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ADVANCED BONUS CORE DEVELOPMENT PROGRAM

Final Report

By

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I. INTRODUCTION

The BOiling NUclear Superheater (BONUS) Power Station, located on the westernmost tip of the island of Puerto Rico, is a 50 Mw(t) reactor designed and constructed as a joint undertaking of the United States Atomic Energy Commission and the Puerto Rico Water Resources Authority and was authorized under AEC Contracts AT(40-1)-2672 and AT(40-1)-2674. The reactor was constructed as a prototype plant with the object of obtaining operating experience and data which would provide statistically significant answers to the question of the feasibility and practicality of integral nuclear superheating reactors. The ability of the BONUS reactor to achieve specified short-term design objectives has now been demonstrated.

The reactor first achieved criticality on April 13, 1964, and attained full design power on September 15, 1965. The long-range design objectives relating to fuel element corrosion resistance and integrity, fuel lifetime, and radioactive contamination remain to be demonstrated.

On November 12, 1963, the AEC authorized General Nuclear Engineering Corp., a subsidiary of Combustion Engineering, Inc., to execute, under Contract AT(40-1)-3154, the design and development of a combination boiling-superheating fuel element suitable for procuring an advanced fuel loading for the BONUS reactor. The use of a combination boiling-superheating fuel element was specified as a consequence of earlier studies at General Nuclear Engineering Corp. under the Nuclear Superheat Development Program sponsored by AEC Contract AT(11-1)-795.

These earlier studies showed the combination boiling-superheating fuel element to offer technological as well as economic advantages over single-purpose fuel elements such as those used in the first BONUS core.⁽¹⁾

The present report is a summary description of the design and development of the Advanced BONUS Core (ABC), sponsored under AEC Contract AT(40-1)-3154 and carried out over the period from January 18, 1963, to December 31, 1965.

II. PROGRAM SUMMARY

A. Objectives

The object of the Advanced BONUS Core development program was the design of an advanced combination boiling-superheating fuel element to be utilized in the BONUS reactor. A corollary design objective was the achievement, in the Advanced Core fuel element, of reductions in the cost of power as compared to BONUS Core 1. It was anticipated that, at the conclusion of the ABC program, the design of the advanced fuel concept would have been established and its performance verified through out-of-pile and in-pile testing, and at least one method of satisfactory fuel fabrication amenable to production line utilization would have been demonstrated.

The objectives of the Advanced BONUS Core program were achieved in all respects but one, namely, the verification of the design through in-pile testing. Although all design work related to the irradiation test had been completed, the fuel assemblies were not procured and the test could not be performed due to a curtailment of funds at the end of the Fiscal Year 1965.

The work program as envisaged in November 1963 is described in GNEC-327.⁽¹⁴⁾ The program was divided into four major phases, i.e., Manufacturing Development; Out-of-Pile Experimental Program; Irradiation Testing; and Advanced BONUS Core Design.

The Manufacturing Development phase covered the entire evaluation and experimental work associated with the development of materials

and processes for the fabrication of the fuel elements as well as the actual fabrication of prototype fuel elements. This included (1) welding development of all the fission gas containing welds of the elements; (2) fuel fabrication by vibratory compaction of fuel elements and by use of pressed and sintered pellets; (3) mechanical property tests of cladding materials; (4) corrosion aging tests of cladding materials; (5) fabrication and evaluation of Zircaloy-Inconel transition joints; (6) irradiation tests of cladding materials; and (7) irradiation tests of Zircaloy-Inconel joints.

The Out-of-Pile Experimental Program included the engineering tests associated with the evaluation of fuel element components or assemblies. Among these were (1) the thermal cycle test of mocked-up steam tubes, which later became a thermal cycle test of prototype fuel elements; (2) plug seal tests to evaluate the fuel retention capability of packing seals; (3) thermal cycle tests of Zircaloy-Inconel transition joints; (4) embedded tube tests to determine the magnitude of interaction forces between fuel and cladding; (5) fatigue tests of coiled and straight tubes; and (6) miscellaneous bench tests to evaluate component fits and clearances, pressure drop, buckling strength of tubes, tube rolling effects, etc.

The Irradiation Test phase comprised the engineering design, analysis, and guidance required to define the irradiation test, specify the test assembly, prepare engineering drawings, manufacture the test assemblies, ship the equipment to the irradiation test facility, and evaluate the test results. Originally, the irradiation

test of the combination boiling-superheating fuel assemblies was to be accomplished in the ESADE loop of the Vallecitos Boiling Water Reactor (VBWR). At the end of 1964, however, VBWR was shut down as a result of a General Electric management decision and it was necessary to re-direct the ABC irradiation test for performance in the Vallecitos Experimental Superheat Reactor (EVESR). Due to the delay involved in this reorientation, the time which had been allocated for two sequential irradiation tests was lost. (see Program Schedule, Figure II-1. Reference 14.)

However, at about the time of the revision in the irradiation test plan, the scope of the out-of-pile thermal cycle test was enlarged to include a full-scale test of prototype fuel elements, a step which compensated adequately in performance evaluation for the omission of the first irradiation test. Also, the irradiation test in EVESR was defined as a test of two complete BONUS fuel assemblies (8 fuel elements), offering considerable improvement in statistical significance over the two irradiation tests originally planned in VBWR.

The design and analysis of the fuel element was performed in parallel with the experimental and developmental work described in the foregoing. This stage included the mechanical, thermal and nuclear evaluation of the fuel element as it would operate in the BONUS reactor. It was necessary to define the Advanced BONUS Core configuration in order to relate the fuel element to its eventual application in the BONUS reactor. This latter work was aimed principally at assuring technical feasibility of the Advanced Core in BONUS and at

evaluating fuel cycle costs by comparison with the BONUS Core I fuel.

A chart describing the work program for the Advanced BONUS Core development is shown in Figure II-1. This chart lists the work which was carried out and the period of time covering each stage of the program. Detailed descriptions of each stage are included in the Monthly and Quarterly Progress Reports for the Advanced BONUS Core Program. (see References 2 through 31.)

B. Summary of Accomplishments

1. The feasibility of using boiling-superheating fuel elements of the multitube design in the BONUS reactor was established and a reference core configuration was described.

2. An economic evaluation was made of boiling-superheating fuel elements in comparison with BONUS Core I superheating fuel elements which showed that a significant reduction in core manufacturing and fuel cycle costs would result for the BONUS plant with the use of the boiling-superheating element of solid multitube design.

Although the fuel cycle cost comparison was made in accordance with the contract on the basis of the predicted exposure of 11,020 Mwd/MTU for Core I and 14,500 Mwd/MTU for ABC, it is believed that the potential of the ABC element for reducing BONUS fuel cycle costs is real and exists by reason of its higher specific power, low clad to fuel ratio, improved cladding material and better control of power split throughout lifetime (see Section III-E-4-b).

3. The design of an irradiation experiment in EVESR and the

design of a combination boiler-superheater fuel assembly for such an experiment were completed.* The feasibility of performing such an irradiation experiment in the BONUS reactor was likewise established.

4. Prototype solid multitube boiling-superheating fuel elements were manufactured and tested in an experimental loop to determine their ability to withstand the cyclic temperature of superheated steam and the pressures of a reactor environment. The elements withstood, without failure, in excess of 2000 cycles of steam temperature, each one-half hour cycle varying from 500°F-1200°F.

5. The feasibility of manufacturing combination boiler-superheater fuel elements of the solid multitube design for reactor service was established. Some work remains to establish the welding procedures for inlet and outlet piping and header parts of the fuel assembly.

6. Out-of-pile materials tests and limited post-irradiation tests of Inconel-625 material were completed and demonstrated acceptable ductility and strength of this material for use as boiler and superheater cladding in multitube fuel elements up to an exposure of 1.21×10^{21} nvt. Additional long term engineering tests under nuclear superheat conditions are required to fully demonstrate the acceptability of the cladding material for the ABC design.

7. The development of procedures and fabrication methods for all fission gas containing welds of the multitube fuel elements was completed and demonstrated the excellent welding qualities of Inconel-625 material.

* The procurement of the test assemblies and the performance of the experiment were deferred due to curtailment of funds.

8. A manufacturing technique for vibratory compaction of large annular fuel elements was developed, demonstrating the feasibility of this fuel fabrication process for large, complex fuel element cross sections in achieving fuel densities of 85%. Fuel-clad interaction forces were encountered in out-of-pile tests and require further study to assess the performance of vibratory compacted fuel elements.

9. Zircaloy-Inconel transition joints 3 1/2 in. in diameter and 1 1/2 in. in diameter were successfully manufactured by the tandem extrusion and the explosive welding process and passed all out-of-pile mechanical, thermal cycling, and helium leak tests. However, irradiation testing showed that the Zircaloy-Inconel joints were unsatisfactory for fuel element cladding service at 540°F. Further experimental effort to confirm and evaluate this effect and its cause should be undertaken in view of the advantage to be gained in using a low poison material on the boiler side of combination boiler-superheater fuel elements.

For the conclusions and proposed plans for future work on combination boiling-superheating fuel elements, the reader's attention is directed to Section VIII of this report.

ADVANCED BONUS CORE DEVELOPMENT

FIG. II-1 Summary of Work Program

III. ADVANCED BONUS CORE DESIGN

A. ABC Design Requirements and Description of BONUS Core I

A discussion of the requirements underlying the design of a fuel element for the Advanced BONUS Core is indicated at this point. The fact that the fuel element must be used in an existing structural support places limitations as to function and size on the fuel element. The core is intended to perform at least as well as the first core in its steady state and transient behavior, and many of the same thermal and nuclear constraints exist. For instance, water coolant flow rate is limited to the rating of the existing pumps; feedwater temperature is established; design pressure is fixed; control rod type and pattern are established; etc. Safety and control aspects requiring that reactivity be controlled within certain existing limits place restrictions on the relative amounts of fuel and water within a given cell or fuel element.

For an understanding of the restrictions imposed upon the Advanced BONUS Core design by the requirements of an already existing reactor, a brief description of that reactor may be of help.

The BONUS reactor pressure vessel and internals are shown in Figure III-1. The vessel has an inside diameter of 84 in. and is 26 ft. 7 in. high. The core is supported within the vessel by a conical skirt and flange which is integrally welded to the lower head of the vessel. A stainless steel reactor core structure contains the fuel assemblies and is bolted to the core support skirt. Within the core structure,

there is space for mounting sixty-four 4 in. by 4 in. boiler fuel assemblies in the central region of the core structure and thirty-two 4 in. by 8 in. superheater fuel assemblies in the outer regions. The Core I cross section and the detailed dimensions of the fuel elements are shown on Figure III-2. The active core length is 54.6 in., the overall width 54.9 in.

A perspective view of the boiler fuel assemblies is given in Figure III-3. Each boiler fuel assembly contains thirty-two Zircaloy-2 clad fuel rods 0.500 in. in diameter, arranged in a six by six square array with a pitch spacing of 0.625 in. A square space is available in the center where four fuel rods are left out to provide space for boiler shims. Each boiler fuel assembly is contained in a 4 in. by 4 in. square box of Zircaloy-2 with a wall thickness of 0.062 in. The boxes, in addition to constituting the main structure for containing and supporting the boiler fuel rods, provide the channel in which the control rods operate. The lower end fitting of each box has a hemispherical surface whose function is to seal the conical surface of the boiler orifice, thus minimizing inlet water coolant bypass leakage.

A perspective of the Core I superheater fuel assembly is given in Figure III-4. Each superheater fuel assembly contains thirty-two 0.540 in. OD Inconel-600 clad fuel rods. Each fuel rod is surrounded by a 0.710 in. ID by 0.012 in. wall Type 348 SS coolant tube and a 0.833 in. OD by 0.018 in. wall Type 348 SS pressure tube.

The control rod drives are of the rack and pinion type and are

mounted from the reactor vessel head. The number and arrangement of control rods is shown on Figure III-2. There are nine cruciform control rods and eight blade control rods.

The boiler fuel elements are lowered into place through the 8 in. square boxes of the upper guide structure of the reactor core structure; there are four assemblies to each guide structure box. The lower end of the boiler fuel assembly fits into an orifice which is mounted in a bored hole in the core plate. A boiler holddown assembly holds four boiler fuel assemblies in place in each of the 16 guide structure boxes.

The Core I superheater fuel assemblies rest on the superheater support which is mechanically fastened to the core plate. When the outlet standpipe of each superheater fuel assembly is connected, the weight of the fuel assembly is taken principally on the outlet steam header. Some weight is taken on the superheater support spring at the bottom of the fuel assembly, and this spring allows for axial differential expansion of the fuel assembly while in operation.

Use of the combination boiling-superheating fuel assemblies in the BONUS reactor imposes the following requirements:

- (1) In view of the higher specific power of the ABC fuel assemblies and the fact that they produce a significant amount of "boiler" power, the assemblies must occupy boiler fuel positions. Accordingly, their cross section must not exceed 4 in. by 4 in.
- (2) The ABC fuel assemblies must provide a surface for defining the control rod channel (a Zircaloy-2 box).

(3) The ABC fuel assemblies must provide a seal to the core plate to minimize coolant leakage.

(4) The choice of core positions for the ABC fuel assemblies is limited by the fact that they must be placed where through-holes exist in the core plate. This is necessary in order to provide forced-flow coolant to the water side of the elements. There are 88 such positions in the core plate.

(5) To provide space for inlet and outlet standpipes in the ABC fuel assemblies located in the boiler region, modification of the hold-down assemblies in these positions is necessary.

(6) The ABC core must use the existing BONUS control rod arrangement and rod number.

(7) The ABC core must produce 50 Mw(t) at a superheated steam flow of 152,000 lb/hr and an average outlet temperature of 915°F, with a recirculating water rate of 4500 gpm and a feedwater temperature of 354°F.

(8) The maximum metal temperature must not exceed 1250°F.

(9) The maximum power in the boiler region is 14.4 kw/ft.

(10) The core design lifetime is 15,000 Mwd/MTU.

(11) The Advanced BONUS Core must fit within the existing core structure.

(12) The outlet superheated steam from the ABC fuel elements must be directed through standpipes which are connected and sealed to the existing steam outlet headers of the reactor pressure vessel.

(13) The four superheater supports must be unbolted and removed

from the core plate to permit use of the six core plate holes in each superheater quadrant as a means for admitting water coolant to the boiler side of the ABC fuel elements.

(14) The fuel assemblies must be designed to withstand an operating pressure of 950 psia and a temperature on the boiling side of 540°F. They must also be capable of withstanding a hydrostatic pressure test of 1250 psia at room temperature.

B. Mechanical Design of the Advanced BONUS Core

At the time of termination of the design phase of the Advanced BONUS Core in June 1965, the reference core design consisted of 56 boiler-superheater fuel assemblies and 32 boiler fuel assemblies arranged in the manner shown in Figure III-5. The active core length is 54.6 in., that is, equal to Core I. Although the total thermal power rating of 50 Mw(t) is identical for BONUS Core I and the Advanced BONUS Core, the latter occupies only two thirds of the volume of the first core, as can be seen by the dotted outline of Core I superimposed on Figure III-5. The boiler fuel assemblies are identical to the BONUS Core I boiler fuel assemblies which were described earlier. The combination boiler-superheater fuel assemblies for the Advanced BONUS Core are shown in cross section on Figure III-5. Each boiler superheater fuel assembly consists of four 1.516 in. OD fuel elements arranged in a 1.875 in. by 1.875 in. square array. The four boiler superheater fuel elements are surrounded by a square Zircaloy-2 box which has the same dimensions as the box of the boiler fuel assemblies.

The fuel element subassemblies are shown on Figure III-6. Each fuel element has a 1.516 in. OD by 0.033 in. wall Inconel-625 boiler cladding. There are seven 0.285 in. OD by 0.026 in. wall Inconel-625 steam tubes equally spaced at an 0.455 in. triangular pitch within the fuel element. The fuel consists of 94% dense UO_2 perforated pellets 0.5 in. high. The lower surface of each pellet is dished 0.006 in. deep to provide space for axial fuel growth. The pellets are pressed and sintered with the seven 0.295 in. steam holes in place. The only surface which is ground after sintering is the pellet outside diameter.

The four fuel elements are welded together at the upper manifold, as shown in Figure III-7. A mid-height spring clip spacer laterally supports the fuel elements by connecting them to each other. The spacer is designed to provide minimum obstruction to heat transfer at its contact points with the boiler cladding. A Zircaloy-2 box and cluster end bracket are provided as shown on Figure III-8. Connection between the fuel assembly and the box is provided at two elevations. The bottom connection is a pilot fit between the cluster end bracket 0.75 in. diameter pin and the spider bracket of the end fitting. The top connection is also a pilot fit but is located at an elevation just above the manifold. It includes a support spring which permits absorption of the axial expansion of the fuel assembly and standpipes during operation and supports the weight of the fuel assembly during fuel handling.

The upper standpipes consist of three concentric pipes of Inconel-600 material. The center tube which contains the flowing outlet superheated

steam has a 1.25 in. OD by 0.065 in. wall and is the thermal liner for the 1.75 in. OD by 0.120 in. wall tube which forms the pressure barrier between the inlet saturated steam and the outlet superheated steam. The inlet saturated steam flows downward in the annulus between the 1.75 in. OD tube and the outer 2.25 in. OD by 0.120 in. wall tube. The design of the standpipe connections to the dryer/preheater and the outlet steam header was not performed during this phase of the ABC program. It was planned to use Flexitallic gasket seals and flanged connections equivalent to those presently used on BONUS Core I. Layouts were completed which demonstrated the feasibility of making standpipe connections to the ABC boiling-superheating fuel assemblies without interfering with the control rod shaft extensions.

The flow path of the steam through the boiler-superheater fuel assembly is indicated by the arrows shown on Figure III-9. The recirculating coolant water enters the lower end of the fuel assembly through the lower end fitting which is seated on the core plate. The water flows upward within the Zircaloy-2 box past the outside diameter of the four fuel elements where it is heated and saturated steam is formed. The saturated steam/water mixture passes up through the square annulus between the outside of the manifold and the inside of the box and then continues upward through the upper guide structure boxes. The saturated steam separates from the water and rises to the dome of the reactor vessel, where it joins the saturated steam produced in the central boiling region of the core. The water crosses through the upper guide structure and returns to the recirculating

pumps. The 540°F saturated steam from the dome of the reactor vessel flows down the outer annulus of the standpipe to the fuel assembly manifold after passing through the dryer/preheater. It passes by the steam outlet chamber in the four corner regions of the manifold and then enters the four first pass steam tubes of each of the four elements. The steam travels down the first heating pass of each fuel element to the interpass region. The steam from each of the four first pass tubes is then mixed in the interpass chamber, enters the three second pass tubes and travels up the second heating pass of each fuel element. The 915°F superheated steam emerges from the element through the thermal sleeves which insulate the upper tube sheet from the high temperature fluid. The thermal sleeves carry the superheated steam from each element to the outlet chamber of the manifold where the steam from all four elements is again mixed and passes up through the insulated center tube of the standpipe. The superheated steam passes out of the reactor vessel through the outlet steam headers and thence to the turbine.

C. Thermal Analysis

1. Performance Characteristics

The performance characteristics of the Advanced BONUS Core are given in Table III-1, which lists the equivalent characteristics of BONUS Core I for comparative purposes. While the thermal output of the cores is the same, the major differences are the higher specific power of the ABC Core and its increased core lifetime, which are

achieved within the same fuel and clad temperature design limits that applied to Core I. The steam side heat flux in the ABC Core is about twice as great as for Core I, although the ABC maximum clad temperature is only slightly higher. This is achieved by an increase in ABC steam velocity to 220 ft/sec over the Core I exit steam velocity of 121 ft/sec. The ABC Core can tolerate such an increase in steam velocity without exceeding the allowable BONUS core pressure drop because it features two steam heating passes instead of the four heating passes of Core I.

2. Thermal Analysis Methods

The thermal code MUTT-1⁽³²⁾ was used for the thermal evaluation of the ABC fuel elements. The MUTT-1 is a two-dimensional thermal conduction code for evaluating steady state conditions through irregular solids with non-uniform internal heat generation. It is a numerical code and essentially a modified PDQ, allowing a completely variable mesh structure and variation by region of thermal conductivity and internal heat generation over a maximum of 50 regions. With use of an IBM-7094 computer, 1296 mesh points are available; with an IBM-704, 2500 mesh points are available.

The MUTT-1 Code is basically a relaxation thermal code which can be used in analyzing combination boiler-superheater elements to calculate the following factors:

- (1) Power split
- (2) Temperature distribution in the fuel
- (3) Heat fluxes
- (4) Steam clad temperature distribution.

The geometric pattern for the MUTT-1 Code was established so as to give an accurate analysis of the two pass fuel element cross section. The geometry studied encompassed a full quarter horizontal section of the ABC fuel element so that a symmetric section could be obtained.

Although the MUTT-1 Code was found to be extremely useful in gaining a more complete understanding of the thermal performance of combination boiler-superheater fuel elements, the analysis has certain limitations. Since it is a two-dimensional code, and the power split at any plane is a function of steam temperatures, to analyze a full length fuel element it is necessary to assume an appropriate axial temperature distribution and then perform a MUTT-1 Code analysis at several selected planes along the length. The steam temperatures that result at each plane are then checked against the original assumptions. If large discrepancies exist, the problems must be re-run with the new temperature distribution. A code which would perform this iteration axially would offer great improvement in performing detailed and repetitive calculations on combination boiler-superheater fuel elements.

A typical temperature distribution obtained from a MUTT-1 analysis of the ABC two pass element is shown in Figure III-10.

3. Pressure Drop

The superheated steam pressure drop, as shown in Table III-1, is defined as the drop in static pressure of the steam, proceeding from the pressure vessel saturated steam pressure to the superheated

TABLE III-1

SUMMARY OF EQUILIBRIUM ADVANCED BONUS CORE CHARACTERISTICS

General	ABC Reference Design	BONUS Core 1
Nominal core power, Mw(t)	50	50
Total steam flow rate, lb/hr	152,000	152,000
Nominal steam temperature, °F	900	900
Nominal feedwater temperature, °F	354	354
Core Description		
Superheater elements		
Number of 4 in. x 4 in. assemblies	56	--
Number of 4 in. x 8 in. assemblies	--	32
Boiler elements		
Number of 4 in. x 4 in. assemblies	32	64
Combined core		
Number of 4 in. x 4 in. assemblies	88	128
Power		
Total fission power, Mw(t)	51.3	51.3
Fission power in boiler, Mw(t)	24.23	38.0
Thermal boiling, Mw(t)	23.03	36.5
Neutron and gamma boiling, Mw(t)	0.6	0.7
Heat loss to environment, Mw(t)	0.6	0.8
Fission power in superheater, Mw(t)	27.07	13.3
Thermal superheating, Mw(t)	11.9	11.9
Thermal boiling, Mw(t)	14.37	0.9
Neutron and gamma boiling, Mw(t)	0.6	0.5
Heat loss to environment, Mw(t)	0.2	---
Total power to steam, Mw(t)	50.5	50.5
Total boiling power, Mw(t)	38.6	38.6
Total superheating power, Mw(t)	11.9	11.9
Fission power fraction to superheater	0.528	0.259
Core Power Split	0.236	0.236
Fuel Element Power Split	0.453	---

TABLE III-1 (Cont)

<u>Power (Cont)</u>	<u>ABC Reference Design</u>	<u>BONUS Core 1</u>
Average specific power in boiler, kw/kg of UO ₂	16.3	12.7
Average specific power in superheater, kw/kg of UO ₂	11.0	7.0
Power distribution factors in boiler region		
Maximum overall	2.8	3.46
Maximum axial	2.10	1.77
Maximum radial among assemblies in region	1.05	1.35
Maximum radial among elements in region	1.33	1.45
Power distribution factors in superheater region		
Maximum overall	3.00	2.70
Maximum axial	2.1	1.60
Maximum radial among assemblies in region	1.24	{ 1.69 }
Maximum radial among elements in region	1.37	
Maximum local azimuthal	1.04	
<u>Fuel Description</u>		
Weight of fuel (UO ₂ pellets, 95% nominal density)		
Boiler elements, lb	3240	6480
Superheater elements, lb	5309	4060
Total, lb	8549	10540
Volume of fuel (UO ₂ pellets)		
Boiler elements, ft ³	4.98	9.95
Superheater elements, ft ³	8.16	6.25
Fuel Enrichment (Unirradiated ^w / _o U-235 in UO ₂)		
Boiler region assemblies	2.6	4 center - Natural UO ₂
		60 remainder - 2.4
Superheater region assemblies	5.4	3.25*
Average fuel depletion of most depleted fuel assembly at removal (Mwd/MTU)		
Boiler assemblies	--	11,000
Superheater assemblies	--	11,000*

* For the Type 1 Replacement Core using Incoloy-800 cladding coolant tubes and pressure tubes, the calculated enrichment as recently computed is 4.5% for a fuel depletion at removal of 11,000 Mwd/MTU. This is calculated to assure that the design power split is maintained to the end of life.

TABLE III-1 (Cont)

<u>Fuel Description (Cont)</u>	<u>ABC Reference Design</u>	<u>BONUS Core 1</u>
Average fuel depletion of the average fuel assembly at removal (Mwd/MTU)		
Boiler assemblies	16,000	9,500
Superheater assemblies	14,600	9,700*
<u>Boiler Fuel Elements</u>		
Fuel elements per assembly	32	32
Cladding material	Zr-2	Zr-2
Cladding outside diameter, in.	0.500	0.500
Cladding thickness, in.	0.025	0.025
Fuel length, in.	54.0	54.0
Pellet diameter, in.	0.445	0.445
<u>Superheater Fuel Element</u>		
Fuel elements per assembly	4	32
Steam passes per element	2	1
Steam passes through assembly	2	4
Steam cladding material	Inconel 625	Inconel 600
Outside diameter, in.	0.280	0.540
Thickness, in.	0.026	0.018
Tube circle diameter, in.	0.910	---
Water cladding material	Inconel 625	---
Inside diameter, in.	1.450	---
Thickness, in.	0.033	---
Fuel length, in.	54.6	54.6
Heating surface per assembly		
Steam surface, ft ²	7.705	20.58
Water surface, ft ²	7.223	---
Steam flow area per assembly		
First pass, ft ² (16 tubes)	0.00465	0.00927
Second pass, ft ² (12 tubes)	0.00349	all passes

* For the Type 1 Replacement Core using Incoloy-800 cladding coolant tubes and pressure tubes, the calculated enrichment as recently computed is 4.5% for a fuel depletion at removal of 11,000 Mwd/MTU. This is calculated to assure that the design power split is maintained to the end of life.

TABLE III-1 (Cont)

	ABC Reference Design	BONUS Core 1
<u>Boiler Thermal Performance</u>		
Average heat flux, Btu/hr-ft ²	132,400	105,500
Maximum heat flux, Btu/hr-ft ²	370,700	365,000
Maximum rod power, kw/ft	14.2	14.0
Maximum fuel temperature, °F	4,215	4,140
<u>Superheater Thermal Performance</u>		
Steam surface		
Average heat flux, Btu/hr-ft ²	94,130	66,300
Maximum heat flux, Btu/hr-ft ²	282,400	179,100
Nominal maximum clad temp., °F	1,125	1,010
Water surface		
Average heat flux, Btu/hr-ft ²	122,900	---
Maximum heat flux, Btu/hr-ft ²	368,700	---
Maximum fuel temperature, °F	2,570	3,160
<u>Steam Data</u>		
Total steam flow through superheater, lb/hr	152,000	152,000
Inlet pressure, psig	950	950
Inlet temperature, °F	540	540
Pressure drop (including standpipes and steam dryer), psi	75	79.3*
Maximum velocity in steam tubes, ft/sec	217	121
Exit temperature, °F	915	915
<u>Design Limits</u>		
Boiler fuel elements (Core 1 limits)		
Maximum heat flux, Btu/hr-ft ²	450,000	450,000
Maximum rod power, kw/ft	14.4	14.4
Maximum fuel temperature, °F	5,000	5,000
Superheater fuel elements		
Maximum heat flux, water surface, Btu/hr-ft ²	500,000	---
Maximum steam clad temperature, °F	1,250	1,250
Maximum fuel temperature, °F	5,000	5,000

* This is the calculated value for Core 1 ΔP - measured value is 100 psi

steam pressure in the octant manifolds which connect to the steam nozzles.*

The pressure drop can be subdivided into the following major categories:

- (1) Δp through the mechanical dryer and preheater.
- (2) Δp through the inlet standpipe.
- (3) Δp through the assembly inlet manifold into the steam tubes.
- (4) Δp through the first pass steam tubes, the bottom turn-around, and the second pass steam tubes.
- (5) Δp through the assembly outlet manifold.
- (6) Δp through the outlet standpipe.
- (7) Δp through the orifice at the inlet to the octant manifold.

The orificing scheme is selected to control the flow to each assembly so as to obtain equal outlet superheated steam temperatures; the orifices are sized to match the steam flow to the radial power distribution of the superheater assemblies. In order to minimize the overall pressure drop, the assembly with the highest steam flow rate that is, the highest power assembly, has no orifice in the line (Item 7, above). The superheater pressure drop is, therefore, calculated by using the assembly in each octant of the symmetrical core which has the highest flow rate. In Table III-1, the radial peaking factor between assemblies is 1.24 for the reference designs. Therefore, the maximum steam flow through an assembly is $(152,000 \times 1.24/56)$ 3370 lb/hr.

On this basis, the Δp through the hottest ABC fuel assembly is calculated to be 55 psi, with an uncertainty allowance of 16 psi.

* The design of the header piping in the BONUS reactor is shown in Figures 301-1, 301-2, and 301-3 of Reference 33.

The Δp through the dryer/preheater is 4 psi. The superheated steam pressure drop is, therefore, 75 psi. Friction losses in the inlet standpipe, downcomer and riser steam tubes, and outlet standpipe account for about half of the calculated pressure drop. Other pressure losses include expansion, contraction, and turning losses in the inlet and outlet headers and acceleration losses in the steam tubes.

Both Core 1 and the ABC fuel elements are clad temperature limited, that is, each reaches the 1250°F clad design temperature limit in the fuel element at maximum fuel temperatures that are well below the melting point for UO_2 . However, at 1250°F the Inconel-625 cladding of the ABC fuel element has a greater margin in its mechanical properties than does the Inconel-600 cladding of the BONUS Core 1 superheater fuel element. As more experience is gained with the irradiation performance of ABC fuel elements, the excellent high temperature strength of the Inconel-625 steam tubes may suggest the possibility of relaxing the 1250°F clad design temperature limit.

4. Power Split

In the Advanced BONUS Core, not only the core power split but also the fuel element power split must be taken into account. The core power split is the fraction of superheating power to total core power. Within the combination boiling superheating fuel element, only a portion of the total power produced goes to generate superheated steam in the steam tubes; the remainder produces saturated steam on the outside diameter of the fuel element. The fraction of power which goes to the steam side compared to the total power produced in the average

boiler-superheater fuel assembly is designated as the fuel element power split.

The relationship between the core power split and the fuel element power split is

$$F = \frac{f P_{BS}}{P_B + P_{BS}}$$

where F = Core power split

f = Fuel element power split

P_{BS} = Average power produced in the boiler-superheater fuel region (Mw)

P_B = Average power produced in the boiler fuel region (Mw)

The core power split (F) is fixed by the BONUS plant parameters of steam outlet temperature, feedwater temperature, reactor vessel pressure and steam flow rate. Variations in the fuel element power split affect, therefore, the percentage of total power that must be produced in the boiling superheating region. During the design phase, such changes were accommodated by variation of the number of boiler-superheater assemblies and by adjustment of the fuel enrichment in the two regions. After the core design is fixed, variations in fuel element power split which occur as a result of manufacturing variations, calculation inaccuracies, irradiation effects, etc. must be corrected by adjustments in control rod positions or by shimming.

The question of the power split within the combination boiler-superheater fuel element and the factors which affect it has received

considerable attention. Studies have been performed principally on the fuel elements to be installed in the EVESR reactor; however, the results are also applicable to the ABC design (see Section IV). The power split has been found to be a function of specific power. While the specific power varies from element to element and from plane to plane within an element, an average power split can be obtained by using the average specific power for the boiler-superheater region. A plot of fuel element power split versus average fuel element power density is shown in Figure III-11 for a fuel enrichment of 5 W/o . It should be pointed out that this curve differs significantly from the equivalent curve plotted for a fuel enrichment of 15 W/o for the EVESR test, as shown in Figure IV-4 of this report.

The power split is also a function of enrichment, steam tube to fuel gap resistance, boiler clad to fuel gap resistance, conductivity of the UO_2 , temperature of the steam, and steam velocity. However, over a surprisingly wide variation of these parameters (within the limits of the designs considered) the power split ranged between 0.4 and 0.6. For instance, the power split for the solid multitube fuel elements using vibratory compacted fuel was about 0.55, but for pellet fuel with an 0.010 diametral gap between steam tubes and pellet the power split was about 0.45. For the eleven-tube annular element using vibratory compacted fuel, the power split was 0.47. (see Reference 20, pp. III-12, 13.) The nominal power split in the reference ABC design is 0.45 ± 0.05 , as reported in Table III-1.

The enrichments of the boiler and the boiler-superheater regions

of the ABC core are higher than the respective enrichments of the boiler and superheater regions of Core 1. This is due to the decreased size of the ABC core and its increased lifetime, which is about twice as long as that of Core 1. A further discussion of these effects is presented in Section E.

5. Vapor Fraction Calculations

The average vapor fractions for the current ABC reference design are 0.275 in the boiler region and 0.245 in the superheater region. These values were calculated using the Marchaterre and Hoglund⁽³⁴⁾ correlations for two-phase flow. The following assumptions and estimates were made:

- (1) 40% of total reactor boiling power is in superheater section;
- (2) 40% of total reactor water flow passes through superheater section.

The first assumption was approximated from values given in Table III-1, the second assumption was obtained by proportioning the superheater water flow rate to the steam production.

Figures III-12 and III-13 show the radially averaged values of vapor fractions in the boiler region and the superheater region, respectively. Using the Jens-Lottes equation,⁽³⁵⁾ it was determined that subcooled boiling would begin just beyond the entrance to the core. Therefore, the vapor fraction curves were extrapolated to zero inches from the bottom of the core. The subcooled boiling portions of the vapor fraction curves were drawn to approximate values calculated by Marchaterre and Petrick.⁽³⁶⁾

6. Fuel Element Leakage Analysis

A calculation was performed to determine the leakage through the joint between the second pass superheater tube and the tube extension. The clearance at this joint provides a leakage path by which entering steam can flow directly to the outlet tubes, thus creating a short circuit of the fueled portion of the element. It is, therefore, important that the amount of this leakage be minimized.

Figure III-14 shows the amount of leakage, in terms of percentage of total flow to the element, as a function of the radial clearance and the length over which this clearance exists. A leakage of 4% of total flow is considered tolerable, and it can be seen from Figure III-14 that a 0.001 in. gap is acceptable for a length of 0.5 in.; the gap would have to be correspondingly smaller for shorter lengths. Since clearances of this order would make assembly of the tube and tube extension difficult, it was decided to make the initial clearance considerably larger and then form a rolled joint.

7. Number of Heating Passes

In the BONUS core arrangement, it is necessary to use an even number of heating passes on the steam side of the combination boiler-superheater fuel elements because the saturated steam is collected at

the top of the reactor and the superheated steam exits at the top of the reactor. Two heating passes were selected instead of the four used for Core 1 to reduce pressure drop and provide small diameter steam tubes. However, it is possible to have either the two passes within a single boiler-superheater element or each pass in a separate fuel element with the elements connected by an interpass chamber. Consideration was given to both one pass per element and two pass per element designs. In the former, saturated steam is heated while passing through all seven tubes of one element, and then cross over at the interpass to the seven second pass steam tubes of another element where it is heated to 915°F. In the two pass design the steam passes down four tubes of an element, turns around and is further heated as it passes up the three second pass steam tubes of the element.

A one pass element appears to have small thermal advantages over the two pass designs that were studied. The two pass element has a lower ΔT between average metal temperatures of each pass, the ΔT being 203°F for a two pass element and 240°F for a one pass element. However, this advantage is offset by the poorer azimuthal temperature distribution around the steam tubes of the two pass element. There is a slightly lower maximum fuel temperature in the two pass element as compared to the one pass design (2450°F versus 2520°F). Also, a one pass fuel element is more amenable to thermal analysis than the two pass design; although both types of element must be thermally evaluated by means of machine codes. However, considerations of mechanical features and arrangement would seem to favor a two pass element in

BONUS. The two pass element has a less complicated lower end and permits the use of a floating plenum; the one pass design can be used only with fixed tube sheets. The two pass design has a higher superheated steam pressure drop by a ratio of $\left(\frac{7}{6}\right)^2$; it also has the higher steam velocity in the hot spot position and, therefore, a better film coefficient.

D. Stress Analysis

A major effort in the design of the ABC fuel elements was directed at the problem of axial stresses due to differential expansion of the boiler and superheater cladding. These are the largest stresses in the fuel element cladding, although there are other stresses due to system pressure, residual effects, and radial thermal gradients.

The axial differential expansion problem is inherent in a combination boiling-superheating fuel element. The boiler cladding which is exposed to operating temperatures at or near those of saturated steam, i.e., 540°F, is connected to the superheater cladding, where operating temperatures are higher than the superheated steam temperature, i.e., 950°F. For Inconel-625 material, this 400°F temperature difference causes a difference of about 3/16 in. in the growth of the two 60 in. long claddings, and if the two clads have equal area the nominal axial stress is about 40,000 psi. This stress is considerably higher than nominal stresses currently considered tolerable in commercial reactor fuel elements. Such a high level of induced stress clearly requires a material with unusually good high temperature strength properties. Inconel-625 is one such material.

Other approaches to the solution of the axial differential expansion problem were investigated analytically and/or experimentally

in the ABC program. These alternative approaches and the conclusions reached as to their acceptability are tabulated in the following.

1. Use of a Floating Plenum at the Interpass Region of the Fuel Element

This concept was evaluated both analytically and experimentally and was found significantly to reduce the operating stress level in the fuel element.

2. Use of Prestress in the Assembly of the Boiler Clad to the Superheater Tubes

Prestress was found by analysis to reduce significantly the peak operating stress level; however, the range of stress level remains unaffected. Experimentally, it was observed that for peak operating stresses in excess of the elastic limit, the effect of a prestress was automatically induced by the initial yielding which took place. Therefore, the benefits of prestress are probably most significant for fuel elements operating within the elastic region.

3. Use of Expansion Coils to Absorb Differential Expansion

Studies showed expansion coils to be too bulky to fit within the space limits of the reference fuel element envelope. They appear also to be too costly an addition and involve a large degree of uncertainty as to actual performance and fatigue life.

4. Use of Materials with Matching (More Favorable) Temperature Coefficients

The material choices available for combination boiling-superheating fuel elements are:

<u>Superheater</u>	<u>Boiler</u>
(1) Type 347/348 SS	(1) Type 347/348 SS

(2) Incoloy-800	(2) Incoloy-800
(3) Inconel-600	(3) Hastelloy-X
(4) Hastelloy-X	(4) Inconel-600
(5) Inconel-625	(5) Inconel-625
	(6) Zircaloy-2

The stainless steels were discarded because of their susceptibility to chloride stress corrosion and their demonstrated poor irradiation lifetime in a boiling water environment, as shown in the VBWR experiments. Incoloy-800 has a higher temperature expansion coefficient than the other nickel alloys and this, in combination with its lower strength, makes it undesirable, at least for a fixed tube sheet (high-stressed) boiler-superheater fuel element. Inconel-600 has been found to embrittle with exposure to irradiation and this aspect, in combination with its lower strength, makes it, too, less desirable both as a superheater and boiler cladding material. Hastelloy-X had been discarded early in the ABC program because of reports at that time of embrittlement due to irradiation. These were subsequently proved erroneous and the use of Hastelloy-X in boiling-superheating fuel elements is now considered feasible on the basis of the results of more recent irradiation experiments at GE-APED. Inconel-625 material, however, offers the highest strength material at its high operating temperature. It gives the lowest axial stresses of any of the "one-material" combinations available. Use of Zircaloy-2 as the boiler clad in a combination boiler-superheater element is feasible only if the boiler cladding is separated from the superheater cladding, that is, with a floating

plenum. However, the very low thermal absorption cross section of Zircaloy-2 makes it a highly desirable material for boiler cladding, and this is the reason why the ABC development provided for a program to manufacture and test Zircaloy to Inconel transition joints.

Tests to evaluate the performance of boiling-superheating fuel elements with Inconel-625 cladding were conducted and are discussed in Section V of this report. The effectiveness of prestressing and the floating plenum were also evaluated. The reference design fuel element for the Advanced BONUS Core can employ any one of four design permutations, all of which have been demonstrated to be acceptable through out-of-pile tests. These four design permutations are

- (1) Fixed tube sheet element - No prestress
- (2) Fixed tube sheet element - With prestress
- (3) Floating tube sheet element - No prestress
- (4) Floating tube sheet element - With prestress

A detailed stress analysis was performed for each of the element designs considered and included prestressed and non-prestressed conditions for both the floating tube sheet and fixed tube sheet element designs. The analysis proceeded from postulated reactor steady state operating conditions. The radial, hoop and axial stresses were calculated, based on pressure, bending, and thermal loadings at steady state operating conditions for each tubular component. Stress summaries of the non-prestressed floating tube sheet and fixed tube sheet element designs and the prestressed floating tube sheet and fixed tube sheet element designs are presented in Tables III-2, III-3, III-4, III-5,

TABLE III-2

NON-PRESTRESSED
FLOATING LOWER HEAD ARRANGEMENT
STEADY STATE CONDITION STRESS TABLE

Loading	BOILER TUBE (570 °F)			FIRST PASS (750 °F)			SECOND PASS (950 °F)			
	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	OUTER FACE
Bending			-6,570							
Thermal		15,530	15,530		*19,172			*-25,564		
Thermal					10,766	10,766		12,300	12,300	
Total	-950	3,798	-14,505	-950	32,408	15,700	-950	-10,794	17,240	
Max. Stress Intensity		-18,303			33,358			28,034		INNER FACE
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	
Bending			6,570							
Thermal		-15,530	-15,530		*19,172			*-25,564		
Thermal					-10,766	-10,766		-12,300	-16,300	
Total	-950	-27,262	-32,425	-950	10,876	-5,826	-950	-35,394	-7,360	
Stress Intensity		31,475			-16,702			-34,444		

σ_r - radial stress
 σ_a - axial stress
 σ_n - hoop stress

Max. stress intensity is
the biggest of:

$$\sigma_n - \sigma_a$$

$$\sigma_a - \sigma_r$$

$$\sigma_r - \sigma_n$$

* Axial stress from differential
expansion between first and
second pass tubes.

TABLE III-3

NON-PRESTRESSED
FIXED LOWER HEAD ARRANGEMENT
STEADY STATE CONDITION STRESS TABLE

Loading	BOILER TUBE (570 °F)			FIRST PASS (750 °F)			SECOND PASS (950 °F)			
	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	OUTER SURFACE
Bending			-6,570							
Thermal		*16,018			*2,107			*-42,629		
Thermal		15,530	15,530		10,766	10,766		12,300	12,300	
Total	-950	19,816	-14,505	-950	15,343	15,706	-950	27,859	17,240	
Stress Intensity		-34,321			16,656			28,809		INNER SURFACE
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	
Bending			6,570							
Thermal		16,018			2,107			-42,629		
Thermal		-15,530	-15,530		-10,766	-10,766		-12,300	-12,300	
Total	-950	-20,692	-32,425	-950	-6,189	-5,826	-950	-52,459	-7,360	
Stress Intensity		31,475			5,239			51,509		
S_p Alt.		58,500			56,250			53,100		

S_p - Alt. stress intensity (60% S_u or 90% S_y , whichever is less).

σ_r - radial stress

σ_a - axial stress

σ_n - hoop stress

* Axial thermal stress from differential expansion interreacting between boiler tube, first pass tubes and second pass tubes.

TABLE III-4
PRESTRESS
FLOATING LOWER HEAD ARRANGEMENT
STEADY STATE CONDITION STRESS TABLE

Loading	BOILER TUBE (570 °F)			FIRST PASS (750 °F)			SECOND PASS (950 °F)			
	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	OUTER FACE
Bending			-6,570							
Thermal		15,530	15,530		*6,412			*-8,544		
Thermal					10,766	10,766		12,300	12,300	
Total }	-950	3,798	-14,505	-950	21,648	15,700	-950	-6,126	17,240	
Max. Stress Intensity		-18,303			22,598			18,190		INNER FACE
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	
Bending			6,570							
Thermal		-15,530	-15,530		*6,412			*-8,544		
Thermal					-10,766	-10,766		-12,300	-16,300	
Total	-950	-27,262	-32,425	-950	-1,884	-5,826	-950	-18,324	-7,360	
Stress Intensity		31,475			-3,942			-17,424		

σ_r - radial stress

σ_a - axial stress

σ_n - hoop stress

Max. stress intensity is the biggest of:

σ_n - σ_a

σ_a - σ_r

σ_r - σ_n

* Axial stress from differential expansion between first and second pass tubes.

TABLE III-5
PRESTRESS
FIXED LOWER HEAD ARRANGEMENT
STEADY STATE CONDITION STRESS TABLE

Loading	BOILER TUBE (570 °F)			FIRST PASS (750 °F)			SECOND PASS (950 °F)			
	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	σ_r	σ_a	σ_n	
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	OUTER SURFACE
Bending			-6,570							
Thermal		11,212			*-6,460			*-17,470		
Thermal		15,530	15,530		10,766	10,766		12,300	12,300	
Total	-950	15,010	-14,505	-950	6,776	15,706	-950	-2,700	17,240	
Stress Intensity		29,520			16,656			19,940		
Pressure	-950	-11,732	-23,465	-950	2,470	4,940	-950	2,470	4,940	INNER SURFACE
Bending			6,570							
Thermal		11,212			-6,460			-17,470		
Thermal		-15,530	-15,530		-10,766	-10,766		-12,300	-12,300	
Total	-950	16,050	-32,425	-950	-14,756	-5,826	-950	-27,300	-7,360	
Stress Intensity		31,475			-13,806			-26,350		
S_p Alt.		58,500			56,250			53,100		

S_p - Alt. stress intensity (60% S_u or 90% S_y , whichever is less).

σ_r - radial stress

σ_a - axial stress

σ_n - hoop stress

* Axial thermal stress from differential expansion interreacting between boiler tube, first pass tubes and second pass tubes.

respectively. In each case, the maximum total stress was found to be at an acceptable level below the material yield strength at its operating temperature.

The effect of the above cyclic thermal stress conditions on fuel element fatigue life were evaluated by first identifying and separating the mean and alternating stress components for each stress intensity. A summary of the mean and alternating stress components for the non-prestressed and prestressed element designs is presented in Tables III-6 and III-7, respectively. The results of the mean and alternating stress components summary are then plotted on a Modified Goodman Diagram (Figure III-15) to determine the equivalent alternating stress for establishing an expected fatigue life.

The fatigue life, that is, number of cycles, can be found by plotting alternating stress on an experimental curve of fatigue cycle test data. Such a curve of experimental data for Inconel-625 steam tube weldments was prepared by Southwest Research Institute in the course of the welding development work described in Section VII-B. The experimental curve for the steam tubes is shown in Figure III-16 and for the boiler cladding in Figure III-17.

An examination of the component fatigue test data in Figures III-16 and III-17 and the results of the element stress analysis tabulated in Tables III-6 and III-7 and the diagram in Figure III-15 predicts fatigue failure in the non-prestressed element at approximately 6000 cycles. The failure would be expected to occur in either the area of the tube to tube lap joint or of the tube to tube sheet joint. Considering that the component fatigue test data from SwRI reflect the results

TABLE III-6

NON-PRESTRESSED ELEMENT DESIGNS
MEAN AND ALTERNATING STRESS COMPONENTS

		STRESS	570 °F BOILER	750 °F FIRST PASS	950 °F SECOND PASS	
Outer Fibre	Alt.		9,150	16,700	14,000	Floating Lower Tube Sheet
	Mean		9,150	16,700	14,000	
Inner Fibre	Alt.		15,735	8,350	17,222	Floating Lower Tube Sheet
	Mean		15,735	8,350	17,222	
Outer Fibre	Alt.		17,160	8,328	14,400	Fixed Lower Tube Sheet
	Mean		17,160	8,328	14,400	
Inner Fibre	Alt.		15,765	2,620	25,750	Fixed Lower Tube Sheet
	Mean		15,765	2,620	25,750	

No. of cycles boiler tube - 10^5

No. of cycles first pass tube - 10^5

No. of cycles second pass tube - 18,500

TABLE III-7
PRESTRESSED ELEMENT DESIGNS
MEAN AND ALTERNATING STRESS COMPONENTS

		<u>STRESS</u>	<u>570 °F BOILER</u>	<u>750 °F FIRST PASS</u>	<u>950 °F SECOND PASS</u>	
Outer Fibre	Alt.		9,150	17,679	585	Floating Lower Tube Sheet
	Mean		9,150	9,838	17,605	
Inner Fibre	Alt.		15,735	4,409	8,351	Floating Lower Tube Sheet
	Mean		15,735	8,350	202	
Outer Fibre	Alt.		2,543	6,593	1,585	Fixed Lower Tube Sheet
	Mean		20,550	10,063	26,525	
Inner Fibre	Alt.		4,193	8,638	24,730	Fixed Lower Tube Sheet
	Mean		27,283	5,168	1,620	

of laboratory-fabricated components and do not include the effects of constraints or interaction forces of shop-assembled components, the 6000 cycles would appear an optimistic estimate of fatigue life. It is known that bending and twisting moments are imposed on the steam tubes during fabrication and assembly of the element. These extraneous loads result in residual stresses that may markedly reduce the element fatigue life. The effects of some of the fabrication bending stresses have, therefore, been included in the tube to tube lap joint fatigue test data. During the fatigue testing of the lap joint weld specimens at Southwest Research Institute, a comparison of an excessively bowed specimen and a straight specimen showed a 50% reduction in fatigue life for the former. If residual stresses of this magnitude were locked into the assembled element, the life of the non-prestressed element would be reduced to approximately 3000 cycles.

Analysis of the prestressed floating tube sheet design by an examination of the component fatigue test data and calculated stresses in the manner outlined above indicates that the floating tube sheet element with prestress would have a fatigue life in excess of 10^5 cycles, as estimated from Tables III-4 and III-7 and Figures III-15 and III-17, providing that the floating tube sheet remains free. However, should the movement of the floating tube sheet be restricted by excessive element distortion or seizure of the steam tubes as a result of binding with the fuel pellets, the element would be expected to behave in the same manner as a modified prestressed fixed tube sheet element.

E. Nuclear Analysis

1. Introduction

Nuclear analyses of the Advanced BONUS Core have been performed with fuel elements of the multitube design which simultaneously provide boiling and superheating. Elements of the 3.5 in. OD annular multitube design as well as the reference 1.5 in. OD solid multitube configuration have been considered for use in the core. Even the solid multitube element contains about ten times as much fuel per unit length as the elements currently in use in commercial light water cooled and moderated reactors, which are generally about 0.4 in. in diameter. The larger diameter of the multitube element has been made possible by cooling the element internally with seven steam tubes.

The 1.5 in. diameter of the multitube elements maximizes the fuel loading of each element as much as is compatible with cooling requirements of the exterior (boiling) surface of the element and with the existing assembly pitch of the present BONUS reactor. The fuel loading is maximized for two reasons: (1) to minimize the fabrication cost per kilogram of contained uranium, and (2) to minimize the ratio of clad and steam tube volumes to that of uranium in order to decrease the parasitic absorptions in clad and steam tubes relative to absorption in the fuel.

Further reductions in parasitic absorptions could be obtained by the use of Zircaloy-2 boiler clad instead of the reference Inconel-625 material. The use of Zircaloy-2 boiler clad requires the development

of satisfactory transition joints between the boiler clad and the Inconel-625 structure of the balance of the element. However, problems which are as yet unresolved have arisen in connection with attempts to produce joints that maintain acceptable levels of integrity and strength following irradiation (see Section VI-C).

While the large diameter and the geometrical complexity of the multitube element present new problems in nuclear analysis, the multitube design offers the promise of improved fuel cycle and manufacturing costs for superheater elements.

2. Analytical Methods

The development of physics calculation techniques appropriate for the multitube element has involved a re-examination of some of the simplifying assumptions and methods used in the analysis of boiling and pressurized water reactor lattices.

In the reference design fuel element configuration, the thermal neutron flux depression is larger than for conventional elements, and the spectrum varies significantly across the fuel element lattice cell. The geometry of the fuel elements requires special attention in the calculation of U-238 resonance capture effects, especially when the internal steam coolant passages of the fuel element are flooded. The spatial variation of the U-235 depletion and plutonium build-up within the element influence local radial power distribution within the element during its lifetime in the reactor.

In the Advanced BONUS Core, as in other water-moderated cores, acceptable temperature and void coefficients must be maintained over

the range of operating conditions, and only small changes in reactivity should occur between flooding and unflooding of the fuel element steam coolant passages. The spatial power distribution must be sufficiently flat to permit the design power output without local overheating and, in addition must lead to the proper division of power between boiler and superheater. To accomplish these objectives, reliable and detailed physics calculations were required at an early stage in design development.

Fortunately, considerable work had previously been done with regard to the physics of superheat lattices, both as to the experimental^(37,38) and analytical aspects^(39,40), as part of the Nuclear Superheat Development (NUSU) Program conducted by Combustion Engineering, Inc., for the USAEC under Contract AT(11-1)-795. Full use has been made of the results of this work.

The following paragraph describes briefly the techniques which have been used for physics calculations associated with the ABC design. The basic scheme involves treatment of the neutron flux in two energy groups, that is, a fast group of neutrons with energies between 10 mev and 0.625 ev, and a thermal group of neutrons with energies less than 0.625 ev. Cross sections for the fast group were obtained with FORM⁽⁴¹⁾, a 54-group slowing down calculation. Self-shielding due to resonances in U-238 was accounted for by the Adler-Hinman-Nordheim method after making suitable "Dancoff type" corrections. This treatment is presented in detail in Reference 42. Thermal neutron properties of the ABC solid multitube elements were obtained from the THERMOS

program ⁽⁴³⁾ which treats the thermal space-energy problem by means of multigroup integral transport theory. Brown and St. John kernels were employed in the THERMOS calculations, and the transport correction to the scattering cross section for hydrogen followed the Radkowsky prescription. ⁽⁴⁴⁾ Infinite medium depletion calculations were performed in four neutron energy groups with the CRUMB program. ⁽⁴⁵⁾ One-dimensional diffusion-theory spatial calculations were done with WAFIB ⁽⁴⁶⁾ and two-dimensional studies employed PDQ. ⁽⁴⁷⁾ Fuel cycle costs were calculated with the UNICYCLE program, ⁽⁴⁸⁾ which is based on the methods established in Reference 49.

3. Nuclear Properties of the ABC Solid Multitube Fuel Element

This section presents the results of calculations of nuclear properties of the ABC solid multitube fuel elements which are of significance to their performance in the Advanced BONUS Core. Primary emphasis is placed on results obtained for elements with the reference Inconel-625 boiler cladding, but additional results are included for elements with Zircaloy-2 boiler cladding in order to demonstrate the advantages of producing satisfactory Zircaloy to Inconel transition joints.

a. Variation of k_{∞} and Materials Inventories with Initial U-235 Enrichment and Depletion

A parameter survey was made of the variation, with initial enrichment and with depletion, of the infinite medium multiplication factor and materials inventories to provide data for fuel management studies and economic evaluations. Initial U-235 enrichments of 4.0, 5.0, and 6.0 w/o were examined for elements with Inconel-625 boiler

clad, and initial enrichments of 3.0 and 4.0 W/O for Zircaloy-2 boiler clad. All calculations were carried out to at least 15,000 Mwd/MTU, the reference discharge exposure for these elements, and were performed at the estimated radially and axially averaged steam bubble content of 28% which is evaluated within the Zircaloy-2 shroud shown in Figure III-9.

Figure III-18 shows the variations of k_{∞} with fuel exposure at various enrichments for Inconel-625 and Zircaloy-2 boiler clad. The k_{∞} is seen to decrease less rapidly with exposure for the Inconel-625 clad elements than for the Zircaloy-2 clad elements for the reason that with higher enrichment, the fractional fuel depletion is less and there is a greater change in the fuel self shielding factors with exposure.

In order to illustrate one of the advantages of Zircaloy-2 boiler clad as compared to Inconel-625 boiler clad, estimates have been made of the U-235 feed enrichments for Zircaloy-2 clad elements which provide the same reactivities, averaged over the 15,000 Mwd/MTU reference lifetime, as Inconel-625 clad elements of various enrichments. These results are presented in Figure III-19, which shows that the boiler-superheater feed enrichment could be reduced from the current 5.4 W/O to 3.8 W/O if satisfactory Zircaloy-2 to Inconel-625 transition joints could be developed.

Table III-8 lists materials inventories with both Inconel-625 and Zircaloy-2 boiler cladding at the reference 15,000 Mwd/MTU discharge exposure for elements of various U-235 feed enrichments. Also included are tabulations of the fractional power generation in the fissile plutonium isotopes at various stages of depletion, and absorption

TABLE III-8

MATERIALS INVENTORY AND NEUTRON BALANCES

- ABC Boiler-Superheater Fuel -

Feed Enrichment, $\text{w}/\%$	<u>4.0</u>	<u>5.0</u>	<u>6.0</u>	<u>3.0</u>	<u>4.0</u>
Boiler Clad Material	Inconel-625			Zircaloy-2	
<u>Material Inventories at 15,000 Mwd/MTU</u>					
U-235; $\text{w}/\%$	2.43	3.32	4.33	1.55	2.42
$\frac{\text{gm fissile Pu present}}{\text{gm U-235 burned}}$	0.266	0.247	0.225	0.273	0.244
$\frac{\text{gm Pu (total)}}{\text{kg U (initial)}}$	4.79	4.57	4.25	4.75	4.45
$\frac{\text{gm Pu (fissile)}}{\text{kg U (initial)}}$	4.29	4.16	3.92	4.12	3.97
<u>Power From Pu, %</u>					
5,000 Mwd/MTU	8.2	6.1	4.5	11.5	7.2
10,000 Mwd/MTU	15.8	11.9	9.0	21.7	15.0
15,000 Mwd/MTU	23.2	17.6	13.4	31.5	22.1
<u>Percentage Absorption With Fresh Fuel</u>					
Moderator	7.12	6.48	6.22	8.62	7.60
U-235	58.14	61.07	63.74	59.41	63.55
U-238	21.43	20.35	18.99	23.65	21.74
Zircaloy-2	1.16	1.37	1.31	1.64	1.59
Inconel-625	12.15	10.73	9.73	6.68	5.51
<u>Absorptions in Clad and Structure</u>					
Absorptions in Fuel	0.1673	0.1486	0.1334	0.1002	0.0832

fractions in fresh fuel. It should be noted that, for enrichments which give comparable exposure-averaged reactivities, the use of Zircaloy-2 boiler clad instead of Inconel-625 cladding would decrease by 40% the number of absorptions in clad and structure relative to those in fuel.

Figure III-20 illustrates the average integrated dose of neutrons with energies greater than 0.82 Mev to the Inconel-625 clad as a function of fuel exposure. By 15,000 Mwd/MTU, the clad would have received a fast neutron exposure of 2.5×10^{21} nvt. Assuming an axial fast flux peak of 1.5 and a flat radial fast flux distribution, the boiler-superheater region being at most two assemblies or about 8 in. thick, the maximum integrated fast neutron dose to the clad would be about 3.8×10^{21} nvt.

b. Power Peaking with ABC Solid Multitube Element

The variation of power peaking within a fresh ABC solid multitube element is shown in Figure III-21 as a function of U-235 enrichment up to 15 w/o which is the upper limit of enrichments considered feasible for irradiation testing. At an enrichment of 5 w/o, for example, the power peak is 1.23. Included in the calculations are the effects of non-thermal fissions in U-235, which are significant, as shown in Figure III-22. For example, at an enrichment of 5 w/o, 23% of the fissions are non-thermal.

Since the steam bubble content of the coolant in an ABC boiler-superheater assembly ranges from 0 at the inlet to about 40% at the exit of the assembly, the variation of power peaking with steam bubble content was examined, and was found to vary at most $\pm 0.2\%$

from the value of the 28% axially averaged steam bubble content.

c. Reactivity Coefficients

Preliminary calculations were made for boiler-superheater fuel of the 540°F void coefficient of reactivity and the zero-void temperature coefficient of reactivity at an initial U-235 enrichment of 4 w/o and at various bucklings up to and including the core geometric buckling. These calculations were made with a view to obtaining indications of the practicability of the boiler-superheater fuel from the standpoint of the reactivity coefficients.

The 540°F void coefficient was calculated to be $-0.35 \times 10^{-3} \Delta k/k/\%$ void at zero buckling, and $-1.98 \times 10^{-3} \Delta k/k/\%$ void at the ABC geometric buckling of 0.002 cm^{-2} . The temperature coefficient of reactivity, on the other hand, was positive at zero buckling, with a value of $6.34 \times 10^{-5} \Delta k/k/^{\circ}\text{F}$, and became negative ($-1.13 \times 10^{-5} \Delta k/k/^{\circ}\text{F}$) at the ABC geometric buckling.

Reactivity coefficients had been measured at BONUS (50) to be acceptably negative for a core composed of an eight by eight array of boiler fuel assemblies, i.e.,

$$\left. \frac{\partial k}{\partial \alpha} \right|_{540^{\circ}\text{F}} = -2.0 \times 10^{-3} \Delta k/k/\% \text{ void}, \quad \left. \frac{\partial k}{\partial T} \right|_{\alpha=0} = -2.2 \times 10^{-4} \Delta k/k/^{\circ}\text{F}$$

The above information suggests that the overall ABC void coefficient is negative, since the boiler region in ABC is about half the size of the Core 1 boiler region which has a measured negative void coefficient, and the boiler-superheater void coefficient is calculated to be negative even at zero buckling. The overall ABC temperature coefficient which

has not been calculated, is also expected to be negative, since the central boiler region has a negative temperature coefficient and the boiler superheater has a negative or a very small positive temperature coefficient. The overall core temperature coefficient calculation was deferred pending specification of the feed enrichments and final selection of the relative sizes of the boiler and boiler-superheater regions of the core.

The flooding coefficient for superheater fuel in the BONUS reactor is intended to be zero or slightly positive so that reactivity changes caused by unflooding during start-up will be negligible or slightly negative. The present steam tube flooding coefficient has been calculated to be positive at a U-235 enrichment of 15 w/o and to have an infinite lattice value of $+ 2.0\% \Delta k_{\infty} / k_{\infty}$ at 68°F*. The flooding coefficient in ABC should be smaller for four reasons: (1) the feed enrichment in the reference core is 5.4 w/o, and the self-shielding of the U-235 is thus less than in the 15 w/o calculation so that the positive reactivity effect of an enhanced internal source due to flooding should be smaller; (2) the poisoning effect of the water in the steam tubes should become larger at lower enrichments; (3) the BONUS superheater must be unflooded with reactor steam at elevated temperature; consequently, the water removed in unflooding is of lower density than at 68°F; and (4) the core is only partially composed of superheater fuel, and negligible reactivity changes occur in the boiler region as the boiler-superheater region is unflooded.

* This calculation was performed in connection with safeguards studies for manufacturing high-enrichment irradiation test elements for EVESR.

4. Fuel Management Studies

a. Objectives and Variables

Studies of fuel management have been performed for the Advanced BONUS Core to determine, first, an equilibrium core which will meet all performance objectives and have minimum fuel cycle costs. Further work is required to determine an initial core which will burn to the equilibrium core in the most economical manner. Included as variables in the studies were the boiler and boiler-superheater feed enrichments, and the relative number of boiler and boiler-superheater assemblies. The total number of assemblies is limited to 88 since 88 openings exist in the core plate for forced circulation flow through the boiler assemblies and the boiler side of the boiler-superheater assemblies.

The performance objectives of the Equilibrium Advanced BONUS Core are listed in Table III-9. The boiler fuel discharge exposure was chosen so as not to exceed present day design exposures of Zircaloy-2 clad BWR fuel and to be a reasonable extrapolation, i.e., 50%, from Core I performance of the boiler fuel. The discharge exposure set for the boiler-superheater fuel was felt to be a minimum exposure of interest for future, larger plants incorporating this type of element.

Design steam conditions at the turbine inlet (850 psig, 900°F) at 50 Mw(t) require that about 24% of the total core power be channeled to superheat throughout life. During a burnup cycle, the control rods are available to adjust the power split between the boiler and the boiler-superheater, and thus the power directed to the superheat

TABLE III-9

PERFORMANCE OBJECTIVES IN THE EQUILIBRIUM
ADVANCED BONUS CORE

1. Discharge Exposures, Mwd/MTU	
Boiler Tube	15,000
Boiler-Superheater Fuel	15,000
2. Refueling Interval, at 0.8 Plant Factor, Months	> 9
3. Superheat/Total Power, %	24
4. Peak Steam Tube Temperature in Boiler-Superheater, °F	1250
5. Peak Boiler Fuel Heat Rating, kw/ft	≤ 14.4
6. Steam Side Pressure Drop Through Boiler-Superheater, psi	< 100
7. Control Rod Worth Required at End-of-Cycle to Maintain Power Split	None
8. Worth Tied Up in Shimming to Flatten the Radial Power Distribution	None

portion. At the end of a burnup cycle, however, control rods are fully withdrawn and are no longer available to adjust the power split. The power split to be provided by the fuel management scheme is, therefore, specified at the end of a burnup cycle. It is recognized, of course, that uncertainties due, for example, to approximations in the calculational model, or to the variation of power split within the boiler superheater element with exposure, may result in an end-of-cycle power split other than the desired value. It should be pointed out, however, that much will have been learned about the thermal performance of the element and the validity of the design calculations, both thermal and nuclear, prior to the attainment of the equilibrium core. Furthermore, additional means of controlling the power split are available, such as the variation of feedwater temperature. Preliminary calculations have estimated that a $\pm 50^{\circ}\text{F}$ change in the feedwater temperature can vary the power split from 25% to 23%, i.e., over a range of about 8%.

One guideline for the design of the Advanced BONUS Core was that the peak heat rating of boiler fuel was to be kept within the Core 1 license limit of 14.4 kw/ft. Thermal-hydraulics analyses of the boiler region have shown that this guideline provides the most stringent limitations for the operation of the boiler fuel, and thus the design target is for operation of the boiler fuel at less than 14.4 kw/ft. Furthermore, consideration of the thermal properties of the boiler-superheater and of the mechanical loading and materials properties of the Inconel-625 have indicated that the maintenance of steam tube temperatures below 1250°F constitutes the most stringent limitation

on the operation of the boiler-superheater fuel.

Extension of these thermal limitations to upper bounds on peaking factors is not straightforward because of the complex interrelationships between the sizes of the boiler and boiler-superheater regions; the power splits between the two core zones; and the axial and radial power distribution within each zone. Thus, the approach taken in the search for workable fuel management schemes was to evolve a series of cores with relative zone sizes and feed enrichments as the variables, which met the other performance requirements, and to select from among these a reference core which gave indications, on the basis of preliminary thermal-hydraulics analyses, of meeting the thermal limitations described above. Detailed power distribution and thermal performance calculations would then be carried out for the reference core.

An additional thermal-hydraulics consideration is the pressure drop through the steam side of the boiler-superheater. Figure III-23 shows the calculated steam side pressure drop through the boiler-superheater versus the boiler-superheater size. The pressure drop is defined between the dome of the vessel and the outlet header upstream of the preheater orifice. The measured Core I pressure drop is 100 psi between these locations. The 100 psia pressure drop for Core 1 results in a pressure in the vessel dome of 975 psig, which is only 25 psi below the scram and proportional relief valve settings at 1000 psig. If the pressure drop could be reduced by 25 psia, then the plant could accommodate twice the pressure surge without a high pressure

scram. It should be pointed out in this connection that the calculated pressure drops in the Advanced BONUS Core have not been experimentally verified, and that calculations of pressure drop have considerable uncertainty. This provides additional reasons for adopting a configuration with a pressure drop of less than 100 psia, which suggests that the number of boiler-superheater assemblies should be at least 52.

b. Selection of the Reference Fuel Management Scheme

The reference fuel management concept in the Advanced BONUS Core is out-in shuffling. In this technique of fuel management, fresh fuel is loaded at the outside of the boiler and boiler-superheater zones at the beginning of each burnup cycle, moved inward at each subsequent refueling, and finally discharged from the innermost positions of each zone. The advantage of out-in shuffling is that fuel of the highest worth is located at the outside of each zone and that gross radial peaking, defined as the ratio of box average to zone average power, is thereby confined to low values, typically about 1.05 in the boiler.

Parameter studies were conducted to support estimates of feed enrichments, relative numbers of boiler and boiler-superheater fuel elements, and economic data for cores which would meet the performance targets, in Table III-9, other than those involving thermal limitations. These studies employed one-dimensional diffusion theory in cylindrical geometry for the spatial calculations. Figure III-24 summarizes the feed enrichments required to direct 24% of the power to superheat and yield $k_{eff} = 1.0$ at end-of-cycle versus the number of

boiler-superheater assemblies.

Geometrical considerations allowed the selection of three cores for thermal-hydraulics analyses in the range of boiler-superheater sizes from 52 to 60 assemblies. As shown in Figure III-25 A and B, cores with 52 and 56 boiler superheater assemblies have octagonal symmetry and thus are appealing from the standpoints of flux symmetries and piping design. While cores with 60 boiler-superheater assemblies could suffer from potentially adverse flux asymmetries and/or peaking due to the irregular and jagged boiler/boiler-superheater interfaces, as illustrated in Figure III-25 C and E, one core with 60 boiler-superheater assemblies was included in the survey for the sake of completeness. Data obtained from these analyses are presented in Table III-10.

Thermal-hydraulics analyses of the three cores described above ruled out the core with 60 boiler-superheater assemblies and 28 boiler assemblies because, even with a conservative estimate of axial peaking factors of 2.0, the peak heat rating in the boiler was about 18% higher than the allowable maximum value of 14.4 kw/ft. Evaluation of boiler-superheater steam tube temperatures for the 56 boiler-superheater core showed a reasonable margin below the design limitation of 1250°F and a favorable pressure drop of 75 psi through the steam side of the boiler-superheater. Boiler-superheater steam tube temperatures were excessive with only 52 boiler-superheater assemblies, and the steam side pressure drop of 86 psi provided only small allowance for uncertainties in estimating the pressure drop. Thus, the core with 32 boiler and 56 boiler-superheater assemblies was selected for the

TABLE III-10

COMPARISON OF FUEL MANAGEMENT SCHEMES

Out-In Fuel Shuffling - Management Schemes

		<u>Target</u>	<u>A</u>	<u>B</u>	<u>C</u>
Assemblies	-Boiler		36	32	28
	-BSH		52	56	60
	Total		88	88	88
Enrichment, ^w / _o	-Boiler		2.4	2.6	3.0
	-BSH		6.0	5.4	5.0
Fraction Refueled Each Cycle	-Boiler		1/4	1/4	1/2
	-BSH		1/4	1/5	1/3
Discharge Mwd/MTU	-Boiler	15,000	16,500	16,000	16,000
	-BSH	15,000	14,500	14,600	14,500
Refueling Interval, Months (at 0.8 Plant Factor)		> 9	10.6	9.2	16.1
BSH Power Fraction	-BOC		0.542	0.537	0.521
	-EOC		0.541	0.550	0.559
Superheat/Total Power, %	-BOC	24	23.6	23.4	22.4
	-EOC	24	24.0	24.0	24.0
Product of Gross and Local Radial Peak*					
Within Boiler**	-BOC		1.37	1.37	1.46
	-EOC		1.37	1.37	1.39
Within BSH	-BOC		1.37	1.40	1.53
	-EOC		1.33	1.37	1.44
Peak Heat Rate in Boiler, kw/ft***		14.4	13	14	17
Boiler-Superheater Steam Side Pressure Drop, psi		< 100	86	75	66
$(k_{BOC} - k_{EOC})/k_{BOC}, \%$			3.98	3.98	6.80

* Based on one-dimensional survey calculations

** Results include estimated 1.30 local max/avg

*** Assuming an axial peaking factor of about 2.0

detailed analysis described in Section III-E-5.

Economic analyses were conducted for cores with 52, 56, and 60 boiler-superheater assemblies. Details appear in Reference 51, and the results are presented in Figure III-26, where they are expressed as fuel cycle cost savings relative to Core I. It will be noted that the curve exhibits maximum savings with the reference core configuration containing 56 boiler-superheater fuel assemblies, but that the cost savings would be only a few percent less with either a 52 or 60 assembly boiler-superheater region.

In order to fulfill the provisions of the contract this study was made for the actual expected exposure lifetime of the Core I fuel elements (11,020 Mwd/MTU) relative to the design value for the ABC lifetime (14,500 Mwd/MTU). A study of whether this is representative of the actual difference in performance of the two element types was not supported by the contract. A difference in ultimate exposure capability of ABC and Core I is to be expected theoretically on two grounds. First, the characteristic property of the ABC combination boiling-superheating element, of yielding an inherent power split which does not change appreciably with exposure, removes the very serious problem of controlling the power split for long exposure values; the problem of controlling power split over long exposures does exist with the Core I elements. As the BONUS reactor is designed, the capability to control power split has a definite upper limit represented by the reactivity control capability of the boiler-superheater control rods and the associated shims. In the more general

case, if one would consider controlling the power split over a larger range of exposure by means of control rods placed within the superheater region, the thermal performance of the superheater would suffer badly from the resulting radial distortion of the power distribution. Secondly, it is not at all clear at this time that the "metallurgical" lifetime of superheater elements is determined by the exposure of the fuel material, as it usually proves to be in the case of conventional water-submerged elements. The actual exposure time to the superheated steam may in fact be the limiting factor. If the latter should prove to be the case, then the 60% higher specific power of the ABC elements (not to mention their expected superior metallurgical performance) would more than compensate for the difference in assumed fuel burnup ratios in the two analyses - that is to say, the ABC elements would actually have a shorter residence time in the hot steam than Core I elements. Aside from the above considerations, the predicted reduction in the manufacturing cost of the ABC elements of 28% of the manufactured cost of Core I fuel elements in dollars per kilogram of uranium (\$/Kg U) is believed to be significant. Both estimates were made on the same basis by the same fuel element fabricator.

Obviously a comparison of general significance would require a much more comprehensive study, and one which was not bound by the existing characteristics of the BONUS reactor. Nevertheless the comparison does appear to support earlier, more general, studies which first demonstrated the cost advantages of the combination boiling-superheating element.

c. Alternate Fuel Management Schemes

As mentioned before, the reference fuel management concept for the ABC employs out-in shuffling, which has the advantage that fuel of the highest worth is located at the outside of each zone, keeping gross radial peaking to low values. Shuffling does have the disadvantage, from an operational standpoint, that each fuel assembly in the core must be moved at each refueling.

Two other techniques of fuel management, i.e., scatter and roundelay refueling, were examined briefly. In both techniques, each fuel assembly remains in one location from installation to discharge, a scheme which reduces fuel handling at each refueling by about 75%. In scatter refueling, fuel assemblies are replaced in a checkerboard pattern, distributed uniformly over each zone, while in roundelay refueling each zone is divided into annular rings, one ring being reloaded with fresh fuel at each refueling.

Table III-11 presents comparisons of scatter and roundelay refueling with out-in shuffling in the reference ABC (32 boilers, 56 multitube fuel assemblies). The table also indicates the manner in which the various situations under examination were represented in cylindrical geometry calculations. The boiler and boiler-superheater were divided into four and five equal-area rings, respectively. The number of rings per zone is the reciprocal of the fraction of each zone refueled each burnup cycle. Proceeding with a 9.2 month refueling interval, and specific powers of about 16.0 kw/kg UO_2 and 11.5 kw/kg UO_2 in the boiler and boiler-superheater, respectively, of the reference core, the burnup

TABLE III-11

COMPARISON OF OUT-IN SHUFFLING, ROUNDELAY, AND SCATTER REFUELING IN
THE REFERENCE ABC (32 BOILERS, 56 BOILER SUPERHEATERS)

Method of Fuel Management	Core Condition	BOILER					BOILER-SUPERHEATER					
		Ring Exposure $\times 10^{-3}$, Mwd/MTU				P_{Max}/P^*	Ring Exposure $\times 10^{-3}$, Mwd/MTU					P_{Max}/P^*
		*** 1	2	3	4		*** 1	2	3	4	5	
Out-In Shuffling	BOC	12	8	<u>4</u> **	0	1.36	<u>12</u>	9	6	3	0	1.41
	EOC	16	12	<u>8</u>	4	1.37	<u>15</u>	12	9	6	3	1.37
Scatter Reloading	BOC	<u>6</u>	6	6	6	1.56	<u>6</u>	6	6	6	6	1.53
	EOC	<u>10</u>	10	10	10	1.53	<u>9</u>	9	9	9	9	1.48
Roundelay Refueling	BOC Cycle 1	12	8	<u>4</u>	0	1.36	<u>12</u>	9	6	3	0	1.41
	BOC Cycle 2	<u>0</u>	12	8	4	1.82	<u>0</u>	12	9	6	3	1.63
	BOC Cycle 3	<u>4</u>	0	12	8	1.67	<u>3</u>	0	12	9	6	1.59
	BOC Cycle 4	<u>8</u>	4	0	12	1.47	<u>6</u>	3	0	12	9	1.54

* Product of gross and local radial peaking factor within zone

** Beginning-of-cycle

*** Location of the peak is indicated by the underlining

increments per cycle are, in the same order, about 4,000 and 3,000 Mwd/MTU. Scatter refueling is represented by placing the zone-averaged compositions from the out-in shuffling scheme throughout each zone. Roundelay refueling is represented by replacing fully exposed fuel in the out-in shuffling scheme at end-of-cycle with fresh fuel, depleting for one cycle, replacing fully exposed fuel with fresh fuel, etc. For roundelay refueling, only the beginning-of-cycle cases were studied since detailed spatial depletion studies performed during other programs have shown that peaking is always lower at end-of-cycle than at beginning-of-cycle.

On the basis of the one-dimensional calculations reported here, the results of the study indicate that both scatter and roundelay refueling have higher peaking than out-in shuffling. For example, scatter refueling has gross radial peaking in the boiler which is 14% higher than for out-in shuffling and 9% higher peaking in the boiler-superheater. The worst roundelay cycle, i.e., the second cycle which places fresh fuel in the innermost positions of each zone, has 34% higher peaking than out-in shuffling in the boiler, and 16% higher peaking in the boiler-superheater. It is thus concluded that out-in shuffling is the preferred method of fuel management in the ABC. It is expected that the inclusion of two-dimensional effects would likewise favor out-in shuffling.

5. Power Distribution Studies in the Equilibrium Core

a. Radial Studies

The power split and peaking values ascertained in connection with the fuel management studies described in the foregoing were

calculated in one-dimensional cylindrical geometry. Detailed two-dimensional calculations of the radial power distribution were performed to support thermal-hydraulics analyses of the reference equilibrium Advanced BONUS Core. Water channels were explicitly included, but in order to obtain conservative values prior to the inclusion of interaction between the void and power distribution, no credit was taken for local flattening effects of spatial depletion in partially exposed fuel. The calculations provided values of assembly average to zone average and element average to zone average power densities for both core zones at end-of-cycle (EOC) with control rods fully withdrawn (see Figure III-27). Figure III-28 shows the beginning-of-cycle (BOC) power distribution in a rod-free radial slice. The general similarity of the rod-free BOC and EOC power distributions suggests the desirability of operating the core with banked control rods within each of the two zones prior to EOC in order to avoid adverse radial peaking.

A comparison of results from one and two-dimensional calculations is presented in Table III-12. It is noteworthy that the fractions of power to superheat estimated by the one and two-dimensional calculations differ from each other by only about 3%. This result validates the employment of a one-dimensional technique for scoping purposes in the ABC, even though small cores such as this are not, in general, well represented by one-dimensional methods. Furthermore, the one-dimensional calculations of power peaking proved to be pessimistic in that they overestimated power peaking in the boiler region by about 7%. This occurred because the one-dimensional treatment could not provide a

TABLE III-12

COMPARISON OF ONE AND TWO-DIMENSIONAL POWER SPLITS AND REACTIVITIES AT END-OF-CYCLE

	<u>One-Dimensional Calculations</u>	<u>Two-Dimensional Calculations</u>
Fraction of Total Power in Boiler	0.446	0.431
Fraction of Total Power in Boiler-Superheater	0.554	0.569
Fraction of Total Power to Superheat	0.241	0.248
Product of Gross and Local Radial Peaking Factor in Zone		
Boiler	1.37	1.28
Boiler-Superheater	1.40	1.42*

* Includes 1.04 azimuthal peaking factor

proper geometrical representation of the environment of the fuel assembly in which the peak power was located.

b. Axial Power Distribution in the Boiler-Superheater

Figure III-29 shows the axial unrodded power profile in the ABC boiler-superheater (solid curve) to have a peaking factor of 1.8, based on a void fraction distribution calculated for ABC thermal-hydraulics conditions (see Table III-1 of this report). The void fraction distribution in turn was calculated by starting the iteration procedure with the axial power profile for the present BONUS Core (dashed curve, Figure III-28).

The agreement between the assumed and first iteration power profile is to within 5% in axial peaking, and the performance of further iterations of the unrodded power distribution was not felt to be necessary in view of the fact that the most severe axial peaking situations are anticipated in the case of partially inserted control rods.

As indicated in Table III-1, core thermal performance will be satisfactory provided that the axial peaking factors are not greater than 2.1. Banked control rod operation within each zone of the ABC is not only possible because the power split between the boiler and boiler-superheater region increases only about 2.4% between BOC and EOC, but is also desirable to avoid aggravation of radial peaking. Since the burnup reactivity swing is only 4%, and the Core I average hot differential rod worth, corrected for the reduction in core size to 88 assemblies, 4 in. x 4 in. is about 0.4% $\Delta k/k/in.$, it would appear that

a bank insertion of between 10 and 20 in. might be required to hold down the excess reactivity at beginning-of-cycle. It is difficult to predict whether such an insertion will raise the rod-free peak of 1.8 in the boiler-superheater, for example, above the allowable maximum of 2.1

If future calculations should show axial peaking to be excessive, it can be reduced by several means, for instance, the use of partial length poison shims or moderator displacers in the lower portion of the core, or axial enrichment zoning.

6. Future Calculations

Completion of nuclear analysis of the Advanced BONUS Core requires the calculations outlined below.

(1) Temperature and flooding coefficient calculations should be performed in order to ensure that these coefficients are acceptable. The temperature effects should be studied in the full equilibrium core at EOC since leakage effects and the positive contribution of resonance effects in the plutonium isotopes are important.

(2) Power distribution studies should be extended to include the effects of partially inserted control rod banks and spatially non-uniform void distributions consistent with the power distributions.

(3) An initial core must be designed which will burn to the equilibrium core in the most economical fashion. It should be recognized that radial and axial enrichment zoning and/or shimming may be necessary to obtain acceptable power distributions.

F. Alternate Design Studies

Although the solid multitube fuel element was selected as the reference design in July 1964, earlier studies had been conducted on annular multitube fuel elements. (See References 14, 17, and 20.) Like the solid multitube element, the annular multitube element is a reactor fuel element which performs both the boiling and superheating functions.

A cross section of an annular multitube fuel element with 11 steam tubes is shown in Figure III-30. This element was proposed to be constructed with two concentric Zircaloy-2 boiler clad tubes, the larger one 3.316 in. OD by 0.115 in. wall and the smaller one 1.378 in. ID by 0.093 in. wall. Boiling would occur on both the outside and inside diameters. Eleven 0.500 in. OD by 0.050 in. wall Inconel-625 steam tubes would be equally spaced in the fuel region between the boiler tubes on a 2.5 in. tube circle. The suggested fuel is vibratory compacted UO₂ having a density equal to 85% of theoretical density. Saturated steam at 540°F would be supplied to the steam tubes through eleven 0.300 in. OD by 0.040 in. stainless steel feeder tubes concentrically placed within each steam tube. The steam would flow down the inside of the feeder tube, turn 180°, and return up the annulus between the feeder tube and the steam tube. A longitudinal section of the 11-tube annular fuel element is shown in Figure III-31.

The 11-tube annular multitube fuel element is considered a promising design for future development. It is of relatively simple construction, as the re-entrant tube arrangement for steam flow minimizes the complexity of header construction and no lower plenum is required. The element

is very large in size and contains a large quantity of fuel, i.e., 44 kg, and may be expected to reduce significantly the manufacturing costs, particularly as inspection and handling costs remain fairly constant independent of the element size. The work on the annular multitube fuel element in the ABC program was terminated for the following reasons:

1. The number of designs under consideration had to be reduced in order to proceed with the program.

2. Adequacy of the annular element depends upon the satisfactory manufacture and performance of

- a. Vibratory compacted fuel,
- b. Zircaloy-Inconel joints,
- c. Large diameter "thick" walled Zircaloy-2 tubes,

the adequacy of which remains to be demonstrated.

3. Manufacture of pellets for large annular elements had not been developed and would have deferred the schedule of the ABC program.

4. Large interaction forces between vibratory compacted fuel and the outer boiler cladding were identified in the out-of-pile embedded tube tests, and the prediction of clad lifetime based upon these forces was unacceptably short (see Reference 20, Section II-3).

5. The in-pile creep fatigue strength of the outer Zircaloy tubes operating at their relatively high inside wall temperature was unknown.

6. No irradiation data were available on the Zircaloy-Inconel transition joints at the time of the decision. (When the data became available nine months later, they indicated that the joints were not

acceptable.)

While the annular multitube fuel element design does offer desirable features for the development of combination boiling superheating fuel elements, further work is required to obtain satisfactory irradiation behavior of Zircaloy transition joints; to arrive at a more complete understanding of the in-pile behavior of vibratory compacted fuel and its interaction with cladding; and to develop methods for fabricating large annular pellets.

FIG. III-1 Perspective Drawing of Reactor Vessel and Internals

FIG. III-2 Horizontal Cross Section of BONUS Core I

FIG. III-3 Perspective Drawing of Enriched Boiler Fuel Assembly

FIG. III-4 Perspective Drawing of Core I Superheater Fuel Assembly

BOILER-SUPERHEATER

**BOILER - SUPERHEATER
FUEL ELEMENT**

FIG. III-5 Advanced BONUS Core Horizontal Section

BILL OF MATERIALS					
QUANTITIES ARE FOR 1 UNIT					
GROUP NO & QUANTITY	ITEM NO	NAME	PIECE NO.	MATERIAL	REMARKS
6 5 4 3 2 1					
	1	SUPERHEATER IRRADIATION FUEL ELEMENT SUB-ASSY	E-2642-1		
	2	FUEL COMPRESSION SPRING	E-2642-5-6		
	3	BUSHING	E-2642-10		
	4	WASHER	E-2642-11		
	5	BOILER TUBE HEAD	E-2642-12		
	6	FUEL PELLET	C-3184		
	7	VIBRATION DAMPER SUB ASSY	E-2642-2		
	8	BOILER TUBE	E-2642-9		
	9	INSULATOR ASSY	E-2642-1		
	10	FUEL SUPPORT PLATE	E-2642-7		
	11	LOWER TUBE SHEET	E-2642-14		
	12	STEAM DEFLECTOR	E-2642-13		
	13	SUPERHEATER TUBE ASSY	E-2642-1

FIG. III-6 Advanced BONUS Core Boiler-Superheater Fuel Assembly

FIG. III-8 Advanced BONUS Core Boiler-Superheater Assembly

FIG. III-9 Perspective View of Advanced BONUS Core Fuel Assembly

FIG. III-10 Maximum Fuel Temperatures in ABC Fuel Element

FIG. IIF-11 Thermal Power Split for ABC Fuel Elements

FIG. III-12 ABC Vapor Fraction in Boiler Region of Core

FIG. III-13 ABC Vapor Fraction in Superheater Region of Core

FIG. III-14 Leakage Through Tube Joints - ABC Fuel Element

FIG. III-15 Modified Goodman Diagram Second Pass Steam Tubes Stress Components

FIG. III-16 Fully Reversed Axial Load Fatigue Test Results on Tube to Tube Lap Joints Using 0.283 In. and 0.337 In. Tubing

FIG. III-17 S-N Curve for Boiler Clad to Tube Sheet Welds at 600°F

FIG. III-18 K_{∞} Versus Fuel Exposure - ABC Solid Multitube Fuel Assemblies

FIG. III-19 Enrichment Comparison of Zircaloy-2 Versus Inconel-625 Boiler Clad

FIG. III-20 Average Nvt ($E > 0.82$ Mev) Versus Mwd/MTU in ABC Boiler Superheater

FIG. III-21 Power Peaking Within the ABC Solid Multitube Element Versus U-235 Enrichment

FIG. III-22 Fast Fissions/Total Fissions Versus Enrichment - ABC Solid Multitube Element

FIG. III-23 Steam Side Pressure Drop Versus Number of Boiler - Superheater Assemblies

FIG. III-24 Uranium - 235 Feed Enrichment Versus Number of Boiler - Superheater Assemblies

(a) - 36B/52BSH

(b) - 32B/56BSH

(c) - 28B/60BSH

(d) - 28B/60BSH

(e) - 28B/60BSH

BOILER

BOILER - SUPERHEATER

FIG. III-25 Advanced BONUS Core Layouts

FIG. III-26 Percent ABC Fuel Cycle Post Savings Relative to BONUS Core I Versus Number of Assemblies in ABC Boiler - Superheater

BOILER - SUPERHEATER

LEGEND

ELEMENT

AVERAGE POWER / ZONE AVERAGE POWER

FUEL EXPOSURE $\times 10^{-3}$, MWD/MTU ASSEMBLY

AVERAGE POWER / ZONE AVERAGE POWER

CORNER FUEL PIN

AVERAGE POWER / ZONE AVERAGE POWER

BOILER

FIG. IIF-27 Two-Dimensional Power Distribution in ABC - Out-In Shuffling, EOC

BOILER - SUPERHEATER

LEGEND

ELEMENT

AVERAGE POWER/ZONE AVERAGE POWER

FUEL EXPOSURE $\times 10^{-3}$ MWD/MTU ASSEMBLY

AVERAGE POWER/ZONE AVERAGE POWER

CORNER FUEL PIN

AVERAGE POWER/ZONE AVERAGE POWER

BOILER

FIG. III-28 Two-Dimensional Power Distribution in ABC - Out-In Shuffling, BOC

FIG. III-29 Axial Power Distribution in Boiler-Superheater

11 TUBES EQ. SPACED WITHIN .020
OF TRUE POSITION ON 2.5 TUBE
CIRCLE . INSIDE TUBE (SS)
.300 O.D. x .040 WALL,
OUTSIDE TUBE (INCONEL 625)
.500 O.D. x .050 WALL

FIG. III-30 Eleven Tube Annular Fuel Element Cross Section

FIG. III-31 One Pass Eleven Tube Annular Fuel Element

IV. IRRADIATION TEST

An irradiation test of ABC combination boiler-superheater fuel elements was planned for the Vallecitos Experimental Superheat Reactor (EVESR). While the detailed nature of the test had been described, its feasibility established and the test assemblies designed, the test was not performed due to funding limitations in the superheat program.

The design of the irradiation test in EVESR was a joint technical undertaking by Combustion Engineering, Inc., Nuclear Division (CEND), which was responsible for establishing the functional requirements for the test and designing the ABC test assembly, and the General Electric Co., San Jose, California (GE-APED), which was in charge of performing the nuclear analysis, establishing the feasibility of testing in EVESR, and conducting the test.

A detailed description of this proposed experiment is important to the ABC program, not only because it absorbed a considerable portion of the effort, but also because the results of the analysis performed (particularly the thermal analysis) furnish a clearer understanding of the manner in which combination boiling-superheating fuel elements of the multitube design are expected to perform in a reactor.

For the irradiation test, it was intended to install two ABC fuel assemblies, each consisting of four elements, into two cells of the EVESR reactor. The fuel assemblies are identical to the assemblies designed for the BONUS core except that the outer Zircaloy box is modified to fit in the EVESR lattice and an emergency coolant drip

pan is added to the irradiation test assembly.

Figure IV-1 shows the irradiation test assembly for EVESR. Note that the 5.531 in. by 5.531 in. cell into which the assembly is placed in EVESR is larger than the 4 in. by 4 in. cross section dimension of the ABC assembly. It is necessary, therefore, to fill the void space with an L-shaped boiling water rod (BWR) assembly. The boiling water rods increase the power in the superheater and also serve as water displacers. The skewed position of the ABC assembly in the cell is designed to assure the highest possible power density in the test element and to better simulate operating conditions in BONUS; however, the position of the ABC assembly in EVESR gives rise to a very large slope in the radial power distribution. The power distribution in EVESR for the ABC irradiation test is shown in Figure IV-2, which is reprinted from Reference 52.

On the basis of the power distribution and power density calculated by GE-APED, CEND performed a thermal analysis of the ABC irradiation test assemblies in EVESR.

Figure IV-4 shows that the axial variation of local power split does not vary greatly from 7 kw/kg to 13 kw/kg, although the local power split variations as shown in Figure IV-3 are sizable. These wider variations are caused by the fact that local power split is influenced by local steam temperatures as well as by local specific power, while the overall power split is primarily a function of average specific power; the steam temperature effect is essentially constant for all cases. Further, it can be seen that higher values of average specific

power tend to suppress the influence of local values of both steam temperature and specific power, as evidenced by the much smaller variation in local power split in the 12.2 kw/kg curve than in the 6.5 kw/kg curve of Figure IV-3.

Results of a MUTT-1 thermal analysis of a fuel element operating in EVESR at an average specific power of 11.7 kw/kg are given in Figure IV-5. The temperature distribution is seen to have quadrant symmetry since the analysis assumes circumferentially uniform heat generation. The higher temperature regions of the fuel tend to "hug" the hotter second pass steam tubes and "avoid" the colder first pass tubes. This temperature pattern is significantly different from the temperature patterns observed in single pass fuel element studies conducted earlier in the ABC program. For comparative purposes, Figure IV-6 shows the temperature pattern in a single pass solid multitube fuel element using vibratory compacted fuel. The temperature distribution in the fuel of the two pass element creates sharper temperature gradients around the periphery of the second pass steam tubes than the gradient which occurs around the steam tubes of one pass elements; however, the average steam tube temperature difference between passes, which affects axial stresses, is about 50°F lower for a two pass element than for a one pass element. Thus, when a two pass element is used, the poorer steam tube circumferential temperature distribution is compensated by the better temperature distribution between the steam tubes of the two passes.

A plot of steam and clad temperatures versus position along the first and second pass steam tubes for an average specific power of 11.7 kw/kg

and a mass flow of 640 lb/hr is shown in Figure IV-7. The center tube can be seen to have the highest clad temperature, with an average of 1075°F and a peak of 1110°F. The maximum boiler heat flux for this case is 200,000 Btu/hr-ft² and occurs 25 in. from the bottom of the element. A plot of the peripheral variation of boiler heat flux is given in Figure IV-8. The peak fuel temperature of this plane is seen to be only 1613°F and is characteristic of the multitube fuel element, which operates at temperatures well below the transition temperature of uranium dioxide fuel. Considerable margin in fuel temperature is thus available to allow for skewing effects, conductivity variation, gap resistance changes, and other thermal uncertainties in the design.

The overall power split of the fuel element, at an average specific power of 11.7 kw/kg, was found to be 0.443; see Figure IV-4.

A parallel thermal evaluation of the ABC test assembly in EVESR was performed by GE-APED. Their analysis^{(52),(53),(54)} included a circumferential power distribution factor of 1.44 which was not included in the CEND studies. This larger circumferential power factor is due to the placement of the ABC fuel assembly in a reactor position of sharp power variation. A tabulation of the performance data presented in Reference 54 is given in Table IV-1. It is noteworthy that the multitube fuel element can be tested in EVESR under conditions which approximate BONUS operating conditions even though the local power variation with the ABC assembly is considerably larger in EVESR. Because of this large circumferential peaking factor, an excess of steam flow is required in the EVESR test to assure maintenance of the clad temperature

within specified limits for the hottest fuel element when the EVESR core is operating at 17 Mw and the control rods are fully out.

The hot ABC element in the EVESR test is shown to operate at a specific power somewhat above the average specific power predicted for the Advanced BONUS Core. Maximum clad temperature in the EVESR test can be maintained at the BONUS design maximum of 1250°F by proper adjustment of steam coolant flow. Fuel temperatures are slightly lower in the EVESR test but not sufficiently low to alter significantly any fuel cladding interaction that might take place. Power split of the ABC fuel element in EVESR can be inferred by determining from the core physics the total power production of the ABC assembly and by measuring, during the test, the power production on the steam side of the element. The power split in the EVESR test would be slightly lower than that expected in the Advanced BONUS Core, principally because of the higher enrichment needed in the EVESR test to assure adequate core reactivity and boost local specific power in the ABC assembly. Power distribution studies conducted both at CEND and at GE-APED have shown a sharp increase in relative power production at the outside of the element when the fuel enrichment is increased. This effect is due to self-shielding within the element which becomes a pronounced effect at enrichments above 10%. (See Reference 52 and 54.)

TABLE IV-1

ADVANCED BONUS CORE IRRADIATION TEST (ABC II) IN EVESR

EVESR Core Power (Mw)	17
Number of ABC Fuel Assemblies in Test	2
Number of Fuel Elements per Assembly	4
ABC Positions in EVESR	Zone 2, Pos. B2 & B5
Assembly Total Power (kw)	458
Assembly Average Specific Power kw/kg of UO ₂	9.8
Hot Element Peak Axial Specific Power kw/kg of UO ₂	16.4
Hot Element Average Specific Power kw/kg UO ₂	11.7
Maximum Steam Tube Temperature (°F)	1250°F
Assembly Steam Flow lb/hr	3340
Peak UO ₂ Temperature (°F)	2115
Peak Heat Flux Steam Side Btu/hr-ft ²	232,000
Peak Heat Flux Water Side	257,000
Fuel Enrichment w/o U ²³⁸ in UO ₂	15
Power Axial Peak/Average (Rods Out)	1.4
Power Circumferential Peak/Average	1.44
Inlet Steam Pressure (psi)	973
Inlet Steam Temperature °F	560
Outlet Steam Temperature °F (Hot Element)	915
Pressure Drop, Active Fuel (psi)	40

FIG. IV-1 ABC Irradiation Test Assembly in EVESR

CORE C

38.5 0.89	35.4 0.81	39.6 0.91
48.4 1.11	44.1 1.01	42.7 0.98
47.2 1.09	44.2 1.02	50.3 1.16

31.3 0.82	33.9 0.89	36.3 0.95
40.9 1.07	36.8 0.96	34.7 0.91
46.8 1.23	37.8 0.99	45.0 1.18

XX.X
X.XX

POWER IN kW

FUEL ELEMENT RELATIVE
POWER COMPARED TO THE
AVERAGE FUEL ELEMENT
POWER WITHIN THE GIVEN
FUEL BUNDLE

73.6 0.95	67.9 0.88	64.8 0.84
92.9 1.20	66.9 0.86	64.9 0.84
100.7 1.30	86.9 1.12	76.9 0.99

359.23	
111.3 0.97	88.11 0.77
137.4 1.20	120.8 1.06

45.5 1.16	35.2 0.90	36.9 0.94
38.8 0.99	37.8 0.96	34.9 0.89
49.4 1.26	42.4 1.08	32.4 0.83

ABC ASSEMBLY

149.57 1.07	150.19 1.07
123.51 0.88	137.91 0.98

38.32 1.11	33.06 0.96
550.23	
35.19 1.02	32.05 0.93

69.6 1.56	46.1 1.04	36.0 0.81
60.8 1.37	40.1 0.90	39.3 0.88
43.8 0.98	36.1 0.81	28.4 0.64

CORE C

390.61 0.74	343.49 0.65	
695.59 1.31	816.83 1.54	353.26 0.66
561.19 1.06	688.86 1.30	400.17 0.75

$k_{eff} = 1.0997$

FIG. IV-2 EVESR Power Distribution - ABC (15% Enriched Test Bundle in Zone 2, Outer ("Black") Rods and Inner Rods Out (17 Mw(t)))

FIG. IV-3 Local Thermal Power Split - EVESR Test Fuel Element

FIG. IV-4 Thermal Power Split for ABC Elements in the EVESR Reactor

$T_{MAX} = 1613^{\circ}F$

$\bar{T} = 1253^{\circ}F$

25 INCHES FROM BOTTOM OF ELEMENT

AVERAGE ELEMENT POWER - 11.7 kw/kg UO₂

FIG. IV-5 Maximum Fuel Temperatures in ABC Fuel Element in EVESR

FIG. IV-6 Temperature Distribution in Single Pass Fuel Element with Vi-Pac UO_2

FIG. IV-7, Steam and Clad Temperatures for ABC Test Fuel Element in EVESR

FIG. IV-8 Circumferential Variation of Heat Flux on Boiler Clad at Plane of Maximum Power - ABC Test Element in EVESR

V. THERMAL CYCLE TESTING

A. General

It is a characteristic of combination boiling-superheating fuel elements that large differential temperatures, and, therefore, stresses exist in operation due to the exposure of the boiler side cladding to saturated steam temperatures and of the steam side cladding to superheated steam temperatures. For the BONUS reactor, the saturated steam temperature is 540°F and the superheated steam temperature 950°F, resulting in a 400°F differential steam temperature that must be accommodated by structural and cladding members of welded construction. A thermal cycle test was devised to investigate experimentally the effects on the structural integrity of the ABC combination boiling-superheating fuel element when the element is repeatedly exposed to the cyclic steam temperatures and the water temperatures and pressures that will prevail during reactor service. A detailed description of the thermal cycle test is given in Reference 31.

A completely automatic thermal cycle test loop was designed and constructed at the CEND laboratories in Windsor, Connecticut, for the purpose of testing Advanced BONUS Core fuel elements. The loop was designed to contain two test fuel elements in an autoclave at 540°F temperature and a pressure of 950 psia. Superheated steam at 1225°F and 950 psia is supplied to the steam passages of the fuel elements from an electrically fired superheater. The superheated steam temperature is varied from saturated temperature to 1200°F and back again in 24 to 30 minutes by means of an automatic controller. Calculations of the metal

temperature and stresses induced in the cladding of the fuel elements were made which showed maximum metal temperatures of 1050⁰F and stresses as high as 61,500 psi in the second pass steam tubes of the fixed tube sheet fuel element during the test.

Two styles of combination boiling-superheating fuel elements, a fixed tube sheet design and a floating tube sheet design, were fabricated and thermal-cycle tested. The fuel elements were exposed to 2026 thermal cycles the severity of which equalled or exceeded that which would occur in a normal reactor startup to full power and subsequent shutdown. The fuel space of both fuel elements remained intact throughout the test, as evidenced by continuously monitored pressure recorders attached to the fuel spaces. After termination of the test because of schedule and funding considerations, both fuel elements were subjected to, and successfully passed, the same x-ray, liquid dye-penetrant, and helium leak tests to which they had been exposed prior to cycling.

Post-test dimensional inspection revealed that some permanent compressive distortion had occurred in the second pass steam tubes of the fixed tube sheet fuel element as a result of the thermal cycling. No permanent distortion of the floating tube sheet fuel element was observed. All fission gas containing welds of the fixed tube sheet element were sectioned and metallographically examined. Evidence of the beginning of fatigue damage to the thermal sleeves of the second pass tubes was observed in the heat affected zone (HAZ) of the lap welds. Circumferentially disposed cracks about 0.002 in. deep were found in the lap weld HAZ of each of the three second pass tubes of the fixed tube sheet element. The cracks were located in a contaminated surface layer of

the thermal sleeve tubing. The contamination occurred during fabrication of the tubing and is believed to have contributed to the fatigue crack formation. No damage was present in any of the welds of the floating plenum design.

The following conclusions can be drawn as a result of the thermal cycle test:

(1) Combination boiling-superheating fuel elements of the multitube design with either fixed or floating tube sheets can be satisfactorily manufactured for reactor service.

(2) Combination boiling-superheating fuel elements of the multi-tube design with either fixed or floating tube sheets can withstand more than 2000 superheated steam temperature cycles from 500°F to 1200°F without failure.

B. Description of Test Facility

The thermal cycle test facility consists of two basic systems, i.e., a complete flow loop capable of generating, superheating, and condensing steam continuously, and a pressure-controlled autoclave. These two systems are hydraulically separated but are thermally connected by the test element itself. During a test, the test element is suspended from the autoclave head so that it is immersed in autoclave water. Connections through the autoclave head route superheated steam from the loop through the internal steam tubes of the test element. Heat is given up by the steam, passes through the fuel of the element, and is transferred to the autoclave water. In this way, temperature

differences between the cladding of the interior steam tubes and the exterior "boiler" cladding are established. Since the loop can vary the superheated steam temperature according to a predetermined program, simulated reactor thermal cycles can be imposed on the test element. The entire experimental setup is automated for unattended operation, with all pertinent data being recorded automatically. A schematic flow diagram of the facility is shown in Figure V-1.

C. Description of Test Specimens

Two solid multitube fuel element designs were developed, and one fuel element of each design was fabricated for the thermal cycle test. The elements were designated as the floating tube sheet element and the fixed tube sheet element. The designs differ only in the details of the lower end features and the manner in which the lower tube sheet interacts with the boiler tube. The lower tube sheet of the floating tube sheet element is free to move in an axial direction with respect to the boiler tube whereas, the lower tube sheet of the fixed tube sheet element is welded to the boiler tube.

Each fuel element performs both boiling and superheating in reactor service. Under the BONUS reactor design conditions, the element produces 915°F superheated steam with a two pass internal tube arrangement and 540°F saturated steam at its external surface. Water surrounds the element and flows vertically up the outside diameter of the fuel element where it is heated to form saturated steam. The saturated steam is collected and directed by manifolding to the four first pass

internal steam tubes of the element. The saturated steam enters the four first pass tubes at the top of the element and travels down to the steam deflector, picking up heat from the stacked UO_2 fuel pellets. The steam deflector redirects the flow of steam to the three second pass steam tubes. The steam then flows back to the top of the element, picking up additional heat and leaving the element at a temperature of 915°F .

The fuel element assembly for the thermal cycle test consists of one boiler tube, seven steam tubes, two fuel compression springs, five fuel support washers, an insulator assembly, an upper and lower tube sheet, one fuel retaining bulkhead (floating tube sheet element only), a steam deflector and a boiler tube head arranged and assembled around a stack of 108 depleted perforated UO_2 pellets, as shown in Figure V-2.

All components, with the exception of the pellets, washers, springs, and insulator assembly, are made of Inconel-625. The fuel pellets are pressed and sintered UO_2 with a minimum density of 94% of theoretical density (10.31 gm/cc). The washer material is Inconel 600 and the springs are Inconel X No. 1 temper wire. The insulator assembly consists of packed stainless steel wool sandwiched between Inconel 600 washers.

The boiler tube forms the basic structure of the fuel element and supports or contains all other components of the assembly. The 1.516 in. OD boiler tube is approximately 60 in. long, with a wall thickness of 0.033 in. (see Item 10 of Figure V-2.) The boiler tube and upper tube sheet are assembled with an interference fit and welded to form a

monolithic structure (see Item 1 of Figure V-2). The upper tube sheet has a cylindrical form and is approximately 1.516 in. in diameter and 5/8 in. thick. The seven steam tubes are expanded for an interference fit with holes in the upper tube sheet and end welded to it. They are symmetrically arranged in the tube sheet, with six tubes equally spaced in an 0.910 in. diameter circle and one tube in the center. The three second pass steam tubes are located on a diametral line. The steam tubes are approximately 60 in. long with an outside diameter of 0.285 in. and a wall thickness of 0.026 in. The upper three inches of the second pass steam tubes are enlarged to an outside diameter of 0.337 in. to provide space for a thermal sleeve without restricting the outlet steam flow at this point.

The fuel compression springs (see Item 2 of Figure V-2) fit over the upper ends of two of the first pass steam tubes and are contained within the boiler tube. They are located on the steam tubes 180° apart and rest against the underside of the tube sheet; they are designed to maintain the fuel in position during handling, shipment, and operation of the fuel element. The lower end of each spring rests on the fuel support washers (see Item 3 of Figure V-2).

The washers are used as pressure plates to transmit the spring force uniformly to the surface of the top pellet, and also provide a convenient means for adjusting the final stack length of the assembly. They are perforated in a symmetrical hole pattern to fit over the seven steam tubes.

The fuel pellets are stacked over the steam tubes and are positioned

between the support washers at the top of the element and the insulator assembly at the bottom of the element. As mentioned before, the pellets are perforated discs pressed and sintered from depleted UO_2 and are 0.5 in. thick with a 1.443 in. OD. The pellets contain seven 0.294 in. diameter steam tube holes with six holes equally spaced on a 0.910 in. diameter circle and one hole in the center. A total of 108 pellets are stacked in each fuel element assembly. The detailed dimensions of the fuel pellet are given on Figure V-3 and a photograph of a typical pellet is shown in Figure V-4.

The insulator assembly (see Item 4 of Figure V-2) is only used in the floating tube sheet element and is located under the pellet stack. It is intended to insulate the lower tube sheet from the hot pellets and to provide a barrier which contains and collects pellet particles, while permitting the steam tubes to slide axially. The assembly consists of tightly packed steel wool contained between two washers and is perforated with a symmetrical hole pattern for fit over the seven tube array.

In the floating tube sheet element, the lower ends of the steam tubes pass through the perforated fuel retaining bulkhead (see Item 7 of Figure V-2) and terminate in the lower tube sheet (see Item 8 of Figure V-2) as a weldment. The lower end of the boiler tube is welded to the fuel retaining bulkhead. The open face of the lower tube sheet is enclosed by the steam deflector (see Item 9 of Figure V-2). The steam deflector and lower tube sheet are welded together to form the floating tube sheet, which fits freely within the bore of the boiler

tube head (see Item 12 of Figure V-2). The boiler tube head is welded to the edge of the lower face of the fuel retaining bulkhead.

In the fixed tube sheet element the fuel retaining bulkhead is not required and is replaced by a fuel pellet (see "Alternate Design" in Figure V-2). The lower ends of the steam tubes are directly terminated as a weldment with the lower tube sheet. The lower tube sheet is also welded to the boiler tube. The boiler tube head (see Item 18 of Figure V-2) with the captured steam deflector (see Item 19 of Figure V-2) is welded to the edge of the open face of the lower tube sheet (see Item 17 of Figure V-2).

Photographs of the thermal cycle test fuel element components and of the completed test assembly ready for installation in the test autoclave are shown in Figures V-5 and V-6, respectively.

D. Description of Test Results

1. General

On November 8, 1965, a total of 2011 automatic cycles had been recorded with no apparent failure of either test fuel element, and the system was shut down and secured. At this point, the records showed the following history of the thermal cycle test:

Total elapsed time of test	1776 hr
Total time at elevated temperature	1061 hr
Total time of actual cycling	932 hr
Total number of 24 min cycles	734
Total number of 30 min cycles	1277

Miscellaneous startup cycles	15
Total number of cycles	2026

2. Temperature Cycles

In the course of the thermal cycle test, the test fuel elements were subjected to a variety of temperature cycles. The data describing these cycles have been taken from the facility records and are presented in Table V-1.

In Table V-1 all the temperature cycles, including miscellaneous cycles which occurred either accidentally or during check-out operation, are tabulated on the basis of groups of identical patterns. The groups are numbered from 1 to 30 and correspond to the group numbers used in Table V-2 for the metal temperatures. The steam temperatures listed occurred either at the peak of the cycle or at the low point of the cycle, as noted in each case. At the low point of the cycle, there was very little heat transfer from the steam, so that the single value in the table represents inlet, interpass, and exit temperature. The autoclave temperature in the table is the temperature of the autoclave water in which the test elements were immersed; each of these values remained essentially constant throughout any given group of cycles.

The steam temperatures given in Table V-1 are instantaneous values at peaks and low points of the cycles. The variation with time of these temperatures for a typical cycle may be seen in Figure V-7, which was plotted directly from a multipoint temperature recorder chart.

TABLE V-1
ABC THERMAL CYCLE TEST TEMPERATURE CYCLES

Group No.	Cycle Numbers	Cycles in Group	Dates	Cycle Time	Autoclave Temp. °F	Steam Temperature at Cycle Low Point, °F	Steam Temperatures at Cycle Peak °F		
							Inlet	Interpass	Exit
1	(1)-(3)	3	9/1 - 9/3	60 min.	540	600	1150	915	825
2	(4)	1	9/3	60 min.	460	600	1150	780	700
3	(5)-(6)	2	9/7	60 min.	540	600	1125	950	875
4	1 - 40	40	9/8	24 min.	540	600	1075	910	830
5	41-271	231	9/9 - 9/13	24 min.	540	625	1125	925	850
6	272-606	335	9/13-9/20	30 min.	540	600	1140	935	870
7	607-705	99	9/20-9/22	30 min.	540	610	1170	945	875
8	(7)	1	9/22	Shock	540	800	1275	1030	945
9	706-740	35	9/22-9/23	30 min.	538	600	1080	890	835
10	741-758	18	9/23	24 min.	538	610	1175	950	880
11	759-803	45	9/23	24 min.	540	610	1150	940	865
12	804-1040	237	10/1-10/6	24 min.	540	620	1180	940	870
13	1041-1048	8	10/6	24 min.	465	600	1195	910	825
14	1049-1053	5	10/6	24 min.	450	580	1180	895	800
15	1054-1104	51	10/7-10/8	30 min.	450	550	1200	935	870
16	1105-1113	9	10/9	30 min.	460	550	1200	900	800
17	(8)-(14)	7	10/15	30-50 min.	460	550	1200	850	750
18	1114-1118	5	10/18	24 min.	445	515	1165	910	780
19	1119-1159	41	10/18-10/19	24 min.	445	525	1080	755	740
20	1160-1325	166	10/19-10/22	30 min.	445	525	1160	895	765
21	1326-1360	35	10/23	30 min.	445	525	1120	880	755
22	1361-1709	349	10/23-10/30	30 min.	445	525	1160	905	775
23	(5)	1	10/31	32 hrs.	445	525	1120	880	750
24	1710-1757	48	11/1-11/2	24 min.	445	555	1115	-	-
25	1758-1814	57	11/2	24 min.	445	550	1085	-	-
26	1815-1859	45	11/3	30 min.	445	570	1165	-	-
27	1860-1877	18	11/4	30 min.	445	540	1135	-	-
28	1878-1894	17	11/4-11/5	30 min.	445	560	1160	-	-
29	1894-1940	47	11/6-11/7	30 min.	445	525	1135	-	-
30	1941-2011	71	11/7-11/8	30 min.	445	530	1115	-	-

Note 1: Numbers in parentheses are miscellaneous cycles.

Note 2: Missing values of temperature indicate corresponding thermocouples had failed.

3. Stress Analysis of Fixed Tube Sheet Elements

From the calculated metal temperatures given in Table V-2, the stresses in the fixed tube sheet fuel element were calculated. The fixed tube sheet element had not been prestressed because one of the test objectives was to obtain cyclic stress levels equivalent to or greater than the operating stress levels considered in any of the element designs, and it was intended to precipitate the earliest possible fatigue failure in this element. No failure occurred, however, during the 2026 test cycles. The maximum and minimum stresses were calculated for each group of temperature cycles. The stresses for the average cycle and the peak cycle are given below.

<u>Maximum Stress</u>	<u>Average Cycle</u>	<u>Peak Cycle*</u>
First pass tubes	37,800 psi compression	41,700 psi compression
Second pass tubes	55,600 psi compression	61,600 psi compression
Boiler tube	43,700 psi tension	49,100 psi tension
 <u>Minimum Stress</u>		
First pass tubes	8,300 psi compression	7,400 psi compression
Second pass tubes	8,300 psi compression	7,400 psi compression
Boiler tube	8,200 psi tension	7,200 psi tension

*For cycles No. 1710 through 1757

As expected, the highest stresses occurred in the second pass steam tubes. The alternating and mean stress components for the average cycle in the second pass steam tubes are 31,900 psi and 23,600 psi,

TABLE V-2
ABC THERMAL CYCLE TEST
CALCULATED CLAD TEMPERATURES.

Group Number	Number of Cycles	2nd Pass Flow, #/hr	1st Pass Flow, #/hr	Overall Average Steam Tube Temperatures, °F			Average Boiler Clad Temperature °F
				Cycle Peak		Cycle Low Point	
				2nd Pass	1st Pass	Both Passes	
1	3	560	357	952	860	595	540
2	1	560	357	885	730	595	460
3	2	560	357	957	902	595	540
4	40	560	357	912	860	595	540
5	231	560	357	945	878	620	540
6	335	560	357	957	892	595	540
7	99	473	224	967	900	605	540
8	1	473	224	1053	983	605	540
9	35	342	162	895	852	595	538
10	18	342	162	972	905	605	538
11	45	342	162	955	892	605	540
12	237	342	162	970	895	615	540
13	8	430	204	942	852	595	485
14	5	430	204	927	835	575	470
15	51	430	204	957	887	545	470
16	9	430	204	940	835	545	480
17	7	430	204	915	785	545	480
18	5	430	204	928	830	510	465
19	41	430	204	817	745	520	465
20	166	430	204	917	815	520	465
21	35	430	204	890	800	520	465
22	349	430	204	922	825	520	465
23	1	430	204	890	815	520	465
24	48	430	204	885	810	550	465
25	57	430	204	917	815	545	465
26	45	430	204	928	830	565	465
27	18	430	204	900	810	535	465
28	17	430	204	922	825	555	465
29	47	430	204	900	810	520	465
30	71	430	204	890	800	525	465

NOTE:

Group numbers correspond to those used in Table VI-1

respectively. If these stress components are superimposed on a Modified Goodman Diagram along with similar stress components considered in the element it appears that the fixed tube sheet element was subjected to stresses equivalent to the highest operating stresses which would occur in reactor service (see Figure III-15). The plot of the stress components falls in a diagram line defined by the calculated maximum design alternating and mean stress components for the non-prestressed fixed tube sheet element for BONUS operating conditions.

E. Post-Test Metallurgical Examination

After being subjected to, and passing, the helium leak test, dye penetrant inspection and radiographic examination, the thermal cycle test fuel elements were subjected to a destructive examination.

Major emphasis was placed on the evaluation of welds, where the highest stresses develop during thermal cycle testing. In addition, the overall post-test quality of the welds was examined for conformance with Southwest Research Institute Acceptance Standards and Inspection Requirements.⁽⁵⁵⁾ With the exception of oxide inclusions in the boiler tube to tube-sheet welds, all welds were found free of cracks or fissures, porosity, incomplete fusion, and undercutting. Weld metal buildup and depth of penetration were in accordance with SwRI requirements in all cases.

No significant microstructural changes in the metal components of the two fuel elements were observed as a result of the thermal cycle test. The weld heat affected zones were free from precipitated

phases, indicating that the highly ductile, annealed structure of the base metal had been retained.

The fragmentation of the fuel pellets and the abrasion of finely powdered UO_2 caused wear between the mating surfaces of the boiler tube head and the pilot diameters of the tube sheet and steam deflector of the floating tube sheet element. Similar wear patterns had developed on the steam tubes in contact with the insulator spacers. Light wear marks present on the boiler tubes at locations where the separator assembly clips were attached to both fuel elements were believed to be the result of differences in the thermal expansion coefficients of the cladding and clip materials.

A shallow contaminated surface layer, apparently due to dye lubricant, was found in the thermal sleeves of the fixed tube element and the floating tube sheet element which were thermal-cycled as well as in the thermal sleeves of unassembled tubing. The thermal sleeves of the floating tube sheet fuel element did not show any fatigue cracks although they exhibited this layer. However, fine transgranular cracks developed in the surface layer on the inside of the thermal sleeves immediately adjacent to the lap welds of the fixed tube sheet element. None of these cracks extended beyond the surface layer into the normal structure of the tubing. (The lap weld is shown in Figure V-8 and the crack location in Figure V-9.) It is concluded, therefore, that the presence of the contaminated layer contributed to the formation of the fatigue cracks which were observed in the fixed tube sheet element. This observation is further supported by the fact that the cracks

appear on the double tube side of the weld rather than on the more highly stressed but uncontaminated single tube side.

Extensive bowing accompanied by 0.001 in. diametral increases developed at the upper end of the second pass tubes of the fixed tube sheet element due to the thermal cycle test. Such changes were not observed in the second pass tubes of the floating tube sheet element, where thermally induced stresses had been substantially lower. Measurements of wall thickness and hardness for a series of locations on both first and second pass tubes of both elements indicated no significant variation in values as a result of the thermal cycle test. All steam tubes were free from severe oxidation, pitting, cracks, or wrinkle formation. The unchanged room temperature properties of first and second pass tubes are confirmation of the satisfactory performance of Inconel-625 as a steam tube material under simulated ABC superheater thermal conditions.

F. Discussion

The thermal cycle test conducted on the combination boiling-superheating fuel elements of the multitube design was an engineering test which demonstrated the structural integrity of both the floating tube sheet and fixed tube sheet concepts. Both fuel elements successfully withstood over 2000 thermal cycles of superheated steam temperature variations from approximately 500°F to 1200°F. The temperature variations to which the fuel elements were exposed in the test were in excess of variations which the elements would experience

in reactor service, and the stresses induced in the fixed tube sheet design during the test likewise exceeded the stresses which any of the designs would experience in service.

The following comparison should contribute to proper assessment of the significance of 2000 thermal cycles in terms of reactor plant operations. The superheated steam changes made in the test simulate those which would occur in service during a change from zero power to full power. If it is assumed that a change from zero power to full power and back again were made every day, almost five and a half years would be required to accumulate 2000 cycles. Clearly, this assumed frequency of cycling is extremely high and would result in the generation of very little reactor power. The lifetime derived from these stringent theoretical conditions would nonetheless be sufficient for an economic fuel element.

On the other hand, if a full reactor power cycle were made on the average of once a week, which is a more realistic assumption, the time required to achieve 2000 cycles would be over 38 years. Clearly, such a long period of time allows a large margin for deterioration of fuel element fatigue life as a result of irradiation damage to the cladding and structural members. Although in-pile fatigue data are not available for Inconel-625, for Inconel and Incoloy type materials, a factor of three has been suggested as an appropriate reduction in fatigue life to account for irradiation damage (see Reference 56).

A reasonably good simulation of the in-reactor steam tube metal temperature distribution was achieved during the thermal cycle test.

A typical plot of axial temperatures imposed on the elements during the test is shown in Figure V-10. The curves represent radially averaged values of temperatures in each of the two passes; actually, each of the three second pass steam tubes and each of the four first pass tubes has a different axial temperature profile. Superimposed on the test profiles are the steam and clad temperatures which could be expected in a "hot element" in reactor service. For the purpose of simulating in-service thermal stresses, it is important to match values of axially averaged temperatures in each steam pass as well as the average boiler clad temperature. Figure V-10 shows that, while the axially averaged second pass temperature fairly well matches that which would obtain in reactor service, the first pass average clad temperature is considerably higher than the service value. Nonetheless, this temperature pattern provided an adequate simulation of service stress in the fixed tube sheet design, since the important thermal stresses are due to the ΔT between boiler clad and the steam tubes in general. In the floating tube sheet design, where the ΔT between the first pass and the second pass steam tubes is most significant, the test temperatures failed to simulate service conditions exactly; however, the test conditions were such that "floating" of the tube sheet was adequately demonstrated.

The peak temperature in the second pass steam tubes occurs very near to the lap weld which was determined by the fatigue tests at SWRI⁵⁵ to be the weakest region of the fuel element. In this respect the thermal cycle test is probably more severe than actual reactor service, where the peak metal temperature occurs down the length of the steam tube

at a considerable distance from the lap weld.

The fact that the proper (in-reactor) relationship between first and second pass steam temperatures could not be maintained in the thermal cycle test renders the strain fatigue results on the floating tube sheet design unrepresentative. However, the thermal cycle test results are valid in that they demonstrate that the floating tube sheet does, in fact, float. The motion of the floating tube sheet with the steam tube temperatures imposed by the thermal cycle test is calculated to be larger than the motion which would occur in the reactor, that is, 0.194 in. versus 0.188 in. This difference must be attributed to the fact that the average first pass metal temperature is too high in the test and thus the first pass tubes do not act to restrain tube sheet motion as they would do at lower, in-reactor temperatures. Even with the larger motion of the tube sheet in the test, however, it is known from the post-test examination that the tube sheet floated. The observed large numbers of scratches on the rubbing surfaces of the floating tube sheet indicate that full length motion of the floating tube sheet occurred many times throughout the test. The fact that no change in the pre-stress of the steam tubes of the floating tube sheet element occurred throughout the test demonstrates that the floating tube sheet never stuck. Had the tube sheet stuck, stresses sufficient to yield the second pass tubes would have been generated and this would have been observed as a permanent set in the steam tubes in the post-test dimensional survey of the floating tube sheet element.

The post-test examination of the fixed tube sheet fuel element

disclosed a number of interesting facts about its design, some of which were anticipated and some of which were unexpected. One significant result is that the need to prestress the fixed tube sheet design is now in question. The fact that residual stresses about equal to the desired values and direction of prestress were found to be present in the steam tubes and boiler cladding at the completion of the test argues strongly for eliminating the prestress altogether. The first few cycles of temperature excursion probably induce the desired prestress automatically. This conclusion is only true for the fixed tube sheet design, however, where the operating stresses are known to be of sufficient magnitude to yield the second pass steam tubes.

No evidence of thermal ratchetting between the fuel and cladding members was observed in the post-test examination of either fuel element. The lack of thermal ratchetting in the thermal cycle test does not necessarily assure that it will not occur in the reactor, because the fuel temperatures during the test were considerably lower than they are during reactor operation. However, it appears that dishing the ends of the fuel pellets will reduce the probability of axial thermal ratchetting of the boiler cladding during reactor operation. The floating tube sheet element did exhibit effects of frictional forces which could have occurred either between the steam tubes and the pellets or between the steam tubes and the insulator assembly. These frictional forces, which were observed to have had similar effects in the embedded tube experiments discussed in Section VI-B, acted to retard the motion of the floating tube sheet but were apparently sufficiently small in

the thermal cycle test not to cause permanent distortion of the tube or cladding members.

The post-test measurements of the diameter of the steam tubes showed the effect of temperature on the axial plastic strain distribution in the second pass steam tubes. Only the upper (high temperature) half of the second pass steam tubes in the fixed tube sheet element increased in diameter, due to permanent axial distortion in compression. This observation indicates that during the early fatigue cycles significant plastic strain occurred only in the hotter region of the second pass steam tubes. Thus, strains considerably higher than the calculated values based on uniform strain distribution must have occurred in the second pass steam tubes during the first cycles.

No attempt was made to continuously cycle pressure in addition to temperature throughout this test. The pressure stresses in the steam tubes are almost negligible (> 5000 psi) in comparison to the axial thermal stresses. While pressure stresses are not negligible in the boiler cladding, the demonstrated higher fatigue strength of the boiler clad to tube-sheet welds makes failure less likely in this region of the fuel element. Because of periodic maintenance and repair, twelve full pressure cycles were imposed on the fuel elements during the thermal cycle test. Diametral and length measurements before and after the test demonstrated that no permanent distortion occurred in the boiler cladding of either fuel element throughout the test.

The metallurgical examination of all welds of both fuel elements after completion of the thermal cycle test revealed defects only in the

thermal sleeves of the three second pass steam tubes of the fixed tube sheet fuel element. The 0.002 in. deep circumferential cracks which were observed on the inner diameter of the thermal sleeves are believed to have been caused by both thermal cycling and the presence of a shallow contaminated surface layer. The belief that the surface layer played a major role in catalyzing the formation of fatigue cracks is based on experience gained in fatigue testing lap welds at SwRI. The fatigue failures in these tests were predominantly in the heat affected zone of the smaller diameter tube, not in the thermal sleeve. The contaminated surface layer of the thermal sleeves is believed to have been caused by a die lubricant and can presumably be eliminated in future tubing orders. Even with this apparent surface defect, a considerable amount of additional thermal cycling in excess of 2000 cycles would be required to propagate the crack completely through the 0.026 in. thick wall of the thermal sleeve.

The wear pattern on the inner diameter of the boiler cladding and the outer diameter of the rubbing surfaces of the floating tube sheet fuel element are believed to have been caused by UO_2 particles which passed by the insulator assembly and abraded the close fitting surfaces of the mating parts. An alternate design of UO_2 seal was developed during the time the thermal cycle elements were undergoing testing. This alternate design, which resembles a valve packing gland seal on each of the seven steam tubes, is believed to represent a considerable improvement and would more effectively retard the entrance of particulate UO_2 into the floating tube sheet region. Even so, it appears

preferable to enlarge the clearances between the floating tube sheet and the inside diameter of the boiler cladding to assure free movement of the parts. The clearance in the tested element was 0.008 ± 0.002 in. on the diameter.

The following conclusions can be drawn from the Advanced BONUS Core fuel element thermal cycling test:

Fixed Tube Sheet Design

(1) The fixed tube sheet fuel element withstood, without failure, in excess of 2000 steam temperature cycles of 24 to 30 min, each cycle varying from about 500°F to 1200°F. These cycles simulated metal temperatures, temperature changes, and stresses that are as great as or greater than those to which the cladding would be exposed in reactor service.

(2) During the early part of the test, permanent shortening of the fuel element by 0.020 in. to 0.042 in. occurred as a result of yielding in compression of the second pass steam tubes at the high temperature end of the cycle. This distortion is not sufficient, however, to adversely affect the fuel element in its intended function.

(3) It does not appear necessary to prestress the fixed tube sheet fuel element because this will be automatically accomplished during the early temperature cycles, as described earlier.

(4) The beginning of fatigue damage to the lap welds was observed at 2026 cycles, but this evidence of damage may be due in part to the presence of a contaminated surface area of the 0.285 in. ID steam tubing, as discussed previously. The contamination occurred during

fabrication of the tubing and is not related to the thermal cycle test.

(5) All fission gas containing welds of the fixed tube sheet fuel element were metallographically examined after the thermal cycle test and were found to comply with the welding procedures and acceptance standards.

(6) Fuel elements of the fixed tube sheet design can be satisfactorily manufactured for reactor service.

Floating Tube Sheet Design

(1) The floating tube sheet fuel element withstood, without failure, 2000 steam temperature cycles of 24 to 30 min, each cycle varying from about 500°F to 1200°F. Although the temperature distribution in this fuel element did not simulate reactor service conditions for inducing proper axial stresses in the steam tubes, the test did demonstrate that the lower tube sheet floated in the manner intended in the design.

(2) From the satisfactory test results of the fixed tube sheet element, which during the test was exposed to stresses considerably in excess of those expected in the floating tube sheet element in service, it can be concluded that the floating tube sheet design would satisfactorily withstand more than 2000 thermal cycles with correctly simulated temperature distribution.

(3) The clearance in the floating plenum should be increased to permit room for ejection of particulate UO₂; more effective means for retaining the UO₂ in the fuel space should be provided.

(4) All fission gas containing welds of the floating tube sheet fuel element were metallographically examined after testing and were found

to comply with the welding procedures and acceptance standards.

(5) Fuel elements of the floating tube sheet design can be satisfactorily manufactured for reactor service.

FIG. V-3 Solid Multitube Fuel Pellets

FIG. V-4 Pellets - Final Lot - Ground Outer Diameter

FIG. V-5 Fuel Element Components

FIG. V-6 Thermal Cycle Test Assembly

FIG. V-7 ABC Thermal Cycle Test Steam Temperatures

FIG. V-8 Cross Sectional View of Steam Tube to Thermal Sleeve Weld

FIG. V-9 View Showing Welding Metal Structure and Location of Crack in Thermal Sleeve (Arrow)

FIG. V-10 Axial Temperature Profiles - ABC Thermal Cycle Test - Typical Plot

VI. MATERIALS AND COMPONENT EVALUATIONS

A. Vibratory Compaction of Annular Multitube Fuel Elements

1. General

The investigation of vibratory compaction methods and techniques under the Advanced BONUS Core Program was conducted using fuel elements of the annular multitube design. Although the design was completed for the jigs and fixtures needed to vibratory compact experimental quantities of small solid fuel elements, the work was terminated when it became apparent that satisfactory fuel for the small solid elements could be obtained by making pressed and sintered UO_2 pellets. However, vibratory compaction offers the promise of reduced fuel fabrication costs in the construction of large and relatively complex fuel element shapes and is, therefore, particularly suited to the fabrication of the annular multitube fuel element. The purpose of the vibratory compaction work conducted under the ABC program was to explore the feasibility, cost, and technical adequacy of fabricating annular multitube fuel elements by the vibratory compaction process.

A cross sectional view of the large annular multitube fuel element is shown in Figure III-31. The element consists of a series of small diameter steam tubes equally spaced between two concentric Zircaloy tubes. Boiling takes place on the Zircaloy tube surfaces, and superheating of the saturated steam occurs inside the steam tubes. Fused UO_2 powder is packed around the steam tubes in the annulus between the two Zircaloy tubes. The performance characteristics which

recommend such a configuration are described in Section III-F of this report. Preliminary to in-reactor testing, the following tasks were set up in regard to manufacture and evaluation of this fuel element design:

- (1) Powder compaction studies to obtain maximum fuel density;
- (2) Design of mechanical methods to control fuel element geometry to close tolerances;
- (3) Fabrication and evaluation of a sliding seal mechanism which permits differential thermal expansion between the Zircaloy cladding and the steam tubes;
- (4) Fabrication of test specimens to determine to what extent binding or ratchetting occurs between the fuel powder and the element cladding;
- (5) Thermal cycle tests to determine the effect of thermal stress cycling on fuel element life and performance.

2. Powder Compaction

The apparatus shown in Figures VI-1 and VI-2 was designed and constructed to perform fuel loading and compaction under a mechanical pump vacuum and prevent possible airborne segregation of the fine powder fraction during loading. The loading funnel was designed to permit controlled powder loading into the inner annulus between the center water clad tube and the steam tubes and into the outer annulus between the steam tubes and the outer water clad. Uniform loading is required to avoid differential fuel powder height in the annuli which could produce lateral loads on the steam tubes and displace

them from design positions. Furthermore, uniform loading must be secured in order to avoid possible screening and particle size segregation by powder flow through the steam tube ligament spaces. To determine whether uniform blending of fuel particle size depends on batch size, fuel elements were fabricated using 2, 7, and 14 kg batch charges, each loaded incrementally. After each incremental loading the compaction jam was inserted and compaction continued until densification ceased. In the batch charges evaluated in this manner, no significant effects on compacted fuel element density were observed.

An MB electromechanical vibration machine, Type C-10, was used for the compaction studies. Since the fuel element and compaction hardware exceeded the table load limit of the machine, a spring suspension from a stationary platform carried the dead weight of the fuel element and compaction hardware. The MB vibrator worked against the mechanical springs and the inertial mass of the element and was operated at maximum force output during loading and compaction. The machine was preset to sweep the vibration frequency range from 150 to 1000 cycles at the rate of one complete sweep every minute.

Fuel powder formulations varying in particle size distribution were tried to optimize the compacted fuel density. The formulation selected for consistent production of the highest compacted densities is shown in Table VI-1.

Compacted fuel densities of 85% of theoretical were achieved. An evaluation of the test data clearly indicates that fuel densities greater than 85% of theoretical could be obtained by the use of an

TABLE VI-1

OPTIMIZED FUEL PARTICLE SIZE DISTRIBUTION FOR ANNULAR
MULTITUBE FUEL ELEMENT

<u>U. S. Mesh Size</u>	<u>Weight %</u>
-6 +8	22
-8 +10	11
-10 +12	11
-12 +16	16
-30 +60	3
-60 +100	12
-200	25

electromechanical vibration machine with a larger force output; by further adjustment of particle size distribution to take into account the increased amount of in situ powder comminution resulting from the increased acceleration forces; and by the use of syntron fuel feeders to ensure more uniform particle size blending.

3. Fuel Element Geometry Control

Control of the steam tube location relative to the Zircaloy cladding was obtained by the use of spacer washers such as shown in Figure VI-3. The spacer washers were designed to accommodate

- (1) the possible accumulation of dimensional variations due to clad tube straightness, ovality, and wall thickness;
- (2) variations in geometry produced by assembly of the element and by the compaction jig and fixture tolerance requirements;
- (3) disturbances in element geometry caused by the fuel loading and compaction operation.

The center water clad tube was the main alignment member for the element. All compaction jigs and fixtures positioned the steam tubes and the outer clad relative to the center water clad tube. The steel spacer washers were inserted at 18 in. intervals along the length of the element to maintain fuel element geometry. The results of a series of compactations of fuel elements utilizing the spacer washers shown in Figure VI-3 demonstrate that the radial and azimuthal positions of the steam tubes can be maintained within ± 0.030 in. of design position. However, tolerances to within ± 0.020 in. are difficult to achieve because alignment of the bowed tube is not possible once fuel

is compacted in the element to the level at which a washer is to be inserted. In order to improve geometry control by locating the tubes in their design positions prior to fuel loading, the feed-through spacer washer shown in Figure VI-4 was fabricated for insertion into the fuel element prior to fuel loading. Although test evaluations using the feed-through spacer were not performed, it is believed that steam tube geometry control to ± 0.020 in. of design position could be achieved with these washers.

The fuel element development work demonstrated that the 3.5 in. diameter annular multitube element can be fabricated by vibratory compaction; that acceptable fuel densities are achievable; and that adequate geometry control can be exercised. Floating tube sheet designs which would reduce clad stresses due to differential thermal expansion are practical. Although thermal cycle tests and irradiation test experiments were not performed on the annular multitube fuel element, it should be considered for further development in any future integral boiling-superheating fuel element program.

4. Sliding Seal Fabrication Bottom Plug and Packing Tests

To permit differential thermal expansion between the steam tubes and the Zircaloy boiler cladding, a fuel element was designed featuring a floating plenum to which the bottoms of the steam tubes were welded. This plenum was not attached to the Zircaloy boiler clad or bottom tube sheet. To prevent fuel particles from entering the floating plenum region, a seal washer was developed which consisted of coarse stainless steel wool matting sandwiched between two retainer

spacer discs set one inch apart. The spacer discs contained drilled holes to accommodate the steam tubes, and corresponding holes were forced into the packing with a piercing tool.

A view of a completed seal is shown in Figure VI-5. Experimental evaluation of the sliding seal washer showed that a stainless steel wool density of 18% of theoretical in the seal body permitted less than 0.1 gm of fine UO_2 powder to sieve through the washer during a one-hour vibration test of the element. The vibration test consisted of a frequency sweep from 150 to 1000 cycles/sec at the rate of one sweep per minute.

5. Fuel Tube Ratchetting and Binding Tests

Investigations conducted at the Combustion Engineering laboratories on binding between the fused UO_2 powder and the steam tubes produced anomalous results. The tests were conducted on 0.250 in. diameter steam tubes embedded over a 48 in. length in fuel compacted to a powder density of 85% of theoretical. Pull tests on the steam tubes were performed with the steam tubes at 700°F and also at 1300°F test temperatures. At the 1300°F steam tube temperature, the outer clad temperature was 750°F. No significant change in the pull-out force was observed as a function of temperature. However, the average static pull force of 400 lb decreased to approximately 50 lb when the element was vibrated during the pull test. After cooldown and storage for approximately one week, the pull-out force increased to 1400 lb for both static and vibration pull tests. Disassembly of the element showed that the steam tube had suffered no visible damage. Examination by x-ray

diffraction revealed no traces of U_3O_8 , and thus showed that the fuel powder had not been oxidized. The selection of pellet fuel and the small solid element as the ABC reference fuel element design in July 1964 terminated further investigation of this phenomenon.

B. Embedded Tube Tests

Complementary investigations of steam tube binding and ratchetting under thermal cycle test conditions were performed at the General Nuclear Engineering, Corp. Fuel elements containing 0.262 in. diameter Incoloy-800 and Inconel-625 steam tubes embedded in a 38 in. length of fuel compacted to a density of 86% of theoretical were thermally cycled by electrical heating of the steam tube. The fuel specimen and test apparatus are shown in Figures VI-6 and VI-7. The behavior of the Incoloy-800 steam tubes during the thermal cycle test closely resembled the behavior of the Inconel-625 steam tubes except that the total shrinkage for Inconel-625 was approximately one half of that shown for the Incoloy-800 tubing (see Figures VI-8 and VI-9).

The tests demonstrated that the steam tubes could withstand in excess of 1000 cycles between room temperature and 1000°F without failure. Test conditions were more severe than any that could be expected during reactor operation. The steam tube, which is the element with the highest coefficient of thermal expansion in the fuel assembly, was the element heated and cooled in the test. The fuel, which has the lowest coefficient of thermal expansion, therefore lagged in temperature. In the reactor, the reverse situation exists, i.e.,

the fuel is heated first and the steam tubes lag behind in temperature. Thus, the steam tubes in the test "grow" into the fuel and are heavily loaded; in the reactor, the fuel "grows" away from the tubes and no loading occurs. The tests did demonstrate, however, that vibratory compacted fuel of the density tested exerts sufficient frictional force on the boiler cladding to cause yielding of the cladding. While the effect of fuel density on the interaction of fuel and cladding was not examined in these tests, it is believed that higher density would increase the interaction forces. The tests showed that although the binding loads between the fuel and steam tube were substantial, the Inconel-625 steam tubes have adequate strength to meet fuel element fatigue life requirements.

C. Zircaloy-Inconel Transition Joints

1. General Fabrication and Mechanical Testing

The fabrication of Zircaloy-Inconel transition joints by the tandem extrusion process (coextrusion) at Metalonics Corp., Boston, and at Nuclear Metals, Inc., Concord, Mass., and by the explosive forming process at Battelle Memorial Institute, Columbus, Ohio (BMI), is described in detail in Reference 26.

A mechanical property evaluation of the tandem extruded joints produced the data shown in Table VI-2.

Evaluation tests at room temperature on 8 in. long specimens sectioned from explosively welded joints showed an average tensile strength of 98,000 psi and an average elongation of 11%. Specimen

TABLE VI-2

MECHANICAL PROPERTIES OF COEXTRUDED

ZIRCALOY-INCONEL JOINTS

<u>Test Temp.</u>	<u>U.T.S. (Ksi)</u>	<u>0.2% Y.S. (Ksi)</u>	<u>% Elongation in 2 In.</u>
RT*	78.9	46.2	12.9
600°F	30.6	26.4	13.6

*Room temperature

failures for both the tandem extruded joints and the explosively formed joints occurred in the Zircaloy-2 component and not at the joint interface.

Thermal cycle testing of a tandem extruded Zircaloy-Inconel joint⁽²³⁾ showed that 10,000 cycles between 250°F and 900°F imposed on a 1.5 in. diameter joint produced no visible evidence of failure.

2. Irradiation Tests of Transition Joints

The irradiation testing of the Zircaloy-Inconel transition joints was conducted at the Battelle Northwest Laboratory⁽⁵⁷⁾ as part of a program sponsored by the Fuels and Materials Development Branch of the Division of Reactor Technology of the USAEC, on the irradiation effects on reactor structural materials. The purpose of the irradiation test was to determine the effects of irradiation and environment upon the mechanical properties of the transition joints which were manufactured by tandem extrusion and by explosive forming. The irradiation test specimens were prepared from the coextruded and the explosively

welded tubing shown in Figure VI-10 and were placed in the G-7 hot water loop of the AEC Engineering Test Reactor at Idaho Falls (ETR). The loop operated at 540°F and 2000 psi when the reactor was at full power. The total integrated exposure and length of exposure time of each quadrant containing the transition joint specimens is shown in Table VI-3.

TABLE VI-3*

IRRADIATION CONDITIONS FOR THE ZIRCALOY-INCONEL TRANSITION
JOINT SPECIMENS EXPOSED IN THE ETR G-7 LOOP

Quadrant No.	ETR Cycles of Exposure	Hanger Position	Approximate Neutron Flux Nvt > 1 Mev	Neutron Exposure Nvt > 1 Mev	Time at Operating Temp.
175	67,68,69	F ₁ - G ₁	1.0×10^{14}	6.2×10^{20}	1765 hr
176	67,68	A ₁ - B ₁	4.2×10^{13}	1.9×10^{20}	1300 hr
177	67	B ₂ - C ₂	5.5×10^{13}	1.4×10^{20}	725 hr
178	67,68, 69,70	B ₃ - C ₃ F ₁ - G ₁	6.8×10^{13}	5.7×10^{20}	2200 hr

*From Reference 57

The out-of-reactor specimens were subject to the same time and temperature conditions as the in-reactor specimens in quadrants 177 and 178. The pre-exposure and post-irradiation specimen characterization data are shown in Table VI-4. The pre-exposure and post-irradiation hydrogen content data are shown in Table VI-5.

The results obtained from the tensile tests on each type of specimen are given in Table VI-6. Because extensometers with 2 in.

TABLE VI-4

SPECIMEN CHARACTERIZATION BEFORE AND AFTER LOOP EXPOSURES^a

Specimen Identification Assigned	Vendor's	Type of Joint ^b	Dimensions in Inches			Weights in Grams			Hardness Values Taken on Rockwell "A" Scale								
			Width	Thickness ^c	ΔThickness	Pre-Exposure	Post-Exposure	Change ^d	Pre-Exposure	Zr-2	Post-Exposure	Zr-2	Pre-Exposure	Inconel	Post-Exposure	Inconel	
175-1	1-1	CE	.4376	.0294-.0328	.0034	19.66159	19.7283	.0667	58.4	58.1	51.9	59.0	59.5	58.7	63.5	64.0	64.0
175-2	3-1 SCJL	EW	.4356	.0853-.0951	.0098	59.18432	59.2225	.0382	59.0	59.2	58.7	61.8	63.7	61.2	50.5	52.0	50.7
175-3	1-2	CE	.4349	.0329-.0331	.0002	20.08288	20.1114	.0285	52.3	52.3	52.0	60.5	60.4	59.9	65.3	64.2	64.5
175-4	3-2 SCJL	EW	.4368	.0824-.0855	.0031	53.86965	53.5092	.0395	59.0	57.5	59.0	61.5	63.2	65.5	53.0	51.3	49.0
175-5	1-3	CE	.4349	.0323-.0328	.0005	19.92549	19.9538	.0283	52.0	51.9	51.8	59.5	60.2	60.4	64.4	64.4	64.4
175-6	3-3 SCJL	EW	.4368	.0870-.0950	.0080	58.99194	59.2714	e	59.3	59.2	59.2	62.7	63.5	62.8	52.3	50.8	49.5
176-1	1-4	CE	.4379	.0323-.0329	.0006	20.30766	20.3362	.0285	52.0	52.0	49.3	57.8	57.8	57.7	64.6	64.5	64.3
176-2	3-4 SCJL	EW	.4359	.0860-.0942	.0082	58.31713	58.3438	.0267	58.6	58.0	58.9	60.7	59.5	61.8	52.5	51.5	49.3
176-3	1-5	CE	.4342	.0320-.0331	.0011	19.95688	19.9833	.0264	52.0	52.1	51.0	58.2	58.7	58.2	64.4	64.3	64.2
176-4	3-5 SCJL	EW	.4372	.0870-.0947	.0077	58.78734	58.8114	.0241	50.8	59.5	50.7	61.9	62.0	61.5	52.5	51.3	49.5
176-5	1-6	CE	.4340	.0319-.0322	.0003	20.00551	20.0303	.0238	52.0	52.0	52.0	57.8	57.4	57.8	64.5	64.5	64.5
176-6	3-6 SCJL	EW	.4362	.0890-.0945	.0055	59.10635	59.1284	.0220	59.0	59.0	50.9	61.3	62.0	62.1	52.3	51.0	49.0
177-1	2-1	CE	.4368	.0320-.0335	.0015	20.18499	20.2023	.0173	52.3	52.0	52.5	56.6	57.5	57.5	64.8	64.5	64.5
177-2	4-1 SCJL	EW	.4362	.0805-.0856	.0051	53.83982	53.8585	.0187	58.8	58.9	59.0	61.5	61.9	61.9	52.2	52.1	50.5
177-3	2-2	CE	.4365	.0317-.0330	.0013	20.00320	20.0219	.0187	52.2	52.0	50.0	58.2	58.5	58.5	64.2	64.0	64.1
177-4	4-2 SCJL	EW	.4382	.0884-.0948	.0064	58.40120	58.5006	.0194	58.7	59.2	58.5	61.4	61.2	61.2	52.1	52.0	51.3
177-5	2-3	CE	.4368	.0312-.0322	.0010	19.74208	19.7604	.0183	51.1	51.0	50.7	57.4	57.4	57.5	64.2	64.4	64.4
177-6	4-3 SCJL	EW	.4360	.0809-.0865	.0056	53.95875	53.9796	.0208	58.6	57.7	59.2	62.2	62.1	62.2	52.2	50.0	48.9
178-1	2-4	CE	.4389	.0300-.0310	.0010	19.19908	19.2344	.0353	52.1	52.2	52.0	60.1	59.4	59.8	64.4	64.0	64.0
178-2	4-4 SCJL	EW	.4350	.0805-.0850	.0055	53.70945	53.8297	.0302	59.0	58.5	59.0	62.5	63.5	63.6	55.0	52.2	50.0
178-3	2-5	CE	.4364	.0299-.0310	.0011	19.25626	19.3019	.0456	52.3	52.5	52.0	58.6	60.5	60.0	64.1	64.2	63.8
178-4	4-5 SCJL	EW	.4378	.0805-.0862	.0057	54.33945	54.3932	.0537	59.0	58.7	59.0	61.5	62.5	62.0	55.0	53.0	51.0
178-5	2-6	CE	.4365	.0321-.0330	.0009	19.73794	19.7800	.0421	52.3	52.2	51.2	60.3	60.5	60.4	64.5	64.6	64.3
178-6	4-6 SCJL	EW	.4368	.0805-.0859	.0054	53.93795	54.0033	.0653	59.2	58.3	59.0	60.1	61.4	62.3	55.1	53.0	51.4
177-1X	--	CE	.4375	.0318-.0334	.0016	19.77422	19.7757	.0015	50.0	49.9	50.2	--	--	--	62.5	63.3	63.8
177-2X	--	EW	.4372	.0961-.0970	.0009	59.80277	59.8046	.0018	58.0	57.5	59.0	--	--	--	67.5	68.5	68.1
177-3X	--	CE	.4375	.0338-.0346	.0008	20.65418	20.6537	h-.0005	46.6	48.5	49.2	49.5	50.1	50.1	63.6	63.8	63.5
177-4X	--	EW	.4355	.0961-.0969	.0008	59.93977	59.8138	e	58.2	58.2	57.8	--	--	--	67.0	68.2	68.2
177-5X	--	CE	.4382	.0317-.0325	.0008	19.40903	19.4085	-.0005	47.5	49.0	50.0	45.9	48.1	49.0	63.6	63.9	63.8
177-6X	--	EW	.4388	.0955-.0968	.0013	60.06993	60.0641	-.0058	57.0	57.8	58.0	58.0	58.0	--	48.3	48.8	48.1
178-1X	--	CE	.4369	.0304-.0330	.0026	19.75131	19.7604	.0091	48.9	49.9	50.3	50.4	50.0	52.0	52.1	63.1	63.6
178-2X	--	EW	.4392	.0960-.0975	.0015	60.53781	60.5412	.0034	58.2	57.9	58.5	57.4	58.0	57.0	47.6	47.3	48.2
178-3X	--	CE	.4368	.0338-.0355	.0017	20.33183	20.3353	.0035	48.5	50.0	51.1	50.0	50.0	51.1	53.5	63.6	64.1
178-4X	--	EW	.4378	.0820-.0865	.0045	53.78430	53.7876	.0033	56.6	58.2	58.6	57.4	57.5	58.0	48.1	48.5	49.9
178-5X	--	CE	.4369	.0340-.0352	.0012	20.64620	20.5505	e	46.2	48.5	49.5	50.0	50.0	50.5	63.0	64.0	63.4
178-6X	--	EW	.4341	.0957-.0968	.0011	59.04277	59.0432	.0004	57.7	58.8	59.2	57.2	57.5	58.0	47.0	49.0	49.8

a) Exposure history for both the G-7 and out-of-reactor loop as given in Table 3 of Reference 51.

b) CE designates a coextruded tube and EW represents an explosively welded tube.

c) Minimum and maximum thickness respectively, noted in gauge length of specimen.

d) All changes reflect a weight gain unless otherwise noted.

e) This value is known to be incorrect and was not considered in overall evaluation.

f) No identification on these specimens.

g) Data were not obtained for these specimens prior to testing.

h) The minus sign indicates a weight loss.

TABLE VI-5
HYDROGEN CONTENT OF THE ZIRCALOY-2 PORTION OF THE BONUS TRANSITION JOINT SPECIMENS

Specimen Identification	Exposure Location	Exposure Time (Hours)	Integrated Neutron Exposure, (nvt >1 Mev)	H ₂ - ppm	
				EW	CE
1	As-Received	0	0		<10-10 (4.8-5.1)
2	As-Received	0	0	<10-10 (2.0-6.5)	
3	As-Received	0	0		<10-10
4	As-Received	0	0	<10-10	
177-1X	X-Reactor Loop	725	0		6.0-8.2
177-2X	X-Reactor Loop	725	0	2.6-3.9	
178-1X	X-Reactor Loop	2200	0		<10-10 (22-22)(a)
178-2X	X-Reactor Loop	2200	0	8.7-11 (20-22)	
177-1	G-7 Loop	725	1.4×10^{20}		58-78
177-2	G-7 Loop	725	1.4×10^{20}	26-27	
176-1	G-7 Loop	1300	1.9×10^{20}		42-48
176-2	G-7 Loop	1300	1.9×10^{20}	11-21	
176-4	G-7 Loop	1300	1.9×10^{20}	17	
176-5	G-7 Loop	1300	1.9×10^{20}		114
175-1	G-7 Loop	1765	6.2×10^{20}		112-126 (105-118)
175-1	G-7 Loop	1765	6.2×10^{20}		92-97
175-2	G-7 Loop	1765	6.2×10^{20}	39-56 (50-50)(b)	
178-1	G-7 Loop	2200	5.7×10^{20}		108-113 (82-98)(b)
178-1	G-7 Loop	2200	5.7×10^{20}		119
178-2	G-7 Loop	2200	5.7×10^{20}	36-38 (40-43)(b)	
178-3	G-7 Loop	2200	5.7×10^{20}		(95-98)(b)

(a) The values in parenthesis were determined by the Chemistry Analytical Laboratory and all other values were determined in the Radiometallurgy Laboratory.

(b) These values were determined from samples taken at the specimen break (i.e., near the transition joint). All other values were taken from samples located in the tab of tensile specimen, away from the break and transition joint.

TABLE VI-6

TENSILE PROPERTIES OF DONUS TRANSITION JOINTS

Specimen Identification	Type of Joint	Specimen Condition	Test Temp.	0.2% Offset Yield Strength ^a	Ultimate Tensile Strength ^a	Uniform Elong. ^b	Total Elong. ^b	Reduction of Area ^b	Elastic Modulus ^c
H03483 ^d	#1	CE ^e As-Fab	RT	59.2	74.0	4.9	6.2	34.7	21.8
H03498	#2	CE As-Fab	RT	68.2	74.5	2.5	5.5	45.2	23.0
H03497	#3	CE As-Fab	RT	72.7	77.1	1.8	5.5	45.9	19.6
H03496	#4	CE As-Fab	550 F	24.3	35.6	5.9	9.6	59.6	19.0
H03495	#5	CE As-Fab	550 F	26.3	37.0	4.9	8.3	66.4	22.2
H03485	#6	CE As-Fab	550 F	23.3	35.2	5.2	8.5	57.8	23.0
H03500	#1	EW ^f As-Fab	RT	90.9	93.1	0.4	0.6	3.8	19.2
H03499	#2	EW As-Fab	RT	81.3	87.3	1.1	3.4	37.2	14.3
H03494	#3	EW As-Fab	550 F	59.5	59.8	0.4	3.4	50.6	14.3
H03493	#4	EW As-Fab	550 F	59.2	59.5	0.4	3.8	49.4	15.5
H03484	#5	EW As-Fab	550 F	51.8	52.3	0.6	3.4	48.4	14.3
--	#6	EW As-Fab	--	--	--	--	--	--	--
H03487	177-1X	CE 725 hr. ^h	RT	58.0	71.4	3.9	5.9	32.9	20.0
H03489	177-3X	CE 725 hr.	550 F	39.0	39.2	0.2	2.4	59.6	15.9
H03491	177-5X	CE 725 hr.	550 F	28.5	35.6	4.3	7.3	47.5	21.7
H03488	177-2X	EW 725 hr.	RT	89.2	92.5	0.7	1.0	3.8	20.6
H03490	177-4X	EW 725 hr.	572 F	57.4	57.6	0.7	3.1	46.2	16.1
H03492	177-6X	EW 725 hr.	550 F	58.0	58.6	0.5	3.4	50.4	16.2
H03475	178-1X	CE 2200 hr. ^h	RT	55.0	64.5	2.7	4.7	41.8	21.0
H03477	178-3X	CE 2200 hr.	550 F	31.5	32.3	1.4	7.6	58.9	10.6
H03478	178-5X ⁱ	CE 2200 hr.	550 F	35.6	38.5	2.3	5.3	58.9	15.9
H03476	178-2X	EW 2200 hr.	RT	79.8	84.5	1.0	1.1	6.8	17.9
--	178-4X ^j	EW 2200 hr.	550 F	--	--	--	--	55.0	--
H03479	178-6X	EW 2200 hr.	550 F	53.5	55.2	0.5	3.3	47.2	18.2
--	175-1	CE 6.2 x 10 ^{20k}	RT	--	23.6	--	--	--	17.6
--	175-3	CE 6.2 x 10 ²⁰	550 F	--	28.4	--	--	--	22.1
--	175-5	CE 6.2 x 10 ²⁰	550 F	--	29.5	--	--	--	19.1
H03474	175-2	EW 6.2 x 10 ²⁰	RT	113.0	114.9	0.4	1.4	16.4	18.1
H03472	175-4	EW 6.2 x 10 ²⁰	550 F	75.2	75.8	0.3	1.6	42.5	15.6
H03473	175-6	EW 6.2 x 10 ²⁰	550 F	72.9 ^l	72.9	0.3	1.8	50.9	15.9
--	176-1	CE 1.9 x 10 ²⁰	RT	--	33.5	--	--	10.0	26.4
--	176-3	CE 1.9 x 10 ²⁰	RT	--	47.6	--	--	10.0	27.1
--	176-5	CE 1.9 x 10 ²⁰	550 F	--	40.8	--	~0.01	14.0	24.3
H03482	176-2	EW 1.9 x 10 ²⁰	RT	97.6	101.3	0.8	0.9	1.0	19.8
H03481	176-4	EW 1.9 x 10 ²⁰	550 F	67.1	67.6	0.3	2.4	41.3	16.8
H03480	176-6	EW 1.9 x 10 ²⁰	550 F	65.0	65.7	0.3	2.0	42.6	16.5
--	177-1	CE 1.4 x 10 ²⁰	550 F	--	39.3	--	--	0.0	14.0
--	177-3	CE 1.4 x 10 ²⁰	m	--	--	--	--	--	--
--	177-5	CE 1.4 x 10 ²⁰	m	--	--	--	--	--	--
--	177-2	EW 1.4 x 10 ²⁰	RT	--	84.9	--	--	5.3	17.3
H03485	177-4	EW 1.4 x 10 ²⁰	550 F	65.4	66.9	0.3	1.9	37.4	16.3
--	177-6	EW 1.4 x 10 ²⁰	RT	--	75.2	--	--	16.1	17.3
--	178-1	CE 5.7 x 10 ²⁰	RT	--	17.9	--	--	0.0	26.1
--	178-3	CE 5.7 x 10 ²⁰	550 F	--	43.0	--	--	0.0	18.7
--	178-5	CE 5.7 x 10 ²⁰	550 F	--	55.6	--	--	0.0	20.1
--	178-2	EW 5.7 x 10 ²⁰	RT	--	116.7	--	~0.01	0.0	18.1
H03470	178-4	EW 5.7 x 10 ²⁰	550 F	76.1	76.5	0.3	1.9	46.7	14.9
H03471	178-6	EW 5.7 x 10 ²⁰	550 F	79.1	79.2	0.4	1.8	44.4	14.5

- a) Ultimate tensile and yield strength values are given in pounds per square inch x 10⁻³.
b) Uniform elongation, total elongation, and reduction of area values are given in percent.
c) Young's modulus values are given in pounds per square inch x 10⁻⁶.
d) The designations in the column reference data from each test which is currently stored on the REM Computer Program.
e) CE represents a coextruded joint.
f) EW represents an explosively welded joint.
g) This specimen was used to develop testing methods and procedures in the hot-cell. The specimen has not been tested.
h) All specimens in quadrants 177-X and 178-X were exposed in the out-of-reactor loop at approximately 550°F for the time indicated.
i) Specimen was loaded above proportional elastic limit before testing began.
j) No chart was produced for this test due to a recorder malfunction.
k) Approximate neutron exposures in nvt for neutrons having energies greater than 1 Mev.
l) A drop-in-load yield point was observed.
m) These specimens were broken during handling in the Radiometallurgy facilities.

gauge lengths were centered on each specimen so that all of the transition joint was within the gauge length, and the specimens failed in the Zircaloy-2 portion in most cases, the effective gauge length was the Zircaloy-2 portion of the gauge section beyond the transition joint. Consequently, the strain values measured represent an elongation over the 2.5 in. or 2.75 in. specimen gauge section of varying Zircaloy and Inconel composition, and, therefore, do not reflect the normal elongation usually observed in Zircaloy-2. This departure from "normal elongation" is at least partly due to the composite nature of the sample.

Exposures in the out-of-reactor loop for 2200 hr at 540°F had little effect on the room temperature and the 550°F properties of either the tandem extruded or the explosively formed specimens. However, after nominal exposures of between 1.4 and 1.2×10^{20} nvt (> 1 Mev), a severe reduction in both strength and ductility at room temperature and at 540°F occurred in the tandem extruded specimens. The explosively formed specimens showed an increase in strength and a decrease in ductility. The effect of irradiation on the mechanical properties of the test specimens is shown in Figure VI-11 and Figure VI-12.

After an exposure of 1.9×10^{20} nvt (> 1 Mev), the coextruded specimens failed in a very brittle manner with no sign of plastic flow at either room temperature or 550°F test temperature. The explosively welded specimens exhibited a failure that was localized but quite ductile at the 550°F temperature. At room temperature, the ductility was drastically reduced, but there still was evidence of

both macroscopic and microscopic plastic flow. As mentioned before, the location of the failures in both the coextruded and the explosively welded specimens was primarily in the Zircaloy-2 portion and was usually very near the transition joint.

The loss of ductility cannot be explained by irradiation hardening alone. Examination showed the hydrogen content of the Zircaloy-2 in the as-received specimens to be nominal. In the specimens exposed for 2200 hr in the out-of-reactor loop, the hydrogen content was 25 ppm, which is acceptable. Chemical analysis of the irradiated specimens showed a hydrogen content of approximately 50 ppm in the explosively formed specimens and approximately 100 ppm in the coextruded specimens. This accelerated corrosion and hydrogen pickup has been observed for other tests on Zircaloy-2 in the G-7 loop. It is attributed to a combination of high oxygen content in the loop water and a postulated neutron flux effect. Metallographic examination of the specimens for hydrogen content show that the coextruded specimens contain at least several hundred ppm of hydrogen while the explosively formed specimens contain approximately 50 ppm. The reason for the discrepancy between the chemically obtained and metallographic values for the hydrogen content of the coextruded joints is not clear.

Further metallographic examination showed that a slight gradient in hydride content exists at the interface between

Zircaloy-2 and Inconel in both the out-of-reactor loop and the in-reactor exposed specimens. The reason for the existence of this hydrogen gradient is not clear. It could occur as a result of the chemical potential difference between either the two metals or between the intermetallic layer and the Zircaloy-2, or if the initial hydrogen content in the Inconel-600 used as starting material was high. Thus far, there appears to be no satisfactory explanation of the hydrogen content variation.

Zircaloy-Inconel transition joints have been fabricated and their performance in all out-of-reactor evaluation tests has been satisfactory. Failure with observed low ductility of tensile test specimens from these joints in post-irradiation tests was due to hydriding of the Zircaloy-2 near the Zircaloy-Inconel interface, and the cause of this phenomenon cannot be adequately explained at present.

In view of the results of the irradiation test reported in Reference 57, the Zircaloy-Inconel transition joints are not, at the time of this writing, considered suitable for use in ABC fuel elements. However, it is believed that the advantages of Zircaloy-2 cladding on the boiler side of the element are sufficiently great to warrant investigation into the causes of their unsatisfactory irradiation behavior.

D. Corrosion Aging Tests

Corrosion aging tests were performed on proposed Advanced BONUS Core steam tube materials. Inconel-625, Inconel-600, Incoloy-800, Type 406 Stainless Steel, and Hastelloy-X samples were aged at 1000°F for 5,000 hr and at 1200°F for 6,500 hr in flowing steam containing oxygen and hydrogen. Rene 62 and IN 102 samples were aged at 1200°F for 3,950 hr in the same environment. The latter two alloys were not candidate steam tube materials in view of the lack of relevant irradiation data; however, aging experiments were performed on them as a preliminary screening test for possible future in-pile investigations. The tensile properties of each alloy are presented in bar chart form for easy comparison in Figures VI-13 through VI-19 and are tabulated in Tables VI-7 through VI-13.

For Inconel-625, the ultimate and yield strengths increased and elongation decreased with aging time at 1000°F (see Figure VI-13). When aged at 1200°F, the material exhibited an increase in the ultimate and yield strengths and a large decrease in elongation, from about 50% to 18% for the first 2,000 hr. Only minor changes took place during the remaining 4,500 hr of the test. The room temperature tensile tests showed a doubling of the yield strength and a considerable decrease in elongation after 6,500 hr at 1200°F.

Aging Incoloy-800 at 1000°F (see Figure VI-14) affected mainly the elongation, with a decrease from 40% to 25% after 5,000 hr exposure. There were virtually no changes in properties for samples aged at 1200°F.

TABLE VI-7

TENSILE PROPERTIES OF INCONEL 625 ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
1A	As-Received		RT*	134,000	69,000	47.0	47.0
2A	As-Received		RT	131,500	65,000	37.7	48.9
3A	As-Received		RT	135,700	72,200	45.9	48.9
18	As-Received		RT	138,000	69,200	56.6	52.4
16A	As-Received		1000	118,850	54,500	53.5	†
17A	As-Received		1000	110,500	47,300	46.5	†
19	As-Received		1000	122,900	50,500	46.9	41.7
13	2000	1000	1000	127,900	75,500	26.1	26.3
14	2000	1000	1000	131,000	76,800	32.5	30.0
10	5000	1000	1000 †	145,400	100,900	27.5	32.7
11	5000	1000	1000	153,200	101,200	27.0	45.3
12	5000	1000	1000	148,300	102,000	33.5	39.8
4A	As-Received		1200	124,550	51,250	55.2	†
5A	As-Received		1200	119,450	55,500	57.8	†
6A	As-Received		1200	115,800	50,000	51.6	†
7A	As-Received		1200	123,150	50,000	51.0	†
4	2000	1200	1200	156,600	115,400	17.5	21.7
8	2000	1200	1200	156,300	119,500	12.5	16.4
16	3950	1200	RT	182,100	137,000	15.5	16.0
17	3950	1200	1200	156,000	104,800	12.8	16.3
18	3950	1200	1200	153,700	105,100	17.7	18.8
1	5000	1200	1200	162,400	117,400	21.0	22.8
7	5000	1200	1200	156,200	106,800	18.3	15.5
9	5000	1200	1200	158,700	109,400	18.2	20.0
5	6500	1200	RT	190,200	150,700	12.7	12.3
3	6500	1200	1200	160,000	111,000	12.6	14.2
2	6500	1200	1200	160,000	110,000	19.6	18.4

* Room temperature.

† Not reported.

† Broke outside of gauge marks.

TABLE VI-8

TENSILE PROPERTIES OF INCOLOY 800 ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
1A		As-Received	RT*	79,900	37,000	40.1	47.5
2A		As-Received	RT	79,900	35,500	38.1	45.2
3A		As-Received	RT	74,850	37,800	37.8	43.3
16		As-Received	1000	67,600	22,500	39.0	40.2
17		As-Received	1000	68,450	24,200	41.7	†
18		As-Received	1000	69,200	29,400	38.3	†
13	2000	1000	1000	68,300	27,900	34.0	37.0
14	2000	1000	1000	68,600	28,200	34.0	41.9
10	5000	1000	1000	72,600	36,200	26.3	34.9
11	5000	1000	1000	74,300	35,200	24.0	36.0
12	5000	1000	1000	72,800	37,100	26.8	42.0
4A		As-Received	1200	56,665	21,100	38.0	37.0
5A		As-Received	1200	56,595	19,750	37.0	32.6
6A		As-Received	1200	54,650	22,500	43.8	38.1
7A		As-Received	1200	56,735	23,000	39.8	34.8
4	2000	1200	1200	54,700	20,900	34.2	52.8
8	2000	1200	1200	54,400	22,700	28.5	48.6
1	5000	1200	1200	53,700	22,200	40.6	43.4
7	5000	1200	1200	54,200	21,200	36.1	45.0
9	5000	1200	1200	54,000	21,700	35.5	44.6
5	6500	1200	RT	81,400	33,900	42.8	46.7
2	6500	1200	1200	53,500	22,000	37.1	44.1
3	6500	1200	1200	53,400	21,900	33.3	45.4

* Room temperature.

† Not reported.

TABLE VI-9

TENSILE PROPERTIES OF INCONEL 600 ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
1A	As-Received		RT*	84,850	41,250	40.4	34.9
2A	As-Received		RT	90,665	38,900	42.0	40.4
3A	As-Received		RT	91,900	39,500	40.7	40.2
13	2000	1000	1000	75,800	26,400	41.0	42.5
14	2000	1000	1000	75,400	25,400	43.8	42.3
10	5000	1000	1000	74,400	26,300	39.4	41.4
11	5000	1000	1000	75,300	26,000	39.0	43.4
12	5000	1000	1000	74,200	26,400	38.6	44.6
4A	As-Received		1200	66,510	27,500	40.6	31.4
5A	As-Received		1200	66,665	26,025	39.0	42.1
6A	As-Received		1200	65,920	24,500	39.8	42.3
4	2000	1200	1200	60,900	25,000	23.8	27.1
8	2000	1200	1200	64,500	21,400	23.7	30.6
1	5000	1200	1200	62,000	20,700	30.0	29.5
7	5000	1200	1200	61,100	22,100	18.8	24.8
9	5000	1200	1200	62,300	22,900	20.0	25.4
5	6500	1200	RT	93,750	34,400	45.2	56.0
3	6500	1200	1200	60,700	22,400	21.8	22.0
2	6500	1200	1200	59,700	22,600	20.8	23.0

* Room temperature.

TABLE VI-10

TENSILE PROPERTIES OF HASTELLOY-X ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
1A		As-Received	RT*	121,275	61,150	30.7	33.0
2A		As-Received	RT	119,000	54,000	31.8	34.1
3A		As-Received	RT	119,000	53,450	38.7	34.1
16		As-Received	1000	98,500	63,000	33.3	†
17		As-Received	1000	100,040	62,800	32.7	†
18		As-Received	1000	103,500	59,200	34.0	34.8
13	2000	1000	1000	104,600	58,900	34.1	29.4
14	2000	1000	1000	102,500	57,800	33.0	34.3
10	5000	1000	1000	102,100	62,700	36.2	38.3
11	5000	1000	1000	102,300	62,900	36.3	31.1
12	5000	1000	1000	102,900	62,900	32.5	32.0
4A		As-Received	1200	90,025	53,400	40.2	34.5
5A		As-Received	1200	107,240	50,500	43.7	33.3
6A		As-Received	1200	93,910	45,000	44.3	33.0
7A		As-Received	1200	86,160	49,900	43.7	34.1
4	2000	1200	1200	103,500	78,100	17.4	33.4
8	2000	1200	1200	105,600	77,900	14.8	31.5
1	5000	1200	1200	91,200	70,700	34.3	33.5
7	5000	1200	1200	102,100	80,400	34.7	35.5
9	5000	1200	1200	101,700	82,000	35.2	39.1
5	6500	1200	RT	164,600	99,700	10.5	9.8
3	6500	1200	1200	99,900	72,600	31.8	35.3
2	6500	1200	1200	99,200	70,600	21.4	35.0

* Room temperature.

† Not reported.

TABLE VI-11

TENSILE PROPERTIES OF TYPE 406 STAINLESS STEEL ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
1A		As-Received	RT*	86,781	56,000	26.4	28.6
2A		As-Received	RT	88,050	55,700	26.0	32.4
3A		As-Received	RT	77,620	54,000	22.1	39.1
16		As-Received	1000	41,000	33,000	35.0	68.1
18		As-Received	1000	45,992	32,500	30.7	†
13	2000	1000	1000	47,300	32,800	32.1	64.7
14	2000	1000	1000	43,500	36,900	33.2	66.0
10	5000	1000	1000	45,900	33,900	26.6	57.6
11	5000	1000	1000	46,800	34,600	28.3	62.9
12	5000	1000	1000	48,400	33,700	28.6	58.8
4A		As-Received	1200	14,808	12,200	52.2	45.4
5A		As-Received	1200	17,915	12,700	92.5	73.3
6A		As-Received	1200	19,050	13,350	49.4	62.9
4	2000	1200	1200	20,800	16,200	53.3	84.5
8	2000	1200	1200	20,300	16,100	55.4	81.9
1	5000	1200	1200	22,600	16,000	59.5	74.8
7	5000	1200	1200	22,300	15,300	121.9	84.6
9	5000	1200	1200	23,100	13,400	80.2	82.2
5	6500	1200	RT	89,000	67,300	32.1	45.3
3	6500	1200	1200	20,300	16,500	66.0	83.5
2	6500	1200	1200	19,500	14,200	116.1	80.6

* Room temperature.

† Not reported.

TABLE VI-12

TENSILE PROPERTIES OF RENE 62 ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
11	As-Received		RT*	143,500	82,000	32.7	33.8
13	As-Received		RT	143,100	82,100	30.2	28.2
15	As-Received		RT	142,400	80,200	34.8	35.8
12	As-Received		1200	132,900	93,700	23.6	24.8
14	As-Received		1200	131,700	93,400	23.5	25.0
16	As-Received		1200	136,400	105,200	27.4	27.2
7	3950		RT †	222,100	188,400	0.5	None
8	3950		RT	207,600	180,800	0.6	None
9	3950		1200	199,700	145,700	12.8	22.6
10	3950		1200 ‡	196,800	135,900	5.1	9.6

* Room temperature.

† Retested in Templin grips. Eye pulled prior to yield.

‡ Piece missing from gauge.

TABLE VI-13

TENSILE PROPERTIES OF IN-102 ON AGING
AT 1000°F AND 1200°F IN STEAM CONTAINING OXYGEN AND HYDROGEN

Sample No.	Corrosion Aging Test Time (Hrs)	Corrosion Aging Test Temperature (°F)	Tensile Test Temperature (°F)	Ultimate (psi)	Yield (psi)	Elongation (% in 1 in.)	Reduction in area(%)
11	As-Received		RT*	129,000	57,500	50.5	48.0
13	As-Received		RT	129,600	59,500	49.3	46.6
15	As-Received		RT	129,200	58,400	50.2	48.2
12	As-Received		1200 †	100,900	43,500	48.7	39.3
14	As-Received		1200	103,700	43,200	51.3	41.4
16	As-Received		1200 †	101,600	43,500	50.7	38.7
7	3950	1200	RT	169,300	115,200	29.1	34.5
8	3950	1200	RT	167,300	111,300	29.7	35.6
9	3950	1200	1200	143,600	97,400	11.0	18.1
10	3950	1200	1200	143,300	94,000	15.2	16.9

* Room temperature.

† Broke at gauge marks.

There was no significant difference in the tensile properties of as-received samples tested at room temperature and samples tested at room temperature after 6,500 hr at 1200°F.

Aging effects on Inconel-600 were almost identical with those noted in Incoloy-800, except that the decrease in elongation was noted on the material aged at 1200°F instead of at 1000°F.

Hastelloy-X shows fairly constant ultimate strengths at both aging temperatures (see Figure VI-16). The yield strength and elongation remain unchanged for samples aged at 1000°F. Samples aged at 1200°F show an increase of about 25,000 psi in yield strength for the first 2,000 hr and then remain relatively constant throughout the remainder of the test. The 1200°F elongation data are indicative of over-aging. The room temperature tensile data indicate an increase of 45,000 psi in ultimate strength and a decrease from 30% to 10% in elongation after the material was subjected to 1200°F for 6,500 hr.

There was no change in the ultimate or yield strength of Type 406 Stainless Steel when the material was aged at either 1000°F or 1200°F. There was also no significant change in the elongation data at 1000°F. The 1200°F elongation data are questionable due to the large scatter in the data points. However, since the strength of this material is too low for ABC design temperatures, no further effort was made to study the erratic 1200°F elongation data.

René 62 and IN 102 both showed a definite increase in ultimate and yield strengths and a decrease in elongation when aged at 1200°F for 3,950 hr (see Figures VI-18 and VI-19). The room temperature

tensile tests of René 62 and IN 102 after 3,950 hr at 1200°F indicated a doubling of the yield strength. Room temperature tensile tests of René 62 after 3,950 hr at 1200°F indicated almost no ductility.

In general, the aging data indicate that Incoloy-800 and Inconel-600 are least affected by aging. Except for Type 406 Stainless Steel, the strength of these two materials is considerably less than that of the other alloys. Inconel-625 and Hastelloy-X are affected by aging in the manner shown, but the degree of seriousness of aging in these alloys depends upon the design selected.

Table VI-14 lists the weight gain data for the various materials aged in 1200°F steam containing oxygen and hydrogen. Of the materials evaluated, Inconel-625, Inconel-600, and GEMP-16* appear to have the best corrosion properties, as shown by weight gain data. Inconel-625 and Inconel-600 show a gain in weight for approximately the first 1,000 hr and then tend to level off, whereas GEMP-16 exhibits a gradual and continuous small increase. The data points for these alloys fall in a relatively small range as compared to Incoloy-800, which shows a large spread. IN 102 shows a larger initial gain in weight than the other alloys discussed above, but this initial rise is followed by a leveling off. Incoloy-800 has a small weight gain for the first 1,500 hr and then increases steadily and sharply for the next 4,500 hr. Type 406 Stainless Steel follows a similar trend. Hastelloy-X and René 62 appear to be slightly better than Incoloy-800 and Type 406 Stainless Steel.

* Material and Analysis supplied by General Electric Nuclear Materials and Propulsion Operation, Evendale, Ohio

TABLE VI-14

WEIGHT GAIN BEHAVIOR OF VARIOUS ALLOYS
IN 1200°F STEAM CONTAINING OXYGEN AND HYDROGEN

Material	WEIGHT GAIN -- MG/DM ²															
	EXPOSURE TIME (hr)															
	150	250	500	775	1000	1500	2100	2600	3100	3650	4150	4625	5000	5375	5875	6500
Hastelloy-X-1	---	9.8	11.5	12.1	12.1	12.3	12.6	14.3	15.2	17.4	19.4	21.6	24.2	----	----	----
-2	---	9.0	11.3	12.3	12.3	11.2	14.6	17.1	19.7	22.8	25.3	26.7	30.3	34.8	36.2	38.8
-3	---	10.1	11.8	12.9	12.9	13.2	14.9	17.7	20.0	23.3	27.0	29.5	31.7	35.1	36.5	41.0
406 S.S.	-1	---	13.2	17.4	19.5	19.5	21.8	24.6	26.5	28.1	30.4	32.0	32.3	35.0	----	----
-2	---	10.5	14.9	17.6	17.6	19.6	23.5	25.7	27.7	30.4	33.4	34.2	38.4	40.6	40.6	41.4
-3	---	18.7	22.6	25.6	25.6	26.2	30.1	32.6	35.6	38.0	43.9	42.2	45.5	48.6	53.0	53.5
Inconel-625-1	---	7.7	10.0	11.8	10.2	12.6	13.4	16.5	17.5	18.5	19.1	18.8	20.3	----	----	----
-2	---	6.7	9.3	11.0	10.2	11.5	12.4	13.6	14.2	14.9	15.4	15.7	16.7	17.8	18.0	20.9
-3	---	8.0	10.3	11.8	11.1	11.8	11.8	13.4	13.4	16.0	16.0	16.5	16.7	18.3	18.5	21.4
Inconel-600-1	---	7.2	9.3	10.2	9.5	10.8	11.8	13.6	15.2	18.3	19.3	22.4	25.2	----	----	----
-2	---	9.3	11.6	12.6	12.1	12.8	12.9	13.1	13.6	14.2	14.9	15.2	18.5	19.6	19.8	25.7
-3	---	7.5	10.0	11.0	10.2	11.0	11.1	11.6	12.6	14.2	16.0	17.3	19.6	21.1	21.4	24.2
Incoloy-800-1	---	6.5	9.6	10.6	10.6	13.2	18.5	25.8	32.6	40.1	44.0	46.4	49.5	----	----	----
-2	---	6.2	9.1	10.3	10.1	11.1	13.6	17.7	19.4	22.8	24.6	26.4	29.8	32.6	36.8	41.2
-3	---	6.5	9.1	10.3	10.1	11.1	14.7	19.6	24.9	34.4	40.9	46.4	52.3	55.4	57.8	66.3
GEMP-16*	-1	4.2	----	6.8	7.5	5.9	9.3	11.0	11.8	12.7	13.5	14.3	16.9	21.9	22.8	----
-2	3.4	----	5.6	5.9	4.2	5.0	8.4	10.1	12.7	11.8	11.8	13.5	15.2	19.4	16.9	----
-3	4.3	----	6.9	8.6	6.0	6.0	9.5	11.2	14.7	18.1	14.7	15.5	21.6	----	----	----
René 62	-1	17.6	20.9	25.0	27.8	30.0	32.2	36.0	37.4	41.0						
-2	17.0	22.0	27.8	32.4	32.3	35.7	37.4	38.8	41.2							
-3	17.9	21.2	25.8	28.6	28.6	29.7	33.0	33.5	37.1							
IN-102	-1	13.9	17.2	19.5	20.6	20.8	21.3	24.4	24.9	26.7						
-2	11.8	15.9	18.0	20.3	20.6	22.4	24.2	24.7	26.0							
-3	9.0	12.6	14.1	15.7	15.7	15.7	18.8	19.3	23.4							

*(.020 in. sheet-cold rolled) Cr - 24.2%, Ni - 80 ppm, C - 42 ppm, S - 30 ppm, Al - 3.9%, Y - 0.79%, Fe - Bal.

E. Mechanical Property Tests

1. Tensile Tests of Steam Tubes

Tensile tests were performed to determine the comparative room temperature and elevated temperature tensile properties of the various lots and sizes of Inconel-625 tubing procured for this program.

The tensile strengths, yield strengths, reduction in area, and elongations for 0.283 (two separate lots), 0.337, 0.396, and 0.262 in. OD tubing are tabulated in Table VI-15 and plotted in Figures VI-20 and VI-21. Each of the data values are representative of a single test specimen.

The as-received tubing was in the annealed and cold straightened condition.

The variations in tensile properties among the heats and sizes of material can be attributed to the different heats of material and to the different amounts of cold straightening which were required to obtain the desired dimensions.

Tests to determine the room temperature and elevated temperature tensile properties of Incoloy-800 tubing were also performed as part of the program to establish specific properties of candidate superheater fuel cladding materials. Room temperature, 600°F, 800°F, 1000°F, and 1200°F tests were run on the 0.396 in. OD by 0.039 in. wall Incoloy-800 tubing. The test results are plotted in Figure VI-22.

2. Compression Tests of Inconel-625 Steam Tubes

The purpose of these tests was to demonstrate that thermally induced compressive axial loads on the steam tubes in the gas void region of the fuel element will not cause failure of the tubes due to

TABLE VI-15

COMPARATIVE TENSILE PROPERTIES OF INCONEL 625 TUBING

OD(In.)*	0.283	0.283	0.337	0.396	0.262
Wall(In.)*	0.026	0.026	0.026	0.039	0.024
Lot No. †	252253	251426	-	-	-

Temp. °F	TENSILE STRENGTH, psi				
70°	147,200	142,400	136,200	131,400	137,620
600°	136,000	128,600	122,000	117,000	123,300
800°	130,000	123,800	116,500	112,800	120,400
1000°	123,000	119,100	112,600	104,000	109,600
1200°	112,000	113,800	107,000	70,300	84,000

YIELD STRENGTH, 0.2% psi					
70°	82,900	72,800	72,000	66,620	77,080
600°	69,400	60,000	56,400	52,740	64,930
800°	65,700	57,100	53,100	50,000	61,680
1000°	62,800	54,900	50,800	48,300	57,760
1200°	57,100	52,800	48,900	45,100	54,370

R.A. %					
70°	40.6	44.2	38	48	39.5
600°	34.4	36.9	25.8	45	30.0
800°	33.0	35.1	24.9	40	32.5
1000°	29.2	33.7	23.2	30	22.5
1200°	27.5	27.7	20.2	22.5	13.0

ELONGATION IN 2 IN. (%)					
70°	43	47	47	58	50
600°	41	46	38	56	39 †
800°	40	44	38	54	39 †
1000°	35	41	33	36	28 †
1200°	32	33	30	33	15 †

* Nominal dimensions

† Where applicable

† Sample failed at the gauge marks.

buckling. The steam tubes are laterally unsupported in this region over a 3.5 in. length. Room temperature, 600°F, 900°F, and 1200°F tests were performed on 0.285 in. ID by 0.026 in. wall Inconel-625 tubing.

A sketch of the experimental test fixture is shown in Figure VI-23. A 4.5 in. tube specimen was placed over the alignment mandrels which were an integral part of the upper and lower mounting plates. The tube specimen was electrical-resistance heated for the elevated temperature tests. A typical axial temperature profile of the specimen is shown in Figure VI-24 for each of the elevated test temperatures.

The compression test data are shown in Table VI-16. The values reported as the buckling load are the highest stress values obtained before a drop-off in the compressive load occurred. It should be noted that the largest decrease in buckling and compressive yield stress loads, viz., approximately 20%, occurs between room temperature and 600°F, while the decrease between 600°F and 1200°F is approximately 12%. The elastic limit values appear to be essentially constant up to 1200°F. Table VI-17 shows a comparison of tensile yield strength for 0.262 OD by 0.024 in. wall and 0.396 OD by 0.039 in. wall Inconel-625 tubing and the compressive yield strength data obtained on the 0.337 OD by 0.026 in. wall tubing.

In order to determine the axial compressive stress at which incipient buckling occurs due to plastic deformation of the tube, two tests were performed at 900°F with axial compressive loads of 56,000 and 60,000 psi, respectively. These loads were maintained on the

TABLE VI-16

COMPRESSION TEST DATA

<u>Sample No.</u>	<u>Test Temp. °F</u>	<u>Buckling Load (psi)</u>	<u>Compressive Yield Strength (psi)</u>	<u>Elastic Limit (psi)</u>
14	RT*	85,800	84,600	48,000
17	RT*	85,800	84,360	54,000
18	RT*	85,400	85,440	51,600
13	RT	86,400	85,800	54,000
23	RT	85,600	82,200	49,200
16	RT	85,800	85,200	52,800
30	600°F	70,400	66,960	54,000
40	600°F	69,800	66,480	51,600
31	604°F	69,400	66,360	51,600
32	605°F	66,920	63,880	51,325
34	900°F	66,520	63,000	54,000
35	900°F	66,600	64,800	56,400
38	900°F	67,000	63,600	56,400
33	1200°F	58,080	55,490	46,800
39	1200°F	64,000	60,480	54,000

* Extensometer on sample.

TABLE VI-17

TENSILE AND COMPRESSIVE YIELD STRENGTH OF INCONEL 625 TUBING

<u>Test Temp.</u>	<u>Tensile Yield Strength (psi)</u>		<u>Compressive Yield Strength (psi)</u>
RT	0.262 In. OD	0.396 In. OD	0.337 In. OD
	0.024 In. Wall	0.039 In. Wall	0.026 In. Wall
600	77,800	66,620	84,600
800	61,680	50,000	*
900	*	*	63,000
1000	57,760	48,300	*
1200	54,370	45,100	58,000

* No test data

specimens for three hours. During both tests, no drop-off in the load was observed; however, the specimen stressed to 56,000 psi returned to its original unstressed length of 4.500 in. upon removal of the load, while the sample stressed to 60,000 psi suffered a permanent deformation of 0.005 in. in length. These results agree closely with the behavior predicted by compressive stress-strain curves.

3. Hydrostatic Pressure Tests of Zircaloy Tubes

The purpose of the test was to determine the hoop stress versus strain properties of heavy wall Zircaloy tubing as used in the center tube of the annular multitube fuel element. The test fixture is shown in Figure VI-25. The mandrel inserted into the tube to be tested permitted the hydraulic application of pure hoop stress without the application of axial stress. The stress was measured with a 10,000 psi bourdon tube type gauge. For strain measurement, twelve Baldwin Lima SR-4 wire strain gauges were attached to the tube, six in the hoop direction and six in the axial direction.

The hoop stress was obtained using Lamé's relation for heavy wall tubes, where

p = applied pressure

r_1 = inside radius

r_2 = outside radius

ρ = radius for the surface under consideration.

The resultant stress strain data are plotted in Figure VI-26.

The measured values of

47,500 psi - for yield strength at 0.2% offset

12.6×10^6 psi - for Young's modulus

0.33 - for Poisson's ratio

are well within the range of established values for annealed Zircaloy-2 material.

4. Collapse Tests of Boiler Cladding

The object of the tests was experimental determination of the external collapse pressure of large-diameter, thin-walled Zircaloy-2 pressure tubes, and a study of the general behavior and strength of the tubes under the specific test conditions.

Specimens tested included three sections of Zircaloy-2 tubing, ranging in length from 24 to 27 in. with wall thicknesses from 0.134 to 0.138 in. The outside diameter of the tubes was 5 in. nominal.

The tubes were sealed at each end by a Type 304 Stainless Steel blind flange with O-ring seals. Strain gauges were placed circumferentially at different points along the tube axis to obtain the characteristics of collapse and stress-strain data at the gauge locations. The tubes were placed horizontally in a 7-5/8 in. ID autoclave. The tests were run at room temperature, with the pressure increased until collapse occurred, which was signalled by a large drop in the autoclave pressure due to a reduction of volume, and by an audible noise.

The tubes collapsed in a two-lobe mode with the same orientation as the initial quasi-elliptical out-of-roundness. It should be pointed

out here that the experimental test conditions did not exactly duplicate the conditions assumed in the previous theoretical analysis. The capped ends of the tubes may have introduced an axial force in the specimens, which had been assumed as zero in the theoretical development. An axial force has two relatively small effects. Firstly, it produces a so-called beam-column effect that slightly reduces the collapse pressure, and secondly, an axial stress affects the stress-strain relationship in the circumferential direction. In addition to the presence of an axial force, the wall thickness variations in the test specimens can also be expected to reduce their collapse pressure, since the collapse pressure is a function of the cube of the wall thickness. Random ovalities existed in the tubes that were tested; however, ovality was not considered as a parameter in the theoretical analysis. Previous investigators have found that a small initial out-of-roundness significantly reduces the resistance of a tube to collapse, and have developed methods to predict the reduction. These calculations usually assume that the tube is infinitely long and has a zero net axial load. (58)

In spite of dissimilarities between test conditions and calculational assumptions, the agreement between experimental and calculated collapse pressures was very good. The first specimen tested (No. 5) was a vacuum annealed seamless tube which collapsed at a somewhat lower pressure than predicted (915 psi versus 1030 psi). The second specimen (No. 6) exhibited a collapse pressure slightly higher than expected (1080 psi versus 1060 psi). Specimen No. 6 was a seamless tube with 15% cold-work, which may explain its higher collapse pressure.

The third specimen tested (No. 4), shown in Figure VI-27, a welded tube with 15% cold-work, gave unexpected results. It collapsed at a pressure slightly lower than predicted (920 psi versus 986 psi) and was found to have split along the weld on the inside of the tube, as shown in Figure VI-28. The crack penetrated almost through to the outer side. The weld evidently provides an area of instability in compression. The specimen was slightly over 25 in. in length, and the crack extended along approximately half of this length.

All tubes were found to be quasi-elliptical in shape before testing and collapsed along an axis which coincided with the minimum tube diameter. Dimensional data and collapse conditions are summarized in Table VI-18.

Additional collapse tests were performed in 3.5 in. diameter Zircaloy-2 tubing at room temperature and at 500°F. The primary purpose of these tests was to confirm experimentally analytical calculations on the collapse pressure of thin-walled Zircaloy-2 tubing considering the effects of initial ovality, end constraint, and temperature. The results were very encouraging. Percentage errors between the calculated and actual collapse pressure ranged from only 4% to 6%.

Two specimens No. 2 and 8, shown in Figure VI-29 and Figure VI-30 were collapsed at temperatures greater than 500°F. A summary of dimensional data and collapse conditions is given in Table VI-19.

The calculation methods were, in general, those given by Sturm.⁽⁵⁹⁾ The reduction of the modulus of elasticity due to temperature was taken from Zima.⁽⁶⁰⁾

TABLE VI-18

DIMENSIONAL DATA AND COLLAPSE CONDITIONS FOR ZIRCALOY-2 TUBES
COLLAPSED AT ROOM TEMPERATURE

	Specimen No. 5 Vacuum Annealed Seamless	Specimen No. 6 15% Cold-Worked Seamless	Specimen No. 4 15% Cold-Worked Welded
Length, In.	27	24-1/2	25-1/4
Average Measured Mean Radius-To-Wall Thickness Ratio	18.36	18.5	18.34
Average Measured Wall Thickness, In., h	.138	.136	.134
Average Positive Wall Thickness Variation, %	4.12	2.94	1.49
Average Negative Wall Thickness Variation, %	2.75	2.21	1.49
Calculated Collapse Pressure, psi, P_c^*	1030	1060	986
Actual Collapse Pressure, psi	915	1080	920

* Reference 57.

TABLE VI-19

DIMENSIONAL DATA AND COLLAPSE CONDITIONS FOR ZIRCALOY-2 TUBES

	Specimen No. 7 Vacuum Annealed Seamless	Specimen No. 2 15% Cold-Worked Seamless	Specimen No. 8 Vacuum Annealed Seamless
Length, In., <i>l</i>	45	22.375	45
Average Measured Outside Diameter, In., OD	3.526	5.070	3.527
Average Measured Wall Thickness, In., <i>t</i>	0.1091	0.1404	0.1046
Length-To-Radius Ratio, $\frac{2l}{OD}$	25.52	8.826	25.52
Outside Diameter-To-Wall Thickness Ratio, $\frac{OD}{t}$	32.32	36.11	33.72
Average Ovality, Mils, (OD Max - OD Min) Avg	4.82	7.833	4.2
Maximum Ovality, Mils, (OD Max - OD Min) Max	8	11	6
Time To Collapse	≈ 1 hr	≈ 4 hrs	≈ 4 hrs
Temperature At Collapse	RT	537.9°F	513.8°F
Calculated Collapse Pressure, psi	970.0	985.1	734.6
Actual Collapse Pressure, psi	910	945	770
Percentage Error	6	4	5

For specimen No. 7, calculations predicted a reduction in collapse pressure of 7% due to initial ovality, based on an allowable stress of 60,000 psi. For specimen No. 8, the reduction was predicted to be 5.7%, based on the same allowable stress.

Analytically, the effect of out-of-roundness (ovality) varies with the ratio of initial deflection to the thickness of the wall; the $\frac{L}{R}$ ratio; the elastic limit of the material; and the bending modulus of failure of the material. An expression of the effect is obtained for an initial out-of-roundness of the same magnitude as the deflected tube at buckling.

In addition to demonstrating that calculations can accurately predict collapse pressure for seamless Zircaloy-2 tubing in the diameter range of 3 - 5 in., the tests pointed up a major disadvantage of welded tubing for fuel element service. The welded tubing failed by rupture of the weld seam and would thus provide less assurance of fission gas and fuel containment in the event of inadvertent pressure overload in a reactor than would the seamless tubing, which merely collapsed flat upon itself without rupture. The information obtained from these tests was used in the design of the annular multitube fuel elements discussed in Section III-F.

5. Creep Tests of Steam Tube Materials

Creep tests were performed on Incoloy-800 at 900°F and stresses of 30,000 psi and 40,000 psi and on Inconel-625 material at temperatures of 1000°F and 1100°F and at stresses of 25,000 psi and 35,000 psi. These tests were performed to determine the primary creep behavior of

the candidate steam tube materials for times up to 500 hrs. At 35,000 psi, the Inconel-625 material had a total elongation of less than 0.00015 in/in. in 500 hr at 1000°F, whereas the Incoloy-800 tested at a stress of 30,000 exhibits a much larger creep, viz., 0.008 in/in. in 500 hr at a temperature of only 900°F. These tests are reported in detail in Reference 20.

The creep behavior of these two alloys demonstrated the superior performance of Inconel-625 as a potential steam tube material for combination boiling-superheating fuel elements. The negligible creep rate of Inconel-625 at 1000°F removes concern about the plastic strain behavior of the steam tubes and the localization of strain due to plastic distortion.

6. Fatigue Tests of Steam Tubes

Early work on steam tube fatigue involved testing of various coils and hairpin configurations which were under consideration in the design of the multitube fuel elements. (References 14, 17, and 20.) An important aspect in the assessment of such coils was their capacity for elastic absorption of the differential expansion between boiler clad and steam clad members. The fatigue tests were terminated when the adoption of a floating plenum concept in the ABC design removed the need for coiled tubes.

Further fatigue data were obtained on straight tube welded members as part of the welding investigations conducted at SwRI. In addition, SwRI performed fatigue tests on 0.283 in. OD by 0.026 in. wall Inconel-625 material at 1000°F and 1200°F. The relevant data appear

in Figure VII-16 of this report.

7. Irradiation Tests of Inconel-625 Material

The effects of neutron irradiation upon the mechanical properties of Inconel-625 were investigated by Claudson.⁽⁶¹⁾ This work was part of a USAEC sponsored program to investigate irradiation effects on several nickel base precipitation and solution hardenable alloys under various reactor conditions. In the following discussion, information relevant to the Advanced BONUS Core fuel element design is quoted from Reference 61 and interpreted in terms of the design application.

Inconel-625 is a nickel-chromium-iron alloy of high strength which results from alloying additions of molybdenum and columbium. The material is not a precipitation hardenable alloy and, in the hot rolled and annealed conditions, does not show the ductility decrease typical of nickel base alloys in the temperature range of 760 to 870°C (1400-1600°F). Because of its relatively high strength and good oxidation resistance in high temperature steam, Inconel-625 is a candidate material for fuel cladding in nuclear superheat reactors. To determine its suitability for use in a reactor fuel element, an evaluation of the effect of irradiation on the mechanical properties of the material was conducted at Battelle Northwest Laboratory.⁽⁶¹⁾

The chemical analysis of the Inconel-625 test material is shown in Table VI-20. After hot rolling at about 1180°C (2160°F), the material was annealed at 1150°C (2100°F) for one hour and air cooled. The 0.250 in. plate was reduced to a thickness of 0.110 by hot rolling according to the schedule shown in Table VI-21. The rolled material was given

TABLE VI-20

CHEMICAL ANALYSIS OF INCONEL 625

Heat Number NX 8137A

Chemical, wt%

<u>C</u>	<u>Ni</u>	<u>Cr</u>	<u>Fe</u>	<u>Cb</u>	<u>Mo</u>	<u>S</u>	<u>P</u>
0.033	62.62	21.81	1.46	3.97	8.99	0.006	0.005
<u>Si</u>	<u>B</u>	<u>Ta</u>	<u>N</u>	<u>H</u>	<u>O</u>	<u>Total rare earths</u>	
0.22	<0.001	<0.01	0.047	0.0006	0.011	20 ppm	

TABLE VI-21

ROLLING SCHEDULE FOR INCONEL 625

Heat Number NX 8137A

<u>Pass No.</u>	<u>Gauge Width, In.</u>	<u>Temperature</u>
1	0.250	1177°C*
2	0.210	
3	0.176	
4	0.155	
5	0.135	
6	0.120-0.127	
7	0.113-0.116	

*Starting temperature

a solution and aging treatment by heating to 1040°C (1900°F) for one hour, water quenching, reheating to 705°C (1300°F) for 24 hours, and water quenching. The material properties after this treatment were similar to the vendor's properties for this specific heat treatment and are shown in Table VI-22.

TABLE VI-22

ROOM TEMPERATURE MECHANICAL PROPERTIES OF INCONEL 625
USED FOR IRRADIATION TEST SPECIMENS

Yield Strength	92,000 psi
Tensile Strength	150,000 psi
Uniform Elongation	73%
% Reduction in Area	70%

Flat tensile specimens were fabricated by surface grinding to the dimensions shown in Figure VI-31.

The specimen irradiation conditions and the post-irradiation test conditions for the evaluation of mechanical properties are shown in Table VI-23. Data obtained from these tests are summarized in bar and line graph forms in Figures VI-32 through VI-38, and a summary of the tensile data from these charts is presented in Table VI-24.

The following discussion of the results and conclusions from the experiments conducted at BNWL on the irradiation performance of Inconel-625 is quoted directly from Reference 61.

"The room temperature tensile properties are characterized by increases in yield strength with

TABLE VI-23

IRRADIATION TEST CONDITIONS AND POST-IRRADIATION TEST
CONDITIONS FOR EVALUATION OF MECHANICAL PROPERTIES

<u>Irradiation Temp °C</u>	<u>Neutron Exposure Nvt (E>1 Mev)</u>	<u>Irradiation Atmosphere</u>	<u>Metallurgical Condition</u>	<u>Post- Irradiation Test Temp °C</u>
50°C	9.8 x 10 ¹⁹	Water	Solution Treated	RT* and 650
	1.84 x 10 ²⁰	"	"	"
	4.61 x 10 ²⁰	"	"	"
	7.64 x 10 ²⁰	"	"	"
280°C	5.3 x 10 ¹⁹	"	"	RT*
	1.28 x 10 ²⁰	"	"	"
	1.57 x 10 ²⁰	"	"	"
	2.55 x 10 ²⁰	"	"	"
	7.71 x 10 ²⁰	"	"	"
	8.27 x 10 ²⁰	"	"	"
	1.03 x 10 ²⁰	"	"	650°C
	3.2 x 10 ²⁰	"	"	"
	5.68 x 10 ²⁰	"	"	"
	1.46 x 10 ²¹	"	"	"
580°C	9.2 x 10 ¹⁹	Helium	Mill Annealed	RT*
740°C	9.2 x 10 ¹⁹	Helium	Solution Treated Mill Annealed Mill Annealed & Solution Treated	RT* 300°C 650°C

*Room temperature

TABLE VI-24

TENSILE DATA FOR IRRADIATED AND CONTROL SPECIMENS OF INCONEL 625

Specimen No.	Temperature, °C Irradiation Test		0.2% Yield Strength, psi	Ultimate Strength, psi	% Uniform Elongation	% Total Elongation	% Reduction In Area	Material Condition	Neutron Exposure nvt
59	--	650	83,580	129,600	19.05	19.96	16.92	Solution Treated	0
373-D3	50	650	107,940	125,660	7.16	8.00	17.46	Solution Treated	1.71 x 10 ²¹
A39X	--	650	80,000	112,000	8.17	9.01	11.50	Annealed	0
A4C	740	650	72,140	96,770	4.81	5.20	7.96	Annealed	9.2 x 10 ¹⁹
A45X	--	650	84,570	124,600	10.45	10.79	11.06	Solution Treated	0
A46	740	650	66,150	96,650	6.62	7.00	11.00	Solution Treated	9.2 x 10 ¹⁹

some loss in ductility at low (50°C , 280°C) irradiation temperatures. Essentially, there was no change within the limits of experimental error in ultimate strength. Material irradiated at these lower temperatures and tested at room temperature maintained good ductility as shown by high uniform and total elongations and large reductions in area. Necking occurred because of plastic instability in a localized region of all specimens irradiated and tested under these conditions.

The major effect found in the room temperature properties of Inconel 625 was caused by irradiation temperature. These data, however, when compared to control specimens, indicate that the effect is primarily because of the thermal environment and not the irradiation since the control specimen also showed a decrease in ductility. This is especially true for the 740°C irradiation temperature. At the 580°C irradiation temperature, a further decrease in ductility was observed in the irradiated specimen as compared to the control specimen. This decrease indicated some effect of irradiation. Essentially, no effect was noticed for specimens irradiated at 740°C . At 740°C , the only effects were increases in yield and ultimate strength.

Although structural components of Inconel 625 will normally operate at elevated temperatures in reactor service, they will also operate satisfactorily under applied stress at lower temperatures during reactor shutdown if adequate design considerations are applied.

Unirradiated specimens of Inconel 625 exposed to superheated steam at 1150°F over long periods of time, increased in yield strength and decreased in room temperature elongations.¹ These effects stabilized after about 4000 hr. The elongation values of over 20%¹ after exposure agree with the values from 580°C control exposures in this study. Comprelli and Wolff¹ also showed that long term exposure of Inconel 625 to high temperature environments caused precipitation. The precipitates were primarily the AB type, TiN and NbC.

Since the precipitation of inherently brittle second phases occurs at elevated temperature

¹ F. A. Comprelli and U. E. Wolff. Stability of High-Nickel Alloys in Superheated Steam, GEAP-4745. General Electric Co., San Jose, California, November 1964

(where the alloy is expected to operate) the elevated temperature mechanical properties are of great interest.

The elevated temperature tests on specimens irradiated at 280°C show a marked decrease in ductility as compared to similar specimens tested at room temperature. It is important to note that this reduction does not seem as dependent upon the irradiation dosage as it is upon the testing temperature. At both test temperatures, only small changes in the percent uniform elongation occurred, compared to a factor of 3 to 4 decrease associated with the difference in test temperature alone.

It appears from the elevated temperature test data that the reduction in strength properties of the material are caused primarily by the effects of the neutron irradiation since significant changes in the yield strengths were found as compared to the control specimens. The reduction in strength may be due to defects in the material relaxing the matrix strain hardening imposed by the solution hardening elements of the alloy, columbium and molybdenum.

In comparing the percent uniform elongation data for both the control and irradiated specimens with the as-heat treated value, a marked decrease is observed. This decrease indicates that exposure of the material to its test environment is the primary cause of the reduced ductility because of thermal aging. Thus, it appears that two mechanisms may be operating to cause the observed changes in the mechanical properties of the alloy. One mechanism is associated with the neutron exposure controlling the strength properties, the other is associated with the thermal exposure controlling the ductility of the material. This apparent small effect of irradiation upon the high temperature ductility of Inconel 625 was not discovered in specimens irradiated at 740°C. Specimens irradiated to an exposure of 9.2×10^{20} nvt ($E > 1$ MeV) showed a factor of 2 decrease in uniform elongation. This decrease indicates irradiation-induced damage in the material. This observation was also made for the solution hardenable nickel base alloy Hastelloy N at Oak Ridge National Laboratory.²

² W. R. Martin & J. R. Weir. Effect of Elevated Temperature Irradiation on the Strength and Ductility of the Nickel-Base Alloy, Hastelloy N, ORNL-TM-1005, Oak Ridge, Tennessee, 2/65

Examination of the data for specimens irradiated at 740°C in the mill-annealed and in the solution-treated and aged conditions and tested both at room temperature and at 650°C, reveals that the ductility of the material at both test temperatures is lower after 740°C irradiation than after 280°C irradiation. For the room temperature tests, the material increased in yield and ultimate strength because of the 740°C irradiation. The converse was true in material tested at 650°C where a decrease in strength properties was observed. In addition, a greater change in strength and ductility due to irradiation was seen at the 650°C test temperature. It also appears that material in the solution treated condition prior to the irradiation retains more ductility during elevated temperature neutron exposure.

It is concluded from these investigations that thermal exposure plays an important role upon the mechanical properties of Inconel 625 after irradiation. Thermal stability of this alloy at elevated temperatures in absence of neutrons is good with most precipitation reactions occurring slowly¹ at temperature of 620°C.

At temperature of 740°C, these reactions must surely increase in rate; and in the presence of high energy neutrons may occur even faster. The extent and distribution of the precipitation and their effects upon the mechanical properties of the material should be the subject of further research."

The conditions under which Inconel-625 is used in the ABC solid multitube fuel element are as follows:

<u>Location</u>	<u>Material</u>	<u>Condition of Material</u>	<u>Operating Temp. °F</u>	<u>In graded °C Fast Neutron Flux (nvt) > 1 Mev</u>
Boiler Cladding	Inc.-625	Seamless Tube	R.T.* & 540°F	R.T.
		Solution Treated	540°F	282°C
		Welded Tubing	540°F	282°C
Steam Tube	Inc.-625	Seamless Tube	R.T.	
		Solution Treated	540°F	282°C
		Welded Tubing	1250°F 950°F	680°C 510°C

*Room Temperature

Although the integrated-fast flux to which the Inconel-625 test specimens were exposed in the BNWL program was not as long as is expected in the ABC fuel element for an average burn-up of 15,000 Mwd/MTU, the post-irradiation mechanical properties observed in the tests are adequate for the design stresses involved in actual reactor operation. Although low, the total elongation of the specimens in the post-

irradiation test at elevated temperature is considered sufficient to permit localized distortion at discontinuities such as welded joints. Since the fuel element is designed so that all stresses are nominally well within the elastic limit of the material, gross distortions are not expected and large values of ductility are not required. Although the number of tests is limited, the retention of reduction in area values of at least 20% as shown by the post-irradiation data tends to support the opinion that adequate ductility exists in Inconel-625 for use as a steam tube material and boiler cladding for the ABC fuel element.

The 25% loss in tensile strength of Inconel-625 is of greater relevance to the ABC element designs than is the loss in ductility. If this reduction in strength would progress at a rapid rate with increased neutron exposure, a shortened fuel element life would have to be expected. However, the data of Figure VI-35, although showing a reduction in ultimate strength from 135,000 psi to 100,000 psi in the exposures of 1.03×10^{20} nvt, do not indicate any significant further reduction in ultimate strength from an exposure of 1.03×10^{20} nvt to 1.46×10^{21} nvt. Further testing at longer exposures is needed, however, to assure that no rapid drop in ultimate strength will occur with fast neutron exposures of the cladding up to the expected maximum in the Advanced BONUS Core. Future work should also take into consideration the need for evaluating irradiation effects on welded samples of Inconel-625 material at the temperatures expected in reactor service. Such effects can be adequately evaluated for the ABC fuel element by irradiation testing prototype fuel elements in a suitable reactor.

FIG. VI-1 ABC Multitube Fuel Loading and Densification Assembly

FIG. VI-2 Perspective View - ABC Fuel Loading Fixture

FIG. VI-3 Steam Tube Spacer Washer

FIG. VI-4 Fuel Feed-Through Spacer Washer

FIG. VI-5 Insulation Seal Assembly

FIG. VI-6 Embedded Tube Test Specimen

FIG. VI-7 Embedded Tube Test

FIG. VI-8 Cyclic Behavior of Incoloy 800 Steam Tube Embedded in Vi-Pac UO_2

FIG. VI-9 Cyclic Behavior of Inconel 625 Steam Tube Embedded in Vi-Pac UO_2

FIG. VI-10 ABC Transition Joint Irradiation Test Specimens

NOTE:
CE = COEXTRUDED
EW = EXPLOSIVELY WELDED
RT = ROOM TEMPERATURE
EACH HORIZONTAL LINE ON THE BAR REPRESENTS A TEST

FIG. VI-11 Summary of Mechanical Strength Test Data on Irradiation Test of Zircaloy-Inconel Transition Joints

NOTE:
 CE = COEXTRUDED
 EW = EXPLOSIVELY WELDED
 RT = ROOM TEMPERATURE
 EACH HORIZONTAL LINE ON THE BAR REPRESENTS A TEST

FIG. VI-12 Summary of Irradiation Effects on Ductility in Zircaloy-Inconel Transition Joint Tests

FIG. VI-13

Effect of Aging on the Tensile Properties of Inconel 625 in Steam

FIG. VI-14 Effect of Aging on the Tensile Properties of Incoloy 800 in Steam

FIG. VI-15 Effect of Aging on the Tensile Properties of Inconel 600 in Steam

FIG. VI-16 Effect of Aging on the Tensile Properties of Hastelloy - X in Steam

FIG. VI-17 Effect of Aging on the Tensile Properties of Type 406 SS in Steam

FIG. VI-18 Effect of Aging on the Tensile Properties of René 62 in Steam

FIG. VI-19 Effect of Aging on the Tensile Properties of In 102 in Steam

FIG. VI-20 Strength of Annealed Inconel 625 Tubing

FIG. VI-21 Ductility of Annealed Inconel 625 Tubing

FIG. VI-22 Tensile Properties of Annealed Incoloy 800 Tubing - 0.396 In. OD by 0.039 In. Wall

FIG. VI-23 Compression Test Fixture Assembly

	600°F	900°F	1200°F
No. 1	330°F	480°F	695°F
No. 2	500°F	763°F	1025°F
No. 3	573°F	898°F	1172°F
No. 4	600°F	900°F	1200°F
No. 5	575°F	865°F	1175°F
No. 6	445°F	685°F	974°F
AVERAGE	500°F	750°F	1000°F

FIG. VI-24 Compression Test Temperature Profiles

FIG. VI-25 Zircaloy Tube Hydrostatic Test Apparatus Setup

FIG. VI-26 Hoop Strain in Zircaloy Tubing Versus Hydrostatic Hoop Pressure

FIG. VI-27 Collapsed 5.06 In. OD, 0.134 In. Wall Zircaloy-2 Tube, Specimen No. 4

FIG. VI-28 Collapsed 5.06 In. OD, 0.134 In. Wall Zircaloy-2 Tube, Specimen No. 4.
Internal View Showing Crack Along Weld on Inside of Tube

FIG. VI-29 Collapsed 5.07 In. OD, 0.140 In. Wall Zircaloy-2 Tube, Specimen No. 2

(Difficulty in removing the specimen from the test chamber produced the deformation seen at the ends.)

FIG. VI-30 Collapsed 3.5 In. OD, 0.104 In. Wall Zircaloy-2 Tube, Specimen No. 8.

(Note the Steam - Roller effect on the tube.)

FIG. VI-31 Specimen Geometry Used to Determine the Effects of Irradiation Upon the Mechanical Properties of Inconel 625

FIG. VI-32 Effects of Neutron Irradiation Upon the Room Temperature Properties of Inconel 625 Irradiated at 50°C in the Solution Treated Condition

FIG. VI-33 Effects of Neutron Irradiation Upon the Room Temperature Properties of Inconel 625 Irradiated at 280°C in the Solution Treated Condition

FIG. VI-34 Effects of Irradiation Temperature Upon the Room Temperature Properties of Inconel 625 Irradiated to an Exposure of About 1×10^{20} nvt ($E > 1 \text{ MeV}$)

FIG. VI-35 Effects of Irradiation at 280°C Upon the Mechanical Properties of Inconel 625 Tested at 650°C in the Solution Treated Condition

FIG. VI-36 Effects of Irradiation Upon the Room Temperature Properties of Inconel 625 in the Annealed and Solution Treated-Aged Condition Irradiated at 740°C to an Exposure of 9×10^{19} nvt ($E > 1$ MeV)

FIG. VI-37 Effect of Neutron Irradiation Upon the Mechanical Properties of Inconel 625 Irradiated at 740°C and Tested at 300°C

FIG. VI-38 Effects of Neutron Irradiation Upon the 650°C Mechanical Properties of Inconel 625 Irradiated at 740°C in the Mill Annealed and Solution Treated Conditions to an Exposure of 9×10^{19} nvt ($E > 1$ Mev)

VII. MANUFACTURING DEVELOPMENT

A. Pellet Development

The original fabrication of large diameter (1.5 in.) UO_2 pellets containing small diameter (0.25 in.) holes perpendicular to the flat surfaces of the pellet and symmetrically spaced within the body of the pellet was performed at CEND. Because of their apparently good quality, consideration was given to using these pellets as spacers placed at intervals in a powder packed fuel element to maintain the steam tubes at design position.

An order for 50 pellets to the configuration shown in Figure V-3 was placed with Nuclear Materials and Equipment Corp. (NUMEC). Evaluation of this lot showed that the pellet densities were in the 94-95% of theoretical range. The diametral and chordal hole spacing was within the pellet drawing requirements but the holes were undersize. Typical data for this pellet lot are shown in Table VII-1.

In view of the quality of this lot of pellets and NUMEC's low cost estimate (approximately \$40.00 per kg for large quantities of pellets of 3% enrichment), it was decided to proceed with the fabrication of pelletized solid multitube fuel elements, and an order for a second lot of 100 pellets was placed with NUMEC.

To evaluate the pellet quality further, 20 of the pellets from the second lot were procured in the as-sintered condition, 40 were ground on the outer diameter and 40 were ground both on the outer diameter and on the top and bottom surfaces. Inspection of the

TABLE VII-1
PHYSICAL DATA ON A SEVEN-HOLE PELLET

1. Green Density - 4.30 gm/cc (calculated)
2. Sintered Density - 10.35 gm/cc (water displacement) = 94.5% theoretical
3. Hole dimensions as measured by optical comparator at magnification of 20X on both top and bottom surfaces:

<u>Hole No.</u>	<u>Top Diameter(In.)</u>	<u>Bottom Diameter (In.)</u>
1	0.2705	0.2737
2	0.2713	0.2712
3	0.2705	0.2700
4	0.2704	0.2709
5	0.2721	0.2711
6	0.2711	0.2714
7(center)	0.2725	0.2716

Hole diameter required by specification (in.) = $\frac{0.271 \text{ min}}{0.274 \text{ max}}$

4. Pellet length (in.) - measured at five places on the pellet:

0.708
0.708
0.711
0.710
0.710

Average = 0.7094 in.

Specified length = $\frac{1}{2}$ in. minimum.

5. Outside diameter (in.) = measured at three places on top, center, and bottom planes:

<u>Top Surface Plane</u>	<u>Center Plane</u>	<u>Bottom Plane</u>
1.473	1.462	1.472
1.471	1.463	1.467
1.470	1.463	1.471

Specified Outer Diameter (in.) = $\frac{1.470 \text{ min}}{1.475 \text{ max}}$

6. Concentricity of the holes - measured by optical comparator at a magnification of 10X. The radius or distance from the center of the central hole to the center of each of the surrounding holes:

<u>Hole No.</u>	<u>Radial Distance(In.)</u>
C7 to C1 =	0.4432
C7 to C2 =	0.4475
C7 to C3 =	0.4434
C7 to C4 =	0.4427
C7 to C5 =	0.4450
C7 to C6 =	0.4442

Specified concentricity (in.) = 0.444 ± 0.005

pellet dimensions showed that they were within the specified tolerances, except for the as-sintered outer diameter surface where some slumping gave enlarged diameters near the surface in contact with the sintering tray.

A third 100 pellet lot was fabricated to correct the slumping condition and to further evaluate process control parameters from batch to batch. Inspection of the third lot revealed that the direction of slump was reversed and the pellet hole size was within specifications; however, the pellet outer diameter had decreased to the minimum drawing dimension and thus precluded the possibility of grinding the outer diameter. Also, the hole circle diameter was 0.005 in. larger than the specified size. Nonetheless, it appeared that the pellet fabrication parameters were controllable and reasonably well established. On the basis of this evaluation, an order was placed for 750 depleted pellets in accordance with CEND drawings and specifications. The pellets were intended for the fabrication of thermal cycle test elements.

Further conditions of acceptability for the pellets from the point of view of their conformance to fuel element-performance requirements were established by defining a visual standard for surface conditions with regard to cracks, surface flaking, and chips. Two equivalent sets of standards were selected from pellets containing defects which defined the limit of acceptability. The visual standards were liberally established because it is believed that the relatively strong, creep resistant Inconel-625 cladding does not require full support over the clad-pellet interface. The subsequent lot of pellets received from

NUMEC was in general of a quality far superior to that of the visual standard sets.

An initial lot of 200 pellets fabricated to the final CEND drawing was evaluated before proceeding with the remaining order of 550 pellets. While this lot conformed to the required drawing dimensions and tolerances, the following defects were found that had not been evident in previous trial lots and had not been covered by the specification visual standards.

- (1) The finish of the ground outside diameter of the pellets was variable and rough due to grinding wheel wear;
- (2) The pellets contained some peripheral and radial cracks;
- (3) Peripheral laminations were discovered near the top face of the pellets after grinding.

To define acceptability standards for these conditions, six additional pellets were added to the visual standard sets, two to define outer diameter surface finish, two to define cracks, and two to define laminations. The following further restrictions were placed on the three new pellet acceptability standards:

- (1) A pellet must not contain multiple cracks;
- (2) The cracks must not appear in the webs of adjacent holes;
- (3) The cracks must not appear related in origin;
- (4) A lamination must be shallow enough to withstand fingernail pressure applied in a manner such as to lift off the top of the laminations;
- (5) The outer diameter ground finish must be 125 r.m.s. or better.

After a final review of the pellet quality and some minor process adjustments, the remaining 550 pellets were produced. In the final 300 pellets of this run, NUMEC had completely eliminated the defective though acceptable conditions noted in the earlier pellets, such as outer diameter finish, pellet cracks, and laminations. A visual comparison of the final and earlier pellets is made in Figures VII-1 and VII-2. Note that the earlier pellet shown in Figure VII-2 does not contain the 0.008 in. deep center reduction in thickness. This reduction provides axial space between each pellet in the assembled stack and permits free axial expansion of the hot center regions of the pellet in operation. Its purpose, therefore, is to reduce or minimize axial ratchetting between the fuel and the boiler cladding. The center depression is on one face of the pellet only; it is obtained by machining the upper die and was found to cause no difficulty in the pellet fabrication.

For the last 500 pellets, production and quality control records showed an overall yield of 53%. A loss of about 20% occurred in the green pellets to final product stages. A breakdown of the losses as a result of final inspection is shown in Table VII-2.

It should be noted that 40% of the rejects were due to undersized outer diameter and oversized holes. Both defects could be corrected without difficulty in any future production run. NUMEC estimated that an overall yield of 75% could be obtained on a 3000 kg production lot of 4% enriched material provided the problem associated with the OD grinding could be satisfactorily resolved on a production basis.

TABLE VII-2

TABULATION OF PELLET REJECTION DATA FOR A 500 PELLET PRODUCTION

RUN OF PERFORATED PELLETS

<u>Pellet Defect Description</u>	<u>No. of Pellets</u>
Number Rejected for Bowing	39
Number Rejected for Undersized Outer Diameter	41
Number Rejected for Oversize Hole Diameter	18
Number Rejected for Flatness	2
Number Rejected for Thickness	15
Number Rejected for Parallelism	2
Number Rejected for Perpendicularity	4
Number Rejected for Concentricity	4
Number Rejected for Chips	9
Number Broken in Processing	2
Total Number Rejected	136
Total Number Acceptable	365
Total Number Fabricated	501

Two fuel elements for thermal cycle tests were fabricated with the UO_2 pellets. Both fuel elements completed the test without failure.

The completion of the 750 pellet lot production run to stringent specifications represents a successful demonstration of the practicability of producing 1.5 in. diameter perforated pellets of the type required for integral boiling-superheating fuel elements.

B. Welding Development

From the outset of the development work on the multitube fuel elements, it had been recognized that the fission gas containing welds would present a manufacturing problem which would require early resolution. The fission gas containing welds are (1) the tube to tube-sheet welds, (2) the boiler clad to tube-sheet welds and (3) the tube to tube lap welds. First of all, the number of welds in a single multitube fuel element is larger than the number required for conventional rod type elements, and secondly, the close spacing of the welds is a basic characteristic of the multitube fuel element, required by thermal and nuclear considerations. End welding of the tube to tube-sheet welds was given preference because of the small diameter of the steam tubes and in view of the extensive experience with end welds in the heat exchanger field. The integrity requirements of the welds were of the highest order; i.e., 100% leak tightness must be assured to prevent the escape of fission products into the steam and water systems.

The ratio of steam tube pitch spacing to steam tube diameter (P/d) is probably the most important single geometric parameter of the multitube fuel element. For a fixed steam tube diameter, which is

established by heat transfer and fluid flow requirements, an increase in P/d results in a larger fuel element; smaller water to uranium ratio; higher fuel temperature; greater ease in welding and manufacturing assembly; improved pellet dimensional stability; etc. The converse is also true. The reference design multitube fuel element has a pitch to diameter ratio in the fuel region of 1.57. However, because of the expanded upper end of the second pass tubes, the P/d ratio is 1.33 which is believed to be the smallest value achievable with this type of welding.

The development of practical and reliable welding procedures for the production welding of the fission gas containing welds of the ABC fuel elements was pursued in parallel with the design and materials development during the period from March 1964 to September 1965. The welding development was performed by the Southwest Research Institute, in cooperation with Combustion Engineering, Inc., Nuclear Division. All work on the welding development was completed and a detailed final report was issued by SwRI on September 20, 1965.⁽⁵⁵⁾

In addition to preparing the welding procedures for the fission gas containing welds, a document defining the standards of acceptance for these welds was prepared which is included in the Appendix of Reference 55. Also, an evaluation of the effect of the variation in welding parameters on weld soundness was made under the welding development program, and design curves of the out-of-pile fatigue life of the welded joints as a function of temperature and stress magnitude were experimentally determined.

Four specific weld joints were investigated: (1) the 0.285 in. ID steam tube to tube-sheet joint; (2) the 0.285 in. OD steam tube to tube-sheet joint; (3) the steam tube lap welded joint; and (4) the boiler clad to tube-sheet joint. These weld joints and the various configurations that were studied for each joint are described in Reference 55 and reproduced in Figures VII-3 and VII-4 of the present report. Of the five weld preparations investigated for the tube to tube-sheet joint, configuration "C" was selected. The plug weld joint design was selected from among the three weld preparations investigated for the boiler clad to tube-sheet weld. The optimum configuration of each type of joint was determined on the basis of lower cost, improved reliability, higher strength, and insensitivity to variations in the welding parameters. The final designs of the selected weld joints were used in the thermal cycle fuel element shown in Figure V-2 of this report.

The welding development work included investigation of Incoloy-800, Inconel-625, and Inconel-600 materials. Weldments containing the following combinations of these materials were investigated:

- (1) Inconel-625 steam tubes welded to Inconel-625 tube sheets
- (2) Inconel-625 steam tubes welded to Inconel-600 tube sheets
- (3) Incoloy-800 steam tubes welded to Inconel-600 tube sheets
- (4) Incoloy-800 steam tubes welded to Incoloy-800 tube sheets
- (5) Inconel-625 boiler clad welded to Inconel-625 tube sheets
- (6) Inconel-625 boiler clad welded to Inconel-600 tube sheets
- (7) Inconel-625 tubes lap welded to Inconel-625 steam tubes.

Several heats of material and three sizes of tubing were investigated. No significant variation in the weldability among the heats

and sizes tested was observed. The favorable results obtained in the welding development and fatigue investigations contributed importantly to the selection of Inconel-625 as the reference material for the tube sheets, the steam tubes, and the boiler cladding of the ABC fuel element.

The critical parameters in making the tube to tube-sheet joints were welding arc length, rotational speed relative to the work, the location of the electrode relative to the seam, and root clearance. Each parameter was optimized separately and the optimized values were included in the final welding procedure. The root clearance was found to be an important parameter which affected weld soundness. Tack expanding of the tubes prior to welding was employed to assure a tight root before starting the weld, as open root configurations had been demonstrated to have a propensity to hot tears and root cracking in all material combinations. (See Figures 32 and 35 of Reference 55.) With proper tack expanding, this problem was eliminated and the production weld mock-ups, which were made at CEND in accordance with the final weld procedure, demonstrated that a clean, uncracked root condition can be consistently obtained. Figure VII-5 shows typical micrographs at a magnification of 50X of tube to tube-sheet welds selected at random from the production weld mock-ups made at CEND.

For the boiler clad to tube-sheet welds the critical parameters of weld amperage, electrode position, back-up gas mixture, and fit-up were also investigated. An interference fit of 0.002 was found to result in low residual stresses and still provide a good fit-up.

The procedure specification for welding the boiler clad to tube-sheet welds is contained in Appendix I of Reference 55. Under this procedure, the welding is performed with the boiler clad in the horizontal plane and the welding torch perpendicular to the axis of the boiler clad and in the one o'clock position. The work piece is rotated at 3 rpm so that the welding operation is carried out in the "uphill" direction. No heat sinks are used with this procedure. The optimum parameters selected were 57 amps, 0.015 in. electrode position, and 0.035 in. arc gap. Representative micrographs of boiler clad to tube-sheet welds made by CEND on the production weld mock-ups in accordance with Procedure Specification SwRI-4 (55) are shown in Figure VII-6.

The development of the tube to tube lap joint was initiated at CEND and completed at SwRI. An enlarged upper section of the second pass steam tubes is required in the ABC fuel element design because of the need for a thermal liner insulating the tube sheet from the 950°F outlet superheated steam. The integrity of the lap weld is of vital importance because it, too, is a fission gas seal. A significant effort was devoted to the selection of welding parameters for the lap welds to assure achieving satisfactory fatigue strength of this joint at operating temperature. It was important to develop a weld with a root configuration which could be inspected with a borescope for determining adequate root penetration. A sketch of the welding arrangement and position of the electrode used in making the lap welds is shown in Figure VII-7.

The lap weld between the second pass steam tubes and the enlarged upper thermal sleeve was found to be the weakest welded member in the

fuel element. Although the thermal cycle test performance to date indicates that the strength of this joint is adequate for reactor service, it is believed that the lap weld joint can be replaced with a roll expanded upper end. This replacement will also result in a reduction in manufacturing cost of a production quantity order of fuel elements.

As a result of the welding development conducted at SwRI, it was concluded that

- (1) The weldability and weld soundness of Inconel-625 in the test joints investigated is superior to Incoloy-800 or Inconel-600 weld joints;
- (2) The optimum weld configuration for the tube to tube-sheet joint is the "C" configuration, which also is the simplest construction that can be obtained;
- (3) Tack expansion of the tube to tube-sheet joints and the lap weld joint and an interference fit of the boiler clad to tube-sheet joint are mandatory to assure a sound weld root and eliminate hot tears;
- (4) Post-welding expansion of the tube to tube-sheet weld joints is beneficial to the fatigue strength of these joints and provides a clean bore for smoother flow and/or insertion of thermal sleeves.

The welds of all types described above were evaluated at SwRI by tensile tests, compression tests, and reversed axial fatigue tests on sample welds and by metallurgical examination of polished sections of the samples. Test specimens for the tensile and fatigue tests of the

tube to tube-sheet welds are shown in Figure VII-8. The test specimens for the boiler clad to tube-sheet welds are shown in Figure VII-9. The hydraulic fatigue machine used in the tests is shown in Figure VII-10, and lap weld test samples are shown in Figure VII-11. Fatigue tests were also performed on base metal steam tubing samples at 1000°F and 1200°F. Figure VII-12 shows the samples used for these tests, which were conducted at the maximum temperature the weld or tube is expected to experience in reactor operation. The fatigue curves for the boiler clad to tube-sheet welds, tube to tube-sheet welds, lap welds, and the steam tubing material are shown in Figures VII-13, VII-14, VII-15, and VII-16. From these curves, the minimum elevated temperature fatigue strength for a 10,000 cycle life in reversed axial loading of the reference design welded joints can be determined; the strength values are tabulated below for comparison with the base metal fatigue strength at its operating temperature.

<u>Type of Joint</u>	<u>In-Reactor Operating Temperature (°F)</u>	<u>Stress (psi)</u>
Tube to tube sheet	750°F	<u>±</u> 60,000
Boiler clad to tube sheet	600°F	<u>±</u> 54,000
Tube to tube lap weld	925°F	<u>±</u> 38,000
Base material	1000°F	<u>±</u> 79,000
Base material	1200°F	<u>±</u> 70,500

The following conclusions are derived from the tensile and fatigue test results:

- (1) Adequate fatigue strength exists in all of the reference

design welded joints and base materials of the multitube fuel element at their respective operating temperatures.

(2) Inconel-625 to Inconel-625 welds are the strongest joints in tension and fatigue strength of any material combinations that were tested.

(3) All welded joints are lower in fatigue strength at their respective maximum operating temperatures than the base material is at its maximum operating temperature, i.e., the welded joints are the weak links in the system and the lap weld joint, specifically, is the weakest link.

(4) There is greater scatter in the fatigue data of the welded samples than there is in the base material fatigue test results. This implies that larger safety margins should be employed in using the weld test data for design purposes.

(5) No significant difference exists in the unit tensile or fatigue strength of welded joints among the steam tube sizes or heats of material that were tested.

The welding procedures developed by SwRI as included in Appendix I of Reference 55 were used in the manufacture of two fuel elements at CEND. These two fuel elements were subjected to a thermal cycle test simulating the thermal and pressure loadings that the fuel elements would experience in reactor service. (See Section V.) The welding fabrication of the test fuel elements was accomplished without difficulties, using the SwRI procedures. Production mock-ups of each of the welds were made, sectioned, and metallurgically examined. A total

of one hundred fifty-seven weld sections of production mock-ups were so examined at a magnification of 100X or greater and judged acceptable under the previously determined SwRI acceptance standards. The welding procedures lend themselves readily to production welding. Although only two fuel elements were actually completed by CEND, no problems relating to the adaptation of these procedures to the larger scale welding of a production order are foreseen.

Upon completion of the thermal cycle test, the fuel elements were destructively examined and the welds were scrutinized in detail. As described in the metallurgical examination (Section V-E), all welds of the two tested fuel elements were examined destructively after the test and found to meet the SwRI acceptance standards and specification welding requirements. The welds had withstood over 2000 thermal cycles whose severity exceeded that which the components would experience in reactor service.

It is considered that the fission gas containing welds of the multi-tube fuel elements have demonstrated acceptability in all out-of-pile testing and in the prototype manufacture.

C. Manufacture of the Thermal Cycle Test Assemblies

Two complete thermal cycle test fuel elements, one of the fixed tube sheet and one of the floating tube sheet design, were manufactured and used for the test described in Section V of this report. In addition, one other fuel element of each design was manufactured to within 85% of completion to be used as spare in the event of handling

damage or early failure in the thermal cycle test. The material procurement, inspection, machining of components, qualification of welding procedures, fabrication, assembly, and final inspection of these four fuel elements constituted a demonstration of the feasibility of manufacture of the boiler-superheater fuel element of the solid multitube design. The fuel elements were completed without any major manufacturing problems or design modifications. Certain minor improvements in fabrication and assembly were made on the basis of experience obtained in the course of the development. For instance, a slight chamfer on the tube sheet edges was found to aid greatly in accomplishing the shrink fit between the boiler clad and the tube sheets. Also, a device for press fitting the parts together during this assembly instead of tapping the parts was advantageous. The use of expanded tubes rather than the lap weld between the second pass steam tubes and the thermal sleeve is believed both feasible and desirable. A detailed description of the manufacture of the fuel elements is contained in Reference 31.

A major effort in the manufacture of the fuel elements was directed at the qualification of the fission gas containing welds. The technique for making these welds, the welding procedures, and the acceptance standards were developed at SwRI, as described in Section VII-B. However, production qualification of the welds to the SwRI procedures was performed in the CEND manufacturing shop prior to welding the thermal cycle elements. All qualification and production welds were made by an automatically controlled tungsten inert gas process.

The production welding mock-ups were made, sectioned and metallurgically

examined. A total of 157 weld sections of production mock-ups were so examined at a magnification of 100X or greater and judged acceptable under the previously established SwRI Standards. Thus, the welding equipment, the operators and the procedures were qualified in a production shop in a manner identical to that used for qualifying welds for a core quantity order of fuel elements.

The thermal cycle test elements and components were handled, assembled and inspected in accordance with CEND quality control standards and the CEND fabrication specification entitled "Specification for the Advanced BONUS Core Boiler-Superheater Test Fuel Element Assembly".

The satisfactory completion of fabrication of the thermal cycle test elements demonstrated the manufacturing feasibility of the multi-tube fuel element concept. Although only two fuel elements were completed, they were manufactured in accordance with reactor fuel requirements, and no problems were encountered which would prevent adapting the manufacturing procedures to the larger scale fabrication of a production order of fuel elements.

Some work remains to be done in establishing the welding procedures and techniques of fabrication for the structural and header parts. Methods of welding the four fuel elements together to form the assembly were under way at the time of the program was terminated. The cross weld between the four assemblies must be made with limited distortion of the assemblies. All other structural and header welds in the fuel assembly are believed to be amenable to conventional welding techniques for Inconel-600 material.

FIG. VII-1 Pellets - Final Lot - Ground Outer Diameter

FIG. VII-2 Pellets - Second Lot of 100 - Ground Outer Diameter - Radial Cracks

A - CONFIGURATION (INSERT)

B - CONFIGURATION (TREPAN)

D - CONFIGURATION (EXPOSED ROOT)

E - CONFIGURATION (FLARED)

C - CONFIGURATION (STANDARD)

FIG. VII-3 Tube to Tube - Sheet Joint Preparation

FIG. VII-4 Boiler Clad to Tube - Sheet Joint Designs

STEAM TUBE

HEADER

TUBE

TUBE

HEADER

HEADER

TUBE

TUBE

HEADER

MAG-50X

FIG. VII-5

Production Mock-Ups No. 132-47 and No. 129-48 of
Tube to Tube-Sheet Welds

TUBE SHEET

TUBE SHEET

FIG. VII-6

Boiler Clad to Tube-Sheet Weld Mock-Ups

FIG. VII-7 Lap Weld Joint Electrode Position

FIG. VII-8 Double-Ended Fatigue Specimen Used to Evaluate the Weldability of Inconel 625 Tubing

FIG. VII-9 Boiler Clad to Tube - Sheet Fatigue Specimens Before (Left) and After (Center and Right) Testing at 600°F. The Center Specimen is Assembled on the Load Transmitting Components

SwRI 3234

FIG. VII-10 Axial Fatigue Specimen and Grips in Southwest Research Institute Hydraulic Fatigue Machine

FIG. VII-11 Components of Double-Ended Fatigue Specimens Before and After Welding. Welded Component in Center Also Tested Without End Grips

SwRI 3233

FIG. VII-12 Axial Fatigue Specimen for Parent Tubing Material

FIG. VII-13 S-N Curve for Boiler Clad to Tube - Sheet Welds at 600°F

FIG. VII-14 S-N Curves for Tube to Tube - Lap Joints Using 0.262 In. and 0.310 In. OD Tubing

FIG. VII-15 Fully Reversed Axial Load Fatigue Test Results on Tube to Tube - Lap Joints Using 0.283 In. and 0.337 In. Tubing

FIG. VII-16 S-N Fatigue Curves for Inconel 625 Tubing at 1000°F and 1200°F

VIII. CONCLUSION AND PROPOSED PLANS FOR FUTURE WORK

The completion of all out-of-pile testing and manufacturing development work on the Advanced BONUS Core fuel element confirmed the reasons for selection of the solid multitube fuel element with Inconel-625 cladding as the reference design. This fuel element offers a number of operating and economic advantages over rod-type fuel elements for integral nuclear superheat reactors.

(1) Lower fuel cycle costs are predicted for the ABC core on the basis of a careful manufacturing cost comparison with BONUS Core I fuel. Although this magnitude of these cost savings is dependent upon the relative actual in-core lifetimes of the ABC and Core I fuel elements, neither of which is known at this time, it is believed that the inherent characteristics of the ABC element will make it more economic both for BONUS and for large integral nuclear superheat reactors.

(2) It is an inherent characteristic of the multitube fuel element that it contains a small amount of cladding material per unit of fuel material in the core region. This characteristic reduces substantially the parasitic loss of neutrons for a given type of structural material; or, alternately, for a given level of neutron economy, it permits the use of higher cross section cladding materials than are permitted by rod-type elements.

(3) The fuel element uses a high strength cladding material which is highly corrosion resistant in superheated steam and which,

because of its favorable elevated temperature strength properties, may permit relaxation of current superheat clad design temperature limits as more knowledge and experience is gained on irradiation performance.

(4) The element has the capability of accepting a low poison boiler clad of Zircaloy material, provided that satisfactory Zircaloy to Inconel transition joints can be produced.

(5) The number of fission gas containing welds in the entire ABC Core boiler region and boiler superheater region is 1400 less than the number of similar welds in the entire BONUS Core I boiler region and superheater region. This 15% reduction improves the statistics of fission gas leak prevention.

(6) In case of loss or interruption of steam coolant, the fuel element has a very good capability for external cooling on the boiler side at low power.

(7) There are no thin-wall unsupported tubes in the multitube fuel element which will collapse if steam flow in excess of the normal rate is passed through the element.

(8) The element provides a large boiling side heat transfer surface, at or near saturation temperature, which is available to reduce markedly internal temperature and pressures within a partially water-logged element during start-up and low power operation, thus eliminating extensive "bake-out" times if pin hole clad leaks develop.

(9) The fuel element operates at characteristically low fuel temperatures with relatively high specific power due to its thorough

cooling by closely spaced internal steam tubes. This aspect suggests that the element could be used in high burn-up, longer lived fuel elements because of the low fission gas release and fuel volume changes that accompany low fuel temperatures.

(10) It has a relatively small value for the flooding coefficient of reactivity because the decreased disadvantage factor with flooding tends to compensate for the increased parasitic absorption of the added water and the attendant decrease in fast fission factor.

(11) It permits the construction of a smaller core for the same power, thus reducing the required size and cost of the pressure vessel, biological shield, containment building, and other auxiliary equipment and systems.

In addition to demonstrating, in the design phase of the ABC program, that these advantages exist in the multitube fuel element, the manufacturing development and fabrication of prototypes has demonstrated the feasibility and practicality of the fuel element manufacture for reactor service. Also, favorable results were obtained from mechanical loading and thermal cycle testing of the prototype fuel elements. If elements of this type can be shown to perform satisfactorily in a reactor, they will offer much greater promise than conventional rod type elements of producing power from an integral boiling-superheating reactor at an attractive price.

Of paramount importance in the work which remains to be done is the irradiation testing of prototype fuel elements in an operating reactor. Although all out-of-pile work conducted so far is highly

encouraging, it remains to be demonstrated that performance of the fuel element in a reactor environment will fulfill design expectations. Some success has been reported for irradiation tests performed on combination boiler-superheater fuel elements by other experiments (see Reference 62). However, this work was related to small (0.75 in. OD) annular fuel elements, and although all reported results are encouraging, the tests have not sufficiently simulated the conditions present in 1.5 in. diameter multitube fuel elements to be considered completely relevant to the ABC fuel element design.

The development of a three-dimensional computer code for rapid analysis of the core thermal performance would be of great advantage in the design of a large superheat reactor using the multitube fuel element.

The completion of the nuclear analysis of the Advanced BONUS Core requires the evaluation and detailed design of a first core that will burn to the equilibrium core in the most economical manner. The use of shims and/or enrichment zoning in the first core must be considered in such nuclear analysis.

The nature and cause of the brittle behavior of the Zircaloy-Inconel transition joints observed in the irradiation tests should be explored and resolved, not only because of the advantage of this joint in the multitube fuel element design for superheat service but also in view of the importance of Zircaloy transition joints to the reactor industry in general.

The detailed mechanical design of the standpipes and connectors for

the inlet and outlet steam lines of the ABC fuel assembly is still pending as well as the preparation of drawings and specifications for the procurement of the Advanced BONUS Core. Work remains to be done in establishing the welding procedures and techniques of fabrication for the structural and header parts of the ABC fuel assembly.

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