¹⁴⁸ Nd Method-F/ccx10 ⁻²¹
XA3G	1-9	33.4	37.8 ⁽²⁾	1.02
"	1-10	30.0	34.0 ⁽²⁾	0.958 ⁽⁴⁾
"	1-11	33.0	37.8	1.02
"	3-9	26.7	30.2 ⁽²⁾	1.02
"	3-10	23.3	28.7	0.973
"	3-11	26.1	29.6 ⁽²⁾	1.00
"	5-9	23.1	26.2 ⁽²⁾	1.07
"	5-10	20.0	24.9	1.02
"	5-11	23.3	26.5 ⁽²⁾	1.09
"	10-11	23.0	26.1	1.07
"	10-12	18.1	20.8 ⁽²⁾	0.852
"	10-13	23.6	26.7 ⁽²⁾	1.09
"	15-11	27.2	30.9 ⁽²⁾	1.27
"	15-12	22.9	26.0 ⁽²⁾	1.07
"	15-13	28.1	31.9 ⁽²⁾	1.31
"	17-11	33.2	37.7	1.28
"	17-12	29.1	32.8	1.11
"	17-13	32.5	36.3	1.23
"	19-1	11.4	13.3 ⁽²⁾	0.358
"	19-2	24.3	27.7 ⁽²⁾	0.745
"	19-4	34.3	38.8 ⁽²⁾	1.04
"	19-8	35.5	40.1 ⁽²⁾	1.08
"	19-11	37.9	42.8	1.15
"	19-12	40.4	45.4	1.16 ⁽⁴⁾
"	19-13	41.1	46.5	1.25
"	19-15	34.2	38.7	1.04
"	19-17	24.8	28.3	0.761
"	19-18	12.5	14.7	0.395
XA8G	5-10	18.4	22.9 ⁽²⁾	0.939
"	10-12	16.5	19.1 ⁽²⁾	0.514
"	19-12	31.9	36.3 ⁽²⁾	0.976
"	19-15	28.4	32.5 ⁽²⁾	0.874
"	19-17	19.1	22.1 ⁽²⁾	0.594
XA20G	19-17		24.2	0.651
"	5-10		23.8	0.976
"	10-12		20.3	0.832
"	10-1 ⁽⁵⁾		5.0	0.205
"	19-12		39.1	1.05

TABLE III-9 (Contd.)

Sample Number ⁽¹⁾	²³⁵ U Atoms-% Fissioned	U Atoms-% Fissioned	Fission Density ⁽³⁾ Calculated
	ASTM 1 Isotopic Method	¹⁴⁸ Nd Method	from ¹⁴⁸ Nd Method-F/ccx1021
A172C 2-10		38.4	1.29
" 10-12		26.6	0.894
" 10-1		10.6	0.356
" 1-10		42.3	1.42
" 1-1		14.9	0.501
A16D 1-1 ⁽⁵⁾		11.4	0.383
" 10-1 ⁽⁵⁾		4.5	0.151
" 1-10		35.8	1.20
" 2-10		32.1	1.08
" 10-12		21.3	0.716

-
- (1) The first number denotes the plate. The second number gives the position according to Figure III-10.
- (2) These numbers were taken from Figure III and therefore represent estimated data.
- (3) These numbers were calculated from the ¹⁴⁸Nd % fissioned values above by multiplying by the nominal U atoms/cc for each plate type.
- (4) These samples were repunched at 1/4 inch from the position indicated along the longitudinal center line.
- (5) This sample was taken 2.75 inches from the top of the plate along the longitudinal center line.

TABLE III-10

PLATE-TO-PLATE DISTRIBUTION OF BURNUP IN XA20G

Plate Number	A	B	$\frac{B - A}{B} \times 100\%$
	Maximum Longitudinal Center Line Burnup-Fiss/cc x 10 ⁻²¹	Maximum Plate Burnup Fiss/cc x 10 ⁻²¹	
1	Not Gamma Scanned		
2	0.896		
3	1.02	1.32	22.7
4	0.876		
5	1.07	1.43	25.2
6	0.955		
7	0.906		
8	0.856		
9	0.860		
10	0.856	1.31	34.3
11	0.876		
12	0.886		
13	0.876		
14	0.965		
15	1.03	1.50	31.3
16	0.896		
17	0.995	1.31	23.7
18	0.925		
19	0.918	1.12	18.0

TABLE III-11

PLATE-TO-PLATE DISTRIBUTION OF BURNUP IN A172C

Plate Number	A	B	$\frac{B - A}{B} \times 100\%$
	Maximum Longitudinal Center Line Burnup-Fiss/cc x 10 ⁻²¹	Maximum Plate Burnup Fiss/cc x 10 ⁻²¹	
1	1.42		
2	1.28	1.48	13.5
3	1.22		
4	1.12		
5	1.13		
6	1.02		
7	1.03		
8	0.990		
9	1.00		
10	0.863	1.12	22.9
11	0.952		
12	0.950		
13	0.920		
14	0.940		
15	0.937		
16	0.940		
17	0.926		
18	0.904	1.08	16.3
19	0.962	1.17	17.8

TABLE III-12

PLATE-TO-PLATE DISTRIBUTION OF BURNUP IN XA3G

<u>Plate Number</u>	A	B	$\frac{B - A}{B} \times 100\%$
	Maximum Longitudinal Center Line Burnup <u>fiss/cc x 10⁻²¹</u>	Max. Plate Burnup <u>fiss/cc x 10⁻²¹</u>	
1	1.040	1.123	7.39
2	0.966		
3	1.074	1.357	20.85
4	0.945		
5	1.069	1.419	25.02
6	1.031		
7	0.913		
8	0.949		
9	0.940		
10	0.943	1.380	31.67
11	0.945		
12	0.962		
13	1.019		
14	1.063		
15	1.102	1.502	26.63
16	1.060		
17	1.164	1.437	19.00
18	0.987		
19	1.035	1.230	15.85

TABLE III-13

INC-16-1 ESTIMATED BURNUP AND OPERATING TEMPERATURE RANGES

<u>Fuel Composition</u>	<u>Number of Sample Fuel Plates</u>	<u>Range of Irradiation Temperature (Fuel C), °F</u>	<u>Range Burnup, 10²¹ fiss/cc</u>
ATR Regular	18	180 - 385	1.0 - 2.0
ATR 7F High	18	195 - 335	1.4 - 2.3
ATR 7F Low	16	200 - 300	1.0 - 1.5
ATR 7F High 0.030 inch Core	13	195 - 310	1.0 - 1.5
ETR UA1 _x	17	160 - 300	0.9 - 1.6
ETR B ₄ C Foil 3.8 g B/element	16	250 - 305	1.6 - 1.8
ETR B ₄ C Foil 4.6 g B/element	12	245 - 300	1.6 - 1.8

TABLE III-14

INC-16-1 MAXIMUM OXIDE THICKNESSES MEASURED IN CANAL,
IN mils

<u>Fuel Composition</u>	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>Tier</u> <u>5</u>	<u>6</u>	<u>7</u>	<u>8</u>	<u>9</u>
ATR Regular	0.85	0.78	0.74	0.68					
ATR 7F High	1.00			0.75				0.60	0.69
ATR 7F Low		0.93	0.77					0.57	0.68
ATR 7F High 0.030" Core	0.99			0.74				0.52	0.60
ETR UA1 _x	0.99	0.69	0.71	0.94	0.67	0.71	0.79		0.57
ETR B ₄ C Foil 3.8 g/element					0.76	0.76	0.76		
ETR B ₄ C Foil 4.6 g/element					0.69	0.78	0.79		

TABLE III-15

DESCRIPTION OF TEST CERMET FUEL PLATES FOR THE FIRST CAPSULE TO BE IRRADIATED
IN THE HIGH FLUX ISOTOPE REACTOR TO A TARGET FISSION DENSITY OF 2.5×10^{21} FISSIONS/cm³
(Maximum for UAl_x-Bearing Plates) (11)

Reference Irradiation Position	Type of Test Plate (a)	Core Composition (b)		235U Enrichment in Fuel Particle (wt %)	235U Loading		Distribution of Fuel Particle Sizes	
		wt %	Type of Dispersoid		Total (g)	Volumetric Core Composition (g/cm ³)	Upper Range (μm)	Diameter <44 μm (wt %)
11	Mini (c)	52	Arc-Cast UAl _x	0.2	0.002	0.00	44-149	25
21	Mini	53	Arc-Cast UAl _x	0.2	0.002	0.00	44-149	25
31	Mini (d)	54	Arc-Cast UAl _x	0.2	0.002	0.00	44-149	25
12	Mini	47	High-Fired U ₃ O ₈	93.2	1.105	1.42	44-88	10
22	Mini	50	Burned U ₃ O ₈	93.2	1.105	1.42	44-88	10
32	Mini (c,e)	51	Arc-Cast UAl _x	93.2	1.012	1.30	44-149	25
01-02	Instrumented	47	High-Fired U ₃ O ₈	93.2	2.432	1.42	44-88	10
13	Mini	40	High-Fired U ₃ O ₈	93.2	0.887	1.14	44-88	10
23	Mini	42	Burned U ₃ O ₈	93.2	0.887	1.14	44-88	10
33	Mini	53	Arc-Cast UAl _x	0.2	0.002	0.00	None	100
14	Mini	53	Arc-Cast UAl _x	93.2	1.012	1.30	44-149	25
24	Mini (e)	52	Arc-Cast UAl _x	93.2	1.012	1.30	44-149	25
34	Mini	53	Arc-Cast UAl _x	93.2	1.012	1.30	None	100
03-04	Instrumented	44	High-Fired U ₃ O ₈	93.2	2.226	1.30	44-88	10
15	Mini	63	Arc-Cast UAl _x	93.2	1.253	1.61	44-149	25
25	Mini (d)	64	Arc-Cast UAl _x	93.2	1.253	1.61	44-149	25
35	Mini	63	Arc-Cast UAl _x	93.2	1.253	1.61	None	100
16	Mini	40	High-Fired U ₃ O ₈	0.2	0.002	0.00	44-88	10
26	Mini	40	High-Fired U ₃ O ₈	0.2	0.002	0.00	44-88	50
36	Mini	24	Alpha Al ₂ O ₃		0.000	0.00	149-44	>50
05-06	Instrumented (f)	51	High-Fired U ₃ O ₈	93.2	2.757	1.61	44-88	10

(a) Cladding is type 6061 Al alloy.

(b) Matrix is type 1100 Al alloy.

(c) Plates hot rolled to final gage

(d) Plates cold rolled 40% in thickness to final gage.

(e) Plates may be pressed after hot rolling to lower fabricated void content.

(f) Maximal operating core temperature of 150°C for the capsule will occur in this plate.

TABLE V.C.1

EXPERIMENTAL BENDING STIFFNESS PARAMETER

<u>l/b</u>	<u>α</u>
0.139	50.36
0.25	15.34
0.50	3.40
0.75	1.28
1.0	0.61
1.5	0.21
2.0	0.10
2.5	0.06
3.0	0.04

TABLE V.C.2

STATIC PRESSURES AND VELOCITIES FOR CHANNELS 17, 18, AND 19 AT MINIMUM CHANNEL 18

<u>Channel</u>	<u>Element ΔP (psid)</u>	<u>Temperature 150°F</u>		<u>Temperature 350°F</u>	
		<u>Relative Static Press. (psi)</u>	<u>Throat Velocity (ft/sec)</u>	<u>Relative Static Press. (psi)</u>	<u>Throat Velocity (ft/sec)</u>
17	17	1.9	27.8	0.0	37.0
18	17	-5.8	42.5	1.0	36.5
19	17	3.8	21.0	4.2	25.2
18	25	-5.0	40.8		
19	25	4.0	21.0		
18	50	-18.1	74.0	8.4	70.4
19	50	12.0	36.5	20.9	45.1
18	75	-24.3	88.5	4.0	76.4
19	75	18.3	46.7	22.0	60.6
18	100	-27.0	98.5	6.5	89.0
19	100	24.7	52.8	11.7	72.9
18	125	-51.0	121.9	27.0	91.3
19	125	28.0	59.0	14.0	79.6
18	150	-51.0	130.3	26.0	94.0
19	150	34.0	62.8	20.0	84.7
18	175	-30.0	133.2	55.0	77.5
19	175	45.0	66.3	7.0	99.2
17	200			-35.0	136.1
18	200	8.0	122.5	11.0	121.8
19	200	40.0	76.9	18.0	111.1
17	230	0.0	116.8		
18	230	3.0	120.0		
19	230	52.0	83.5		

TABLE VI.A.2.1

FUEL ELEMENT U-235 AND BORON CONTENT AS
INPUT TO THE CYCLE 8 PDQ-7 CALCULATIONS

<u>Fuel Element Type</u>	<u>U-235 Content (g)</u>	<u>Natural Boron Content (g)</u>	<u>B-10 Content (g)</u>
Standard	975	4.136	0.757
7F	1075	4.100	0.750
Special Long Term Burnup	1075	4.835	0.885

TABLE VI.A.2.2

SUMMARY OF ATR SHIM POSITIONS DURING DEPLETION OF THE ATR CYCLE 8 SW ONE-HALF CORE MODEL

Time at Power (days)	Total Core Power (MW)	NW Lobe		SW Lobe		SE Lobe		Center Fixed Shims
		Outer Shims (Degrees)	Neck Shims Inserted	Outer Shims (Degrees)	Neck Shims Inserted	Outer Shims (Degrees)	Neck Shims Inserted	
ATR CYCLE 8A								
0.0	236.7	55	1,2,3,4,5,6	35	1,2,3,4,5,6	45	1,2,3,4,5,6	4
0.5	231.8	80	1,2,3,4,5,6	70	1,2,3,4,5,6	75	1,2,3,4,5,6	4
1.0	218.9	80	1,2,3,4,5	85	1,2,3,6	90	1,2,3,4,5	4
3.0	220.8	75	1,2,3,4	85	1,2,3,6	90	1,2,3,4	4
10.0	220.8	75	1,2,3,4	85	1,2,3,6	90	1,2,3,4	4
15.0	220.8	75	1,2,3,4	85	1,2,3,6	90	1,2,3,4	4
ATR CYCLE 8B								
0.0	244.2	90	1,2,3,4,5,6	60	1,2,3,4,5,6	70	1,2,3,4,5,6	4
0.5	237.2	100	1,2,3,4,5	75	1,2,3,6	80	1,2,3,4,5	4
1.0	238.2	100	1,2,3,4	75	1,2,3,6	80	1,2,3,4	4
3.0	237.2	105	1,2,3,4	80	1,2,3,6	85	1,2,3,4	4
10.0	238.2	115	1,2,3	85	1,2,3	90	1,2,3	4
14.79	236.2	125	1,2,3	90	1,2,3	95	1,2,3	4
ATR CYCLE 8C								
0.0	244.2	80	1,2,3,4,5,6	40	1,2,3,4,5,6	55	1,2,3,4,5,6	4
0.5	239.2	95	1,2,3,4,5,6	65	1,2,3,5,6	75	1,2,3,4,5,6	4
1.0	239.2	105	1,2,3,4,5	70	1,2,3,5,6	85	1,2,3,4,5	4
3.0	239.2	105	1,2,3,4,5	70	1,2,3,5,6	85	1,2,3,4,5	4
10.0	237.2	115	1,2,3,4,5	75	1,2,3,5,6	85	1,2,3,4,5	4
15.0	236.2	125	1,2,3,4,5	80	1,2,3,5,6	90	1,2,3,4,5	4

TABLE VI.A.2.3

SUMMARY OF ATR LOBE POWER AND CALCULATED Keff DURING DEPLETION OF THE ATR CYCLE 8 SW ONE-HALF CORE MODEL

Time at Power (days)	Total Core Power (MW)	NW Lobe	Lobe Power (MW)			Keff Multiplication Factor Calculated By PDQ-7
			Center Lobe	SW Lobe	SE Lobe	
<u>CYCLE 8A</u>						
0.0	236.7	53.2	54.7	42.9	43.0	0.9847
0.5	231.8	52.1	48.1	44.6	44.5	0.9925
1.0	218.9	45.1	43.4	43.7	42.9	1.0002
3.0	220.8	43.4	44.8	44.2	44.3	1.0031
10.0	220.8	43.6	44.7	43.9	44.6	1.0024
15.0	220.8	43.9	44.5	43.8	44.9	1.0015
<u>CYCLE 8B</u>						
0.0	244.2	60.2	50.2	44.3	45.2	0.4962
0.5	237.2	58.2	47.4	43.9	43.9	0.9977
1.0	238.2	58.0	48.2	44.1	44.0	1.0007
3.0	237.2	57.0	47.3	44.6	43.8	1.0058
10.0	238.2	56.6	47.4	44.9	44.3	1.0125
14.79	236.2	55.6	46.0	45.1	44.3	1.0098
<u>CYCLE 8C</u>						
0.0	244.2	60.6	54.5	43.0	43.1	0.9943
0.5	239.2	57.5	49.8	44.7	42.6	0.9939
1.0	239.2	58.1	48.6	48.7	45.1	0.9967
3.0	239.2	58.1	48.7	43.6	45.3	0.9928
10.0	237.2	58.5	47.3	44.0	43.4	0.9939
15.0	236.2	58.0	46.1	44.7	42.8	0.9967

TABLE VI.A.2.4

SUMMARY OF FUEL ELEMENT POWERS DURING DEPLETION OF THE ATR CYCLE 8 SW ONE-HALF CORE MODEL

Time at Power (days)	Total Core Power (MW)	Power (MW) for Element Numbers																			
		<u>16</u>	<u>17</u>	<u>18</u>	<u>19</u>	<u>20</u>	<u>21</u>	<u>22</u>	<u>23</u>	<u>24</u>	<u>25</u>	<u>26</u>	<u>27</u>	<u>28</u>	<u>29</u>	<u>30</u>	<u>31</u>	<u>32</u>	<u>33</u>	<u>34</u>	<u>35</u>
<u>CYCLE 8A</u>																					
0.0	236.7	4.0	4.9	6.2	6.5	6.6	6.7	6.6	6.1	4.6	3.7	3.8	4.7	6.5	6.9	7.1	7.1	7.6	7.4	6.3	5.3
0.5	231.8	4.6	5.1	5.9	5.7	5.7	5.9	6.1	6.1	5.2	4.6	4.6	5.3	6.4	6.4	6.3	6.2	6.8	7.0	6.4	5.9
1.0	218.9	4.9	5.2	5.8	5.5	5.2	5.4	5.6	5.9	5.3	4.9	4.9	5.3	6.1	5.8	5.6	5.5	6.1	6.1	5.4	5.0
3.0	220.8	5.0	5.3	6.0	5.9	5.5	5.6	5.8	6.0	5.3	4.9	4.9	5.3	6.1	6.0	5.8	5.6	6.2	5.9	5.1	4.5
10.0	220.8	5.0	5.4	6.1	5.9	5.5	5.5	5.8	5.9	5.2	4.8	4.8	5.3	6.0	6.0	5.8	5.6	6.2	6.0	5.1	4.5
15.0	220.8	5.1	5.4	6.1	5.9	5.4	5.5	5.8	5.9	5.2	4.8	4.8	5.3	6.0	5.9	5.7	5.6	6.2	6.0	5.2	4.5
<u>CYCLE 8B</u>																					
0.0	244.2	4.7	5.4	6.4	6.1	6.0	6.1	6.2	6.1	5.1	4.3	4.3	5.3	6.5	6.4	6.5	6.6	7.4	8.0	7.6	7.2
0.5	237.2	4.8	5.2	6.1	5.9	5.6	5.7	5.9	5.9	5.1	4.6	4.6	5.3	6.3	6.2	6.2	6.2	7.1	7.6	7.3	7.0
1.0	238.2	4.7	5.2	6.1	6.1	5.7	5.8	6.1	5.9	5.1	4.5	4.6	5.2	6.3	6.4	6.3	6.3	7.3	7.6	7.2	6.8
3.0	237.2	4.7	5.2	6.0	6.0	5.6	5.7	6.0	6.0	5.2	4.7	4.8	5.3	6.3	6.3	6.1	6.1	7.2	7.5	7.1	6.8
10.0	238.2	4.8	5.2	6.1	6.0	5.7	5.8	6.2	6.0	5.2	4.8	4.8	5.3	6.3	6.4	6.1	6.2	7.2	7.4	7.1	6.7
14.79	236.2	4.9	5.3	6.0	5.9	5.5	5.6	6.1	5.9	5.3	4.9	5.0	5.4	6.2	6.3	5.9	6.0	7.0	7.2	7.0	6.6
<u>CYCLE 8C</u>																					
0.0	244.2	4.2	5.1	5.7	6.5	6.5	6.6	6.5	6.1	4.6	3.8	3.8	4.8	6.5	6.9	7.1	7.1	8.0	7.6	7.6	7.0
0.5	239.2	4.6	5.2	5.6	5.9	5.8	6.1	6.2	6.1	5.1	4.4	4.5	5.3	6.5	6.5	6.5	6.5	7.2	7.2	7.3	6.9
1.0	239.2	5.0	5.5	5.8	6.1	5.8	5.9	6.0	6.0	5.0	4.4	4.4	5.2	6.3	6.3	6.3	6.3	7.3	7.2	7.4	7.1
3.0	239.2	5.1	5.6	5.9	6.2	5.8	5.9	6.0	6.0	5.0	4.4	4.4	5.2	6.3	6.3	6.3	6.3	7.3	7.2	7.4	7.1
10.0	237.2	4.9	5.3	5.6	5.9	5.6	5.7	5.9	5.9	5.1	4.6	4.6	5.3	6.3	6.2	6.1	6.2	7.2	7.1	7.7	7.3
15.0	236.2	4.9	5.3	5.4	5.8	5.4	5.6	5.8	6.0	5.2	4.8	4.9	5.4	6.4	6.1	6.0	6.0	7.1	6.9	7.7	7.3

TABLE VI.A.3.1

MAXIMUM FISSIONS IN THE ATR CYCLE 8 SW ONE-HALF CORE MODEL

Depletion Time (days)	MWd	Element 33, Plate 15 Mesh (222-223, 41-42)		Element 28, Plate 19 Mesh (176-177, 66-67)		Element 18, Plate 15 Mesh (57-58, 41-42)	
		Homog. (fiss/cc)	Peak (fiss/cc)	Homog. (fiss/cc)	Peak (fiss/cc)	Homog. (fiss/cc)	Peak (fiss/cc)
CYCLE 8A							
1.0	234.3	5.01×10^{18}	$.0463 \times 10^{21}$	4.54×10^{18}	$.0582 \times 10^{21}$	4.03×10^{18}	$.037 \times 10^{21}$
3.0	672.1	1.38×10^{19}	$.128 \times 10^{21}$	1.25×10^{19}	$.160 \times 10^{21}$	1.28×10^{19}	$.118 \times 10^{21}$
10.0	2217.7	4.26×10^{19}	$.394 \times 10^{21}$	3.96×10^{19}	$.507 \times 10^{21}$	4.39×10^{19}	$.405 \times 10^{21}$
15.0	3321.7	6.32×10^{19}	$.586 \times 10^{21}$	4.80×10^{19}	$.741 \times 10^{21}$	6.63×10^{19}	$.614 \times 10^{21}$
CYCLE 8B							
1.0	3562.4	6.91×10^{19}	$.641 \times 10^{21}$	6.20×10^{19}	$.791 \times 10^{21}$	7.07×10^{19}	$.655 \times 10^{21}$
3.0	4038.8	8.05×10^{19}	$.746 \times 10^{21}$	6.97×10^{19}	$.888 \times 10^{21}$	7.93×10^{19}	$.735 \times 10^{21}$
10.0	5699.2	1.19×10^{20}	1.090×10^{21}	9.54×10^{19}	1.210×10^{21}	1.09×10^{19}	1.01×10^{21}
14.8	6840.1	1.45×10^{20}	1.32×10^{21}	1.12×10^{20}	1.41×10^{21}	1.30×10^{20}	1.19×10^{21}
CYCLE 8C							
1.0	7081.8	1.49×10^{20}	1.35×10^{21}	3.97×10^{19}	$.0489 \times 10^{21}$	1.35×10^{20}	1.23×10^{21}
3.0	7560.2	1.57×10^{20}	1.42×10^{21}	1.15×10^{19}	$.141 \times 10^{21}$	1.45×10^{20}	1.32×10^{21}
10.0	9234.6	1.83×10^{20}	1.64×10^{21}	3.73×10^{19}	$.450 \times 10^{21}$	1.80×10^{20}	1.62×10^{21}
15.0	10415.6	2.02×10^{20}	1.79×10^{21}	5.46×10^{19}		2.05×10^{20}	1.82×10^{21}

TABLE VII.A.3.1

MINIMUM CLAD THICKNESS

(Those plates not marked have clad thickness > 0.012 in.)

<u>Plate Number</u>	<u>Plate Surface</u>	<u>Element # XA-003-F</u>		<u>Element # XA-004-F</u>	
		<u>Top End</u>	<u>Bottom End</u>	<u>Top End</u>	<u>Bottom End</u>
2	Inner Outer				
3	Inner Outer				
4	Inner Outer				
5	Inner Outer		0.012"		
6	Inner Outer			0.011"	0.011"
7	Inner Outer	0.0095"	0.010" 0.0095"		0.011"
8	Inner Outer		0.0115" 0.0115"		0.011"
9	Inner Outer	0.011"	0.0085" 0.0075"	0.009" 0.010"	0.009" 0.007"
10	Inner Outer		0.0075" 0.0085"		0.0085" 0.011"
11	Inner Outer				0.012"
12	Inner Outer			0.012"	0.011" 0.0105"
13	Inner Outer	0.0115" 0.010"		0.0095" 0.0095"	0.010" 0.009"
14	Inner Outer		0.008" 0.009"	0.0095"	0.0085"
15	Inner Outer	0.0105" 0.0105"	0.010"	0.010"	0.0075" 0.008"
16	Inner Outer			0.0125"	
17	Inner Outer			0.013"	
18	Inner Outer				

APPENDIX V.C.C

FIGURES

Fig. I.1 Cycle 8A/B core composition.

1st ATRC Power

2nd Calculated Power

3rd % Difference

$$\frac{\text{Calc} - \text{ATRC}}{\text{ATRC}} \times 100$$

Fig. II.B.1 ATRC versus calculated power distribution in Core II -- clean critical.

Fig. II.B.2 ATRC versus calculated power distribution in Core II with Boron.

1st ATRC Power
2nd Calculated
3rd % Difference

Fig. II.B.3 ATRC versus calculated power distribution in Core III -- clean critical.

1st ATRC Power
2nd Calculated
3rd % Difference

Fig. II.B.2 ATRC versus calculated power distribution in Core II with Boron.

1st ATRC Power
2nd Calculated
3rd% Difference

Fig. II.B.3 ATRC versus calculated power distribution in Core III -- clean critical.

1st ATRC Power
2nd Calculated
3rd % Difference

Fig. II.B.4 ATRC versus calculated power distribution in Core III with Boron.

Fig. II.B.5 Azimuthal specific power distribution in fuel position 26.

Fig. II.B.6 Azimuthal specific power distribution in fuel position 27.

Fig. II.B.7 Azimuthal specific power distribution in fuel position 28.

Fig. II.B.8 Azimuthal specific power distribution in fuel position 29.

Fig. II.B.9 Azimuthal specific power distribution in fuel position 30.

Fig. II.B.10 Azimuthal specific power distribution in fuel position 31.

Fig. II.B.11 Azimuthal specific power distribution in fuel position 32.

Fig. II.B.12 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.13 Azimuthal specific power distribution in fuel position 34.

Fig. II.B.14 Azimuthal specific power distribution in fuel position 35.

● Channel 2 measured
○ Channel 2 calculated

Fig. II.B.15 Azimuthal specific power distribution in fuel position 33.

● Channel 6 measured
⊙ Channel 6 calculated

Fig. II.B.16 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.17 Azimuthal specific power distribution in fuel position 33.

● Channel 19 measured
○ Channel 19 calculated

Fig. II.B.18 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.19 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.20 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.21 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.22 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.23 Azimuthal specific power distribution in fuel position 33.

Fig. II.B.25 Ratio of measured to calculated
specific power for positions 28 and 33 of
Core II.

Fig. II.B.26 Ratio of measured to calculated specific power for positions 30 and 31 of Core II.

Fig. II.B.27 Ratio of measured to calculated specific power for position 33 of Core III.

Fig. II.B.29 Summary of ratio of measured to calculated specific power for channels 15 and 19.

Fig. II.C.1 Gamma scan versus calculated total fission density for Core II position 13.

Fig. II.C.2 Gamma scan versus calculated total fission density for Core II position 13.

Fig. II.C.3 Gamma scan versus calculated total fission density for Core II position 13.

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Fig. II.C.4 Gamma scan versus calculated total fission density for Core II position 13.

Fig. II.C.5 Gamma scan versus calculated total fission density for Core II position 11.

Fig. II.C.6 Gamma scan versus calculated total fission density for Core II position 11.

Fig. II.C.7 Gamma scan versus calculated total fission density for Core III position 13.

Fig. II.C.8 Gamma scan versus calculated total fission density for Core III position 13.

1st Perturbed Power
2nd Base Case Power
3rd % Difference

Fig. II.D.1 Power distribution in perturbed core compared with base case core.

Fig. II.D.2 Percent variation in ratio of perturbed to base case specific power.

Fig. II.D.3 Percent variation in ratio of perturbed to base case specific power.

Fig. II.D.4 Percent variation in ratio of perturbed to base case specific power.

Fig. II.E.1 ATR Cycle 8 peak axial fissions in long term burnup elements with core refueling and interchange of elements 18 and 33 at the end of Cycle 8B.

Fig. III.1 Oxide thickness versus temperature for element XA3G, plates (convex side) after 35 days in the ATR. (Griess predicted values are also shown.)

Fig. III.2 Comparison of metallographically determined oxide thickness with oxide thickness determined by eddy current device for XA20G.

As-Polished

50X

As-Polished

200X

(a) Sample from plate 9 along the longitudinal C plane and 28-1/2 in. from the top.
(0.9×10^{21} fiss/cc)

As-Polished

50X

As-Polished

200X

(b) Sample taken from plate 15 transverse at the C plane and 24-3/4 in. from top.
(1.5×10^{21} fiss/cc)

Fig. III.3 Intermediate (a) and high (b) burnup metallography samples from element XA3G.

Fig. III.4 Typical oxide at 29 inches from the top of plate 14 at the "A" plane showing the double layer and a maximum of 0.8 mils of oxide.

Fig. III.5 Metallographic oxide thicknesses compared to the Griess correlation for XA3G.

As-Polished

(a) 10 inches from top of plate 7 after 1050°F.

50X

As-Polished

(b) 24-3/4 in. from top of plate 15 after 10000°F.

50X

Fig. III.6 Metallography of UAl_x plates (a) intermediate burnup and (b) high burnup after blister testing.

Fig. III.7 Matrix crack initiation higher magnification detail of Figure III.6.

As-Polished

200X

(a) Material found in the core end of plate 17 element A96C.

As-Polished

200X

(b) Material found in the core end of plate 11 element XA8G.

Fig. III.8 Photomicrographs showing the similarity between the pimple materials produced in (a) a U_3O_8 fuel plate compared to (b) a $UA1_x$ fuel plate.

Fig. III.9 ATR Core II fuel plate blister test results.

Plates 1, 3, 5 - Elements XA3G and XA8G

Plates 1, 2 - Element A172C

Plates 10, 19, 19 - Element A172C

Plates 10, 15, 17, 19 - Elements XA3G and XA8G

Fig. III.10 Burnup sample location plan - 1/4 inch diameter circular punch.

Fig. III.11 Chart used for conversion of isotopic burnup to neodymium burnup.

Fig. III.12 Correlation curve of fission density versus normalized gamma amplitude for fuel element XA3G with 95% confidence limits.

Fig. III.13 Correlation curve of fission density versus normalized gamma amplitude for fuel element XA20G with 95% confidence limits.

GAMMA SCAN SCHEMATIC

Fig. III.14 Gamma scan schematic.

Fig. III.15 Plot of peak plate burnup and maximum burnup along the horizontal center line versus fuel plate location in XA3G.

Fig. III.16 Plot of peak plate burnup and maximum burnup along the longitudinal center line versus fuel plate location in XA20G.

Fig. III.17 Plot of peak plate burnup and maximum burnup along the longitudinal center line versus fuel plate location in A172C.

Fig. III.18 Canal photograph of plates in tier 4 of INC-16-1.

PLATE NUMBER 1 FROM XA-3-G

CONTOUR INTERVAL = 2.5+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.19 Iso-burnup profiles for plate number 1 from XA-3-G.

PLATE NUMBER 3 FROM XA-3-G

CONTOUR INTERVAL = 5.0+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.20 Iso-burnup profiles for plate number 3 from XA-3-G.

PLATE NUMBER 15 FROM XA-3-G

CONTOUR INTERVAL = 5.0+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.21 Iso-burnup profiles for plate number 15 from XA-3-G.

PLATE NUMBER 5 FROM XA-20-G

CONTOUR INTERVAL = 5.0+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.22 Iso-burnup profiles for plate number 5 from XA-20-G.

PLATE NUMBER 15 FROM XA-20-G

CONTOUR INTERVAL = 5.0+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.23 Iso-burnup profiles for plate number 15 from XA-20-G.

PLATE NUMBER 19 FROM XA-20-G

CONTOUR INTERVAL = 5.0+19

MULTIPLY LEVELS BY 1.0+19

Fig. III.24 Iso-burnup profiles for plate number 19 from XA-20-G.

Core Plan View

Fuel Element Details

Fig. IV.A.1 ATR fuel configuration.

Fig. IV.A.2 ATR fuel assembly details.

Fig. IV.B.2.1 ATR-startup cladding corrosion test.

Fig. IV.B.2.2 ATR Cycle 4B oxide measurements.

Fig. IV.C.1 ATR Cycle 8 maximum thermal stresses position 33 evaluated at 2 standard deviations.

Fig. V.C.1 V_{CR} versus l/b for flat fuel plates.

Fig. V.C.2 Relative critical velocity versus β .

Fig. V.C.3 Fuel matrix center line temperature versus blister diameters.

Fig. V.C.4 Kr and Xe release versus cleanup time.

Fig. V.C.5 Pre-test horizontal profile at deformation vertical midpoint for channel 18.

Fig. V.C.6 Pre-test vertical profile for channel 18 from side plate X to comb.

Fig. V.C.7 Pre-test vertical profile for channel 18 from side plate Z to comb.

Fig. V.C.8 Post-test vertical profile for channel 18 "X" side.

Fig. V.C.9 Post-test vertical profile for channel 18 "Z" side.

Fig. VI.A.3.1 Cycle 8 peak fissions with core refueled at end of Cycle 8B except elements 18 and 33 which are interchanged.