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GEAP-13765

AEC RESEARCH AND DEVELOPMENT REPORT
DECEMBER 1971

MASTER

DESIGN BASIS OF THE F9E GRID SPACED LMFBR DIAGNOSTIC ASSEMBLY FOR IRRADIATION TEST IN EBR-II

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U. S. ATOMIC ENERGY COMMISSION
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DESIGN BASIS OF THE F9E GRID SPACED
LMFBR DIAGNOSTIC ASSEMBLY
FOR
IRRADIATION TEST IN EBR-II

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Prepared for the
United States Atomic Energy Commission
Under Contract AT (04-3)-189
Project Agreement No. 10

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ABSTRACT

The F9E group is the first in-pile test of a grid spaced fuel assembly utilizing the straight-wave beam grid spacer concept. This diagnostic test is designed to produce conditions in the EBR-II assembly that simulate design limiting conditions in the LMFBR. Assembly design and design methods can be evaluated and improved based on a comparison of the predicted performance summarized in this report with the actual grid assembly performance observed during the interim and post-irradiation examination.

1. INTRODUCTION AND SUMMARY

1.1 BACKGROUND AND HISTORY

Proper spacing of fuel rods in the core of an operating LMFBR is necessary to avoid hot spots and damaging vibration which could shorten the life of the fuel rods. Separation of rods by wire wrap spacers has several shortcomings which stimulated the development of alternative spacer systems to achieve higher rod burnups.

The wave beam grid spacer concept, which has several unique advantages over other spacer systems, is being developed by General Electric as a part of Project Agreement 10. In the latter part of 1969 the first inpile wave beam spaced assembly F9E was approved in principle for irradiation in EBR-II. F9E is the first in-pile grid spaced fuel assembly utilizing the straight wave beam grid spacer concept conceived by General Electric. F9E is a "diagnostic" irradiation test of an LMFBR grid spaced assembly. "Diagnostic" is used in the sense that the experiment is designed to produce conditions in the EBR-II assembly that simulate design limiting conditions in the LMFBR. Assembly design methods can be evaluated and improved based on a comparison of predicted and actual grid assembly performance. The grid assembly has been designed to operate for 470 full power days to a total peak fluence of 8.8×10^{22} (n/cm²), which corresponds to a peak burnup of 100,000 MWd/Te. However, an interim examination of the subassembly will be conducted in order to determine the condition of eight of the fuel rods which have been designed to be removable from the assembly.

As a result of engineering analysis and tests on this and other GE programs, several modifications were made to the original design concept that represented important improvements on the original design concept. The modified F9E design has been fabricated and insertion in EBR-II is scheduled for early 1972.

This report is primarily the record of analytical predictions of the behavior to be expected of the modified grid system during its irradiation in EBR-II. The report also includes a description of the original concept and the modifications, and a discussion of the basis for the analytical predictions. In those instances where the design modifications did not negate analyses in support of the original concept, these results are referred to as part of the design basis of the F9E assembly.

1.2 WAVE BEAM GRID SPACER DESIGNS

The original design involved wave beams having a cross-section in the form shown in Figure 1-1(a). The improved wave beam design is illustrated in Figure 1-1(b). These beams are placed with their long dimensions horizontal to separate a set of vertical fuel rods and are combined into grid spacer assemblies of two tiers. The two tiers have an angle between them of 60 degrees in the horizontal plane, and the beams in the two tiers are projection-welded at all points of contact. Figure 1-2(a) shows how the beams are arranged in a "full" grid assembly, and Figure 1-2(b) shows the improved "staggered" grid assembly, which has every other beam removed.

The two-tier full grid assemblies are placed at 6-inch vertical spacing in the rod bundle and each successive assembly is oriented 120 degrees from its neighbor. The staggered grid assemblies are spaced at 3-inch intervals. The staggered grid beams are constructed so that two staggered grid assemblies provide the same fuel support as the full grids, but with the 3-inch axial displacement.

Figure 1-1. Wave Beam Cross Sections

Figure 1-2. Grid Assemblies

The stresses, strains and contact loads against the fuel rods were computed for the expected regimes of temperature, and fluence. All values appeared to be satisfactory. However, the constraint of the projection-weld produced a larger force against the fuel rod at the contact closest to the weld than was thought to be necessary. To reduce the contact loads, the original grid beam was modified by the addition of a 0.1-inch vertical lip to have a cross-section as shown in Figure 1-1(b). This lip is on the bottom of the upper tier and the top of the lower tier in each assembly spacer.

As a further means of reducing the contact forces against the fuel rods and to lower the grid pressure drop, the staggered grid system shown in Figure 1-2(b) was adopted. Analysis indicated that the staggered grid assembly provided better support when the orientation of successive grid assemblies was ± 180 degrees rather than 0, 120, 240 degrees as selected for the full grid. Hydraulic tests on the staggered and full grid assembly have established that the pressure drop across two staggered grids is less than that across one full grid assembly.

1.3 PURPOSE OF THE F9E EXPERIMENT

The F9E experiment is a 37-rod bundle to be irradiated in EBR-II to $\sim 100,000$ MWd/Te to test the concept of a wave beam grid spacer and to compare the performance of three different versions of this concept. The overall objective was to test a design which would provide engineering information in support of an LMFBR grid spaced fuel assembly design.

The three different designs are:

1. The full grid with the modified wave beam profile.
2. The staggered grid with the modified beam in both tiers.
3. The staggered grid with the original beam in the upper tier and the modified beam in the lower tier.

1.4 SUMMARY OF COMPARATIVE PREDICTIONS

Table 1-1 provides a comparison of the predicted behavior of different F9E wave beam grid spacer designs under different expected conditions of operation or reactor test. The FFTF staggered grid predictions are based on a 217-rod assembly while the LMFBR full grid predictions are based on a 169-rod assembly.

The grid spaced LMFBR assembly and the FFTF grid spaced driver assembly predictions are limiting hypothetical values based on a core location in FFTF which has a high flux gradient equivalent to the inner row of zone 2 and a high fluence equivalent to the center of the core. EBR-II beam-to-channel and rod-to-channel differential growth at end-of-life are below the levels expected in the FFTF test assemblies.

The table shows that the predicted bowing loads, rod stresses and grid stresses in the EBR-II are comparable to or exceed the values expected in an LMFBR and FFTF for irradiation of 50% or more of the design burnup. Although the predicted rod-to-grid differential swelling is considerably below LMFBR and FFTF values at 50% of life, because of the reduced irradiation creep of stainless steel in EBR-II,* contact loads and stresses are comparable to those found under LMFBR and FFTF design conditions.

F9E rod bowing induced grid stresses are predicted to be considerably higher than in the FFTF test assemblies. This is due to the lesser amount of creep expected in the EBR-II environment. The E.o.L. (End of Design Life) value of grid stress shown exceeds the design criteria at 10,700 hours. However, due to conservatism in the present analysis, the implications of this prediction will be further assessed prior to shortening the design life of the experiment.

1.5 COMMENTS ON CERTAIN ASSUMPTIONS USED IN THE ANALYSIS

The design basis analyses presented in this report were completed prior to the fabrication of F9E and before the final EBR-II environment for F9E had been determined. The grid structural analyses described in this report were performed assuming that the assembly would be placed in the EBR-II core operating at 62.5 MWt in position 6N2 or

*Solution annealed Type 316 stainless steel swells at a higher rate than cold worked Type-321 or -316; however, it has a lower creep rate. Therefore, the lower fluence/burnup ratio results in comparable swelling rates but reduced creep rates.

Table 1-1
COMPARISON OF LOADS, STRESSES AND DEFLECTIONS
EXPECTED IN F9E WITH FULL AND STAGGERED GRIDS IN FFTF

Material (Channel, Cladding and Grid) SS	F9E in EBR-II Full Grid		
	50%	70%	End of Life 316 Annealed
Beam-to-Channel Differential Growth (mils)	1	2	3
Rod Bowing			
Rod Bending Stress (psi)	10900	15300	23300
Contact Load (lb)	1.18	1.88	3.01
Grid Stress (psi)	20000	24950	39890
Rod-to-Grid Swelling			
ΔD (mils)	0.30	0.55	1.05
Contact Load, Elastic (lb)	0.29	0.53	1.02
Contact Load, Creep	0.21	0.34	0.52
Grid Stress, Elastic (psi)	3490	6450	12300
Grid Stress, Creep	2510	4050	6240
Rod-to-Channel Differential Growth			
Grid Bending Stress (psi)	1870	3230	5020

F9E in EBR-II Staggered Grid

50%	70%	End of Life 316 Annealed	FFTF ^(a) Staggered Grid 316 20% Cold Worked	LMFBR Full Grid in FFTF 321 Annealed
1	2	3	12	27
10900	15300	23300 (70% ult)	11800	15900 (36% ult)
1.18	1.88	3.01	0.35	0.48
20000	24950	39890	6350	5500
0.44	0.81	1.55	0.71	2.20
0.19	0.35	0.67	0.23	2.31
0.09	0.22	0.34	0.07	0.23
2340	4330	8240	3840	26000
1170	2680	4130	1036	2660
960	2030	3360	3840	15900

(a) Analysis based on WADCO swelling correlation and a hypothetical core location with a peak pellet burnup of 80,000 MWd/Te and a fluence gradient equivalent to the inner row of zone 2.

6N5 and have a design flow rate of 48 gallons per minute. We were advised while this report was in draft by EBR-II that F9E will be placed in position 6E3 (or equivalent) and have a design flow rate of 50 gallons per minute. The flow rate was increased 4% to compensate for possible changes in assembly flow due to changes in core loading. Failure to make this provision would risk possible removal of the assembly if the mixed mean outlet temperature exceeded 1010°F. The effect of this small change in flow on assembly temperatures is not considered to significantly influence the predicted assembly performance. We were later advised by EBR-II that the Radial blanket of the EBR-II core would be changed to a stainless steel reflector at some time near the beginning of life of this experiment. Based on this information it was decided to move F9E to position 7N1 at the time of installation of the stainless steel reflector. The assembly power is expected to remain at the value calculated for position 6E3 (within ~2%), and the design flow rate remains 50 gpm. However, softening of the neutron spectrum by the reflector is expected to decrease the fast ($E > 0.1$ Mev) neutron flux by ~28%. The effect of this decrease on test performance is to be evaluated. If local flux tailoring cannot be used to harden the neutron spectrum in the vicinity of this test, the magnitude of irradiation-induced steel swelling will be decreased by ~40% for a given burnup.

The structural analysis was done on the basis of an edge rod-to-channel wall spacing of 0.042 inch at beginning of life. The as-built dimensions of the fuel channel and grid spacer have resulted in an average edge rod-to-channel wall spacing of 0.0445 inch at beginning of life. This deviation will result in somewhat higher edge rod temperature gradients because of the greater edge bypass flow. These higher edge gradients will result in somewhat higher edge rod bowing loads. The corner rod temperature gradients and bowing loads will be decreased slightly. These changes will have no significant effect on the value of this assembly experiment.

It is planned to update the prediction of the F9E grid assembly performance in the EBR-II as improved methods of analysis and as new irradiation induced swelling and creep correlations become available for the as-built F9E configuration.

2. DESIGN BASIS ENVIRONMENT AND MATERIALS

2.1 ENVIRONMENTAL CHARACTERISTICS

The reactor environments upon which the test assembly design is based are the 6N2 and 6N5 positions in EBR-II. Figure 2-1 indicates the EBR-II core configuration and defines the nomenclature used for location of fuel assemblies within the core. It may be seen from Figure 2-1 that 12 equivalent test positions exist in the core (i.e., two positions exist in each of the six core sectors). These positions lie in the outer row of the core; row 7 contains radial reflector assemblies.

The location of the test assembly in the EBR-II core was chosen so that linear power densities in the test assembly would correspond to the peak without overpower or uncertainties in 1000 MWe cores.²⁻¹ The EBR-II power level was assumed to be 62.5 MW (i.e., the power level after the planned increase), and it was assumed that no change in the shape of the radial or axial power profiles will occur when the reactor power is increased. The power of the test assembly was determined using specific powers obtained from a "Guide for Irradiation Experiments in EBR-II" (reference 2-2); these specific powers are presented on Table 2-2 for a reactor power level of 50 MW. The assembly power obtained from these values was multiplied by 1.25 to account for the reactor power increase.

Figure 2-2 is a plot of the normalized linear power density profile in the EBR-II core. This curve is based on a core height of 13.5 inches. Figures 2-3 and 2-4 show the axial and radial variations of the EBR-II flux spectrum. All three figures were drawn directly from the "Guide for Irradiations Experiments in EBR-II."²⁻² Table 2-3 defines the energy groups used on Figures 2-3 and 2-4. Table 2-1 summarizes the nuclear characteristics expected at core positions 6N2 and 6N5 based on the data of Figures 2-3 and 2-4. All parameters are presented as nominal without uncertainties.

2.2 MATERIAL PROPERTIES

Material properties required for this study are enumerated in this section for reference. The coolant is sodium; the fuel cladding, grid assemblies and channel walls are fabricated from fully annealed Type-316 stainless steel. The annealed material is used to provide a duplication of wire wrapped fuel rods of comparable design which are currently under irradiation in EBR-II. Interim examination of these wire wrapped fuel rods will provide performance information at high burnup (75,000 MWd/Te) prior to insertion of the F9E experiment. The principle reason for the use of the annealed material, however, is the greater rate of irradiation induced swelling which will result in greater geometric distortion of the fuel cladding and grid structure when irradiated to the limited fluences achievable in EBR-II. The extent to which this is true is dependent on which swelling correlation is most physically realistic and to this end both a GE correlation and a PNL correlation (about 60% of the GE) have been used in the analysis.

Comprelli et al.²⁻³ and Moen²⁻⁴ provided much information for many of the mechanical properties of Type-316 stainless steel and for fluence above and below a value of 10^{22} n/cm². The materials in the F9E assembly operate at fluences above 10^{22} n/cm².

The ultimate strength and 0.2% offset yield strength are shown on Figure 2-5 as a function of temperature between 700°F and 1200°F which is representative of the temperature range that material in F9E will be subjected to. Young's modulus and the rigidity modulus are plotted on Figure 2-6, while Poisson's ratio is shown on Figure 2-7. Creep rupture stress as a function of time to rupture is shown on Figure 2-8 for temperatures of 800°F to 1200°F in 100°F increments for fluence greater than 10^{22} nvt.

The thermal properties, including specific heat, conductivity and the coefficient of thermal expansion for Type-316 stainless steel are shown on Figure 2-9.

Irradiation induced swelling can be calculated from the following GE correlation²⁻⁵ for Type-316 annealed stainless steel:

$$\frac{\Delta L}{L} = 1.05 \times 10^{-6} \left(\frac{\phi t}{10^{22}} \right)^{1.81} (T - 633.16)^{0.683} (1048.16 - T)^{0.577} \quad (2-1)$$

Table 2-1
DESIGN BASIS NUCLEAR ENVIRONMENT ASSUMED FOR F9E TEST ASSEMBLY
(REFERENCE 2-2)

Parameter	Value
Radial Position of Test Assembly in EBR-II	Row 6, positions 6N2 or 6N5
Axial Peak-to-Average Power Density	1.12
Average Total Flux Level at the Core Midplane ($n/cm^2 \cdot sec$)	2.01×10^{15}
Maximum Total Flux Level at the Core Midplane ($n/cm^2 \cdot sec$)	2.19×10^{15}
Minimum Total Flux Level at the Core Midplane ($n/cm^2 \cdot sec$)	1.83×10^{15}
Radial Flux Gradient at the Core Midplane ($n/cm^2 \cdot sec/in.$)	0.228×10^{15}
Flux greater than 0.11 MeV (%)	82

Table 2-2
SPECIFIC POWERS FOR EBR-II CORE POSITIONS 6N2 AND 6N5
(REFERENCE 2-2)

Isotope	Specific Power in Center of Assembly at a Reactor Power Level of 50 MW (Fissions/gm $\cdot$ sec)
U-235	0.574×10^{13}
U-238	0.320×10^{12}
Pu-239	0.675×10^{13}
Pu-240	0.211×10^{13}

Note: Conversion to units of watt/gm was accomplished using the factor $3.07 \times 10^{-11} \frac{\text{Watts}}{\text{fission}}$ (192 MeV/Fission).

Table 2-3
DEFINITIONS OF ENERGY GROUPS USED TO DESCRIBE EBR-II FLUX SPECTRUM
(REFERENCE 2-2)

Group Number	Lowest Energy in Group (MeV)
1	3.68
2	2.23
3	1.35
4	0.821
5	0.498
6	0.302
7	0.183
8	0.111
9	0.0674
10	0.0409
11	0.0248
12	0.0150
13	0.00912
14	0.00431
15	0.00261
16	0.00203
17	0.00123
18	0.000961
19	0.000583
20	0.000275
21	0.000101
22	0.000029

while the PNL correlation²⁻⁶

is

$$\frac{\Delta L}{L} = 1.6 \times 10^{-5} (\phi t)^{1.71} (10) \left[\frac{1.549 \times 10^4}{T} - \frac{5.988 \times 10^6}{T^2} \right] \quad (2-2)$$

where

L	Length	inches
ϕ	Flux (>0.1 MeV)	n/cm ² /sec
t	Time	seconds
T	Temperature	°K

Swelling for both of these equations is shown on Figure 2-10 as a function of temperature from 700°F to 1200°F for fluences of 0.1, 0.3, 0.5, 1.0 and 3.0 X 10²³ nvt. The PNL correlation is about 60% of the GE correlation at their respective peaks. A second important difference occurs in the slope of the swelling curves with temperature with the slope of the PNL curve being much lower. This results in a lower differential swelling if the material functions as the PNL curves would indicate. Thus, in both respects the PNL correlation results in much less distortion.

The irradiation and thermal induced creep for both GE and PNL²⁻⁷ can be computed by the following equation:

$$\epsilon = A \bar{\sigma} \left[1 - e^{-\frac{\phi t}{B}} \right] + c e^{(D-ET)} \bar{\sigma} \phi t + e^{-\frac{58870}{T}} \left[\sinh (1.79 \times 10^{-5} \bar{\sigma}) \right]^{8.23} e^{62.48 t} \quad (2-3)$$

where

$\frac{\epsilon}{\bar{\sigma}}$	Strain	inch/inch
$\bar{\sigma}$	Stress	psi
T	Temperature	°K
t	Time	seconds
ϕ	Flux (total)	n/cm ² /sec
	General Electric	PNL
A	2.0 X 10 ⁻⁹	1.8 X 10 ⁻⁹
B	1.0	14.65 X 10 ²⁰
C	1.87 X 10 ⁻³⁰	1.87 X 10 ⁻³⁰
D	1.405	1.405
E	0.0027	0.0027

The first two terms in Equation 2-3 describe the irradiation creep while the last term accounts for the thermal creep. This equation is connected to strain rate by differentiation and plotted on Figure 2-11 as a function of inverse temperature for a flux of 6 X 10¹⁵ n/cm²/sec for stress levels from 10,000 to 50,000 psi. The curves are the sum of two exponentials; the left one being the result of the thermal creep occurring at the higher temperatures and the right side from the irradiation creep at the lower temperatures.

A summary evaluation of corrosion of Type-316 stainless steel in sodium has been prepared by Wozadlo & Spalaris²⁻⁸ and a conservative estimate of corrosion rate versus temperature is shown on Figure 2-12. In the temperature range of interest, the corrosion rate does not exceed 0.65 mils per year.

The thermal properties of sodium have been taken from the "SNAP Technology Handbook."²⁻⁹ These include density, dynamic viscosity, thermal conductivity and specific heat; these properties are shown on Figure 2-13 as a function of temperature.

NOTE:

SUBASSEMBLY LOCATIONS ARE IDENTIFIED BY ROW (1 THROUGH 7), SECTOR (A THROUGH F), AND POSITION IN THE ROW (COUNTING CLOCKWISE FROM THE EDGE OF THE ROW). FOR EXAMPLE, POSITIONS 7D1, 6D2 AND 6D5 ARE IDENTIFIED ON THE FIGURE.

Figure 2-1. EBR-II Core Arrangement (Reference 2-2)

x/L	LOCAL/AVERAGE FISSION RATE
0	0.875
0.05	0.917
0.10	0.960
0.15	0.997
0.20	1.035
0.25	1.065
0.30	1.086
0.35	1.107
0.40	1.117
0.45	1.120
0.50	1.114
0.55	1.105
0.60	1.086
0.65	1.062
0.70	1.034
0.75	1.001
0.80	0.963
0.85	0.926
0.90	0.883
0.95	0.853
1.00	0.841

Figure 2-2. Normalized Linear Power Density in EBR-II versus Distance Above Bottom of Fuel Region (Reference 2-2)

Figure 2-3. Neutron Flux Spectrum in EBR-II versus Axial Position (Reference 2-2)

Figure 2-4. Neutron Flux Spectrum in EBR-II versus Radial Position (Reference 2-2) Locations of Core Rows

Figure 2-5. Tensile Properties Above Fluence of 10^{22} nvt Type-316 Stainless Steel

Figure 2-6. Modulus Type-316 Stainless Steel

Figure 2-7. Type-316 Stainless Steel—Poisson's Ratio

Figure 2-8. Creep Rupture Properties of Type-316 Stainless Steel

Figure 2-9. Thermal Properties of Type-316 Stainless Steel

Figure 2-10. Swelling of Type-316 Stainless Steel, Annealed

Figure 2-11. Irradiation and Thermal Creep Rate, Type-316 Stainless Steel

Figure 2-12. Corrosion Rate in Flowing Sodium, for Type-316 Stainless Steel

Figure 2-13. Liquid Sodium Properties

3. EXPERIMENT DESCRIPTION

3.1 DESCRIPTION OF THE ORIGINAL F9E TEST ASSEMBLY DESIGN (SEVEN UNMODIFIED FULL GRIDS)

The test assembly contains 37 fuel rods with an outside diameter of 0.250 inch, and a pitch-to-diameter ratio equal to 1.28. This results in a pitch of 0.320 inch, which is representative of early LMFBR Demonstration Plant designs. An inner channel was included in the design to set the size of the fuel rod-to-wall spacing so that lateral thermal gradients across outer rods would be minimized. The determination of this spacing (0.042 inch) is discussed in Section 9.1. Table 3-1 summarizes the nominal test assembly design parameters. The isotopic composition of the fuel also appears in Table 3-1.

The mechanical design of the original F9E assembly grid spacer concept is shown on the following figures: Figure 3-1 (original F9E fuel rod), Figure 3-2 (original grid spacer), and Figure 3-3 (original individual grid beam).

The overall assembly consists of a lower nosepiece which acts as a hydraulic holddown. This is followed by an orifice plate located below the lower shield which, in turn, supports the lower grid. The first grid spacer is located 1.5 inches above the lower grid plate and on six inch intervals for the length of the fuel rods. The top end fixture is above the rods and is connected to the lower shield through the outer channel.

A typical original grid spacer is shown in Figure 3-2. The directions of the retaining tabs and wave beams are shown. Dimples are shown on the outer beams to provide lateral restraint from the inner channel to the grid. Seven grid spacers are used to support the fuel rods on six inch centers. Each grid spacer is rotated 60 degrees with the result that each pair of grid spacers provides complete hexagonal support for the fuel rod. Inspection is facilitated by two removable windows located on the inner channel at locations in the high temperature and high power zones. The actual disassembly technique involves the removal of the top end fixture and outer channel which makes the eight removable fuel rods and two removable windows accessible.

A typical non-removable fuel rod is shown on Figure 3-1. The rod consists of a 0.250-inch diameter rod with a 0.015-inch thick wall and an upper and lower end plug. The fuel loading consists of a 2.5-inch lower blanket of natural UO_2 , a 13.5-inch fueled section consisting of PuO_2 and fully enriched UO_2 and a 1-inch natural UO_2 upper blanket. Above this is a hold spring and spacer. Twenty-nine of the rods are fabricated to this design. In the remaining eight rods a full slot is cut in the lower end plug so that it can be removed from the support grid by pulling the rod axially. The upper end plug is modified to provide a gripping shoulder for the removal tool.

The original beam detail is shown on Figure 3-3. The grid spacer assembly is fabricated by a projection welding technique that results in a weld between the upper and lower set of wave beams. The welding process results in a weld setdown of 0.030 inch as shown on Figure 3-2. These welds hold the assembly together for insertion into the inner channel. The welds also provide a pivot point between wave beams to allow partial collapse of the assembly during the insertion of the grid assembly. Once the grid is inserted into its proper location, the wave beams are straightened up and the locking tabs set in their recesses.

When the rods are loaded into the grid the wave beam tabs are positively secured. Once this has occurred, the welds are no longer required for structural integrity. On this basis, weld failure is not expected to be of concern except as it might provide the origin for crack propagation.

The interaction between the grid spacer assemblies and the channels occurs at the spacer locking tabs. These must be sufficiently strong to withstand shear due to axial loading on the grid spacer assembly. There also exists a differential swelling between the wave beam and the channel. As will be seen, this requires a differential growth capability of 0.0015 inch, which is within the assembly tolerances and, thus, does not result in grid loading. Further tab distortion is not expected during operation of the test assembly.

A photograph showing the lower nosepiece, lower shield orifice plate and support grid appears as Figure 3-4. The original F9E design concept assembly hardware is in the foreground and the flow test assembly is in the background. The F9E design was subsequently revised as described in Section 3.2. A flow test on the original design was completed and confirmed grid spacer pressure drop correlations. The flow test lower shield and support grid are shown on Figure 3-5 with an assembly of fuel rods in place; an exposed grid spacer and a section of inner channel over the rods also

Table 3-1
SUMMARY DESCRIPTION OF ORIGINAL FULL GRID TEST ASSEMBLY AND FUEL DESIGN

Parameter	Value
Outer Channel Inside Distance Across Flats (inch)	2.210
Gap Between Outer Channel I.D. and Inner Channel O.D. (inch)	0.068
Wall Thickness of Inner Channel (inch)	0.040
I.D. of Inner Channel Across Flats (inch)	1.997
Rod-to-Inner Channel Spacing (inch)	0.042
Pitch-to-Diameter Ratio (inch)	1.28
Cladding O.D. (inch)	0.250
Cladding I.D. (inch)	0.220
Number of Rods in Assembly	37
Length of Fuel Region (inch)	13.5
Length of Lower Blanket (inch)	2.5
Length of Upper Axial Blanket (inch)	1.0
Total Fuel Rod Length (inch)	39.656
Total Fuel Assembly Length (inch)	91.875
Flow Area of Central Cell (inch ²)	0.0396
Hydraulic Diameter of Central Cell (inch)	0.202
Total Fuel Assembly Flow Area (inch ²)	1.639
Axial Spacing of Grid Spacers (inch)	6
Total Spacer Beam Height (inch)	0.500
Effective Spacer Beam Height (inch)	0.350
Spacer Beam Thickness (inch)	0.012
Apparent Beam Thickness (inch)	0.022

Note: Dimensions of liner and channel noted here are nominal values; actual sizes depend on availability of EBR-II stock items.

Fuel Parameters

Initial Fuel Composition (gm per rod):

Pu-239	16.67
Pu-240	1.47
Pu-241	0
U-235	49.91
U-238	4.94

Total (gm)	72.99
------------	-------

Cladding Material	316 SS, annealed
O/M Ratio of Fuel	1.98 $\pm$ 0.010
Fuel Form	Pellets
Method of Powder Preparation	Physically mixed
Fabrication Process	Cold press and sinter
Fuel Pellet Density (gm/cc)	10.34
Smeared Density (gm/cc)	9.89
U-235 Enrichment (%)	91

Blanket and Insulator Properties

Material	UO ₂
Enrichment (U-235)	Natural or depleted
Form	Pellets
Fabrication Process	Cold press and sinter
O/M Ratio	2.000 $\pm$ 0.003
Density (% T.D.)	92 $\pm$ 96.0
Diameter (inch)	0.215

1. CLEAN AND PASSIVATE ALL PARTS PER SPEC. 22A1807.
2. PRIOR TO WELDING BOTTOM END PLUG TO CLAD, INSPECT CLAD SURFACE FOR NICKS AND SCRATCHES PER SPECIAL TASK F INSTRUCTIONS. NICKS AND SCRATCHES SHALL NOT EXCEED .0015" IN DEPTH.
3. WELD ACCEPTANCE CRITERIA PER FABRICATION SPEC. 22A1832 AS DETERMINED BY X-RAYS @ 0°, 60° & 120°.
4. END PLUG TO CLAD WELD OVERHEAD TO BE SMOOTH AND NOT TO EXCEED .251 INCHES IN DIAMETER.
5. ALL VOIDS TO BE FILLED WITH REACTOR GRADE HELIUM TO 1 ATMOSPHERE PRESSURE. LEAK RATE ON END PLUG TO CLAD WELD TO BE LESS THAN 2×10^{-9} CC/SEC. STD.
6. NATURAL OR DEPLETED UO₂ PELLETS OR POWDER.
7. UO₂ - 25% PuO₂ PER FABRICATION SPEC. 22A1832.
8. FUEL COLUMN TO CONTAIN THREE 1" SECTIONS AT TOP, MIDDLE, & BOTTOM WITH PELLETS OF EQUAL O.D. $\pm$.0005 PER FABRICATION SPEC. 22A1832.
9. CUT TUBE TO LENGTH @ ASSEMBLY TO PROVIDE SPACE LENGTH FOR PARTS 1 & 2.
10. FUEL PIN TO BE STRAIGHT WITHIN .010 INCHES PER FOOT AND .030 TOTAL.
11. FUEL PIN CLAD MATERIAL AND FUEL PIN IDENTIFICATION TO BE DESIGNATED IN FABRICATION SPEC. 22A1832.
12. FABRICATE & ASSEMBLE TO SPEC. 22A1832.
13. TUBE MATERIAL TYPE TO BE DESIGNATE IN FABRICATION SPEC. 22A1832.
14. PRE & POST SAMPLE WELDS TO BE MADE @ ASSEMBLY PER DWG. NO. 145C6689.

FCF 793E456

DESCRIPTION OF GROUPS		REVISIONS		PRINTS TO
3	15 FEB 1970 1970 CHGR DELIN. P5 666	1	C 2 202 GENERAL CHANGES REQ.	
		2	2 202 ADDED NOTES 13 F 18 P3. CHGD R65 DEX. REQ.	
MADE BY S. H. F. FEB. 17, 1970 FEB 20, 1970	APPROVED BY B. R. D. FEB 20, 1970	ENV OR BY DATE	LOC. OR LOCATION	115 D7884 SH NO.

Figure 3-1. Original Fuel Pin (F9E)

FCF - 793E455
793E456
FOR DESIGN DATA SEE 325X152
325X153

NOTE
1. ELECTROLYZE .0005-.0010 ALL OVER
PER G.E. SPEC P16B-YPI

- G1 .012 THK BEAM
- G2 .010 THK BEAM
- G3 .008 THK BEAM

REVISIONS	PRINTS TO
1 C. Williams 1/10/70 ADDED (FULL) TO TITLE	

MADE BY 12 JUNE 70 BRDQ
Checked by 1/10/70 SUNNYVALE
145C6717

Figure 3-2. Original Grid Spacer

PART	'A' DIM.	'D' DIM.	MATERIAL
1	1.178	.037	.0120 ±.0005
2	1.498	OMIT	THK 316 SST
3	1.818	OMIT	ANNEALED
4	2.138	OMIT	
5	1.178	.037	.0080 ±.0005
6	1.498	OMIT	THK 316 SST
7	1.818	OMIT	ANNEALED
8	2.138	OMIT	
9	1.178	.037	.0080 ±.0005
10	1.498	OMIT	THK 316 SST
11	1.818	OMIT	ANNEALED
12	2.138	OMIT	

FCF 1456717
FOR DESIGN DATA SEE 325X152
325X153

SECTION B-B

SECTION C-C
PI, 5, & 9 ONLY

REVISIONS	PRINTS TO
1. 350 WAS .400 .175 WAS .200 5° 30' WAS 5° REF	

DATE: 12 JUNE 70	BY: BRD	1456717
APPROVED: 11/1/70	DEPT: SUNNYVALE	

Figure 3-3. Original Beam

Figure 3-4. F9E In-Pile and Flow Test Lower Nosepiece, Orifice Plate, Lower Shield and Grid Support

Figure 3-5. Flow Test Assembly with Fuel Rods Mounted on Grid Support and Typical Grid Spacer and Channel Section

appear on Figure 3-5. The grid spacers are shown in about the position of the second spacer in the assembly, 7.5 inches from the bottom of the rods. Figure 3-6 shows the fuel rods as they are attached to the support grid. This support functions as a simple support point. The complete original grid assembly design is shown in Figures 3-7 and 3-8. In Figure 3-7, the locking tabs are shown in the bent position. The dimples which hold the outer wave beams out from the channel wall are also shown. The bent locking tabs also appear in Figure 3-8. Each successive grid assembly is turned 60 degrees from the position shown in Figure 3-8. This provides a complete hexagonal restraint of the fuel rods for every two grid assemblies. The original grid assembly design concept is shown in a dummy channel in Figure 3-9 with the locking tabs in position in the channel pockets (notches).

3.2 DESCRIPTION OF THE REVISED F9E TEST ASSEMBLY DESIGN

The final revised overall F9E in-reactor test bundle design drawing is shown in Figure 3-10, and the revised inner channel design drawing is shown in Figure 3-11. Assembly components such as the lower nosepiece, lower shield orificing plate, and support grids are basically unchanged from those exhibited in Figure 3-4. The removable fuel rod design (8 removable rods in F9E) is shown in Figure 3-12. The fixed rod design is unchanged; the design is shown on Figure 3-1. The nominal test assembly design parameters which were summarized in Table 3-1 still apply to the modified test assembly except for changes in several grid spacer axial spacings from 6 inches to the 3-inch spacings shown in Figure 3-13, the design drawing of the modified F9E inner channel assembly, and in Figure 3-14, a schematic drawing of the test assembly grid locations. The removable window in the inner channel of the original design has been deleted from the revised design.

The revised F9E test assembly will contain three grid types of different design than the single original grid type. This original grid type, the straight wave beam full grid, will not be used in the revised F9E test assembly.

Table 3-1a summarizes the normal test assembly design parameters. The fuel and blanket design parameters remain the same as shown in Table 3-1.

Table 3-1a
SUMMARY DESCRIPTION OF REVISED F9E TEST ASSEMBLY GEOMETRY

Parameter	Nominal Value
Fuel Assembly Outside Distance Across Flats (inch)	2.290
Gap Between Outer Channel I.D. and Inner O.D. (inch)	0.062
Wall Thickness of Outer Channel (inch)	0.040
Wall Thickness of Inner Channel (inch)	0.040 ^(a)
I.D. of Liner Across Flats (inch)	2.006 ^(b)
Rod-to-Liner Spacing (inch)	0.042
Pitch to Diameter Ratio (inch)	1.28
Cladding O.D. (inch)	0.250
Cladding I.D. (inch)	0.220
Number of Rods in Assembly	37
Length of Fuel Region (inches)	13.5
Length of Lower Blanket (inches)	2.5
Length of Upper Axial Blanket (inch)	1.0
Total Fuel Rod Length (inches)	39.594
Total Assembly Length (inches)	91.812
Axial Spacing of Full Grid Spacers (inches)	6
Axial Spacing of Staggered Grid Spacers (inches)	3
Total Spacer Beam Height (inch)	0.640
Effective Spacer Beam Height (inch)	0.350
Spacer Beam Thickness (inch)	0.012
Apparent Beam Thickness (inch)	0.027

^(a) 0.035 As-Built, Channel Received was out of Spec.

^(b) 2.001 As-Built, Channel Received was out of Spec.

Figure 3-6. F9E Flow Test with Fuel Rods Mounted on Grid Support

Figure 3-7. Original F9E Full Grid Spacer

Figure 3-8. Original F9E Full Grid Spacer End View

Figure 3-9. Original F9E Full Grid Spacer Mounted in Channel with Tabs in Channel Notches

1. CLEAN AND PASSIVATE ALL PARTS PER SPEC. 22A1807.
2. PRIOR TO WELDING BOTTOM END PLUG TO CLAD, INSPECT CLAD SURFACE FOR NICKS AND SCRATCHES PER SPECIAL TASK F INSTRUCTIONS. NICKS AND SCRATCHES SHALL NOT EXCEED .0015" IN DEPTH.
3. WELD ACCEPTANCE CRITERIA PER FABRICATION SPEC. 22A1832 AS DETERMINED BY X-RAYS @ 0°, 60° & 120°.
4. END PLUG TO CLAD WELD OVERBEAD TO BE SMOOTH AND NOT TO EXCEED .251 INCHES IN DIAMETER.
5. ALL VOIDS TO BE FILLED WITH REACTOR GRADE HELIUM TO 1 ATMOSPHERE PRESSURE. LEAK RATE ON END PLUG TO CLAD WELD TO BE LESS THAN 2×10^{-9} CC/SEC. STD.
6. NATURAL OR DEPLETED UO_2 PELLETS OR POWDER.
 $UO_2 + 25\% PuO_2$ PER FABRICATION SPEC. 22A1832.
8. FUEL COLUMN TO CONTAIN THREE 1" SECTIONS AT TOP, MIDDLE, & BOTTOM WITH PELLETS OF EQUAL O.D. $\pm .0005$ PER FABRICATION SPEC. 22A1832.
9. CUT TUBE TO LENGTH $\pm$ ASSEMBLY TO PROVIDE SPACE LENGTH FOR PARTS 1 & 2.
10. FUEL PIN TO BE STRAIGHT WITHIN .010 INCHES PER FOOT AND .030 TOTAL.
11. FUEL PIN CLAD MATERIAL AND FUEL PIN IDENTIFICATION TO BE DESIGNATED IN FABRICATION SPEC. 22A1832.
12. FABRICATE & ASSEMBLE TO SPEC. 22A1832.
13. TUBE MATERIAL TYPE TO BE DESIGNATE IN FABRICATION SPEC. 22A1832.
14. PRE & POST SAMPLE WELDS TO BE MADE $\pm$ ASSEMBLY PER Dwg. NO. 145C6689.

Top View Dimensions:

- Total length: $39 \frac{19}{32}$ REF.
- End plug P6: 5.406 REF.
- Clad section 3: $2 \frac{1}{2} + \frac{1}{32}$ SEE NOTE 6
- Central section: $13 \frac{1}{2} + \frac{1}{32}$ SEE NOTE 7
- Section between welds: $1 - \frac{1}{4}$ SEE NOTE 8
- Weld area: $4 - \frac{1}{16}$ SEE NOTE 6
- Other sections: $1 - \frac{1}{4}$, $1 - \frac{1}{4}$, $5 \frac{1}{4}$, $1 - \frac{1}{4}$

Callouts and Notes:

- LOAD FROM THIS END TOP
- ELECTRO-ETCHED NUMBER ON CLAD AT TOP (REF.)
- STAMP IDENTITY NUMBER ON FLAT OF BOTTOM END PLUG WITH $\frac{3}{32}$ HIGH CHARACTERS (SEE NOTE 11)
- HELIARC WELD AFTER FUEL LOADING (SEE NOTE 5) FINISH WELD AT Q.D. OF TUBE (SEE NOTE 4) TYPICAL BOTH ENDS
- SCRIBE MARK .005 MAX WIDE X .001 MAX DEEP X APPROX. LONG LINE ON END OF CLAD, P3, AFTER WELDING TO BOTTOM END PLUG, P6. ORIENTATION MARK TO BE 90° WITH RESPECT TO SLOT "D" OF THE BOTTOM END PLUG WITHIN 5° AS SHOWN (SCRIBE MARK TO BE 0° ORIENTATION REFERENCE FOR X-RAYS, PROFILOMETRY, NEUTRON RADIOGRAPHY)
- POSITION TOP END PLUG, P5, INTO CLAD, P3, PRIOR TO FUEL LOADING AND SCRIBE LINE ON TOP END PLUG IN LINE WITH SCRIBE MARK AT TOP END OF CLAD WITHIN 2° AS SHOWN. (SCRIBE LINE TO BE 0° ORIENTATION REFERENCE FOR X-RAYS, PROFILOMETRY, & NEUTRON RADIOGRAPHY)
- NOTE ORIENTATION OF TOP END PLUG IN RELATION TO SCRIBE LINE & SLOT IN BOTTOM END PLUG
- (G) SEE NOTES 1, 2 & 10

DESCRIPTION OF GROUPS	REVISIONS

[illegible]

FCF-816E113

Figure 3-13. Inner Channel Assembly

REVISED F9E GRIDS

Three different grid spacer designs are included in this test. A schematic comparison of the three concepts is shown in Figure 3-15 and a drawing of a typical modified straight wave beam full grid spacer assembly (F) is shown in Figures 3-16 and 3-17. Each grid spacer assembly consists of two sets of parallel wave beams. The top set of wave beams forms a 60 degree angle with the lower set of beams. The resulting grids support the fuel rod on four sides.

The modified straight wave beam staggered grid(s), as shown in Figure 3-18, is the same as the modified straight wave beam full grid (F), but with every other beam removed. A pair of these assemblies is required to support each fuel rod on four sides. Successive staggered grid spacer assemblies are spaced at three inch intervals in the assembly compared with the six inch spacing between full grid assemblies. A plan view of two successive staggered spacer grids appears as a single full grid as shown schematically in Figure 3-19. The darkened beams are in the staggered grid assembly in the plane of the paper and the light beams are in the staggered grid three inches below the plane of the paper.

In order to assess the effectiveness of the modified straight wave beam design, a third grid spacer design will be incorporated into the F9E. This design is a combination of an unmodified straight wave beam full grid (R) on the top and a modified straight wave beam full grid on the bottom (F). See Figure 3-20A. This is referred to as a combination modified-unmodified straight wave beam full grid (R/F).

The grid spacer assembly is fabricated using a projection welding technique that produces a weld at each intersection of upper and lower sets of wave beams. These welds hold the grid assembly together for insertion into the inner hexagonal channel of the test assembly. When the grid is inserted into the channel, the tabs at the ends of each wave beam lock into notches in the channel wall. The wave beam alignment, when the fuel rods are in place in the grid, is shown on Figure 3-20B.

The three wave beam grid concepts are positioned in the inner hexagonal channel as shown in Figure 3-14. The plan provides for testing one set of each type of grid spacer in the core region of the subassembly. In total, three staggered grid spacer assemblies, five full grid spacer assemblies and one combination assembly are being tested.

No differences are encountered in the modified grids with respect to grid spacer assembly fabrication and the basic method of grid insertion into the inner channel. Spacer locking tabs are unchanged in the modified grids. Changes in grid loads, grid structural analysis, and grid structural evaluation due to the modified grid designs are discussed in subsequent sections.

3.3 PREDICTED OPERATING CHARACTERISTICS OF THE ORIGINAL F9E DESIGN

The predicted operating characteristics of the test assembly are described in Table 3-2. These characteristics apply to the test assembly operating in either the 6N2 or the 6N5 position in EBR-II. These predictions are based upon the environmental and materials data presented in Section 2 and on the geometrical data of Table 3-1. In addition, the fuel assembly coolant flow rate was chosen to produce a coolant temperature rise of 300°F across the core; this temperature rise is typical of LMFBR designs. The mixed mean coolant outlet temperature (1000°F) is within 100°F of the average of the outlet temperatures of assemblies surrounding positions 6N2 and 6N5 (937°F). The outlet temperatures of surrounding fuel assemblies would be larger than those given in Reference 2-1 due to the increase in reactor power. This increases the average surrounding outlet temperature from 894°F to 937°F.

The predicted hot channel coolant outlet temperature is 1135°F as shown on Table 3-2. This includes a statistical treatment of uncertainties; the uncertainty factors used in the calculation are shown on Table 3-3.

The coolant temperatures and thermal gradients shown in Table 3-2 are products of the thermal-hydraulic analysis which is described in Section 9. Two types of fuel rods are mentioned in Table 3-2; these are the "corner rod" and the "edge rod." Both rods are located adjacent to the hottest flat edge of the hexagonal channel. The corner rod lies at the corner of the hexagon and the edge rod lies at the center of the flat side. These locations are illustrated on Figure 3-21. Figure 3-21 indicates the variation with axial position of thermal gradients acting on edge and corner rods.

Table 3-2
PREDICTED OPERATING CHARACTERISTICS OF ORIGINAL F9E TEST ASSEMBLY IN EBR-II

Parameter	Value
Coolant Inlet Temperature (°F)	700
Mixed Mean Coolant Outlet Temperature (°F)	1,000
Hot Channel Coolant Temperature at Top of Fuel Region (including uncertainties) (°F)	1135
Maximum Midwall Clad Temperature (including uncertainties) (°F)	1180
Average Outlet Temperature of Surrounding Assemblies (°F)	937
Maximum Mid-Clad Temperature Averaged Over 60° of Arc (°F)	1,090
Thermal Bowing Gradient Across an Average Central Rod (Top of Fuel Region) (°F)	12
Maximum Thermal Bowing Gradient Across an Edge Rod (°F)	39
Maximum Thermal Bowing Gradient Across a Corner Rod (°F)	40
Maximum Local Circumferential Thermal Gradient on an Edge Rod (°F)	44
Maximum Local Circumferential Thermal Gradient on a Corner Rod (°F/60° Arc)	72
Fuel Assembly Coolant Flow Rate (Inside Liner) (lb/h)	19,200
Coolant Velocity in Central Flow Cell (ft/sec)	10.0
Fuel Assembly Mass Flux Inside Liner (lb/h · ft ²)	1.78 X 10 ⁶
Film Heat Transfer Coefficient at 1000°F for a Central Flow Cell (Btu/h · ft ² · °F)	32,160
Maximum Heat Flux (Btu/h · ft ²)	7.83 X 10 ⁵
Average Heat Flux (Btu/h · ft ²)	7.15 X 10 ⁵
Maximum Linear Power of Hottest Rod (kW/ft)	15.0
Maximum Linear Power of Average Rod (kW/ft)	13.7
Maximum Linear Power of Coldest Rod (kW/ft)	12.4
Total Fuel Assembly Power (kW)	511
Fuel Assembly Pressure Drop (psi)	8
Time at Full Power to Achieve 100,000 MWd/T Burnup on Lead Rod (days)	470
Atomic Percent Burnup at 100,000 MWd/T (%)	14.3
Fluence in Lead Rod at 100,000 MWd/T	
Total (n/cm ²)	8.9 X 10 ²²
(> 0.1 MeV) (n/cm ²)	(7.3 X 10 ²²)

Table 3-3
UNCERTAINTY FACTORS FOR ORIGINAL F9E TEMPERATURES

Parameter	Coolant Axial ΔT	Film ΔT	Cladding ΔT	Gap ΔT	Fuel ΔT
Neutron and Gamma Flux	0.07	0.10	0.10	0.10	0.10
Assembly Flow Rate	0.05				
Flow Profile Within Assembly	0.05				
Cladding Dimensions			0.01		
Gap Dimensions				0.02	
Fuel Dimensions		0.01	0.01	0.01	0.01
Fissile Concentration	0.01	0.01	0.01	0.01	0.01
Cladding Thermal Conductivity			0.10		
Gap Conductance				0.30	
Fuel Thermal Conductivity					0.15
Film Heat Transfer Coefficient		0.20			
Reactor Power Level	0.02	0.02	0.02	0.02	0.02
Transient Overload	0.05	0.05	0.05	0.05	0.05

Local Temperature Increase due to
Clad-Spacer Contact

$$\frac{\text{Local Coolant Temperature at Spacer}}{\text{Local Coolant Temperature without Spacer}} = 1.04$$

*TAB TO NOTCH CLEARANCE EXISTS IN BEAMS OF THE TOP LAYER

Figure 3-14. Original Fuel Rod Support Model Compared with Revised F9E Grid Design

REVISED SUPPORTS

(F) MODIFIED STRAIGHT WAVE BEAM FULL GRID

(R/F) COMBINATION STRAIGHT WAVE BEAM FULL GRID

(S) MODIFIED STRAIGHT WAVE BEAM STAGGERED GRID

(OF) ORIGINAL STRAIGHT WAVE BEAM FULL GRID

Figure 3-15

Schematic Comparison of Grid Concepts

FCF-188F475 (G1)
816E140 (G1, G2, G3)
FOR DESIGN DATA SEE 325X157
158

G1 G2 G3

REVISIONS		PRINTS TO	
30E-1	1/13/71	1	1/13/71
TITLE WAS, MODIFIED STRAIGHT WAVE BEAM, FULL GRID		TITLE WAS, GRID SPACER. CALLOUT: ALL WELDS ---- SIMUL-TANEOUSLY, WAS, (42 TOTAL)	
4, Y.B.E. NAME		P.L. REVISED NAME COLUMN, WAS, REAM. BBR	
Added G2, G3, P5-P8 P1-P4 DELETED QTY 4, FROM P.L.		2 1/13/71	
SK		1.250 WAS 1.240 .030 WAS .040 BBR	
MADE BY C. Williams JAN 8 1971		BRD	
P.B. Brawfield Jan 15, 1971		SUNNYVALE	
		145C6773	
		LOCATION CONT ON SHEET SH NO.	

Figure 3-16. Modified Straight Wave Beam, Full Grid

PART	"A" DIM	"D" DIM	MATERIAL	"E" DIM	"F" DIM	"G"	"E" DIM AS-BUILT
1	1.178	.040	.0120 ± .0005	1.302	.040 x 45°	67° 30'	
2	1.498	OMIT	THK 316 SST ANNEALED	1.622	OMIT		
3	1.818	OMIT		1.942	OMIT		
4	2.138	OMIT		2.262	OMIT		
5	1.178	.040		1.319	.040 x 45°	61°	1.314
6	1.498	OMIT		1.639	OMIT		1.634
7	1.818	OMIT		1.959	OMIT		1.954
8	2.138	OMIT		2.279	OMIT		2.274

FCF-145C6773
-145C6774
-145C6775
FOR DESIGN DATA SEE 325X157
- 325X158

SECTION B-B

SECTION C-C
PI ONLY

REVISIONS				PRINTS TO	
3	CHAMBERS	9/10	1	WILLIAMS	9/10
TITLE WAS "MODIFIED STRAIGHT WAVE BEAM"				GENERAL REVISIONS	
4	DE	11/11	2	BELMONT	11/11
ADDED PS - PB				E DIMS WERE	
"G" WAS 67° 30' ± 30'				1.264, 1.584	
± .005 WAS ± .000 - .010				1.904, 2.224	
ADDED "E" DIM AS BUILT				SK	
MADE BY C. Williams JAN 7 1971				B E D	
J. J. Murrell Jan 15, 1971				SUNNYVALE	
				145C6607	

Figure 3-17. Modified Straight Wave Beam

G1 G2 G3

FCF - 188 F 475 (G1 & G2)
816 E 140 (G1, G2 & G3)
FOR DESIGN DATA SEE 325 X 157
325 X 158

REF
G1 & G2 (BOTTOM BEAMS P5-P8)
G3 (BOTTOM BEAMS P1-P4)

* TYP
ALL WELDS TO BE
MADE SIMULTANEOUSLY
(12 TOTAL)

.030 WELD SET DOWN

REVISIONS		PRINTS TO	
3	DEL. 1/15/71	1	DEL. 1/15/71
TITLE WAS, MODIFIED STRAIGHT WAVE BEAM-STAGGERED GRID		TITLE WAS, GRID SPACER ALLOCATE. P/L, REVERSED NAME COLUMN, W/S, BEA, RI.	
4	ADD. 3/11/71	2	ADD. 3/11/71
ADDED G2 & G3 P5-P8, P1-P4 DELETED QTY. 2 FROM G1		1.250 WAS 1.240 .030 WAS .040	
MADE BY S. Williams JAN 8 1971		BRD	
1/15/71		SUNNYVALE	
		145C6774	

Figure 3-18. Modified Straight Wave Beam, Staggered Grid

Figure 3-19. Schematic Drawing of Two Layers of Staggered Grids

Maximum values drawn from this figure appear on Table 3-2. Two types of thermal gradients have been distinguished; these are "local" gradients and "bowing" gradients. The local thermal gradient is computed as the maximum change in cladding temperature over 60 degrees of rod circumference; the bowing gradient is the change in temperature over 180 degrees of rod circumference.

Figure 3-22 shows the predicted axial variation of coolant temperature for two locations within the fuel assembly; one location is the coolant channel between the edge rod and the inner channel, and the other lies between the corner rod and the inner channel. Figure 3-23 is a plot of the radial variation of coolant temperature across the assembly flats.

The heat transfer coefficient shown in Table 3-2 was obtained from data supplied by Bender and Magee.³⁻¹ The time required to reach 100,000 MWd/Te burnup that is shown in the table is an approximation that neglects conversion, but which includes a correction for loss of fissile material. This calculation was based on nuclear data contained in Section 2. The pressure drop value is based upon GE empirical data for rod bundles with grid spacers.

In order to make a clear distinction between the predicted core outlet temperatures shown on Table 3-2, a discussion of the methods used to calculate these temperatures is given here. Three outlet temperatures are shown in the table; these are the "mixed-mean coolant outlet temperature (1000°F)," the "hot channel coolant temperature at top of fuel region (1135°F)," and the "maximum mid-cladding temperature averaged over 60 degrees of arc (1090°F)."

(G1)

FCF 188F475
FOR DESIGN DATA SEE 325X157

REVISIONS				PRINTS TO	
3	08/13/71	GRS	1	08/13/71	GRS
TITLE WAS, COMBINED STRAIGHT WAVE BEAM (F9E) FULL GRID.				TITLE WAS, GRID SPACER. CALLOUT: ALL WELDS --- SIMULTANEOUSLY, WAS, (44 TOTAL) PL, REVISED NAME COL. WAS, BEAM @GR	
4				DESCRIP. OF P5 - P8 WAS, 145C6607 P1 - P4	
2				1.110 WAS 1.160 .030 WAS .040	
SK				145C6775	
B R D				SUNNYVALE	
JAN. 12, '70				JAN. 15, 1971	

Figure 3-20A. Combined Straight Wave Beam (F9E), Full Grid

Figure 3-20B. Loading of Modified Straight Beam by the Fuel Rods

The mixed-mean coolant outlet temperature is derived directly from the total power output of the fuel assembly (511 kW) and the total coolant flow rate (19,200 lbm/h). Assuming perfect mixing, so that all the coolant is at the same temperature at the core outlet, and that the channel walls are adiabatic surfaces, the coolant temperature at the top of the core is given by

$$T_{\text{out}} = T_{\text{inlet}} + \Delta T_{\text{core}} \quad (3-1)$$

where ΔT_{core} is found by equating heat generated in fuel to heat absorbed by coolant:

$$\text{Power generated} = C_p \Delta T_{\text{core}} W \quad (3-2)$$

Since the inlet temperature is 700°F, equation (3-1) becomes

$$T_{\text{out}} = 700^\circ\text{F} + \frac{P}{C_p W} \quad (3-3)$$

which can be solved for T_{out} . The result of this procedure, evaluating the specific heat of sodium at 850°F, is that $T_{\text{out}} = 1000^\circ\text{F}$ when $P = 511$ kW and $W = 19,200$ lb/h.

The predicted distribution of coolant temperatures within a fuel assembly that operates under nominal conditions is calculated by computer. The computer program calculates the coolant flow in edge and interior flow channels and includes the effect of interchannel mixing. The hottest coolant outlet temperature predicted is found to be 1051°F. This is the nominal hot-channel coolant outlet temperature; it is "nominal" because it does not include the effects of uncertainties in geometry, assembly flow rate, reactor power, etc. These effects are included by using the uncertainty factors shown on Table 3-3. In this table, the effect of off-nominal geometry on the coolant enthalpy rise is represented by the factor titled "flow profile within assembly." The other sources of uncertainty shown on Table 3-3 are self-explanatory.

Figure 3-21. Thermal Gradients on Edge and Corner Rods of Original F9E versus Axial Position

Figure 3-22. Axial Variation of Coolant Temperature for Edge and Corner Rods for Original F9E Test Assembly

Figure 3-23. Radial Variation of Coolant Temperature Across Original F9E Test Assembly at the Top of the Fuel Region

The "hot channel coolant temperature at the top of the fuel region, including uncertainties" that is shown on Table 3-2 is calculated in the following manner. The nominal coolant temperature rise is taken to be 351°F, and the uncertainty factors in the first column of Table 3-3 are combined using the relation

$$\text{Combined uncertainty} = F = \sqrt{(0.07)^2 + (0.05)^2 + (0.01)^2 + (0.02)^2 + (0.05)^2} \cdot \quad (3-4)$$

$$F = 0.114.$$

Equation (3-4) assumes that the uncertainties are all statistically independent of one another. The uncertainty in the core ΔT is 351°F · 0.114, or 40.0°F. The hot-channel core ΔT becomes 391°F, and the hot-channel coolant temperature is 1091°F. This neglects the effect of a local hot spot caused by the presence of a grid spacer. As shown on Table 3-3, the coolant temperature is increased 4% by a grid spacer. The hot-channel coolant temperature, including this effect, is 1.04 (1091°F). This is equal to 1135°F. It is this value that appears on Table 3-2 as the hot-channel coolant temperature.

The "maximum mid-clad temperature averaged over 60 degrees of arc" that appears on Table 3-2 does not include uncertainties. This temperature is the nominal hot-channel coolant temperature calculated by the computer routine mentioned above plus a nominal temperature rise from coolant to mid-clad of 39°F. As shown in the table, this temperature is 1090°F. Applying the uncertainties of Table 3-3 to the temperature drop from coolant to mid-clad increases this ΔT to 45°F. Adding 45°F to the hot-channel coolant temperature (including uncertainties) of 1135°F yields a hot spot mid-clad temperature of 1180°F. This temperature is conservatively high, since the heat flow toward the cladding hot spot is expected to be less than that toward cooler areas of the cladding. The temperature drop that forms the basis for the calculations shown here assumes the clad temperature (and heat flux) is relatively uniform around the rod circumference.

The uncertainty factors shown on Table 3-3 are drawn from the "Hazards Summary Report."^{3-2,3-3} The values given by this reference were modified to reflect an improved knowledge of geometrical parameters and fuel thermal conductivity. The uncertainty in gap coefficient was modified to reflect current GE estimates.

The maximum coolant temperature predicted in an FFTF driver fuel assembly, including uncertainties, is calculated using modified FFTF uncertainty factors. The original factors were drawn from the "Thermal Design Methods and Results"³⁻⁴ and appear on Table 3-4. The modified factors appear on Table 3-5. Modifications made to Table 3-4 reflect calculations of flow maldistribution within a grid-spaced fuel assembly operating in FFTF. The method used to show the effect of a grid spacer-induced hot spot is different from the method used for F9E (Table 3-3) and for FFTF wire wrap (Table 3-4). The change from the form of Table 3-3 was made to allow easy comparison with Table 3-4. The axial coolant ΔT was used as a basis for the grid spacer coefficient on Table 3-5 because this ΔT is more accurately known. In summary, the grid uncertainty shown on Table 3-5 (1.13) is equivalent to the factor of 1.04 shown on Table 3-3, but the form of presentation has been changed. As shown earlier in this section, the factor 1.04 that is used for the analysis of the F9E assembly is applied to the hot channel coolant temperature; in contrast, the factor 1.13 is applied to the axial coolant temperature rise for the FFTF analysis. That the factors yield comparable results is shown by the following calculations. For F9E, the temperature rise due to rod-grid contact is found to be

$$\Delta T_{\text{Grid}} = 0.04 \cdot 1091 = 43.6^\circ\text{F}. \quad (3-5)$$

For FFTF, the same quantity is

$$\Delta T_{\text{Grid}} = 0.13 \cdot 358 = 46.5^\circ\text{F}. \quad (3-6)$$

Therefore, the two different factors yield comparable results. This treatment is believed to yield conservatively large temperatures, particularly at the top of the core.

In addition to the modifications discussed above, Table 3-5 differs from Table 3-4 in the treatment of flow maldistribution within the fuel assembly. The flow distribution expected in a nominal assembly gives rise to the "direct" factor of 1.08. A statistical flow distribution factor is included in Table 3-5 to represent the effect on flow of off-nominal geometry.

Table 3-4
HOT CHANNEL FACTORS USED BY FFTF

Contributor		Coolant 1	Film 2	Cladding 3	Gap 4	Fuel 5
A	Direct					
1	Inlet Flow Maldistribution	1.05	1.012			
2	Intrasubassembly Flow Maldistribution	1.15	1.035			
3	Interchannel Coolant Mixing	0.99 ^(a)				
4	Power Control Band	1.02	1.02	1.02	1.02	1.02
5	Wire Wrap Peaking ^(b)		2.00(1.214)			
6	Direct Combination ^(b)	1.219	2.133(1.297)	1.02	1.02	1.02
B	Statistical					
7	Fissile Fuel Maldistribution	1.02	1.035	1.035	1.035	1.035
8	Power Level Measurement	1.090	1.090	1.090	1.090	1.090
9	Burned Fuel Reload	1.012	1.012	1.012	1.012	1.012
10	Rod Diameter, Pitch and Bow	1.011				
11	Film Coefficient		1.14			
12	Gap Coefficient				1.85	
13	Fuel Conductivity					1.10
14	Cladding Conductivity and Thickness			1.12	$1.0 + \frac{760}{1410}$	
15	Statistical Combination of Items 7-14	1.094	1.170	1.154	1.509	1.139
16	Product of Items 6 and 15 ^(b)	1.334	2.495(1.517)	1.177	1.539	1.162

^(a) Worst Condition

^(b) Numbers in parentheses should be used for calculating fuel temperatures

The method of combination of the uncertainty factors is shown on Table 3-5. As shown, the "direct" factors always produce the indicated effects; therefore, they are simply multiplied together. The "statistical" uncertainties are combined by assuming they represent standard deviations about the means of independent variables; therefore, they probably do not all occur at one point or at one time, and they are combined statistically.

The maximum, nominal, mixed-mean coolant outlet temperature of a driver fuel assembly in FFTF is 958°F.³⁻⁵ The corresponding axial temperature rise is 358°F. Applying the uncertainty factor from Table 3-5 yields a hot channel coolant outlet temperature of 1110.5°F (600 + 1.426 X 358°F). Since the F9E nominal outlet temperature is 1000°F, and the outlet temperature with uncertainties is 1135°F, the F9E test assembly should provide a good model of peak FFTF thermal conditions.

3.4 PREDICTED OPERATING CHARACTERISTICS OF THE REVISED F9E ASSEMBLY

The predicted operating characteristics of the original test assembly which were summarized in Table 3-2 still describe the modified test assembly except for slight changes in the fuel assembly pressure drop and local clad temperatures. These changes are discussed in Section 9. Figures 3-21, 3-22, and 3-23, which describe the thermal parameters of the original test assembly, are also assumed to be applicable to the modified test assembly. These figures neglect flow mixing due to grids. Although some changes in flow mixing due to the use of the three modified staggered grids in the modified F9E assembly are expected, these will not significantly affect Section 3 results. A qualitative discussion of the expected thermal effects caused by mixing due to grids is given in Section 9.

Table 3-5
MODIFIED FFTF HOT CHANNEL FACTORS USED FOR GRID ANALYSIS

	Contributor	Coolant	Film	Cladding	Gap	Fuel
A	Direct					
1	Inlet Flow Maldistribution	1.05	1.012			
2	Intrasubassembly Flow Maldistribution	1.08	1.017			
3	Interchannel Coolant Mixing	0.99 ^(a)				
4	Power Control Band	1.02	1.02	1.02	1.02	1.02
5	Grid Peaking	1.13				
6	Direct Combination	1.294	1.049	1.02	1.02	1.02
B	Statistical					
7	Fissile Fuel Maldistribution	1.02	1.035	1.035	1.035	1.035
8	Power Level Measurement	1.090	1.090	1.090	1.090	1.090
9	Burned Fuel Reload	1.012	1.012	1.012	1.012	1.012
10	Rod Diameter, Pitch and Bow	1.011				
11	Film Coefficient		1.14			
12	Gap Coefficient				1.85	
13	Fuel Conductivity					1.10
14	Cladding Conductivity and Thickness			1.12	$1.0 + \frac{760}{1410}$	
15	Intrasubassembly Flow Maldistribution due to Manufacturing Tolerances	1.04				
16	Statistical Combination of Items 7-15	1.102	1.170	1.154	1.509	1.139
17	Product of Items 6 and 16	1.426	1.227	1.177	1.539	1.162

^(a) Worst Condition

4. FUEL ROD EVALUATION

4.1 FUEL ROD BENDING - ORIGINAL DESIGN FULL GRIDS

Fuel rods operate for long periods of time in an environment where both the neutron flux and the coolant temperature vary spatially. This environment results in the deformation of structural members (cladding, grids and channels) by thermal expansion, irradiation-induced swelling, thermal creep, and irradiation-induced creep. Swelling and creep vary in accordance with the relations shown in the material properties section.

The radial neutron flux and the coolant temperature gradients tend to reinforce or cancel each other, depending on the position in the assembly. In general, a corner rod nearest the center of the core will have the worst combination of environmental conditions. The temperature and flux distributions and their radial gradients are shown on Figures 4-1 and 4-2, respectively, for a corner rod operating in row 6 of the EBR-II core. The effect of these transverse gradients is that the fuel rod tends to bow. If unrestrained, the rod would tend to bow with a large radius of curvature. For a corner rod, the bowing is in the direction of the hexagon corner. The fuel rod is restrained by a set of grid structures located six inches apart along its length in the original F9E design. This causes the rod to act as a set of short, coupled, indeterminant structures (Figure 4-3). In general, the lateral deformation that results from the imposed gradients consists of alternating deflections between successive grid supports. Thus, the rod deflection looks something like a sine wave. This rod deflection also results in bending stresses and lateral loads on the grid support structures. Since the rod operates to a high fluence, irradiation and thermal creep occurs which tends to reduce stresses in the rod accompanied by decreased deflections between the grids and a decrease in grid restraint loads.

The analysis of the rod bending has been performed using CRASIB I⁴⁻¹ which is a finite element procedure for the two-dimensional creep analysis of a statically indeterminant beam with multiple supports. The rod model for full grids every six inches is shown on Figure 4-3. The model simulates the seven-grid locations in the original F9E design and includes seven additional deflection nodes in intermediate positions between grids in the core area.

A series of CRASIB I problems was run using this model for an irradiation time of 11,280 hours. The cases used both the GE swelling correlation and the PNL swelling correlation (approximately 60% of GE's). In each case 50%, 100% and 125% creep was used. Beginning and end-of-life grid loads and fuel rod deflection are shown on Table 4-1. A summary tabulation of maximum rod deflection, grid load, and rod stress and strain is shown on Table 4-2. From this table, it can be seen that the PNL swelling correlation results in 0.77 of the nominal GE grid load, 2.58 of the nominal GE rod deflection, 0.39 of the nominal GE fuel rod stress and 0.51 of the nominal GE fuel rod strain for the case of nominal creep.

The time dependent results are plotted on Figure 4-4 (grid loads), 4-5 (rod deflection), and 4-6 (rod stress). From these curves, it is evident that rod deflection is relatively insensitive to the creep limits while rod stress and grid load vary almost inversely with the extent of creep.

4.2 PREDICTED BENDING OF CORNER ROD - REVISED DESIGN WITH STAGGERED GRIDS

An analysis of the rod stresses, rod deflections, and rod-grid reaction forces that act on a fuel rod constrained by the unmodified grid-support system was presented in Section 4.1.

The model used in this section to describe rod support differs from that used in Section 4.1 as shown on Figure 4-7; this figure shows that grids are spaced at three-inch axial intervals along the lower (below midcore) portion of the fuel rod in the modified system. Grids were spaced at six-inch intervals in the original system. Grid numbers 2, 3, and 4 (see Figure 4-7) in the modified system are staggered-beam grids. Figure 4-8 illustrates how these differ from the original full grids that were analyzed in Section 4.1. The manner in which staggered-beam grids support a corner rod is shown on Figure 4-9. Note that the rods numbered "one" and "three" on Figure 4-8 are supported by the Type I restraint which is defined on Figure 4-9. Similarly, the rod numbered "two" on Figure 4-8 is supported by the Type II restraint defined on Figure 4-9. The difference between the two types of restraint is that a corner rod is supported in only one direction (toward or away from the channel corner) by a Type I restraint, while the rod is supported in both directions by a Type II restraint. Figure 4-8 shows that, of the two corner rods nearest the core center, one will be restrained by a Type I restraint and the other by a Type II restraint.

Figure 4-1. Predicted Average Temperature and Radial Thermal Gradients versus Axial Position for Hot Corner Rod in F9E

Figure 4-2. Predicted Variation of Flux and Flux Gradient versus Axial Position in F9E

Figure 4-3. CRASIB I Model Original Design

Figure 4-4. Predicted Grid Load Corner Rod

Table 4-1
FUEL ROD DEFLECTIONS AND GRID LOADS FOR HOT CORNER ROD
(POSITIVE SIGN TOWARDS CORE CENTERLINE)

	Δ 1	Δ 2		Δ 4		Δ 6		Δ 3		Δ 10		Δ 12	Δ 14
Time = 0 hours													
(lb)	0.0025	0.0038		0.418		0.2931		-0.4702		0.1545		0.1918	0.00034
(mil)			0.0039		0.0304		0.198		0.186	- 0.1804		0.0314	
Case A GE													
100% Creep, Time = 11,280 hours													
(lb)	-0.6284	1.095		1.326		- 2.816		0.314		0.2967		0.3772	0.03574
(mil)			- 1.21		2.26		0.2943		0.9475	- 0.785		0.2749	
Case B GE													
50% Creep, Time = 11,280 hours													
(lb)	- 0.7854	1.392		1.784		- 3.816		0.484		0.4514		0.4586	0.0329
(mil)			- 1.32		2.46		0.1081		1.009	- 0.875		0.313	
Case C GE													
125% Creep, Time = 11,280 hours													
(lb)	- 0.5697	0.984		1.166		- 2.467		0.264		0.2425		0.3436	0.03659
(mil)			- 1.17		2.208		0.349		0.929	- 0.750		0.260	
Case C PNL													
100% Creep, Time = 11,280 hours													
(lb)	- 0.8477	1.421		0.2377		- 1.699		2.165		- 1.017		0.1879	0.0284
(mil)			- 1.81		5.849		- 4.622		2.12	0.601		0.179	
Case E PNL													
50% Creep, Time = 11,280 hours													
(lb)	- 1.041	1.785		0.2284		- 2.203		2.555		- 1.148		0.2566	0.02529
(mil)			- 1.92		5.94		- 4.802		2.26	- 0.691		0.219	
Case F PNL													
125% Creep, Time = 11,280 hours													
(lb)	- 0.773	1.283		0.2259		- 1.528		2.005		- 0.9507		0.16023	0.02948
(mil)			- 1.78		5.822		- 4.55		2.06	- 0.55		0.163	

Grids 2 and 4 in the modified grid support system are of the Level I type. Grid 3 is of the Level II type. This is shown on Figure 4-8 where the solid lines represent beams of grids 2 and 4; dashed lines represent grid 3. Figure 4-8 shows that rod 1 can be held away from the channel corner only at grid 3, while rod 3 can be held away from the corner at grids 2 and 4. Rod 2 is supported by a Type II restraint, and so it can be held away from the corner at all grid locations. These modes of support are shown on Figure 4-10 to be consistent with the calculated rod deflection shape.

Three separate calculations of the deflection of corner rods were performed using CRASIB I. Calculations were performed for rod 1, rod 2, and rod 3 (for rod numbering, see Figure 4-8). The node structure used in CRASIB to analyze the modified system is shown on Figure 4-7. Swelling and creep correlations used were those for nominal behavior of Type-316 annealed stainless steel. The General Electric correlations were used. The results from Section 4.1 are compared with the new values on Table 4-3. This table shows that the rod-grid reaction loads and rod stresses for rod 1 are higher when the corner rods are supported by the modified grid support system. The rod deflections vary considerably between the three cases; the maximum deflection occurs on rod 1 of the modified system at grid 4. This

Table 4-2

SUMMARY OF PREDICTED GRID LOADS, ROD DEFLECTIONS, AND STRESSES FOR HOT CORNER ROD

GE SWELLING

Percent of Creep	Node	Maximum Grid Load (lb)	Node	Maximum Rod Deflection (mils)	Node	Maximum Stress (psi)	Maximum Strain (%)
50	6	3.81	5	2.46	6	27196	0.047
100	6	2.82	5	2.27	6	20517	0.075
125	6	2.47	5	2.21	6	18123	0.085

PNL SWELLING

Percent of Creep	Node	Maximum Grid Load (lb)	Node	Maximum Rod Deflection (mils)	Node	Maximum Stress (psi)	Maximum Strain (%)
50	8	2.56	5	5.95	6	11108	0.025
100	8	2.16	5	5.85	6	7987	0.038
125	8	2.01	5	5.82	6	6967	0.043

Table 4-3

PREDICTED CORNER ROD STRESSES, DEFLECTIONS, AND ROD-GRID REACTION LOADS FOR MODIFIED SUPPORT SYSTEM AND COMPARISON WITH SECTION 4 RESULTS

Parameter	Section 4.1 Results	Rod 1 Results	Rod 2 Results	Rod 3 Results
Maximum Rod-Grid Reaction, Load at $t = 0$ h (lb)	0.470	0.499	0.419	0.421
Maximum Rod-Grid Reaction, Load at $t = 11280$ h (lb)	2.82	3.53 (3.01) ^(a)	2.46	2.83
Maximum Rod Deflection at $t = 11280$ h (mils)	2.27	5.29	1.01	2.47
Maximum Rod Stress at $t = 0$ h (psi)	5035	4700	4598	4600
Maximum Rod Stress at $t = 11280$ h (psi)	20517	23309	21513	21447

(a) The value of rod-grid reaction load shown in parentheses occurs at Grid Number 5, while the maximum value of 3.53 pounds occurs at Grid Number 2. The temperature of Grid Number 5 is 930°F while the temperature of Grid Number 2 is 700°F. The lower load at Grid 5 is the more critical of the two with respect to the criteria.

Figure 4-5. Predicted Corner Rod Deflection—Node 5

Figure 4-6. Predicted Corner Rod Bending Stress—Node 6

FULL GRID:

SOLID AND DASHED BEAMS PRESENT AT
EVERY GRID LOCATION

STAGGERED GRID:

SOLID OR DASHED BEAMS
PRESENT AT EACH
GRID LOCATION

Figure 4-8. Comparison of Full Grid with Staggered-Beam Grid

grid provides no support to the rod because of the direction of the deflection. Figure 4-10 compares the deflected rod shapes at end-of-life, and shows the locations of the maximum reaction forces and end-of-life stresses. This figure shows that, due to the shape of the deflected rods, rod 1 is supported by grids 2 and 3, rod 2 is supported by grids 2, 3, and 4, and rod 3 is supported by grids 3 and 4. Of course, all of the full grids support all the rods since full grids provide two-way support. Thus, the difference between rods 1, 2, and 3 that are shown on Table 4-7 and Figure 4-10 are caused by the different types of support provided by the staggered-beam grids.

4.3 PREDICTED BENDING OF EDGE AND INTERIOR RODS - REVISED DESIGN

Table 9-1 shows the thermal effects of varying the edge and corner rod wall spacing in the design optimization. The temperature differences acting on edge and corner rods to cause bowing are equal for the reference rod-to-channel spacing of 42 mils. However, the corner rods are hottest on the sides nearest the channel wall, while edge rods are hottest on the sides nearest the center of the fuel assembly. This means that edge rods bow away from the channel wall in the core region, while corner rods bow toward the wall. Edge rods located on the same side of the hexagon as corner rods 1 and 2 are supported in the same manner as corner rod 3; they are supported on their hot sides by beams represented by dashed lines on Figure 4-8. Therefore, the results presented on Table 4-3 also apply to edge rods.

Interior rods are acted upon by small temperature differences that could lead to bowing. From Table 3-2, the average of these differences is 12°F. When compared to the 40°F differences across edge and corner rods, 12°F is regarded as insignificant.

The evaluation of bowing deflections of edge and interior rods is complicated by the possibility of two adjacent rods bowing toward one another. This situation would exclude coolant from the flow channel between rods, and this in turn would produce further bowing together of the two rods. This effect is one of the areas in which the F9E diagnostic test should provide information.

TYPE I RESTRAINT

LEVEL I
(3 in. ABOVE OR BELOW LEVEL II)

ROD NOT RESTRAINED FROM
MOVEMENT TOWARD CORNER

LEVEL II

ROD NOT RESTRAINED FROM
MOVEMENT AWAY FROM CORNER

TYPE II RESTRAINT

LEVEL I
(3 in. ABOVE OR BELOW LEVEL II)

LEVEL II

ROD RESTRAINED FROM MOVEMENT TOWARD
OR AWAY FROM CORNER ON BOTH LEVELS

NOTE: ROD 1 (FIG. 11-2) SEES TYPE I RESTRAINT
ROD 2 (FIG. 11-2) SEES TYPE II RESTRAINT

Figure 4-9. Corner Rod Restraint by Staggered-Beam Grids

CORNER RODS

NOTE: + DEFLECTION IS TOWARD CHANNEL CORNER

Figure 4-10. Predicted Deflected Rod Shapes at End-of-Life

4.4 PREDICTED LOCAL STRESSES IN FUEL RODS

Local stresses are produced in the fuel cladding due to local clad bending at the grid supports. The cladding thickness falls outside the range that could be considered either thin-wall or thick-wall tubing. A conservative estimate of local stress can be made by considering the cladding to be thin-wall tubing and by using an empirical formulation for the stress distribution.⁴⁻²

In general,

$$\sigma = \frac{KP}{t^2}$$

where

σ	Stress	psi
P	Load	pounds
t	Wall Thickness	inch
K	Empirical Coefficient	nondimensional

The maximum value of K occurs under the load; its value is 1.9 for longitudinal stress and 2.2 for circumferential stress. For our nominal GE creep case, this results in a longitudinal stress of 29,400 psi and circumferential stress of 34,000 psi at 7.5 inches above the core bottom. At 1.5 inches below the bottom of the core, the longitudinal stress is 34,500 psi and the circumferential stress is 40,000 psi. These stresses reduce to 10% of maximum within 0.020 inches of the point of application of the load. Thus, these stresses are highly localized and occur at the end-of-life. They satisfy the structural criteria limit for peak stresses, which is to be less than yield.

4.5 ROD VIBRATIONS

Fuel rods subjected to fluid flow parallel to their axes exhibit continuous random lateral vibration. This vibration is self-induced through interaction between rods and fluid, and generally occurs near the natural frequencies of the fuel rod. The largest displacement amplitudes occur at the lowest natural frequency.

Vibration of rod-type fuel elements was first shown to be significant in a study reported by Burgreen.⁴⁻³ It was found, and since confirmed by others, that rod vibration frequency was independent of fluid velocity. Boundary layer turbulence was assumed to be the forcing function, and Thomson⁴⁻⁴ worked out the complete theoretical solution for the first mode response of a pinned beam to distributed random forces. These theoretical RMS values of displacement calculated by Thomson were found to be 3.5 to 420 times lower than the measured maximum displacement. Reavis⁴⁻⁵ explained this as high "noise" levels in some loops. He also noted the fact that disturbances damped out sooner in channels with small hydraulic diameter-to-length ratios. He then correlated the experimental work of Burgreen⁴⁻³, Pavlica⁴⁻⁶, Quinn⁴⁻⁷, and Sogroah⁴⁻⁸ into an empirical equation which included a factor which adjusted for "noise" level was further reinforced by Addae and Fenech of M.I.T.⁴⁻⁹ who designed a special test rig in which, by eliminating all known sources of "noise," they were able to test Reavis' theory.

An empirical correlation by Paidoussis^{4-10, 4-11} also includes a factor to account for "quiet" or "noisy" loops. His experiments with 19-rod bundles showed deflections from 2 to 15 times greater than predicted by the equation for single rods. Paidoussis also noted that fluid swirl, such as induced by wire-wrap bundles, might increase vibration amplitudes by a substantial amount.

Reavis' latest correlation⁴⁻¹² is considered to be quite accurate at the present time and was used as a basis for data correlation. His correlation takes the form:

$$\delta = C \eta_d \eta_D \eta_L \frac{D L N^{0.5}}{W f_o^{1.5} \epsilon^{0.5}} \rho V \gamma^{0.5}$$

where

C	Noise Coefficient (a function of inlet conditions and distance from inlet region)	Figure 4-11
$\eta_d \eta_D \eta_L$	Dimensionless Function	Figure 4-12
D	Rod Diameter ^(a)	inch
L	Rod Length ^(a)	inch
N	Number of Rods ^(a)	nondimensional
V	Flow Velocity	in./sec
W	Weight of Rod ^(a)	Lb
f_o	Rod Natural Frequency ^(a)	Hz
δ	Maximum Amplitude of Displacement	inch
ρ	Coolant Density	lb · sec ² /in. ⁴
ν	Kinematic Viscosity	in. ² /sec
ξ	Critical Damping Ratio	nondimensional

(a) Between simple supports

A comparison with the experimental data of the various experimenters is shown in Figure 4-13 with the required characteristic parameters shown in Figures 4-11 and 4-12.

The natural frequency is given by:

$$f_o = \frac{\lambda^2}{2\pi L^2} \sqrt{\frac{EI}{m}}$$

where

f	Frequency	Hz
λ	Eigenvalue	nondimensional
E	Young's Modulus	psi
I	Moment of Inertia	inch ⁴
m	Total Mass	lb · sec ² /in. ²
L	Rod Length (Between Simple Supports)	inch

With the assumption of a rod deflection curve of

$$y = \delta \sin \frac{\pi x}{L}$$

we can determine (Appendix B) the bending stress in the rod to be

$$\sigma = \frac{E \delta \pi^2}{2L^2}$$

where

σ	Bending Stress	psi
E	Young's Modulus	psi
δ	Rod Deflection	inch
L	Rod Length (Between Simple Supports)	inch
d	Rod Outside Diameter	inch

The grid load can also be determined as

$$Q = \delta E I \frac{\pi^3}{L^3}$$

where

Q = Grid Load (lb)

Using these relations, we can predict the behavior of a fuel rod as a function of axial position. Although the rod stress, grid loads, and deflections occur at discrete points along the rod, they have been plotted on Figure 4-14 as continuous functions of axial position. The frequency is also shown as a function of axial position. Rod frequency varies only slightly with position as a result of temperature variation. Grid load, rod stress, and deflection have larger variations; they generally decrease as flow proceeds up the assembly. This is primarily the result of flow smoothing that occurs as the flow proceeds through the assembly.

The above results (Figure 4-14) are primarily directed to the full grid locations where pin support is at approximately 6 inches. In those areas where staggered grids are located, a more complex situation exists since each grid provides rod support only on one side in an alternating pattern at shorter, three-inch axial locations. This presents a rod geometry which cannot be readily evaluated without experimental backup; however, inspection of the support locations per rod (Figure 4-10) reveals that at no point in the bundle does rod support exceed 6 inches in any direction and that a number of 3-inch supports exist. Thus, it is concluded that rod restraint has been increased in the area of the staggered grids and that application of the results of Figure 4-14 will result in conservative design values.

Figure 4-11. Average Density Between Theoretical and Experimental Displacement

Figure 4-12. Dimensionless Scale Factors

SYMBOL	△	●	◆	▲	▽	□	◇	◻	■	◊	⊗	⊙	⬡	⬢	⬣	⬤
INVESTIGATOR	S	B	B	B	B	B	B	B	B	P	P	P	Q	Q	Q	Q
WATER TEMP., °F	70	70	70	70	70	70	70	70	70	70	70	70	500	200	93	80
No. RODS IN BEAM	1	1	1	1	1	1	1	1	1	16	16	16	1	1	1	1
BEAM WT., lb	1.0	0.25	1.5	1.5	0.92	4.5	1.5	4.5	0.25	57	57	57	3.3	3.3	3.3	3.3
BEAM NAT. FREQ., Hz	41	20	15	15	12	10	15	10	20	11	6	10	3.1	3.1	3.0	3.1
ROD DIA., in.	0.47	0.62	0.62	0.62	0.50	0.62	0.62	0.62	0.62	0.44	0.44	0.44	0.39	0.39	0.39	0.39
HYDRAULIC DIA., in.	5.8	2.4	5.6	2.4	2.4	2.4	0.85	0.85	0.85	0.58	0.58	0.58	0.86	0.86	0.24	1.2
DAMPING RATIO x 10 ³	10	12	12	12	12	12	12	12	12	18	18	18	13	13	13	13
BEAM LENGTH, in.	45	48	48	48	48	48	48	48	48	72	72	72	74	74	74	74

B - BURGREN

Q - QUINN

S - SOGREAH

P - PAVLICA

Figure 4-13. Comparison of Experimental Data with Westinghouse Vibration Correlation

Figure 4-14. Computed Fuel Rod Vibration Results Full Grids at 6-inch Pitch

5. GRID STRUCTURAL ANALYSIS

Differential rod-to-grid thermal and irradiation swelling is accommodated by the wave beam grid spacer primarily through the flattening of the wave section of the beam in the full grid design, and a combination of fuel rod bending and grid flattening in the staggered grid design concept. Only some 10% distortion occurs by bending along the length of the beam in the full grid.

5.1 ORIGINAL FULL GRID

As shown in Figure 5-1, the loading of a typical section of the wave beam grid spacer is in the form of point contacts on a flat plate. However, analysis of the flattening mechanism and preliminary testing of the prototypical models has shown that a given wave beam section may be analyzed as a simply supported (at the contact points) uniform beam without introducing unreasonable error. This allows easier definition and investigation of the parameters affecting the operating characteristics of different wave beam grid designs. Verification, through the use of finite element analysis, of the above model has further confirmed that no appreciable error is introduced.

ELASTIC MODEL—The elastic response of the wave beam grid, based on the uniform beam model discussed above, includes (a) wave beam spring constant, (b) peak bending stress per unit load, and (c) peak bending stress per unit deflection. The derivation and results of these parameters are described in the following paragraphs:

WAVE BEAM SPRING CONSTANT—Relative rod-to-grid swelling is accommodated through the flattening of the beam, as well as beam bending, in the transverse direction due to lateral load. Thus, the spring constant, defined as elastic load per unit deflection, must include both deformations.

For the flattening of the wave beam, a simply supported beam was used; this yields

$$\Delta_t = \frac{Ph^3}{48EI} = \frac{P}{4Eb} \frac{h^3}{t^3} \quad (5-1)$$

In the transverse direction, the wave beam was considered to be symmetrical about the contact points; this results in beam bending:

$$\Delta_t = \frac{Pb^3}{192EI_2} = \frac{P}{16Eh} \frac{b^3}{t^3} \quad (5-2)$$

The total deformation must be the sum of these components:

$$\Delta = \frac{P}{4Et^3} \left[\frac{h^3}{b} + \frac{b^3}{4h} \right] \quad (5-3)$$

Thus, the elastic spring constant for the wave beam grid spacer may be expressed as:

$$K = \frac{P}{\Delta} = 4E \frac{t^3}{\left(\frac{h^3}{b} + \frac{b^3}{4h} \right)} \quad (5-4)$$

For this analysis, $P/D = 1.28$ and $b = 0.320$ inches; the evaluation of this expression is shown in Figure 5-2.

Figure 5-1. Original Full Grid Structural Model

BENDING STRESS PER UNIT LOAD—As distortion of the spacer results primarily from the flattening of the wave beam, the maximum stress intensity will be controlled by this mode of bending. This results in

$$\sigma = \frac{Mc}{I} = \frac{Ph}{4} \cdot \frac{t}{2} \cdot \frac{12}{bt^3}$$

$$\sigma = \frac{3Ph}{2bt^2}$$

therefore,

$$\frac{\sigma}{P} = \frac{3h}{2bt^2} \quad (5-5)$$

Again, for the above design conditions, the evaluation of this expression is shown in Figure 5-3.

BENDING STRESS PER UNIT DEFLECTION—Combining equations (5-4) and (5-5) results in an expression which relates the maximum bending stress to unit deflection

$$\frac{\sigma}{\Delta} = \frac{6Eht}{b} \left(\frac{h^3}{b} + \frac{b^3}{4h} \right) \quad (5-6)$$

For design conditions, the evaluation of this expression is shown in Figure 5-4.

Figure 5-2. EBR-II F9E Experiment—Unmodified Full Wave Beam Grid Design Spring Constant

Figure 5-3. EBR-II F9E Experiment—Unmodified Full Wave Beam Grid Design Bending Stress per Unit Deflection

Figure 5-4. EBR-II F9E Experiment—Unmodified Full Wave Beam Grid Design Bending Stress per Unit Deflection

5.2 ELASTIC ANALYSIS OF THE MODIFIED STRAIGHT WAVE BEAM FULL GRID

(F Grids 1, 6 through 9, bottom layer grid 5)

Spring-like behavior of a wave beam is obtained by bending the beam through a wave-flattening displacement pattern. Thus the analytical model described in Section 5.1 considered a plate segment supported along edges located at the upper and lower wave crests, and loaded at the interior wave crest (Figure 5-1). Subsequent consideration of the "wave flattening" behavior led to an investigation of the effect of the welded attachments between beams upon the resulting displacements and loads. The results of the investigation indicated that the nearness of the wave crest to the welded junction resulted in the welded connection partially restraining wave flattening displacements, thus increasing both the spring constant of the wave beam and the rod to grid contact loads.

An increment of depth was added to the wave beam between the weld and the wave crest nearest to the weld to displace the wave-flattening behavior from the constraint imposed at the weld (see Figure 5-5). As the weld occurs at discrete points separated by a length of a free, unrestrained edge equal to the rod pitch distance, the increase in depth above the weld was selected to be in excess of half the pitch distance. This then provides an adequate load path away from the constraint to a free edge, permitting the wave beam flattening displacements to occur more consistent with the original wave beam conception. The constraining effect of the welds cannot be considered as completely eliminated. To rigorously assess the influence of the welds, further analysis must be performed, supported by mechanical testing. Simplified one-dimensional beam model analysis is not adequate because the welds represent discrete, intermittent support points rather than continuous support. A two-dimensional plate bending analysis was performed on the original grid design to support the argument for modification. A discussion of the results of this analysis is presented in Section 5.3.

STRAIGHT WAVE BEAM

MODIFIED STRAIGHT WAVE BEAM

Figure 5-5. Wave Beam Design Models

The effect of the welded constraint for the modified beam will result in a decrease in the contact load of the support near the edge farthest from the weld, an increase in the contact load at the support nearest to the weld and at the central contact load. The free body diagram is qualitatively shown in Sketch A below:

From statics, the maximum bending moment will be decreased (and hence the maximum stress will decrease) by an amount $(0.5) (\Delta p)_A (h)$.

Without a rigorous analysis, the load-displacement characteristics of the wave flattening deformation is still best described by equation (5-1).

The increase in the total depth of the beam will increase the portion of the spring stiffness associated with transverse bending of the beam; however, the principal contributor to the spring constant is derived from wave flattening and is not significantly affected by the addition of the lip. To be more exact equation (5-4) should be modified as follows

$$K = \frac{P}{\Delta} = 4E \frac{t^3}{\frac{h^3}{b} + \frac{b^3}{4\bar{h}}} \quad (5-4a)$$

where the depth, $\bar{h}$, in the second term of the denominator replaces the formerly derived value, h . The first term of the denominator, which is associated with the flattening of the wave, remains unchanged, with h still being the depth between the outer wave crests. The effect of the correction made in equation (5-4a) is an increase in the stiffness, K , of approximately 5%.

Equation (5-5) remains unchanged. Equation (5-6) changes slightly as follows:

$$\frac{\sigma}{\Delta} = \frac{6Eht}{b} \left(\frac{1}{\left(\frac{h^3}{b} + \frac{b^3}{4\bar{h}} \right)} \right) \quad (5-6a)$$

The minor corrections made in equations (5-4a) and (5-6a) are similarly applicable to creep analyses described in Section 5.5.

5.3 ELASTIC ANALYSIS OF THE STRAIGHT WAVE BEAM, FULL GRID, REVERSED (R/F Grid 6, Top Layer)

In the straight wave beam full grid, the orientation of the grid beams has been reversed so that the two parallel beams containing the central row of rods have the mid-height wave crests in contact with the contained row of rods rather than the upper and lower wave crests. See Figure 5-6. All other rows of rods are contacted by the mid-height wave crest at one location and by the upper and lower wave crests at a location 180 degrees away. It is considered advantageous to have contact made by mid-height wave crests on each side, rather than upper and lower wave crests on each side, because of the potentially stiff load path between the beam weld and the near-by wave crest. The stiffer load path would result in higher contact loads between the rods and the beams.

Although the straight wave beam reversed represents an improvement over the straight wave beam design, the constraint at the welds still significantly stiffens the spring behavior of the grid when compared with the modified straight wave beam discussed earlier. The two-dimensional plate bending analysis mentioned earlier in Section 5.2 which analytically provided the basis for adapting the modified straight wave beam, can be used to evaluate the loads and stresses which will occur in the straight wave beam reversed.

The analysis, which is discussed in more detail in Appendix H, consists of a finite difference solution, the plate bending differential equation and the appropriate boundary conditions. The use of a finite difference two-dimensional mesh conveniently allows consideration of relatively concentrated effects such as the point-like restraints which occur at the welds and the point-like load application at the rod-to-grid contact points. Sketch B shows the boundary conditions used along the edge of the plate.

The finite difference formulation used square array over the plate surface interval which subdivided both L_x and L_z into five intervals. The proper aspect ratio of the plate was maintained by formulating the difference equations using a rectangular mesh. To avoid the complication of resorting to either a completely variable mesh or interpolation schemes, the location of the rod to grid contact loads were placed at mesh intervals rather than their exact locations. The following table compares the contact locations of the model with the actual locations.

X - coordinate

Model - loads at $\frac{x}{L_x} = 0.2, 0.8$

Actual - loads at $\frac{x}{L_x} = 0.25, 0.75$

Z - coordinate

Model - loads at $\frac{z}{L_z} = 0.2, 0.5, 0.8$

Actual - loads at $\frac{z}{L_z} = 0.15, 0.5, 0.85$

FULL GRID DESIGN MODELS

Figure 5-6. Full Grid Design Models

The following free body (Sketch C) shows the model

NOTE:
OMITTED ARE THE
SHEAR, BENDING
MOMENT AND
TWISTING MOMENT
DISTRIBUTIONS
ALONG SIDES
A AND B

Although the discussion to this point has referred to the rod-to-grid contact loads, the formulation of the problem was in terms of imposed displacements, with the solution providing the loads occurring at the points of the imposed displacements.

5.4 ELASTIC ANALYSIS OF THE MODIFIED STRAIGHT WAVE BEAM, STAGGERED GRID (S Grids 2, 3, 4, Revised Design)

A physical description of the staggered grid system has been presented in Section 3. To understand the mechanical behavior of a staggered grid, consider the comparison between a full grid and a staggered grid as schematically shown in Figure 5-7. In the full grid model shown, the springs represent the resistance offered by the waves contacting the rods. As relative expansion develops between the rod and the grid, the waves flatten due to flexure induced displacements to provide space to accommodate swelling. At any grid assembly the rod is loaded symmetrically in the schematic model, and its centerline remains fixed along the length of the channel.

In the staggered grid assembly, at any grid level the rod interferes laterally with a restraint on only one side; as shown in the schematic model. The direction of lateral restraint alternates with consecutive grid assemblies due to the "staggered" arrangement of the beams. Thus the rod deforms in the manner shown (grossly exaggerated to show the effect). Both the lateral displacement flexibility of the constraining beams at two consecutive grids and the bending displacement flexibility of the rod between the consecutive grids contribute to the flexibility of the system to accommodate the differential expansion between the rods and the grid openings.

It is clear that if the individual beams of a staggered grid assembly are exactly the same as that of a full grid assembly, the staggered system will provide lower beam to rod contact loads.

For the full grid, the total deformation is given in equation (5-3) as

$$\Delta = \frac{P}{4Et^3} \frac{h^3}{b} + \frac{b^3}{4h} \quad (5-3)$$

where Δ is the relative diameter change of the rod which must be accommodated by the wave beam spring behavior.

For the staggered grid, in addition to the deflection components associated with wave flattening flexure in the beams, the deflection in the rod is calculated as shown in Sketch D below.

In terms of the contact load, P , and the relative diameter change, Δ_D ,

$$\Delta_D = \frac{PL^3}{12(EI)_{Rod}}$$

The total displacement for the staggered grid is then

$$\Delta = \frac{P}{4Et^3} \left[\frac{h^3}{b} + \frac{b^3}{4h} \right] + \frac{PL^3}{12(EI)_{Rod}}$$

or

$$\Delta = P \frac{1}{4Et^3} \left(\frac{h^3}{b} + \frac{b^3}{4h} \right) + \frac{L^3}{12(EI)_{Rod}} \quad (5-6b)$$

The spring constant of the staggered grid beam/rod system becomes

$$K = \frac{1}{\frac{1}{4Et^3} \left(\frac{h^3}{b} + \frac{b^3}{4h} \right) + \frac{L^3}{12(EI)_{Rod}}} \quad (5-7)$$

Figure 5-7. Grid Spacer Operating Characteristics Schematic Representation

From Figure 5-2, the spring constant for 0.012 inch thick beam with $h = 0.35$, and a modulus of elasticity of 24×10^6 is 1.12 lb/mil. The spring constant contributed by bending of the rod only (the reciprocal of the second term within the denominator in equation 5-7) is obtained using the following values:

$$\begin{aligned} L &= 3.0 \text{ inch} \\ \text{O.D.} &= 0.25 \text{ inch} \\ \text{I.D.} &= 0.22 \text{ inch} \\ E &= 24 \times 10^6 \text{ psi} \end{aligned}$$

$$(EI)_{\text{EFF}} = (24 \times 10^6) \cdot \left(\frac{1}{8}\right) (\pi) \left(\frac{0.250 + 0.220}{2}\right)^3 \left(\frac{0.250 - 0.220}{2}\right) = 1836 \text{ lb-in}^2$$

Therefore

$$\frac{12(EI)_{\text{Rod}}}{L^3} \left[\frac{(12)(1836)}{(3)^3} \right] \left[\frac{1}{1000} \right] = 0.817 \text{ lb/mil}$$

The combined spring constant for the contribution from wave flattening and rod bending is:

$$K_{\text{Total}} = \frac{1}{\frac{1}{1.12} + \frac{1}{0.817}} = 0.472 \text{ lb/mil}$$

The maximum bending moment in the rod is given by:

$$M_{\text{Max., Rod}} = \frac{PL_{\text{Rod}}}{2} \text{ in-lb} \quad (5-8)$$

To obtain the contact loads and the beam bending stresses associated with wave flattening, results for the analysis of a full grid can be scaled by the ratio of the spring constants, a factor of 0.42. This scaling factor is applicable for both elastic and creep analyses.

Figures 5-3 and 5-4 present the bending stress in the full grid wave beam for a one pound load and a one mil differential growth, respectively. For the F9E staggered grid, Figure 5-3 is applicable. However, for Figure 5-4, the stresses for $h = 0.35$ inch must be scaled down by a factor of 0.42.

Figure 5-3 is applicable for determining beam bending stresses due to bowing loads. The influence of the staggered grid on restraining the thermal and irradiation induced bowing of the rod is discussed in Section 4.3.

5.5 CREEP MODEL

Wave beam grid creep distortion during the application of long-term loads can be extremely important. It directly affects the rod-to-rod or rod-to-channel spacing within the bundle. Normally, this deformation could be neglected since the majority of the grid spacers will be operating at temperatures of 900°F or less, which is below the temperature at which thermal creep is important. However, since the advent of irradiation-induced creep, which is greatest at lower temperatures, this is no longer true.

As with the elastic analysis, a uniform beam model has been used to evaluate the effects of long-term creep. Details of this work, including derivation of the working equations, are included in Appendices C, D, and E; the results are summarized in the following paragraphs:

BEAM DEFLECTION (APPENDIX C)—Irradiation creep strain has been expressed as:

$$\epsilon(t) = \bar{\sigma} (K_1 + K_2 e^{-ct} + K_3 t)$$

or

$$\epsilon(t) = \bar{\sigma} K(t)$$

and its time derivative is

$$\dot{\epsilon}(t) = \bar{\sigma} \dot{K}(t)$$

For this strain rate, the creep deflection rate for a constant load (P) may be expressed as

$$\dot{Y}_c(t) = \frac{Ph^3}{4bt^3} \dot{K}(t) \quad (5-9)$$

with a peak deflection at $t = T$ of

$$Y_{\max} = \frac{Ph^3}{4bt^3} K(t)$$

LOAD RELAXATION (APPENDIX D)—The above relates the maximum deflection for a constant load (P). In a like manner, the resulting load for a given deflection with time is given as

$$P(t) = P_0 e^{-EK(t)} \quad (5-10)$$

where P_0 is the load at $t = 0$ and $K(t)$ is the irradiation-creep strain value as previously defined, and $P(t)$ is the reduced load because of creep relaxation at $t = t$.

5.6 DESIGN LOADS AND STRESSES

There are seven major load sources listed in Table 5-1 which have been considered in the design of the wave beam grid spacers for test in EBR-II.

These load sources result in four generalized stresses in the beams as shown in Table 5-2. The stresses resulting from each load are further classified as either primary or secondary. Primary stresses are those stresses which are no self-limiting and are required to balance external forces and to satisfy the laws of equilibrium. They are induced by mechanical loads which are not relieved as a result of deformation under loading. Secondary stresses are those stresses which are self-limiting and that are necessary to satisfy continuity of the structure. They are caused by both mechanical loads, thermal growth restraint, and irradiation swelling restraint.

As indicated in Table 5-2, there are three primary stress classifications to be considered. The grid axial bending stresses due to longitudinal forces are obvious since they are required to balance these external forces. The

longitudinal* bending stresses due to rod vibration are somewhat deflection limited; however, since gross support movement would compromise rod spacing and lead to higher loads at adjacent support points, it was decided to classify vibratory loads as primary. Finally, the longitudinal bending stresses due to rod and channel bending were classified as primary since small deformations of the grid would not lead to large reductions in the applied load.

Discussion of the magnitudes of each of the listed loads is included in Section 6, and the various grid concepts are evaluated in Section 7.

5.7 STRUCTURAL CRITERIA

The structural criteria used in designing the wave beam grid consists of two general criteria. The first is for material in which the irradiation damage is such that the material can be treated as a ductile material. The second applies to material for which the mechanical properties are such that it cannot be considered ductile. This second criterion considers the material to be quasi-brittle and sets limits based upon the very low capacity of the material to sustain plastic strain from cyclic and short term loading. A detailed discussion of this criterion may be found in Appendix G; however, quasi-brittle limitations will be summarized at this point in Table 5-3. Figure 5-8 is a plot of the stress limits listed on Table 5-3.

Table 5-1
DESIGN LOADS

1. Rod and Channel Bowing
Thermal
Irradiation
2. Relative Rod-to-Grid Swelling
Thermal
Irradiation
3. Rod Vibration
4. Grid Expansion
Thermal
Irradiation
5. Axial* Gradients
Thermal
Irradiation
6. Axial* Pressure Gradients
7. Axial* Rod Growth
Thermal
Irradiation

*See Table 5-2 for definitions of "axial" and "longitudinal"

Table 5-2
GRID SPACER DESIGN LOADS AND STRESSES

	Longitudinal Beam Bending	Axial Beam Bending	Beam Compression	Tab Shear Stress
Rod and Channel Bowing				
Thermal	P			
Irradiation	P			
Rod-to-Grid Swelling				
Thermal	S			
Irradiation	S			
Rod Vibration	P			
Grid Expansion				
Thermal			S	
Irradiation			S	
Axial Gradients				
Thermal		S		
Irradiation		S		
Pressure		P		P
Axial Rod Growth				
Thermal		S		S
Irradiation		S		S

P = Primary Stress

S = Secondary Stress

P — PRIMARY STRESS

S — SECONDARY STRESS

Table 5-3
LIMITS FOR QUASI-BRITTLE STAINLESS STEEL (LESS THAN 10% ELONGATION)^(a)

1. Primary Membrane

The smaller value of:

- a. 50% of minimum ultimate strength
- b. 67% of minimum yield strength
- c. 80% of the minimum stress to produce rupture in the design life of the component
- d. 100% of stress to produce 1% creep in design life of the component

2. Primary Membrane plus Bending

- a. 75% of minimum ultimate strength
- b. 100% of minimum yield strength
- c. 80% of the minimum stress to produce rupture in the design life of component
- d. 100% of stress to produce 1% creep in design life of the component

3. Primary plus Secondary

- a. 100% of the irradiated yield strength

^(a) Assumed to occur at total fluence $>10^{22}$ nvt

Figure 5-8. Stress Limits, Type-304 and Type-316 Stainless Steel $F > 10^{22}$

6. GRID LOADS

6.1 ROD AND CHANNEL BOWING

Both the fuel rod and the hexagonal channels exhibit bowing due to the thermal gradients and irradiation-induced swelling gradients. The channels are very stiff compared with the bundle of rods; the channel moment of inertia is more than ten times the moment of inertia of the rod bundle in its most rigid configuration. As a result, the channels will bow and establish the lateral position of the spacer grids, which in turn locate the fuel rods. Fuel rod bowing is superposed on the bowing imposed by the channel. Because of the mismatch in stiffness between the channel and bundle, the channel bowing results in very small loads between the fuel rod and grid structure and very small bending stresses in the fuel rods. The major portion of the lateral loading between the fuel rods and grids is due to the bowing of the fuel rods; this also results in substantial rod bending stresses. Because of this, the effect of channel bowing or grid stresses can be neglected.

The resultant effects of fuel rod bowing due to thermal and irradiation-induced swelling gradients, including the effects of irradiation induced creep for both the GE and PNL correlations are summarized in Section 4. Of primary interest in grid design are the magnitudes and locations of the support reactions; which are reported in Table 4-3.

These loads were computed for the hot corner rod, which experiences the largest thermal bowing gradient; this gradient is reinforced by the irradiation-induced swelling gradient.

For design purposes, rod 1, which incorporates the effects of nominal irradiation-induced creep, was used. As shown in Section 4, loads up to 3.5 pounds are developed at end-of-life in the nominal case for the modified full grid. Since the grids must at all times be capable of supporting these loads, the load support capabilities of the beams define the lower limit of grid geometry. Furthermore, considering the effects of irradiation-induced creep, the spacer design becomes limited by deflection of the grid support.

It was found that the loads closest to the core midplane (where the fluence levels are highest) are the controlling loads. In the reference design, this occurs at spacer point 5 for the GE correlation, Figure 4-7. Details of this evaluation are described in Section 7.

It must be noted that in the absence of radial thermal gradients, little loading of the grid is found. The central rod support loads are negligible compared to those on the hot edge rods which have large thermal gradients. This case of little or no radial thermal gradient is typical of rods within the center of the bundle where thermal gradients are small and is more realistic indication of the loading to be expected by a majority of the grid cells.

6.2 ROD-TO-GRID SWELLING

While certain components of the core must be designed to accommodate the total swelling, the grids are only required to accommodate that amount of differential swelling which occurs between the fuel rods and grid structure.

Considering a unit cell within the spacer, the growth of the grid structure itself is expected to be the sum of thermal and irradiation-induced expansion. The growth of the fuel is somewhat more complex in nature due to unknowns involved in predicting the effects of fuel swelling, fission gas pressure, etc.; however, in-pile fuel rod irradiations (to date) tend to indicate that long-term fuel rod growth is due primarily to cladding irradiation swelling.* When proper pellet-clad gaps are included in the initial design there is no fuel-cladding mechanical interaction. Thus, for this analysis, the differential swelling between the fuel rods and grid structure was taken as the sum of the thermal growth differential and the irradiation swelling differential caused by the cladding and grid temperature differences. No fluence difference was used due to the close proximity of the grid and rod. Also included was the small rod creep strain due to internal fission gas pressure.

*See Addendum to Appendix G for up to date information

Assuming that the grid temperature follows the coolant temperature, the difference between the grid and average cladding temperature is a direction function of the heat flux or linear power, and may be evaluated for a linear power Q , (kW/ft) as:

$$\Delta T = 4.3 Q (^{\circ}\text{F})$$

Irradiation swelling differences between the cladding and grid were then evaluated using this temperature difference and the GE annealed Type-316 stainless steel correlation:

$$100 \frac{\Delta v}{v} = 3.14 \times 10^{-4} \left(\frac{\phi t}{10^{22}} \right)^{1.81} (T - 633.16)^{0.683} (1048.16 - T)^{0.577} \quad (6-2a)$$

and the PNL annealed Type-316 correlation

$$100 \frac{\Delta v}{v} = 4.9 \times 10^{-49} (\phi t)^{1.71} 10 \left[\frac{1.549 \times 10^4}{T} - \frac{5.988 \times 10^6}{T^2} \right] \quad (6-2b)$$

The coolant temperature used for the hot corner rod, Figure 6-1, which is the temperature of the coolant passage at that location.

Rod strain due to internal fission gas pressure was computed assuming a linear increase from zero to 700 psi at 11,280 hours. The strain was computed for a 15 mil cladding thickness assuming strain hardening with time. Using the irradiation creep strain correlations:

$$\epsilon = 2 \times 10^{-9} \bar{\sigma} + 1.87 \times 10^{-30} e^{(1.405 - 0.0027T)} \bar{\sigma} \phi t + e^{-\frac{58870}{T}} (\sinh(1.79 \times 10^{-5} \bar{\sigma}))^{8.23} e^{62.48} t \quad (6-3)$$

Creep strain was calculated. Due to the low temperatures involved, thermal creep strain, radial creep due to fission gas pressure was very much smaller than rod-to-grid differential swelling.

The resulting relative rod-to-grid growth is very strongly dependent on both linear power and fluence level. This results in the peak relative growth occurring near the peak power point in the rod. However, it occurs before the peak power region is reached because of thermal effects on irradiation swelling which peaks at a temperature of 730°K (850°F) for the above correlations (6-2a, 6-2b). The results of this work are summarized in Figure 6-2.

6.3 VIBRATION

Fuel rods subjected to fluid flow parallel to their axes will exhibit continuous lateral vibration. This vibration generally occurs near the natural frequencies of the rod and has the largest displacement amplitudes at the lowest natural frequency of the supported rod. At the present, most of the reported empirical correlations of the amplitude of a single rod vibration are predictable to a reasonable degree; however, very little work has been reported on tests with multi-rod bundles and hence there are some discrepancies between reported correlations.

The multi-rod prediction method proposed by Reavis can be used with satisfactory accuracy in predicting peak supported rod vibration. Specifically, for F9E conditions, the application of the correlation predicts a peak amplitude of vibration of 0.0002 inches at 284 cps as discussed in Section 4.

Figure 6-1. Coolant Temperature and Radial Thermal Gradient for Hot Corner Rod

Figure 6-2. F9E Rod-to-Grid Swelling

This vibrating rod causes grid support reactions which, when based on a sinusoidal deflection shape, cannot exceed the value predicted by the following relationship:

$$R = \Delta_M \left(\frac{\pi}{L} \right)^3 EI \quad (6-4)$$

where

R	Grid Reaction	pound
Δ_M	Maximum Rod Deflection	inch
L	Rod Length Between Single Supports	inch
E	Modulus of Elasticity	psi
I	Rod Moment of Inertia	inch ⁴

With the predicted maximum displacement, estimates of the grid reactions may be obtained; however, due to the limited amplitude of vibration, these effects are minimal.

GRID VIBRATION—In the previous discussion, it was assumed that the grid structure was rigidly restrained within the channel, preventing any lateral vibration of the grid itself or the rod support points. Grid restraint will be accomplished in practice through the use of grid spacer locking tabs, Figure 3-2. Some rod support movement will occur due to the elastic reaction of the wave beams to the cyclic rod loads from parallel coolant flow; however, since the grid structure is to be designed to resist higher loads than shown, little or no adverse motion is expected.

Unstable, self-excited wave beam vibration is not expected. Although a complete hydro-elastic analysis of the grid structure has not been attempted, these effects are expected to be negligible due to the inherent stiffness of each beam in the assembled bundle and the dynamic fluid effect tending to compress the bundle during operation.

Grid loads due to rod vibration must be revised upward for the modified system because of the presence of staggered grids. The effective distance between rod support points is not clear in the situation where the rod is vibrating. This distance could be anything between 3 inches and 12 inches depending upon whether one-way (staggered grid) support acts as a vibration restraint or not.

Since the reaction force on the grid depends inversely on the cube of the length between supports, the grid load in the revised system could be anywhere between 1/8 and 8 times that on the original grid system. The load in any event will remain small.

6.4 GRID EXPANSION

The undercooled nature of the channel compared to the majority of the internal rods and coolant will result in differential grid expansion relative to the channel. This differential expansion will swell the grid beams to a larger extent than the channel at any axial position. For the longest beam in the grid, this differential growth was computed to be 0.0030 inches for the GE Correlation and 0.0022 inches for the PNL data, (Figure 6-4). This value includes both the effects of thermal and irradiation-induced swelling and is found to peak just beyond the core midplane for the GE data and just before the midplane for the PNL correlation.

In this evaluation, the temperature of the grid beam was taken as the average of the coolant in contact with this beam across the bundle, while the channel was assumed to be at the average temperature of the coolant in the periphery of the bundle. This results in a temperature difference between the grid beam and the channel, as shown in Figure 6-3. This temperature difference, combined with the effects of temperature and fluence on swelling, results in the differential growth shown in Figure 6-4.

Figure 6-3. Grid-to-Channel ΔT F9E, EBR-II Center Beam

Figure 6-4. F9E Grid-Channel Interference Due to Thermal and Irradiation Differential Expansion

In the initial design of F9E it was decided to minimize the spacer tab stress and strain by designing clearances between the grid spacer beam and channel notches by designing a clearance between the grid spacer beam and channel notches under all tolerance conditions. It was a goal to relax this requirement and permit tab-to-channel interference in later designs.

During the fabrication and development testing of F9E grid spacers, it was observed that:

1. Grid spacer tabs did not properly seat in channel notches when the grids and the channel were built to design tolerances;
2. The load required to dislodge tight fitting grid was approximately 75 pounds instead of 30.0 pounds for the grid built to design dimensions.

Because of concern over grid dislodgement during shipping and increased concern over axial grid loads causing axial grid displacement during the life of F9E, a structural analysis of tab stress and strain was conducted.

The structural analysis for the grid spacer that incurs the maximum grid-to-channel relative growth indicates that the maximum bending stress in the tab will be 30,400 psi and that the maximum mechanical strain (creep strain plus elastic strain) will be 0.38%. The former is within the structural criteria limits of 45,300 psi (yield strength of irradiated Type-316 stainless steel at 877°F) which is a secondary stress limiting criteria. The creep strain is within the allowable strain for primary bending.

It was therefore decided to change the design of the F9E grid spacers to incorporate tab interference by increasing the tab angle on most of the grid from 61 to 67 degrees.

There is currently considerable uncertainty in rod to grid loading in the direction of the assembly axis, therefore, in order to investigate the load carrying capability of the initial design without jeopardizing its positioning during shipping and irradiation, the upper load carrying tabs of one staggered and one full grid were built with the initial 61-degree tab angle design. These grids, with both tight fitting and loose fitting grids, could move in the direction of the assembly axis up to 0.040 inches before the lower set of beams would pick up the load and prevent further movement. In this way, it will be possible to investigate the axial loading condition without risking significant grid displacement.

6.5 THERMAL AND FLUENCE GRADIENTS ALONG AXIS OF ASSEMBLY

The grid spacer structure will distort due to swelling gradients in the axial direction caused by both thermal and irradiation effects. Assuming that the local grid temperature is equal to the coolant temperature at that location, this results in an axial thermal gradient of approximately 23°F/in. for F9E.

Considering these gradients by themselves, they represent a negligible loading source on the grid, resulting in insignificant deflections and bending stresses. However, when considered in conjunction with the effects of irradiation swelling, these thermal gradients combined with the axial fluence gradient can result in significant loadings on the grid beams.

The reference irradiation swelling correlations of GE and PNL can be equated to equivalent temperatures or temperature gradients (see Appendix A). For the axial gradients involved, an equivalent thermal gradient (∇T^1) due to irradiation swelling takes the form:

$$\nabla T^1 = C_1 F^{0.81} \frac{\delta F}{\delta Z} + C_2 F^{1.81} \frac{\delta T}{\delta Z} \quad \text{GE} \quad (6-5a)$$

$$\nabla T^1 = C_1 F^{0.71} \frac{\delta F}{\delta Z} + C_2 F^{1.71} \frac{\delta T}{\delta Z} \quad \text{PNL} \quad (6-5b)$$

Where $\delta F/\delta Z$ and $\delta T/\delta Z$ are the actual fluence and thermal gradients occurring at a given location. This equivalent gradient due to irradiation must be added to the actual thermal gradient to obtain the equivalent thermal gradient.

As shown in Figure 6-5, these gradients are additive in the lower half of the core where all gradients are positive. The peak gradient therefore occurs 3 inches from the bottom of the core and is $\sim 415^\circ\text{F/in.}$ for the GE data, and occurs at 3-1/4 inches and is $\sim 305^\circ\text{F/in.}$ for the PNL data. As shown, the upper portion of the core has a lower gradient because of the reversal of the fluence portion of the gradient. Figure 6-6 gives the combined effective gradients. Distortion caused by these gradients is calculated in Section 7.

6.6 PRESSURE GRADIENT

The hydraulic pressure drop through the core results in an axial force on each grid structure. In the region of the grid, this pressure drop is the sum of form losses and frictional losses caused by the grid and fuel rods. For design purposes, however, it is assumed that the entire grid drop is caused by the grid structure.

From Section 7, the pressure drop per unmodified full grid beam was computed to be:

$$\Delta P = 0.355 \text{ psi.} \quad (6-6)$$

Since there are two beams in each grid, the total pressure drop is twice the above. In summary, for the original full grid F9E design conditions:

Coolant Velocity	10 fps
Grid Projected Thickness	0.027 inch
Total Pressure Drop	0.71 psi
Total Grid Load	0.125 pound
Grid Beam Load	0.0048 lb/in.

These loads are very small and can, in practice, be neglected. The pressure drop associated with the staggered design is lower than the full grid and the loads are even smaller than shown.

6.7 AXIAL ROD GROWTH

With the grid spacer attached to the channel, significant relative axial motion can occur between the grid and fuel rods due to the undercooled conditions of the channel relative to the hotter fuel rod cladding. At peak power conditions this difference can be as high as 60°F with relative motions from startup to full power of 0.010 inch. Friction forces during these periods of changing power will result in axial loads on both the fuel rods and grid structure. Due to the limited magnitude of the normal loads applied and the continuous effects of flow induced vibration, significant rod restraint by the grids is not expected. From a design standpoint, however, an upper estimate of the possible magnitude of these loads is required.

A single rod will contact the full wave beam grid at four full contact points because of rod-to-grid differential swelling. Including the effects of creep near end-of-life, a total load of about 1.12 pounds is expected at these points using the GE correlation. Using a coefficient of friction of 1.0 results in a total axial rod load of 4.5 pounds. As this axial load is deflection-limited and far below the elastic buckling load of a 6-inch fuel rod, it does not represent a loading detrimental to fuel rod operation. However, with 37 rods in a bundle, this could result in significant grid loadings if this restraint occurs. A staggered grid beam will have the same number of load contact points as a full grid beam. The loads will be lower, of course, due to the lower spring constant of the staggered grid.

The maximum effects of these loads on the grid beams have been estimated by considering the response of the longest, top-layer, corner-to-corner beam located in the center of the grid structure. For a 37-rod bundle, this beam will feel the axial effects of 13 fuel rods. This results in a distributed load along the longest beam of 14.62 pounds for the conditions considered. For the F9E design, the peak axial load due to relative rod movement is summarized below:

Normal Load	1.12 pounds
Friction Coefficient	1.0
Axial Rod Load	4.50 pounds
Total Grid Load	148 pounds
Maximum Beam Load	14.6 pounds

For the PNL correlation, the loads are:

Normal Load	0.70 pound
Axial Rod Load	2.80 pounds
Total Grid Load	103 pounds
Maximum Beam Load	9.1 pounds

For details at all grid points, see Section 7.

Figure 6-5. Equivalent Axial Temperature Gradient on Grid Due to Thermal and Irradiation Effects
F9E Experiment

Figure 6-6. Combined Equivalent Axial Temperature Gradient on Grid Due to Thermal and Irradiation Effects F9E Experiment

7. GRID STRUCTURAL EVALUATION

7.1 ROD BOWING LOADS - ORIGINAL F9E DESIGN

As previously mentioned in Section 6, the grids must always be capable of supporting the loads imposed by rod bowing, thus defining the lower limit of grid design. Furthermore, considering the effects of irradiation creep, the minimum grid geometry becomes limited by creep deflection considerations of the grid support which, if unlimited, would result in severe reductions in the rod-to-rod or rod-to-channel spacing. The total deflection is the sum of elastic plus creep deformation.

The elastic deflection at end-of-life was computed using the relationships and the total creep deflection described in Section 5 by integrating the effects of a time varying load. Details of this work are found in Appendix E. The resulting procedure was followed at each grid location (Table 7-1) and included the variations in beginning and end-of-life load, fluence, and temperature which exist at each point.

Considering these variations, it is found that the maximum total deflection for the original F9E design occurs at grid point 6 for both the PNL and GE correlations. A combination of high load, fluence, and temperature occurs at this point. At the peak load for the PNL correlation, grid position 13.5 inches, the grid deflection is approximately 90% of the peak deflection point at grid position 7.5 inches due to the reduction in fluence at this point. The GE peak load and peak fluence occur at approximately the same position and so a large deflection of 5.66 mils is noted.

For the peak deflection point, grid position 7.5 inches, the deflection for various grid geometries is shown in Figure 7-1. The effects of 50% and 125% of the reference irradiation creep value were calculated and found to be minimal. As shown, in Table 4-1, there is very little effect of the variation since the grid loads at this point also vary in a proportional fashion.

The original reference grid geometry of 0.350 X 0.012 inch ($h/t = 29$) was selected to limit the creep deflection of the outer rod rows to approximately 8% of the rod-to-channel clearance. For this reference, the resulting grid deflections and grid bending stresses are shown in Table 7-1. Grid stresses were computed from the expression in Section 5,

$$\sigma/P = 11,500 \text{ psi/lb}$$

since at all times the grid must be in equilibrium with the applied loads.

Table 7-1
ORIGINAL GRID DESIGN - GRID DEFLECTIONS AND STRESSES

Grid Position (inches from core bottom)	Initial Load (lb)	Final Load (lb)	Elastic Deflection (mils)	Creep Deflection (mils)	Total Deflection (mils)	Grid Bending Stress (psi)
GE Correlation						
1.5	0.418	1.326	1.19	1.84	3.03	15249
7.5	-0.293	-2.816	-2.61	-3.04	-5.66	32384
13.5	-0.470	0.314	0.30	-0.07	0.22	3611
PNL Correlation						
1.5	0.418	-0.237	-0.21	0.18	-0.03	2725
7.5	-0.293	-1.699	-1.57	-1.94	-3.52	19538
13.5	-0.470	2.165	2.09	1.04	3.13	24897

Figure 7-1. Original F9E Grid Spacer Design Grid Position 7.5 inch from Core Bottom

7.2 ROD BOWING LOADS - REVISED F9E DESIGN

As presented in Section 4, in the core the maximum reaction between the corner rod and the grid beam increases from 2.82 pounds as given in Table 4-1 to 3.01 pounds as a result of grid design revisions made. This load occurs at the grid located at 7.5 inches from the bottom of the core. The maximum reaction to the rod is 3.53 and occurs at 1.5 inches below the core. It is less severe, however, due to the much lower temperature.

The predicted behavior of the grid due to the rod bowing load is being revised at this time because of a reassessment of the structural behavior which is unrelated to the changes in the experiment.

The assessment of the original grid design in subsection 7.1 treated the grid behavior as if the rod was deflecting in a direction normal to a single grid beam. However, as the rods which are most critically affected by bowing are those located at corners, consideration must be given to the specific mechanical behavior at a corner. Consider the following (Sketch E) which shows the corner rods in a full grid.

Two corner rods are shown, labeled "1" and "2". Note that the manner in which the rods are enclosed by the contacting grid beams differs for the two rods. If the grid is rotated 60 degrees counterclockwise, then both corner rods of interest would be type 1. At 7.5 inches from the bottom of the core, the orientation is as shown in Sketch E above.

The directions of the rod displacement at the corners, and thus the line of action of the resultant forces restraining the deflections (3.01 pounds, as stated earlier), are along the diagonal lines which go across the corners (see Sketch F). For rods 2 and 1 these loads are as follows:

The resultant reaction on rod 2 is provided by contacts at two points on the rod by contacts made with two different beams. Rod 1 is restrained by contact at one point. Assuming contact loads can only be transferred as normal loads, the following diagrams (Sketch G) illustrate the manner in which the contact loads are developed.

It is thus shown that for the *same resultant load* the value of the contact *load* for a type 1 corner is twice the value as that at a type 2 corner. Furthermore, at a type 1 corner the contact load is 15.6% greater than the resultant restraining load. Thus, for a 3.01-pound resultant load, the contact load at the type 1 corner is 3.48 pounds. This is the load at 7.5 inches from the bottom of the core.

It should further be considered that the contact loads have been assumed to be supported by a length of beam equal to one pitch. This would be quite appropriate if all rods along the outer row were forcing against the beam. For a load at only the corner rod, the wave beam will be supporting the load by plate bending in two directions rather than just by wave flattening. Considering this manner of support should result in a decrease of both the deflection and the maximum bending stress. In the future, the problem will be rigorously investigated, either analytically or by mechanical testing. Until then the conservative assumption of bending only along the wave direction will be followed.

Table 7-2 shows the revisions to Table 7-1 due to the modified grid designs in the reference F9E design for the grid position at 7.5 inches from the bottom of the core and, in addition, shows the results at 1.5 inches below the core. The original design had no grid at this location.

Table 7-2
RESULTS FOR REFERENCE F9E MODIFIED GRIDS - REVISION TO TABLE 7-1

Grid Position (inches from core bottom)	Initial Load (lb)	Final Load (lb)	Elastic Deflection (mils)	Creep Deflection (mils)	Total Deflection (mils)	Grid Stress (psi)
GE Correlation						
-1.5 (Below Core)	-0.49	-4.08	-3.80	-2.50(~)	-6.30	46,800
7.5 (Corner 1)	-0.37	-3.48	-3.24	-3.75	-6.99	39,900

7.3 ROD-TO-GRID SWELLING - ORIGINAL FULL GRID

The maximum rod-to-grid swelling was computed in Section 6 to be 0.62%, Figure 6-2. As swelling occurs, the resulting grid distortion produces a reaction load between the rod and grid. If no creep were occurring, this reaction would merely be related to the grid distortion in an elastic manner as shown in Section 5. This is not the case, however, and the effects of creep will play an important role with the peak reaction coinciding with maximum growth rate, not total growth. In theory, due to the secondary nature of the loading, at a point of zero relative growth rate, no rod-to-grid reaction would occur.

With rod-to-grid growth related primarily to irradiation swelling, peak reaction loads will occur at end-of-life due to the ever increasing rod-to-grid growth rate. This growth rate which equals the deflection rate of the grid was then used to determine the reaction of the grids. Details of this procedure are shown in Appendix F.

The results of this work are shown in Figure 7-2 as a function of axial position and include the variations in grid temperature, fluence, and linear power. As shown in this figure, the resulting grid reaction peaks for the GE correlation at the start of the core and just below the point of maximum fluence (6 inches) for the PNL correlation. This figure is based on the original full grid geometry of 0.350 X 0.012 inches. For various grid geometries, the variation in the peak reaction is shown in Figure 7-3. The effects of 50% and 125% of the creep correlations were calculated, but are not shown here.

Finally, a summary of relative growth, grid reaction, and grid stress intensity are presented in Table 7-3 for each specific grid location in the original bundle design. The grid stresses and swelling loads are calculated considering creep.

Table 7-3
ORIGINAL DESIGN ROD-TO-GRID SWELLING (CONSIDER CREEP)

Grid Position (inches from core bottom)	Relative Strain (%)	Swelling Load (lb)	Grid Stress (psi)
GE Correlation			
1.5	0.62	0.82	9850
7.5	0.42	0.52	6240
13.5	0.07	0.10	1200
PNL Correlation			
1.5	0.36	0.51	6120
7.5	0.17	0.24	2880
13.5	0.08	0.00	0

Figure 7-2. F9E Original Design Rod-to-Grid Swelling Loads

Figure 7-3. F9E Original Full Grid Spacer Design Grid Position 0-inch GE, 3-inch PNL, from Core Bottom 100% Creep Type-316 annealed

7.4 ROD-TO-GRID SWELLING - REVISED GRID DESIGN

In Section 5 it was explained that the modified straight wave beam full grid needs no further analytic evaluation. The modifications were made so that the behavior of the wave beams would be as originally conceived, and as originally analytically investigated, except for an increase in the spring constant of approximately 5%.

The behavior of the staggered grid is different, and in Section 5 it was shown that its spring constant and the resulting loads and stresses in the beam can be scaled by a factor of 0.42. Thus, Table 7-3 can be modified to represent the substitution of the staggered grid for a full grid at 1.5 inches from the bottom of the core.

The behavior of the straight wave beam full grid reversed has been evaluated using a two-dimensional plate bending analysis. The complete results of the analysis are presented in Appendix H. With these results, Table 7-3 has been revised as shown in Table 7-4 to consider both layers of beams in the grid at 7.5 inches from the bottom of the core, revised values for the straight wave beam full grid reversed and the original values for the modified straight wave beam.

Table 7-4
ROD-TO-GRID SWELLING

Grid Position (inches from core bottom)	Relative Strain (%)	Swelling Load (lb)	Grid Stress (psi)
GE Correlation			
1.5	0.62	0.34	4130
7.5	0.42	1.43	12100
PNL Correlation			
1.5	0.36	0.21	2570
7.5	0.17	Not available	Not available

As a part of the revised evaluation, it has been reasoned that when the grid stress and contact loads induced by rod-to-grid swelling are to be combined with grid stresses and contact loads from rod bowing restraint, consideration must first be given to grid deflections associated with both behaviors. The deflection induced in the grid from a rod bowing load provides clearance to accommodate the rod-to-grid swelling displacement. Thus, the wave beam deflection is required to accommodate rod swelling in excess of the deflection induced by rod bowing. It is to be recognized that in order to fully utilize the clearance provided by the bowing induced grid deflection, some bending along the length of the rod must occur, in the same manner as occurs for the staggered grid concept. In the previous sections it was shown that a 3-inch length of rod between grids will significantly decrease the contact loads caused by rod-to-grid swelling. Consider that the grid at 7.5 inches from the bottom of the core has a 3-inch length of rod in one direction and a 6-inch length of rod in the other direction. In Section 5, it was shown that the flexibility contribution from rod bending varies directly as the *cube* of the length.

At 7.5 inches from the bottom of the core, the grid deflection caused by rod bowing loads is 6.99 mils, which is the sum of elastic and creep components of 3.24 mils plus 3.75 mils. The total grid displacement necessary to accommodate the 0.42% relative strain from rod-to-grid swelling is 1.12 mils, or less than 20% of the clearance provided by the rod bowing induced grid deflection. Thus, rod-to-grid swelling-induced stresses will not occur near edge and corner rods (which bow), but these stresses will be present in the interior of the rod bundle, where bowing is much less.

7.5 ROD VIBRATION

The results of the rod vibration analysis are shown in Section 4, Figure 4-14, where grid loads from 0.20 to 0.015 pounds are expected depending on axial location in the bundle. For the original full grid geometry (0.350 X 0.012 inches) the resultant stress per unit load was computed to be:

$$\sigma/P = 11500 \text{ psi/lb}$$

which, when considering the grid loads due to flow induced vibration, results in the following:

Grid Position (inches from core bottom)	Grid Load (lb)	Grid Stress (psi)
1.5	0.13	1490
7.5	0.07	800
13.5	0.04	460

As mentioned in Section 5, these stresses were classified as primary when combined with other stresses caused by longitudinal grid loads.

7.6 GRID EXPANSION

The GE and PNL swelling correlations predicted 3.3 and 2.2 mils respectively for grid-to-channel differential expansion at the worst position, at end of life conditions for F9E. Thus, for the GE swelling correlation, 1.65 mils is the maximum interference predicted between each of the two tab ends of the longest grid beams.

The relative fit of the beam tab into the channel notch is designed as an interference fit. To evaluate the worst condition during irradiation, the maximum possible initial interference is assumed, which results in yielding of the tab at its cantilever root at insertion. This possible yielding is considered the equivalent of the tab bending incurred during fabrication. It results in a combined elastic and creep strain during irradiation that is limited to a value which is equal to the elastic recovery strain available at insertion. This strain is conservatively estimated as 0.125%, i.e., $\sigma_y/E \times 100$). During irradiation the combined effects of irradiation induced swelling and creep occur. This results in an initial decrease in the tab bending stress to less than 20,000 psi due to creep relocation of the initial load. The effects of swelling then causes the stress to increase to 30,400 psi at E.o.L.

A structural analysis of the tab bending characteristics showed that, for elastic bending in the presence of irradiation induced creep, a total strain (elastic and irradiation creep) of 0.00158 in./in. is incurred at the worst strained location for each mil of tab to notch interference. Using the GE correlation value of 1.65 mils interference due to swelling, an additional strain of 0.00257 in./in. is added to the 0.00125 of recoverable strain incurred at assembly. Thus, the maximum total strain subjected to an irradiative environment is 0.382%.

7.7 AXIAL THERMAL AND IRRADIATION GRADIENTS

The axial thermal gradient plus the equivalent thermal gradient due to irradiation swelling have been computed in Section 6. Since the grid is simply supported at the channel interface, it is free to distort in a spherical fashion from these gradients. This unrestrained distortion will not produce significant stresses in the beams; however, movement relative to the rods will occur. The magnitude of this movement can be found by looking at the unrestrained distortion of the longest beam in the grid to the gradient.

$$\Delta = \frac{\alpha L^2}{8} \frac{dT}{dx}$$

For the maximum 410°F/in. which occurs at the bottom of the core and a maximum beam length of 2.138 inches,

$$\Delta = 10 \times 10^{-6} \frac{(2.138)^2}{8} (410),$$

$$\Delta = 0.0023 \text{ in.}$$

This is an end-of-life value which includes the effects of irradiation swelling. Thermal distortion which might occur at beginning of life would be approximately 1/20 of this value.

7.8 AXIAL PRESSURE GRADIENT

The axial pressure drop through the full grid results in an axial load on the grid beams of 0.00416 lb/in. The longest beam will see a total load due to its own drag plus a contribution from the lower beam level. If it is assumed that the total load is taken up by the beam, then the area of the lower beam resisting pressure which will be transferred to the upper beam is the length between two beams (rod pitch) times the number of lower beams crossing the upper beam. This is illustrated in Sketch H below.

The total load for this beam is then given as:

$$W = 0.00476 (2.138) + 0.00476 (0.320) (7),$$

$$W = 0.0208 \text{ lb.}$$

For a uniformly loaded beam, the bending moment is:

$$M = \frac{WL}{8} = \frac{0.0208 (2.138)}{8},$$

$$M = 0.0056 \text{ in/lb.,}$$

and a bending stress in the reference geometry (0.350 X 0.012) of

$$\sigma = \frac{Mc}{I} = \frac{0.0056 (0.250)}{\frac{0.012 (0.5)^3}{12}},$$

$$\sigma = 11 \text{ psi}$$

which may be neglected.

7.9 AXIAL ROD GROWTH - ORIGINAL FULL GRID DESIGN

With the grid spacer attached to the channel, significant relative motion can occur between the grid and fuel rods due to the undercooled nature of the channel relative to the hotter fuel rod cladding and due to irradiation-thermal distortion of the grid itself.

Including the effects of creep, the peak normal load near end-of-life was found to be 0.82 pounds at the grid at 1.5 inches above the bottom of the core (Table 7-3 B, 100% creep GE correlation) which, with a coefficient of unity, will equal the axial grid load per contact point. This load is typical of most internal cell locations where thermal bowing gradients are small. At the edge row of rods the normal grid loads can be higher; however, they are not typical of the majority of the cells within the grid and thus have not been considered.

The longest top layer, corner-to-corner, beam will see a total axial load from 13 rods. For this analysis, it will further be assumed that the total axial load is not only due to rods in contact with it, but also due to a contribution from the lower beam level (Sketch I).

Thus the total load for this beam is then given as:

$$W = 0.82 (13) + \frac{0.82 (13) (2)}{2}$$

$$W = 21.32 \text{ lb.}$$

For a uniformly loaded beam, the bending moment is:

$$M = \frac{W\ell}{8} = \frac{21.32 (2.136)}{8}$$

$$M = 5.69 \text{ in./lb.}$$

and a bending stress in the reference beam geometry, (0.350 X 0.012) of

$$\sigma = \frac{Mc}{I} = \frac{5.69 (0.25)}{\frac{(0.012) (0.5)^3}{12}} = 11384 \text{ psi}$$

$$\sigma = 11384 \text{ psi.}$$

This stress is classified as a secondary bending stress since small deflections will reduce its magnitude, i.e., it is self-limiting.

Since the normal loads on the grid vary from position to position (Table 7-3), the magnitude of the computed bending stress will also change. This results in the stresses at each grid point given in Table 7-5.

Table 7-5
GRID BENDING STRESSES CAUSED BY ROD-GRID FRICTION ORIGINAL F9E DESIGN

Grid Position (inches from core bottom)	Normal Reaction (lb)	Beam ^(a) Load (lb)	Bending Stress (psi)
GE Correlation			
1.5	0.82	21.3	11384
7.5	0.52	13.5	7219
13.5	0.10	2.6	1388
PNL Correlation			
1.5	0.51	13.2	7080
7.5	0.24	6.2	3331
13.5	0	0	0

^(a) Pressure loads have been neglected.

7.10 AXIAL ROD GROWTH - REVISED GRID DESIGN

A revision must be made to subsection 7.9 to consider the reduced contact loads at the grid at 1.5 inches from the bottom of the core due to the revised F9E design. The loads for the staggered grid at this location can be scaled down by the spring stiffness ratio factor of 0.42, and the stresses can be further scaled by a factor of 0.70 due to the increased bending section modulus which results from the increased depth of the modified beam. The grid at 7.5 inches from the bottom of the core will have significantly higher contact loads on the layer with the straight wave beam full grid reversed and thus have increased bending stresses in the beam. Table 7-5 is changed as shown in Table 7-6. (A proportionality between the "Normal Reaction" column and the "Beam Load" column does not exist between the two entries for the grid at 7.5 inches because of the different manner in which the grid beams are loaded).

Table 7-6
REVISED GRID STRESS DUE TO AXIAL ROD GROWTH

Grid Position (inches from core bottom)	Normal Reaction (lb)	Beam ^(a) Load (lb)	Bending Stress (psi)
GE Correlation			
1.5 (Modified)	0.34	9.0	3360
7.5 (Reversed)	1.43	29.7	15900
PNL Correlation			
1.5 (Modified)	0.21	5.6	2060
7.5 (Reversed)		Not Available	

(a) Pressure loads have been neglected.

7.11 TAB SHEAR LOAD - ORIGINAL FULL GRID DESIGN

In addition to the bending stress induced in the beams due to the axial load, this load will induce shearing in the holding tabs at the grid-channel interface. The end tab is 0.125 inch thick in the original full grid geometry (0.350 X 0.012 inch)

$$A = t_h = 0.012 (0.125)$$

$$A = 0.0015 \text{ in.}^2$$

and at the peak load position (grid point 10 at 1.5 inches, Table 7-5) for the GE correlation

$$R = P/2 = \frac{21.3}{2}$$

$$R = 10.6 \text{ lb,}$$

thus

$$\sigma = \frac{R}{A} = \frac{10.6}{0.0015} = 7066 \text{ psi.}$$

Since the normal loads vary from position to position. Table 7-5, the magnitude of the computed tab shear stress will also change. This results in the following stresses at each grid point shown in Table 7-7.

Table 7-7
TAB SHEAR LOADS IN ORIGINAL FULL GRID DESIGN

Grid Position (inches from core bottom)	Beam ^(a) Load (lb)	Tab Reaction (lb)	Tab Shear (psi)
GE Correlation			
1.5	21.3	10.6	7066
7.5	13.5	6.7	4466
13.5	2.6	1.3	866
PNL Correlation			
1.5	13.2	6.6	4399
7.5	6.2	3.1	2066
13.5	0	0	0

(a) Pressure loads have been neglected.

7.12 TAB SHEAR LOAD - REVISED F9E DESIGN

A revision must be made to subsection 7.11 to consider the changes in contact loads. This results in changes in Table 7-7 due to the grid design changes in the reference F9E assembly. Some changes are also reflected in Table 7-8.

Table 7-8
TAB SHEAR LOADS IN REVISED F9E DESIGN

Grid Position (inches from core bottom)	Beam ^(a) Load (lb)	Tab Reaction (lb)	Tab Shear (psi)
GE Correlation			
1.5 (Modified)	9.0	4.5	2970
7.5 (Reversed)	29.7	14.9	9920
PNL Correlation			
1.5 (Modified)	5.6	2.8	1850
7.5 (Reversed)	Not Available		

(a) Pressure loads have been neglected.

7.13 SUMMARY OF ORIGINAL GRID DESIGN STRUCTURAL EVALUATION

Tables 7-9 and 7-10 summarize the results of the work supported in Section 7 on the original F9E design. As shown, the reported stresses have been classified and combined as was shown in Table 5-2 and compared with the limits applicable at the location of the grid. Note that at two points the criteria appears to be exceeded. Both of these bowing loads which cause large primary stresses. However, the criteria is for a steady-state stress, whereas the bowing loads are initially small and increase rapidly toward the end of life. The criteria allows the stress-rupture evaluation to be performed by integrating through time to calculate cumulative value. This is summarized in Figure 7.4 which shows the

Table 7-9
LONGITUDINAL GRID STRESSES—ORIGINAL DESIGN

Grid Position (inches from core bottom)	Grid Temperature (°F)	Rod Bowing ^(b) (primary) (psi)	Rod Vibration (primary) (psi)	Rod-to-Grid ^(b) Swelling (secondary) (psi)	Grid ^(b) Expansion (secondary) (psi)	Total Primary (psi)	Criteria Primary (steady-state stress) (psi)	Primary and Secondary (psi)	Criteria Primary and Secondary (steady-state stress) (psi)
GE Correlation									
1.5	730	15249	600	9850	0	15849	31000	25699	55000
7.5	871	32384	400	6240	0	32784 ^(c)	30700	39024	46000
13.5	1033	3611	300	1200	0	3911	16000	5111	35500
PNL Correlation									
1.5	730	2725	600	6120	0	3325	31000	9445	55000
7.5	871	19538	400	2880	0	19938	30700	22818	46000
13.5	1033	24897	300	0	0	25197 ^(d)	16000	25197	35500

(a) See Table 5-2 for direction conversion.

(b) End-of-life value of stress, stress built up throughout life in core.

(c) Equivalent to steady-state stress of 26100 psi.

(d) Equivalent to steady-state stress of 14500 psi.

Table 7-10
AXIAL GRID STRESSES—ORIGINAL DESIGN

Grid Position (inches from core bottom)	Grid Temperature (°F)	Axial Thermal Irradiation and Pressure Gradients (psi)	Axial Rod Growth (psi)	Total Primary (psi)	Criteria, Primary (psi)	Primary and Secondary (psi)	Criteria, Primary and Secondary (psi)
GE Correlation							
1.5	730	0	11384	0	31000	11384	55000
7.5	930	0	7219	0	30700	7219	46000
13.5	1033	0	1388	0	16000	1388	35500
PNL Correlation							
1.5	730	0	7080	0	31000	7080	55000
7.5	930	0	3331	0	30700	3331	46000
13.5	1033	0	0	0	16000	0	35500

(a) See Table 5-2 for direction conversion.

(b) Primary Pressure Stress of 11 psi was neglected.

Figure 7-4. Rupture Life Fraction of Original Grid Beam Design

fraction of life used for different load factors using the General Electric material behavior correlation. The "load factor" represents a scale factor applied to the load through the entire life. Thus the load factor of 1.00 is for the predicted loading, and 1.10, for example, assumes the load to be 10% higher than the predicted load at all times. It is seen that for the predicted load history, at the end of life in the reactor, the fraction of rupture life used is 9.3%. However, an increase of loading through entire life of approximately 17% will cause rupture, and an increase in load of approximately 15.5% will exceed the criteria of 80% of rupture life. These latter two considerations more meaningfully assess the degree of positive margin of safety.

As an exercise, an equivalent steady-state stress was calculated which results in 9.3% of rupture life being used by end of life in the reactor. This value was 26,000 psi which is slightly less than the criteria limit of 30,700 psi.

When the same evaluation was performed using the PNL correlation, the fraction of life used was 52%. Therefore, both correlations indicate the design will meet the criteria. The difference between the correlations occurs because the peak bowing loads are at different grid locations, and hence different temperatures. Therefore, the PNL correlation gives a more critical stress because it occurs at a higher temperature even through it is a small load.

7.14 SUMMARY OF CHANGES IN GRID STRUCTURAL EVALUATION DUE TO REVISIONS IN ORIGINAL F9E DESIGN CONCEPT

Table 7-2 shows that due to the revised F9E grid designs, high grid bending stresses are caused by corner rod bowing loads at the grids at 7.5 inches above the bottom of the core and at 1.5 inches below the core. At both of these locations the stress levels exceed the criteria. At the grid which is 7.5 inches above the bottom of the core, the limiting criteria is 80% of stress rupture life. This criteria is exceeded after 10,700 hours. At the grid which is 1.5 inches below the core, the limiting criteria is 3/4 ultimate tensile stress, which is used for primary bending. The criteria allowable of 46,500 psi is exceeded at approximately 11,000 hours. At this latter location stress rupture is not limiting because of a considerably lower temperature than exists at 7.5 inches above the bottom of the core. Figure 7-5 shows the stress history at the two locations. Figure 7-6 shows the cumulative stress rupture effect.

It has been stated earlier, in the latter part of subsection 7.2, that the analysis performed to evaluate the grid bending stress caused by corner rod bowing is considered conservative. A rigorous assessment of the structural behavior of the grid bending due to corner rod bowing will be performed in the future to assess the implication of the high stress levels on planned F9E 11,280 hour life.

Tables 7-11 and 7-12 show revisions to Tables 7-9 and 7-10 which result from the revised F9E design.

As discussed in subsection 7.6 the reference grid to channel interference fit results in stresses and strains which satisfy the structural criteria.

Table 7-11
LONGITUDINAL STRESSES (REVISION OF TABLE 7-9)

Grid Position (inches from core bottom)	Grid Temperature (°F)	Rod Bowing (primary) (psi)	Rod Vibration (primary) (psi)	Rod-to-Grid Swelling (secondary) (psi)	Grid Expansion (secondary) (psi)	Total Primary (psi)	Criteria, Primary (steady-state stress) (psi)
GE Correlation							
-1.5	700	46900	920	0	0	47820	46500
7.5	930	39890	400	6240	0	40690 ^(b)	30700

(Type 1 Corner)

(a) See Table 5-2 for direction conversion.

(b) Considering only the rod-bowing stress history, as the vibration stress does not contribute to stress rupture, this is equivalent to a steady-state stress of 31,100 psi.

Table 7-12
AXIAL STRESSES

Grid Position (inches from core bottom)	Grid Temperature (°F)	Axial Thermal Irradiation and Pressure Gradients (psi)	Axial Rod Growth (psi)	Total Primary (psi)	Criteria, Primary (psi)	Primary and Secondary (psi)	Criteria, Primary and Secondary (psi)
GE Correlation							
1.5	730	0	3360	0	46500	3360	55000
7.5 (Modified)	930	0	5020	0	30700	5020	46000
7.5 (Reversed)	930	0	15900	0	30700	15900	46000

(a) See Table 5-2 for direction conversion.

Figure 7-5. Grid Bending Stress History

Figure 7-6. Stress-Rupture Analysis for Grid at 7.5-inch above Core Bottom

8. SPECIAL STRUCTURAL EFFECTS

Two special structural effects were examined to determine whether or not they would affect wave beam grid performance in a detrimental manner. The first effect dealt with propagation of cracks if a weld should fail on the grid and the second with the possibility of self- or diffusion-welding between grid and rod.

8.1 CRACK PROPAGATION

The upper and lower grid sections are spot welded together at an angle of sixty degrees. The weld penetrates each grid beam to a depth of 0.030 inch. If one of these welds were to break it would leave a semicircular hole or crack in the edge of the grid of radius 0.030 inch. If this crack, because of thermal or mechanical cycling, propagates itself until the beam is broken completely into two pieces, the utility of the grid spacer might be compromised. The following analysis will show that the crack is not expected to propagate for any meaningful distance.

For a large variety of steels,⁸⁻¹ the growth rate of a fracture has been shown to be approximately equal to the power of the cyclic range of K, or as:

$$\frac{da}{dN} = A (\Delta K)^B$$

where

$\frac{da}{dN}$ = crack propagation rate, inches/cycle

ΔK = range of crack-tip stress intensification and

A,B = constants of the material.

An expression which is based on the data in Figure 8-2 is stated:

$$\frac{da}{dN} = 10^{-22} \Delta K^4$$

For the crack shown in Figure 8-1 an expression for ΔK was derived:

$$\Delta K = \sigma \sqrt{\pi a} f(a/b)$$

where $f(a/b)$ vs a/b is plotted in Figure 8-3. The definition of a and b are shown on Figure 8-1.

The present reference design for the grid spacer is composed of beams 0.35-inch high. When two of these beams are welded together the weld is 30 mils deep in each beam. If one of these welds breaks it will leave a 30 mil semicircular crack in the beam. These beams are subject to various stress at different frequencies. The following studies examine the effect of some of these stresses for the F9E grid environments.

	<u>Stress, psi</u>
Rod Bowing	46900
Rod-to-Grid Swelling	12100
Rod Vibration	920
Grid Expansion	0
Axial Gradients	0
Axial Rod Growth	15900

Because vibration is the only high cyclic stress, it is the only one applicable to crack propagation. The present mode of vibration is at ~280 Hz.

where

$$\Delta K = \sqrt{\sigma \pi a} \quad f(a/b)$$

$$a = 30 \text{ mils}$$

$$b = 350 \text{ mils}$$

$$a/b = 0.086 \text{ mils} \quad \text{from Figure 8-3 } f(a/b) = 1.15$$

$$\Delta K = 2.04 \sigma \sqrt{a}$$

or

$$\frac{da}{dN} = 10^{-22} (2.04)^4 \sigma^4 a^2$$

$$\frac{da}{dN} = 10^{22} \frac{1}{17.26 \times a^2 \sigma^4}$$

$$dN = \frac{10^{22}}{17.26 \sigma^4} \frac{da}{a^2}$$

$$\int_0^N dN = \frac{10^{22}}{17.26 \sigma^4} \int_{a_0}^{a_N} \frac{da}{a^2}$$

$$N = \frac{10^{22}}{17.26 \sigma^4} \left(-\frac{1}{a_N} + \frac{1}{a_0} \right)$$

Of interest is in the number of cycles it will take for the crack to double its length, then:

$$a_N = 2a_0$$

$$\begin{aligned} N &= \frac{10^{22}}{17.26 \sigma^4} \left(-\frac{1}{2a_0} + \frac{1}{a_0} \right) \\ &= \frac{10^{22}}{17.26 \sigma^4} \left(\frac{1}{2a_0} \right) = \frac{10^{24}}{34.5 \sigma^4} \end{aligned}$$

For F9E, the stress at the point of contact with the rod is 920 psi. This however is not the stress needed in the equation but rather the stress at the point of the crack. Finite element analysis has shown that for the F9E grid this stress is about 25% of the stress at the point of contact. Therefore we will input 230 psi.

$$N = \frac{10^{24}}{103.5 (0.230)^4 \times 10^{12}} = 3.45 \times 10^{12} \text{ cycles}$$

The number of cycles F9E will see is related to the time in the reactor (11,280 hours).

$$N = 11,280 \times 3600 \times 280 = 1.137 \times 10^{10} \text{ cycles}$$

Therefore the crack will not double its length. Exposed to 1.137×10^{10} cycles it will increase by:

$$1.137 \times 10^{10} = \frac{10^{22}}{17.26\sigma^4} \left(-\frac{1}{a_N} + \frac{1}{a_0} \right)$$

$$17.26 * \frac{(0.230)^4 * 10^{22} * 1.137 \times 10^{10}}{10^{22}} = \left(-\frac{1}{a_N} + \frac{1}{a_0} \right)$$

$$0.05488 = \left(-\frac{1}{a_N} + \frac{1}{a_0} \right)$$

$$= \left(-\frac{1}{a_N} + \frac{1}{0.03} \right)$$

$$0.05488 = -\frac{1}{a_N} + 100$$

$$\frac{1}{a_N} = 33.2784$$

$$a_N = 0.0300495$$

$$a_N - a_0 = 0.000050 \quad \text{a negligible amount}$$

Therefore the grid will not fail if a weld either breaks or is improperly formed. The case of the total stress at the rod-grid contact point was calculated and it was found that for a constant stress of 920 psi the grid crack propagated to 1.5% the height of the grid. This analysis is also conservative in that the formula used is for a sharp edged crack, rather than a semicircular hole, in the edge of the grid. It is therefore concluded that the grid is extremely unlikely to fail because of crack propagation.

8.2 SELF-WELDING

The literature was examined for data on self-welding and/or diffusion welds. At this point in time it does not appear that there is significant experience in this area. No correlations were found which would predict that self-welding will take place under the F9E loading and temperature conditions or what would be the strength of the welds formed in this manner.

Experience has demonstrated that self-welding depends on environmental conditions, cleanness of surfaces, roughness of surfaces, force of compression and materials that are in contact. Fuel rod vibration will discourage self-welding between the grids and fuel rods. This will further inhibit the possibility of self-welding occurring. In any case, the data from F9E on whether or not self-welding does occur will be extremely valuable.

Figure 8-1. Simulated Weld Crack

Figure 8-2. Estimated ΔK versus da/dN Curves for Type-300 Stainless Steel

Figure 8-3. Geometrical Coefficients for Crack Propagation Equation

9. THERMAL-HYDRAULIC ANALYSIS

9.1 EDGE SPACING

A study was performed to determine an optimum fuel rod-to-liner spacing for the test assembly. For this effort, the optimum spacing was defined as that spacing which would minimize the thermal bowing gradient acting on any fuel rod in the assembly. Information on these data is reported in Tables 9-1 and 9-2. The thermal bowing gradient was defined in Section 3 as the change in cladding temperature around 180 degrees of rod circumference. As its name implies, this thermal gradient is a prime driving force which produces lateral deflection of fuel rods between support points. Thus, optimization of the rod-to-liner spacing was directed toward minimizing fuel rod lateral deflections caused by differential thermal expansion. The optimum spacing was found to be 42 mils; the analysis is described below.

Table 9-1
OPTIMUM SPACING OF FUEL ROD-FROM-CHANNEL
WALL FOR VARIOUS DESIGN CRITERIA
(37 ROD ASSEMBLY IN EBR-II, ROW 5)

Design Objective	Spacing (mils)	Bowing ΔT on Edge Rod ($^{\circ}F$)	Bowing ΔT on Corner Rod ($^{\circ}F$)	Local ΔT on Edge Rod ($^{\circ}F$)	Local ΔT on Corner Rod ($^{\circ}F$)	Hottest Coolant Outlet Temperature ($^{\circ}F$)
Minimize Edge Rod Bowing ΔT	36	0	90	28	98	1072
Minimize Edge Rod Local ΔT	35	4	100	24	104	1081
Minimize Corner Rod Bowing ΔT	46	58	0	66	54	1000
Minimize Corner Rod Local ΔT	46	58	0	66	54	1000
Minimize Hot Spot Coolant Temperature	46	58	0	66	54	1000
Minimize Bowing ΔT on any Rod	42	36	36	52	70	1026
Minimize Local ΔT on any Rod	44	48	16	62	62	1013

Table 9-2
SENSITIVITIES OF THERMAL GRADIENTS ACROSS FUEL RODS AND OF THE HOT CHANNEL
COOLANT TEMPERATURE TO CHANGES IN ROD-TO-WALL SPACING
(37 ROD ASSEMBLY IN EBR-II, ROW 5)

Thermal Parameter	Sensitivity to Increase in Spacing ($^{\circ}F/mil$)	Range of Spacings
Bowing Gradient	-9.2	34-46
Across a Corner Rod	+7.5	46-50
Bowing Gradient	-5.0	34-36
Across an Edge Rod	+6.0	36-50
Local Gradient	-5.0	34-46
on a Corner Rod	+4.0	46-50
Local Gradient	-6.0	34-35
on an Edge Rod	+4.0	35-50
Hot Channel Coolant	-6.0	34-46
Temperature	+2.0	46-50

Coolant temperature distributions within the fuel assembly were calculated for rod-to-liner spacings that ranged from 34 to 50 mils. These calculations were based upon interchannel turbulent mixing data found empirically by GE. The study was made for an assembly operating in row 5 of the EBR-II core, but the operating conditions were very similar to those expected of row 6 operation. Fuel rods located in the outer row of the bundle were found to exhibit the largest thermal gradients. Accordingly, the study was limited to evaluating two characteristic fuel rods; these were the corner rod and the edge rod. The locations of these rods are illustrated on Figures 9-1 and 9-2.

Figure 9-1. Coolant Temperature Variation Around the Circumference of a Corner Rod versus Fuel Rod-to-Channel Wall Spacing

Figure 9-2. Coolant Temperature Variation Around the Circumference of an Edge Rod versus Fuel Rod-to-Channel Wall Spacing

Figure 9-1 shows the variation of coolant temperature around the circumference of the corner rod. Figure 9-2 is a similar plot for the edge rod. From these figures it is apparent that two types of thermal gradient may be distinguished. In general, the thermal bowing gradient is smaller than "local" gradients, which are defined as temperature changes over only 60 degrees of rod circumference. However, the bowing gradient produces lateral deflection of a rod, while the local gradient causes only local stresses. Figure 9-3 shows the variation of the local thermal gradient with changes in rod-to-wall spacing. Figure 9-4 is a similar plot showing the variation of bowing gradient with rod-to-wall spacing. Figure 9-5 shows how the location and magnitude of the local peak coolant temperature change with edge spacing as shown on the figure, an edge spacing of 42 mils, does not minimize the peak coolant temperature; the peak coolant temperature is raised $\sim 30^{\circ}\text{F}$ above the achievable minimum in an effort to minimize thermal bowing gradients.

9.2 EQUILIBRIUM BOWING DEFLECTION

The term "equilibrium fuel rod deflection" refers to the lateral rod deflection produced by a thermal gradient when the interdependence of rod deflection and thermal gradient has been taken into account. This is to be compared with rod deflection resulting from the thermal gradient that acts on the undeflected rod. The axial variation of the gradient acting on undeflected edge and corner rods is presented in Section 3. For comparison, Figure 9-6 shows the axial variation of the thermal bowing gradient for an undeflected corner rod and for a corner rod deflected 14 and 25 mils over a four-inch span at the bottom of the fueled region. The deflection was assumed to be a step function with the remainder of the fuel rod in an undeflected position.

Fuel rod bowing under the influence of temperature and flux gradients has been evaluated using the computer code CRASIB.⁴⁻¹ This analysis is described in Section 4. In order to account for the change in thermal gradient that accompanies rod deflection, the CRASIB code was modified. A linear relation between thermal gradient and rod deflection was included in the program so that the thermal gradient was modified each time a deflection was computed. The result of this computation is compared on Figure 9-7 with the result obtained before CRASIB was modified. The figure shows that the effect of thermal feedback on rod deflection is to increase the end-of-life deflection to 6.75 mils from 3.73 mils. In addition, thermal feedback causes rod deflection to increase rapidly with time near end-of-life.

The maximum rod deflection occurs near the bottom of the fuel region, where the thermal gradient changes rapidly with axial position. Elsewhere the deflections are relatively small; the second largest deflection is only 1.4 mils. This fact verifies the model used to calculate the variation of thermal gradient with axial position. The model assumes that rod deflection is a step function 4-inches long located at the bottom of the fuel region. Clearly, the actual rod deflection is not a step function; however, the axial length of the step was chosen so that the change in coolant channel area of adjacent channels was equal to the change produced by a cosine curve of equal maximum amplitude. Therefore, to the extent that a cosine curve represents the actual deflection, the thermal gradients are correct.

Thermal gradients across corner rods are more sensitive to changes in rod position than gradients across other rods in the fuel assembly. This is illustrated on Figure 9-8, which is a plot of the thermal gradients produced by given rod deflections for a corner rod and a central rod. A slight difference in sensitivity exists between the top and bottom of a corner rod, but the difference appears small enough to be neglected. These results are physically reasonable since corner rods are relatively close (42 mils) to the channel wall while central rods are separated by a minimum distance of 70 mils.

9.3 REVISED DESIGN PRESSURE DROP ANALYSIS

Water flow tests of the original F9E test assembly (this assembly is described in Section 3) have been performed to determine the orifice required to achieve a flow rate of 50 gpm in row 6 of the EBR-II core. The available overall pressure drop in the EBR-II core at this location is 41 psi. The flow tests indicate that an orifice with 7 sharp-edged holes of diameter 0.266 inch on a pitch of 0.350 inch would yield the required pressure drop through the original test assembly. Table 9-3 shows the contributions of the various test assembly components to the total pressure drop. Of the total drop of 41 psi, it is clear from Table 9-3 that the grids contribute only 3.0 psi.

The pressure drop due to the combined effect of three staggered-beam grids has been determined by experiment to be 1.13 times the pressure drop due to one full grid spacer. Therefore, the pressure drop due to all grids in the revised assembly is given by:

$$(\Delta p)_{\text{grid}} = (6 + 1.13) \left(\frac{3.0 \text{ psi}}{7 \text{ full grids}} \right)$$

and

$$(\Delta p)_{\text{grid}} = 3.05 \text{ psi.}$$

This value is to be compared with the 3.0 psi drop due to grids in the original assembly. The change of 0.05 psi in fuel assembly pressure drop may be neglected. It follows that the original orifice need not be modified and that the total coolant flow rate through the assembly remains as shown in Section 3.

The radial and axial temperature profiles presented in Section 3 remain valid; even though staggered-beam-grids cause interchannel coolant mixing that differs from full grid mixing, the Section 3 data assume no mixing due to grids. Therefore, the Section 3 temperature distributions show conservatively large differences between the temperatures of adjacent flow channels. The addition of mixing due to full or staggered grids would decrease corner temperatures and all temperature gradients from those used for the analyses presented in this report.

Table 9-3
CONTRIBUTIONS OF FUEL ASSEMBLY COMPONENTS
TO THE TOTAL PRESSURE DROP THROUGH THE F9E TEST ASSEMBLY

Original Assembly (Seven Full Grids)

Component	Δp (psi)
Entrance Region	24.8
Orifice	9.5
Grids	3.0
Fuel Rod Friction	2.9
Exit Region	0.8
Total	41.0

Revised Assembly (Six Full Grids, Three Staggered Grids)

Entrance Region	24.8
Orifice	9.5
Grids	3.05
Rod Friction	2.9
Exit Region	0.8
Total	41.05

9.4 LOCAL THERMAL-HYDRAULICS

In Table 3-3, a hot spot factor of 1.04 was used to describe the increase in rod cladding temperature caused by the presence of a grid spacer near the top of the core. This factor was determined using the THTD heat transfer code; the grid spacer was modelled as a full unmodified wave-beam grid for the purpose of that calculation. This section considers the effect of modified full grid spacers and staggered-beam spacers on the coolant temperature profiles and thermal bowing gradients presented in Section 3. The discussion must remain qualitative at this point; further analytical work is in progress to quantify these effects.

The fuel assembly hot spot temperature is not expected to change significantly. The grids located near the top of the core are full grids in the modified support system, as they were in the original system. The original and modified beam shapes are compared on Figure 3-15, and these are almost identical from the heat transfer standpoint. Staggered-beam grids are located at the bottom of the core in the revised system so that possible increased hot spots due to these grids will not add to the hottest coolant temperatures at the top of the core. Thus, the presence of staggered-beam grids does not affect the hot spot temperature. Any change that may occur in the hot spot temperature

is expected to be caused by the fact that corners 1 and 4 (Figure 9-9) are not welded in the modified full grids and that two additional dimples have been added on each beam at the corners. Rod motion toward the channel corner plus the added dimples will exclude coolant from the area between the grid and the channel wall. These effects are expected to be small, however.

The axial coolant temperature profiles presented in Section 3 do not include the effects of grid mixing or local temperature rises caused by grids. Local coolant heating at grid locations would cause a coolant temperature rise in the outer flow channels greater than that shown in Section 3, if there were no coolant mixing between flow channels. Increased mixing due to grids, however, tends to minimize this overheating. Thus, the two effects tend to cancel one another over short axial distances from each grid. For this reason, the temperature spikes present at grid locations were smoothed out in the axial temperature profiles of Section 3. The same reasoning applies to the modified grid support system, and no change in the axial temperature profile is expected.

The most significant local thermal difference between the original grid system and the revised system is an expected increase in the temperature difference acting on edge and corner rods to produce bowing. This increase will occur at grid locations 3 and 4 where staggered beams are present. Figure 4-8 is reproduced in this section as Figure 9-10. This figure shows the difference between full and staggered grids and illustrates the three types of corner rod support provided by staggered-beam grids. Rod 1 at grid location 3 and rod 3 at grid location 4 provide a type of corner rod support that is especially likely to cause a larger temperature difference across the rod than is found when a full grid provides rod support. With reference to Figure 9-10, at grid location 4 (defined on Figure 4-8), rod 3 is supported by the beams shown as solid lines. The dashed beams are not present at this axial location. Under these circumstances, coolant is excluded from the side of the rod nearest the corner and is redistributed toward rods A and B. The net effect of this flow redistribution, which does not occur to such an extent when a full grid is present, is to increase the temperature difference acting across the rod. Since rod stress, rod deflection, and rod-grid-reaction force are extremely sensitive to the temperature difference, this behavior could be a significant drawback to the use of staggered grids in the core region. This behavior presently is being evaluated quantitatively.

Figure 9-3. Local Coolant Thermal Gradients Around 60 Degrees of Rod Circumference for Edge and Corner Rods versus Fuel Rod-to-Channel Wall Spacing

Figure 9-4. Coolant-to-Coolant Bowing Gradient Across Edge and Corner Rods versus Fuel Rod-to-Channel Wall Spacing for 37 Rod Bundle, EBR-II Row 5 Environment

Figure 9-5. Coolant Temperatures of Two Hottest Coolant Channels versus Fuel Rod-to-Channel Wall Spacing for 37 Rod Bundle in EBR-II

Figure 9-6. Axial Variation of Thermal Bowing Gradient Across F9E Corner Rod for Rods Bowed 0, 14, and 25 mils Over 4 inch at Core Bottom

Figure 9-7. Fuel Rod Bowing Deflection for F9E Corner Rod versus Irradiation Time

Figure 9-8. Comparison of Bowing Gradients Produced by Deflections from Nominal Position in F9E Fuel Rods

Figure 9-9. F9E Full Grid

FULL GRID: SOLID AND DASHED BEAMS PRESENT AT EVERY GRID LOCATION
 STAGGERED GRID: SOLID OR DASHED BEAMS PRESENT AT EACH GRID LOCATION

Figure 9-10. Comparison of Full Grid with Staggered-Beam Grid

10. FUNCTION OF TABS AND WELDS IN GRID SPACER POSITIONING

This section describes how the grids are positioned in the flow channel in F9E and how it is assured that those positions are maintained throughout the in-reactor life of the grid. The objectives will be twofold: 1) analyses of the procedures followed during fabrication which try to guarantee proper grid positioning prior to testing; and 2) analyses of the reasons which indicate that in-core stresses and strains will not disturb these positions or damage the grids. The role of the grid tabs which provide the method by which the grid is attached to the channel in maintaining grid positions prior to and during testing, will be of special concern in this discussion. Thus, an additional objective will be to assess the role which the grid welds play in overall grid performance. This assessment will identify how critical the F9E welds are to grid performance, especially in maintaining the position of the grid in the channel and that of maintaining the structural integrity of the grid.

10.1 CONSIDERATIONS OF F9E GRIDS ATTAINING PROPER POSITIONING AND STRUCTURE DURING FABRICATION, SHIPPING, AND FUEL ROD INSERTION

10.1.1 Positioning of Grids into Channel During Fabrication

The F9E type grid spacers are positioned axially in the inner channel by the engagement of tabs on the grid beams into notches in the channel wall. Grid insertion is accomplished through the use of a specially designed insertion tool, which first deflects each grid in such a way as to permit it to slide through the channel to its proper axial location. The tabs are then deflected in the opposite direction, thereby "locking" them into the notches in the channel wall. Due to the design of the tabs and the notches, once the tabs are positioned in the notches, a mechanical interference occurs which restrains the release of the tabs. Figure 10-1 show planar views of the two grid-channel combinations which are similar to two of the types that are going to be tested in the F9E assembly in EBR-II. It is apparent from these figures that proper tab engagement, and thus proper grid positioning, is dependent on the correct combination of channel and grid dimensions. Inserting an undersize grid into an oversize channel would result in the lack of initial tab engagement in the notches, with the resultant possibility of axial grid movement during shipping or initial operation. The actual F9E grid and channel dimensions were selected in order to preclude the occurrence of this situation.

The grid is sized so that it is inserted with a slight initial interference at the tab end. The degree of interference possible due to design tolerances is not critical as any strain beyond yield is equivalent to that induced during the tab bending manufacturing procedure, with only strains up to yield remaining. The relaxation of this initial interference because of creep combined with the beam length and increasing relative to the channel because of swelling results in a stress history in the tab which shows the worst loaded tab to remain within the structural design criteria.

Extensive procedures were followed during the fabrication of the grids and inner channel and the final assembly of the grids into the inner channel in order to assure that these dimensions and tolerances were attained in the individual pieces, and that the final assembly of the pieces was performed correctly. All critical dimensions of the grid beams, welded grids, and the completed inner channel were measured in order to assure that the specified dimension and tolerances were attained. Additionally, continuous profilometer measurements of the inner channel outside dimensions (such as distances across the flats and across the corners) have been performed after the channel was notched to detect any dimensional irregularities produced by the notching operation.

Following acceptance of the completed inner channel and the nine F9E welded spacer grids, a test program was performed using a section of F9E-type channel and three welded grids of the same type as the actual in-reactor F9E grids. Both the channel section and the three grids were fabricated according to the same dimensions and tolerances as were used for the in-reactor components. This test was performed prior to the insertion of the actual F9E spacer grids into the F9E inner channel. As a result of the test, it was decided to insert dummy rods in all corners and edge locations to lock the tabs in place.

10.1.2 Maintenance of Grid Position During Shipping and Rod Insertion

The shipping container containing the F9E inner channel assembly may be subjected to shock and vibratory loading during shipping and handling. In order to assess the magnitude of these loads, the container was instrumented with accelerometers which measured the g-loadings of the container at the mounting block of the accelerometers. The

(a) STAGGERED GRID
IN CHANNEL

(b) FULL GRID
IN CHANNEL

Figure 10-1. Typical Views of Grids in Channel Sections

relationships between the various g-loadings which may be recorded on these accelerometers during shipping and handling and the possibility of grids becoming disengaged from their axial locations in the channel were assessed prior to the shipping of the actual test assembly to EBR-II by a series of drop tests and by vibration testing. The drop tests, which were performed on a modeled F9E type shipping container, grids, dummy fuel rods and inner channel vibration test was performed in a 20-inch long channel segment containing 4 grid spacers and 18 simulated shipping tubes around the edge. These tests will allow an immediate assessment to be made at EBR-II of the actual F9E accelerometer readings in that it determines the degree of grid dislodgement resulting from various shock and vibration conditions.

The vibration test covered the frequency range of 5 cps to 600 cps, with accelerations imposed on the assembly of up to 3 g's. The objective was to consider a low frequency range as calculated for the rigid body response of the channel which is isolated by the foam packing material, and to cover a higher frequency range as may be transmitted at a very low g level through the foam packing material.

The drop tests consisted of drops from various elevations ranging from 3 to 10 feet, with the crate inspecting at various pre-selected altitudes. Accelerations of up to 100 g's were measured (these being the values on the non-isolated instrumentation package and not on the foam isolated channel assembly). The objective was to establish the ability of the assembly to withstand a severe impact imposed on the crate, to qualitatively observe the effect of known mishandling anomalies and to establish a means of interpreting accelerometer readings in terms of an anomalous event.

No dislodging of grid spacers was observed during any of the tests.

10.1.3 Role of Welds in Grid Fabrication and Positioning

The prime role that the grid welds play in the fabrication of the F9E grids and their positioning in the inner channel is to allow the grid beams to be inserted into their respective positions as a single unit rather than as a set of individual units. Once the beam tabs are engaged in the notches in the channel wall, the welds are no longer required to hold the beams together into nine single grid units, since each grid beam will be positioned independently. On the basis of their primary mode of positioning, which is by means of tab-notch engagement, these F9E grids should behave the same as if they were assembled into the inner channel one beam at a time instead of as a single welded unit. Purely from a grid fabrication and positioning point of view, the grid welds are non-essential since in the event of their failure or complete absence the grids could still be assembled as designed into their proper positions in the inner channel.

Although the integrity of the grid welds is not essential for the structural function of the grid assembly, an evaluation of the quality of the welds was made. This evaluation included metallographic examination of the grid welds on grids which were fabricated by the same welder using the same grid beams and at the same time as the F9E in-reactor grids. They also included tensile testing of selected welds to determine their room temperature and elevated temperature strengths. The results included that F9E-type welds are of high quality, especially in relation to the performance which will be expected of them.

10.2 CONSIDERATIONS OF F9E GRIDS REMAINING PROPERLY POSITIONED AND STRUCTURALLY INTACT DURING EBR-II TESTING

10.2.1 Effect of Differential Swelling Between the Grids and the Inner Channel

Because of the difference in average temperatures between the inner channel and the spacer grids, a differential dimensional change due to metal swelling will occur between these two components. At all points in the F9E test assembly, this difference will be such that the spacer grids will grow larger with respect to the channel, thereby tending to maintain the attachment of the grid tabs in the channel notches. Thus, there is no reason to believe that the grid tabs will disengage from their respective notches during in-reactor testing due to differential swelling.

A second consideration involving differential swelling between the grid and the channel is the effect on tab life of the tab tightening resulting from the expansion of the tabs into the notches. Calculations have shown that a maximum bending strain of 0.38% will occur in the worst tab if the channel and grids behave according to the swelling equations that predict the largest amounts of swelling. For equations predicting lower swelling rates, a maximum bending strain of 0.27% is calculated to occur in this tab. Both of these predicted strains are considerably below the 1% strain limit which

was set as the limit for the total bending strains in the structural design criteria. The associated stress history shows stress rupture not to be a problem. Since the above strains are maximum values, they will occur in only a few of the total number of F9E grid tabs. Thus, tab failures due to strains produced by differential swelling are not predicted to occur in the F9E assembly; hence there are expected to be no detrimental effects of this mechanism on the axial positioning of the grids in the F9E test assembly.

Since 75% of the total assembly pressure drop occurs in the orifice below the fuel rods, no channel "ballooning" is expected to occur. Therefore, the channel will not move away from the grids under the influence of internal forces.

10.2.2 Effect of In-Reactor Axial Stresses on Grid Positioning and Stability

The F9E grids will be exposed to three forces in the axial direction which will attempt to displace them from their positions in the inner channel. These forces are as follows:

1. Gravity due to the weight of each grid in sodium which acts downward on the grids.
2. The pressure drop across each grid resulting from the upward flowing sodium, and
3. The friction between the fuel rods and grids due to the rod sliding through the grid openings as a result of differential axial growth between the rod and channel.

Of these only three rod-to-grid friction is significant. The various components of the total rod-to-grid friction load which is contributed to by each of four loading mechanisms are: rod-to-grid swelling, rod bowing, diametral tolerance and out-of-straightness. The loads for the grid at 7.5 inches above the bottom of the assembly are considerably higher than other grids in the assembly because of the much higher wave beam spring constant at this location and the effect this has on rod-to-grid swelling loads and diametral tolerance loads. The friction loads assume all the rods to have the maximum deviation from straightness and use the actual diameter measurements made in the 37 F9E rods. Finally, the coefficient of friction has been assumed to be equal to 1.0. This is thought to be high, particularly at the beginning of life.

The forces due to gravity and pressure drop produce maximum loads of 0.131 lb which are considerably below the 5 lb friction loads which have been applied without effect on grids during fuel rod insertion tests. It is apparent from these values that the loads due to gravity and pressure drop are so low that by themselves they should have little effect on changing the axial positioning of the grids which are initially positioned properly. In contrast, however, the friction force between the fuel rods and grids can result in significant loads. In order to verify that the grids will not disengage, tests provided an estimate of the force necessary to dislodge each F9E grid spacer from its axial location. These tests provide lower limits on the loads required, since they do not reflect the grid tightening effect which results from differential grid-to-channel swelling. The load required to dislodge a grid was found to be proportional to the number of tabs holding the grid in place. The load per tab that caused dislodgement was found to be approximately 5 pounds. (In F9E full grids, the number of tabs acting is 16; staggered grids are restrained by 8 tabs).

The load per tab resulting from the rod-to-grid friction is 1.47 pounds at beginning of life and 6.72 at end of life. It is significant to point out that the tab load history, although not specifically calculated, can be stated qualitatively to initially decrease at beginning of life due to creep induced relaxation and then to increase to the maximum value at end of life as swelling effects over-take the initial creep effects.

It should be noted that the maximum magnitude of any dislodgements (if it should occur) due to differential rod-to-channel axial growth would be on the order of 20 to 40 mils for a F9E grid at the top of the channel. At the mid-core region where the maximum loads are experienced, the maximum amount which a dislodged grid could be moved would be approximately 10 to 20 mils. The friction force which produced this dislodgement would then oppose any further movement due to the much smaller net force from gravity and pressure drop. This point will be expanded in a later section of this report.

10.2.3 Effect of In-Reactor Grid Loads on Weld Integrity

As has been previously discussed, the grid welds which are made primarily to facilitate handling of the grid are not required for maintaining the initial positions of the F9E grids in the inner channel during the shipping of the assembly to EBR-II and during subsequent insertion of the fuel rods into the inner channel. This is true because the primary method of attachment between the grids and the inner channel is through the engagement of the tabs on the grid beams with the notches in the inner channel wall. For in-reactor considerations, once the properly fabricated F9E grids are inserted into the inner channel and the tabs are locked into place, the factors discussed in the preceding two sections indicate that there is little reason to believe that the tabs will become disengaged during EBR-II testing of the F9E grid-spaced assembly. Thus, the primary method of grid attachment will still be maintained during EBR-II testing, and the grid welds will not be necessary for grid positioning.

In the event, however, that both of the grid tabs on a given beam did fail or dislodge, the welds would maintain the attachment of the failed grid beam to the balance of the grid. The largest loads which are expected to act on a grid beam in the F9E test assembly are less than 8.0 pounds. If the tabs on an upper beam fail, the friction load on that upper beam must be transmitted through the welds to a lower beam. There are a minimum of four welds which are available in the grid to retain a grid beam in position in the event of tab failures. Each of these welds would be required to support an axial load of approximately 2 pounds in this worst case example.

Tests of the tensile strengths of F9E type welds have shown that average tensile loads of between 40 and 50 pounds are required to fracture these welds in the cold condition. Even in the worst case, the strength appears to be considerably more than adequate. In fact, in the event that faulty welds were made on three of these four welds such that they could support no load, the weld strength would be sufficient for the one remaining weld to support the entire worst case load of 8 pounds.

On the basis of these weld strengths, in comparison with the calculated worst case loads to which the welds will be subjected, it appears that weld failures in the F9E assembly testing in EBR-II are very unlikely to occur. This point is especially evident when it is considered that the welds will only be subjected to loads as a consequence of the unlikely failure of the tabs which hold the grid beams in position.

10.3 CONSEQUENCES OF F9E GRID TAB AND GRID WELD FAILURES DURING EBR-II TESTING

10.3.1 Consequences of Grid Tab Failures

Preceding sections have discussed the factors which might lead to the failure of the beam tabs in the F9E test assembly, whether such tab failures are defined as the disengagement of the tabs from the notches in the channel wall or as shear or brittle fracture of the tabs. Although these failures are unlikely, the probability of their occurrence is not zero and therefore the consequence, if it does occur, must be considered.

The first type of tab failure—the disengagement of a beam tab from its position in the notch in the inner channel wall—would most likely occur in combination with similar disengagements of all of the beam tabs on a given grid. This is due partly to the presence of the grid welds and partly to the restraints on lateral movement which are imposed on the grids by the presence of the fuel rods in the grid openings. Since any given grid beam is welded to a set of other beams in the same grid, the disengagement of the beam tabs on that beam in the absence of weld failures is not possible without the simultaneous disengagement of all of the tabs on the grid. In addition, the presence of the fuel rods makes it impossible for the grids to tilt in the channel after only the tabs on one side of the channel have become disengaged. Thus, in the full grids in the F9E test assembly a failure by tab dislodgement would involve the simultaneous disengagement of 32 beam tabs; while in the staggered grids, 16 beam tabs would have to be disengaged.

Since there will be essentially no force acting downward on the F9E grids during irradiation in EBR-II, the only tab disengagement considerations of even remote possibility are in the upward direction. If the upward axial force on the grids succeeds in dislodging all of the grid tabs in a given grid from the positions in the inner channel, the consequences should be negligible. The most that the load due to the differential rod-to-channel friction force could be expected to move the grid following dislodgment of all of its tabs is on the order of 10 to 20 mils. That distance is the maximum amount which the fuel rods are expected to grow axially with respect to the inner channel in the regions of the F9E test assembly where appreciable loads will be exerted on the grids. Continued axial movement, in addition to

this distance, could only be produced by the very small net upward force resulting from gravity and pressure drop (less than 0.2 pound). Such movement will be prevented by the much larger friction force between the fuel rods and the grids and an additional undetermined friction force between the dislodged tabs and the inner channel wall. Preliminary measurements indicate that this force will be ~50 pounds in full grids and ~25 pounds in staggered grids. Thus, if the F9E grids do become dislodged, they should only move a short upward distance of less than 20 mils in the inner channel which will have essentially no effects on their support of the fuel rods or on their thermal hydraulic effects within the assembly.

The second type of tab failure—a shear or brittle fracture of a beam tab due to excessive shear stresses and strains as the tab interacts with the notch—should have the same negligible consequences as are expected for the tab dislodgement type of failure. All of the tabs on a given grid would have to fracture before the grid would be able to respond through upward movement to the forces acting on it. The upward movement of the grid would again be limited to the 10 to 20 mils which the fuel rods slide past in the grids, even though the additional friction force due to the tabs interacting with the channel wall will not be present. The consequences of this limited movement should again be negligible.

Another point whose consequences must be considered in the event of tab failures by fracture is that the fractured tabs will be free to move through the F9E assembly and possibly into the EBR-II system. Because of their very small size (~0.12 inch high by 0.10 inch wide by 0.01 inch thick) it does not appear that flow blockages due to these tabs wedging within the flow channels is possible. The smallest clearance of approximately 0.021 inch between the outermost grid beam and the channel wall should permit easy passage of this 0.01 inch thick tab. Even less possibility of flow blockage exists in the inner flow channels, where there is approximately a 0.070 inch spacing between fuel rods. Above the fuel assembly, there are no areas which will prevent the flowing sodium from sweeping any possible fractured tabs out of the F9E fuel assembly, where they will most likely simply settle to the bottom of the EBR-II vessel. Thus, it appears that the consequences of the fractured tabs themselves on the performance of the F9E test assembly will also be negligible.

If a tab broke off and became lodged adjacent to a fuel rod, a local hot spot would occur. Since the tabs are 10 mils thick, the maximum temperature rise associated with such an occurrence may be estimated by calculating the temperature rise caused by an increase in cladding thickness of 10 mils. This increase is conservatively estimated to be about 40°F.

10.3.2 Consequences of Grid Weld Failures

Since it has previously been established that the grid welds are not required for the normal operation of the F9E grids—either during shipping, fuel rod insertion, or EBR-II testing—it is readily seen that the consequences of weld failures in the absence of tab failures are negligible. The only imaginable adverse effect of weld failures—the initiation and propagation of cracks from the point of weld failure through the grid beam—has been analyzed in detail in subsection 8.1. The conclusion of this analysis was that for the conditions which the F9E assembly will see in EBR-II, it is extremely unlikely that a grid will fail because of crack propagation from a weld failure since it is extremely unlikely that crack propagation will occur.

10.3.3 Consequences of Simultaneous Grid Tab and Grid Weld Failures

The first points to be discussed are the consequences of both tab failures on a given beam in combination with the failure of all of the welds which attach the beam to the grid. If this beam was in the upper row of beams in its grid, movement in the downward direction would be restrained by the row of grid beam beneath it. The situation would be just the opposite for a beam in the lower row of a given grid which could only move downward. Design analyses have shown, as was discussed previously, that after the initial 10 to 20 mil amount of beam movement due to fuel rod-to-channel differential growth, the friction force between the fuel rods and the grid beam would prevent further movement due to the net gravity pressure drop force acting on the beam. Thus, there would be essentially no consequences of such a combination of failure.

In the event that the conditions predicted by the analyses are considerably different from the actual F9E in-reactor conditions, such that a grid beam with failed tabs and welds can move further than 20 mils, such upper or lower beams will move upward or downward, respectively, until their movement is stopped by the next grid in the direction in which they are moving. During this movement, the grids will remain in the same orientation with respect to the fuel rods the inner channel, and the remainder of the grid to which it was previously attached because of the restraint imposed by adjacent rods and the channel wall.

ACKNOWLEDGEMENT

This PA-10 Task G-6 report is the result of significant technical contributions by many individuals. The major contributors are:

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APPENDIX A

SWELLING EVALUATION

The current GE Type-316 annealed swelling correlation can be given as:

$$\frac{\Delta v}{v} = 3.14 \times 10^{-6} F^{1.81} (T-633.16)^{0.683} (1048.16-T)^{0.577} \quad (A-1)$$

where

v = Volume arbitrary units

F = (fluence)/ 10^{22} n/cm²

T = ° Kelvin

For isotropic swelling, the linear growth is:

$$\frac{\Delta L}{L} = \frac{1}{3} \frac{\Delta v}{v} \quad (A-2)$$

where

L = Length in arbitrary units.

Now we wish to define an "effective temperature gradient" $\Delta T'$ (°C/in.). When $\Delta T'$ is used with the thermal coefficient of expansion, α (in./in./°C), it will result in the same differential strain as is induced by swelling. Thus

$$\alpha \Delta T' = \frac{d}{dz} \left(\frac{\Delta L}{L} \right) \quad (A-3)$$

where

$\frac{d}{dz} \frac{\Delta L}{L}$ represents

the total differentiation with respect to a direction z (in.). In this case both F and T are functions of position so that

$$\frac{d}{dz} \left(\frac{\Delta L}{L} \right) = \frac{\delta}{\delta F} \left(\frac{\Delta L}{L} \right) \frac{\delta F}{\delta z} + \frac{\delta}{\delta T} \left(\frac{\Delta L}{L} \right) \frac{\delta T}{\delta z} \quad (A-4)$$

and

$$\begin{aligned} \Delta T' = \frac{1}{\alpha} & \left[1.891 \times 10^{-6} F^{0.81} (T-633.16)^{0.683} (1048.16-T)^{0.577} \frac{\delta F}{\delta z} \right. \\ & + 1.046 \times 10^{-6} F^{1.81} * 0.683 * (1048.16-T)^{0.577} / (T-633.16)^{0.317} \frac{\delta T}{\delta z} \\ & \left. - 1.046 \times 10^{-6} F^{1.81} * 0.577 * (T-633.16)^{0.683} / (1048.16-T)^{0.423} \frac{\delta T}{\delta z} \right] \quad (A-5) \end{aligned}$$

where the first term provides the effect of fluence gradient and the second and third term the effect of temperature gradient. This formulation can be used in the calculation of bending due to lateral fluence gradients along with thermal gradients in the form:

$$\alpha (\nabla T + \nabla T'). \quad (A-6)$$

In this way standard computer codes can be used for the evaluation of bending.

In the same manner an "equivalent expansion coefficient" α' (in./in./°C) can be evaluated. This provides a way of accounting for swelling effects due only to a temperature difference between parts in a constant fluence. In this case

$$\alpha' \Delta T = \Delta \left(\frac{\Delta L}{L} \right) \quad (A-7)$$

or in the limit

$$\alpha' = \frac{d}{dT} \left(\frac{\Delta L}{L} \right)$$

where $\frac{d}{dT} \frac{\Delta L}{L}$ is the derivative with respect to temperature.

Performing the differentiation

$$\alpha' = 1.046 \times 10^{-6} F^{1.81} \left\{ 0.683 \frac{(1048.16-T)^{0.577}}{(T-633.16)^{0.317}} - \frac{0.577 (T-633.16)^{0.683}}{(1048.16-T)^{0.423}} \right\} \quad (A-8)$$

This α' can now be used with the thermal α to determine component expansion associated with temperature differences as

$$\Delta L/L = (\alpha' + \alpha) \Delta T. \quad (A-9)$$

Caution should be used with regard to units of temperature which are all °K in this appendix.

APPENDIX B

VIBRATION STRESS AND GRID LOAD EQUATIONS

To a good approximation the fuel rod vibrates between supports in the first mode with a sinusoidal wave shape given by:

$$y = \delta_c \sin(kx) \quad (B-1)$$

where

$$\begin{aligned} y &= \text{deflection @ } x && \text{inch} \\ \delta_c &= \text{deflection @ } \frac{L}{2} && \text{inch} \\ L &= \text{length between grids} && \text{inch} \end{aligned}$$

The differential equation for the deflection curve of a beam is:

$$EI \frac{d^2 y}{dx^2} = M \quad (B-2)$$

where

$$\begin{aligned} E &= \text{Young's modulus} && \text{psi} \\ I &= \text{moment of inertia} && \text{inch}^4 \\ M &= \text{moment @ } x && \text{inch/pounds} \\ y &= \text{deflection @ } x && \text{inch} \end{aligned}$$

by differentiation

$$EI \frac{d^3 y}{dx^2} = \frac{dM}{dx} = Q \quad (B-3)$$

where

$$Q = \text{shear load} \quad \text{pounds}$$

but

$$\frac{d^3 y}{dx^3} = \delta_c K^3 \cos Kx$$

For the first mode deflection $K = \pi/L$ and at the point of grid loading L the shear load is:

$$Q = EI \delta_c K^3 \cos Kx$$

or

$$Q = \delta_c \frac{\pi^3}{L^3} EI \quad (B-4)$$

We can also evaluate the bending stress in the rod midway between grids since

$$M = E I \frac{d^2 y}{dx^2}$$

$$y = \delta_c \sin \frac{\pi x}{L} ; x = \frac{L}{2}$$

$$M = E I \delta_c \frac{\pi^2}{L^2}$$

$$\text{or } \sigma = \frac{Mc}{I} = \frac{E \delta_c \pi^2 c}{L^2}$$

(B-5)

where c is radius of rod in inches.

These equations, the frequency equation and Reavis' correlation, have been coded with the input shown on Table B-1.

Table B-1

Input Code

DI	Rod inside diameter	inch
DO	Rod outside diameter	inch
DP	Pellet diameter	inch
TD	Pellet % density	nondimensional
PD	Pitch-to-diameter	nondimensional
TC	Cladding temperature	°F
TN	Sodium temperature	°F
V	Coolant velocity	inch/seconds
PN	Number of rods	nondimensional
xxx	Axial position	inch
ET	Damping factor	nondimensional
AM	Lambda	nondimensional

The code will calculate the frequency, rod deflection, grid load, and rod stress as a function of grid spacing.

It will also output all of the intermediate calculated values. The program is shown on Table B-2. The code consists of a short main program and an interpolating subroutine to evaluate the ETA functions.

APPENDIX C

CREEP DEFLECTION WITH CONSTANT LOAD

Grid Model

Bending stress is given as:

$$\sigma = \frac{Mc}{I} = \left(\frac{Ph}{4} - \frac{Px}{2} \right) \frac{c}{I} \quad (C-1)$$

and the strain in outer fiber given as

$$\epsilon = \frac{c}{\rho} = c \frac{d^2 y}{dx^2}$$

and strain rate

$$\frac{d\epsilon}{dt} = \dot{\epsilon} = c \frac{d}{dt} \left(\frac{d^2 y}{dx^2} \right) \quad (C-2)$$

For irradiation creep, the strain is expressed as:

$$\epsilon_c = \sigma (K_1 + K_2 e^{-ct} + K_3 \phi t) = \sigma K(t) \quad (C-3)$$

and the strain rate as

$$\dot{\epsilon}'_c = \dot{\sigma} K(t) \quad (C-4)$$

with 1, 2, and 4

$$c \frac{d}{dt} \left(\frac{d^2 y}{dx^2} \right) = \left(\frac{Ph}{4} - \frac{Px}{2} \right) \frac{C}{I} \dot{K}(t)$$

or

$$\frac{d^2 y}{dx^2} = \left(\frac{Ph}{4} - \frac{Px}{2} \right) \frac{\dot{K}(t)}{I} \quad (C-5)$$

integrating

$$\frac{dy}{dx} = \left(\frac{Phx}{4} - \frac{Px^2}{4} \right) \frac{\dot{K}(t)}{I} + C_1$$

$$y = \left(\frac{Phx^2}{8} - \frac{Px^3}{12} \right) \frac{\dot{K}(t)}{I} + C_1 x + C_2$$

However, the boundary conditions require that

$$\text{at } x = 0 \quad \frac{dy}{dx} = 0 \quad \text{yields } C_1 = 0$$

$$\text{at } x = h/2 \quad y = 0$$

is

$$0 = \left(\frac{Ph^3}{32} - \frac{Ph^3}{96} \right) \frac{\dot{K}(t)}{I} + C_2$$

$$C_2 = - \frac{Ph^3}{48} \frac{\dot{K}(t)}{I}$$

thus

$$y = \frac{P}{96} (12hx^2 - 8x^3 - 2h^3) \frac{\dot{K}(t)}{I} \quad (C-6)$$

At the point of maximum displacement, $x = 0$

$$y_{\max} = - \frac{Ph^3}{48} \frac{\dot{K}(t)}{I} \quad (C-7)$$

and integrating with time

$$y_{\max} = \frac{Ph^3}{48} \frac{K(T)}{I} \quad (C-8)$$

where $K(T)$ is the creep strain constant evaluated at time, T .

APPENDIX D

CREEP RELAXATION WITH CONSTANT DEFLECTION

For a constant deflection

creep deflection + elastic deflection = Constant

$$\Delta_c + \Delta_E = \Delta_0 \quad (D-1)$$

or the deflection rates, $\frac{d}{dt} (\Delta)$

$$\dot{\Delta}_c + \dot{\Delta}_E = 0 \quad (D-2)$$

from Appendix C,

$$\dot{\Delta}_c = \frac{Ph^3}{48} \frac{\dot{K}(t)}{I} \quad (C-7)$$

and from elastic considerations

$$\dot{\Delta}_E = \frac{h^3}{48EI} \frac{dP}{dt}$$

thus

$$\frac{Ph^3}{48} \frac{K'(t)}{I} + \frac{h^3}{48EI} \frac{dP}{dt} = 0$$

or

$$\frac{dP}{dt} = -PEK(t) \quad (D-3)$$

and

$$\frac{dP}{P} = -EK(t) dt \quad (D-4)$$

integrating yields

$$\ln \frac{P}{P_0} = -EK(T)$$

or

$$P = P_0 e^{-EK(T)}$$

Where $K(T)$ is the creep strain constant (C-3) evaluated at time T , and P_0 is the original load on the beam from the deflection, Δ_0 .

APPENDIX E

TOTAL DEFLECTION OF GRID

The total deflection of the grid at any time is the sum of the elastic and creep deflections:

$$Y_{\text{Total}} = Y_{\text{Elastic}} + Y_{\text{Creep}} \quad (\text{E-1})$$

The elastic deflection is discussed in Section 5 and may be written as:

$$Y_{\text{Elastic}} = \frac{P}{4Et^3} \left[\frac{h^3}{b} + \frac{b^3}{4h} \right] \quad (\text{E-2})$$

The creep deflection of the grid was also considered in Section 5 and Appendix C. For a constant load, the creep deflection was found to be of the form:

$$Y_{\text{Creep}} = \frac{Ph^3}{4bt^3} K(T) \quad (\text{E-3})$$

where from the irradiation creep strain correlation

$$\epsilon(T) = \sigma K(T) \quad (\text{E-4})$$

In our case, however, the load is not constant and varies with time as shown in Figure 4-4.

Treating this load as linearly increasing from zero time as shown on Sketch E-1,

the deflection at the final time, T , due to a small load increase, ΔP , is:

$$\Delta Y_c(T) = \Delta P \frac{h^3}{4bt^3} K(T-t) \quad (\text{E-5})$$

Using this procedure, the total deflection will be equal to the sum of these incremental values from time zero to the final time;

$$Y_c(T) = \sum \Delta Y_c(T) \quad (\text{E-6})$$

This method was followed at each grid point and load (Table 4-1) and includes the variations in fluence and temperature which exist at each point.

A listing of the TIME SHARE BASIC Program is included, with the following variable definitions:

		<u>Units</u>
XI	Node number	—
X	Distance from core bottom	inch
P0	Original load at zero time (thermal)	pounds
P	Final load	pounds
T1	Grid temperature	°F
H	Grid height	inch
W	Grid width (rod pitch)	inch
L	Grid thickness	inch
L1	Total operating time	hours
N	Time increments	nondimensional
F	Flux distribution	nondimensional
E	Modulus of elasticity	psi
D1	Elastic deflection	inch
F2	Temperature factor in creep correlation	—
I	Grid moment of inertia	inch ⁴
Z	Total flux X 10E-22	nvt/cm ² /h
K0	Creep strain constant (t) = 0	in./in./psi
D0	Creep deflection due to original load	inch
L2	Time increment	hours
S	Slop of load vs time curve	pounds/hours
P1	Load increment	pounds
K1	Creep strain constant (t > 0)	nondimensional
D2	Incremental creep deflection	inch
D3	Total creep deflection	inch

```

10 READ X1, X, PO, P, T1
20 LET H = 0.35
30 LET W=0.3125
50 LET L = 0.010
70 LET LI=19200.
80 LET N = 1000
85 PRINT "AT NODE #"; X1
90 PRINT "FOR A TEMPERATURE OF"; T1
100 PRINT " AND A LINEAR LOAD OF"; P; " IN "; LI; " HOURS"
110 PRINT " WITH "; N; " ITERATIONS"
120 PRINT
130 PRINT " A GRID HEIGHT OF "; H
140 PRINT " A GRID WIDTH OF "; W
150 PRINT
160 PRINT "      THICKNESS", "      ELASTIC", "      CREEP", "      TOTAL"
170 PRINT "      ", "DEFLECTION", "DEFLECTION", "DEFLECTION"
180 PRINT
200 LET F=SIN((X+8.706)*3.1416/53.412)
205 IF X <= 36 THEN 210
206 LET F = 0.570/1.15857*EXP(0.15*(36 -X))
210 LET E= (29.42-0.006*T1)*1.0E6
220 LET D1=P/(4*E*L^3)*(H^3/H+H^3/(4*H))
230 LET T2=(T1-32.)*5/9 + 273.16
240 LET F2=EXP(1.405-0.0027*T2)
250 LET I= W*L^3/12.
260 LET H1=H/2
262 LET Z = 22.32*F*1.0E-4
263 REM
264 LET K0 = 7.82E-9*(1-EXP(-Z/6.37*1.0E2*LI))
265 LET K0 = K0 + 1.87E-8*Z*LI*F2
268 LET D0 = K0/I*H1^3/6*PO
270 REM
280 LET D3 = D0
290 LET L2=LI/N
300 FOR M= 1 TO N
310 LET T= M*L2
340 LET S = (P-PO)/LI
350 LET P1=S*L2
355 REM
360 LET K1=7.82E-9*(1-EXP(-Z/6.37*1.0E2*(LI-T)))
365 LET K1 = K1 + 1.87E-8*Z*(LI-T)*F2
370 LET D2= K1/I*H1^3/6*P1
380 REM
390 LET D3=D3+D2
400 NEXT M
410 PRINT L, D1, D3, (D1+D3)
420 LET L = L + 0.002
430 IF L<=0.016 THEN 200
440 PRINT
450 PRINT
460 PRINT
470 PRINT
480 PRINT
490 GO TO 10
999 END

```

ready

APPENDIX F

RELATIVE ROD-TO-GRID SWELLING REACTION

For a given grid deflection due to relative rod-to-grid growth, the grid reaction as a function of time was found to be (Appendix D):

$$P(t) = P_0 e^{-EK(t)} \quad (F-1)$$

where $K(t)$ is the irradiation creep strain constant

$$\epsilon(t) = \sigma K(t) \quad (F-2)$$

with a grid spring constant, C , the above may be expressed as

$$P(t) = Y_0 C e^{-EK(t)} \quad (F-3)$$

Thus for a given grid deflection, Y_0 , the resulting reaction $P(t)$, would decay in an exponential fashion.

This is countered by the fact that the relative rod-to-grid swelling is increasing proportional to the power of time;

$$Y_g \propto t^{1.69} \quad (F-4)$$

Thus the resulting rod-to-grid reaction is a balance between the relative growth and load decay with time.

The relative rod-to-grid growth may be illustrated as shown on Sketch F-1.

For any given small increase in grid deflection, ΔY_N , the decayed load increment at end-of-life, time T , is given as:

$$\Delta P(T) = \Delta Y_N C e^{-EK(T-t_N)}$$

By adding the end-of-life contributions of each load increment, the total load may be found;

$$P(T) = \sum \Delta P(T)$$

The TIME SHARE computer program for this evaluation is attached. This program computes the relative rod-to-grid growth and growth rate using the "equivalent expansion coefficient" as described in Appendix I.

A definition of terms is as follows:

H	Grid height	inch
W	Grid width (rod pitch)	inch
L	Grid thickness	inch
R	Universal gas constant	—
U	Total time	hour
N	Time increments	nondimensional
X	Axial location above core bottom	inch
T1	Grid temperature	°F
F2	Temperature factor in creep correlation	nondimensional
A1	Thermal factor in creep correlation	nondimensional
A1	Thermal expansion coefficient	in./in./°F
E	Modulus of elasticity	psi
C2	Temperature factor in swelling correlation	nondimensional
P	Total flux X 10E-22	n/cm ² /h
C	Grid spring constant	pounds/inch
D0	Thermal differential expansion	inch
P0	Thermal load	pound
K0	Creep constant (t = 0)	—
R0	End-of-life reaction (thermal)	pounds
D1	Time increment	hour
T	Total time at increment	hour
P1	Total fluence at increment	n/cm ²
A2	Equivalent expansion coefficient	in./in./°F
D	Total rod-to-grid growth	inch
D2	Total rod-to-grid strain	in./in.
A3	Rate of change of A2	in./in./°F/h
S	Slope of growth vs time curve	in./h
P1	Incremental load	pounds
K1	Creep constant (t = t _N)	—
R2	End-of-life reaction	pounds
R3	Total end-of-life reaction	pounds

10 LET H=.35

20 LET W=0.32

40 LET L=.014

GEAP-13765

50 LET R=.1.986

60 LET L1=11280

70 LET N = 1000

80 PRINT " FOR A GRID THICKNESS OF "; L

90 PRINT " AND A TOTAL TIME OF"; L1; "HOURS"

100 PRINT " WITH"; N; "ITERATIONS"

110 PRINT

120 PRINT " A GRID HEIGHT OF"; H

130 PRINT " A GRID WIDTH OF"; W

140 PRINT

150 PRINT "AXIAL", "GRID", "RELATIVE"

151 PRINT "LOCATION", "LOAD", "STRAIN"

160 PRINT

170 FOR X=0 TO 14

175 LET T1=700+333*X/13.5

180 READ F

190 LET T5=4.3146*15.0*F/1.12

200 LET T2=T1

210 LET K = (T2 -32)*5/9 +273.16

220 LET F2 = EXP(1.405 -0.0027*K)

230 LET A1 = (9.38 +0.0017*T1)*1.0E-6

240 LET E = (29.42 - 0.006*T1)*1.0E6

250 LET C2=1.043E-6*(.683*(1048.16-K)^.577/(K-633.16)^.317)

251 LET C2=C2-1.043E-6*(.577*(K-633.16)^.683/(1048.16-K)^.423)

252 LET C2=C2*1E5

255 LET P=7.89*F/1.12*1E-4

260 LET C = 4*E*L^3/(H^3/W +W^3/(4*H))

262 LET D0 = 0.250*T5*A1

264 LET P0 = C*D0

266 LET K0=2E-9

267 LET K0=K0+1.87E-8*P*L1*F2

269 LET R0 = P0*EXP(-E*K0)

270 REM

280 LET R3 = R0

290 LET D1 = L1/W

300 FOR M = 1 TO N

310 LET T = M*D1

330 LET P1 = P*T

340 LET A2=1E-5*C2*(0.84*P1)^1.81

350 LET A2 = A2*5/9

360 LET D = 0.250*T5*(A1 +A2)

365 LET D = D/0.250

380 REM

390 LET A3=1E-5*C2*1.81*(0.84*P)*(0.84*P1)^0.81

395 LET A3 = A3*5/9

400 LET S = 0.250*T5*A3

410 LET P1 = S*D1*C

420 LET K1=2E-9

421 LET K1=K1+1.87E-8*P*(L1-1)*F2

440 LET R2 = P1*EXP(-E*K1)

450 LET R3 = R3 +R2

460 NEXT M

470 PRINT X,R3,D

474 NEXT X

475 PRINT

476 PRINT

477 PRINT

478 PRINT

479 PRINT

900 DATA .875,.940,1.00,1.05,1.086,1.111,1.12,1.111

901 DATA 1.092,1.059,1.011,.956,.893,.850,.841,.833

999END

F-3/F-4

APPENDIX G

STRUCTURAL CRITERIA

G.1 INTRODUCTION

The purpose of the criteria presented in this section is to establish interim design limits on stress and strain levels in core components. It is denoted interim because for irradiated material the pertinent material properties are not adequately established and will be updated as development programs on irradiated material produce data.

Two sets of criteria are presented. The first is for material in which the irradiation damage is such that the material can be treated as a ductile material. This criteria would only apply early in life for stainless steel components in the active core, but may be applicable for a substantial part of the residence time for parts of the peripheral regions such as the blanket and shielding regions.

The second criterion applies to material in which the material properties are such that the material cannot be considered ductile. This is somewhat arbitrarily defined by the condition that the elongation as measured in a uniaxial tensile test is less than 10%. For material in the active core, accumulated fluence may be as high as 5.0×10^{22} nvt-total. Experimental evidence indicates that the tensile elongation at this condition may be reduced to about 1% or lower. The criteria consider the material to be quasi-brittle and set limits based upon the very low capacity of the material to sustain plastic strain from cyclic and short term loading. Advantage is taken of the fact that yield and ultimate strengths increases as ductility decreases.

A second major consideration is the creep behavior of the material. In addition to the temperature dependent creep mechanism which is similar to the creep in unirradiated material, recent data on a limited number of tests at 700°F indicate that the creep rate induced by irradiation in the 700°F range may be as severe as at the higher temperatures. Current estimates are that the rupture strength is not seriously affected by this phenomenon.

A third consideration is the fact that the material swells at a rate which depends on both accumulated neutron flux exposure and the temperature. While this does not affect the material properties per se, it results in stresses and strains which are not covered by conventional structural criteria. The restraint of gross and differential swelling effects cause stresses which can be likened to thermal stresses with the exception that they are not cyclic in nature, but tend to be steadily increasing with residence time.

The two major concerns of a structural criterion are:

- 1) to limit stress-strain conditions so that material rupture will not result;
- 2) limit stress-strain conditions so that the function of the stressed component is not jeopardized by excessive distortion.

The establishment of stress-strain limits so that the combined stress from mechanical, swelling and temperature loadings in a low ductility material with poor creep resistance will not lead to failure or excessive distortion is primarily judgmental at this point in time. The criteria are written as a basis for proceeding with the design and must be reviewed and updated as new data becomes available.

Stresses are categorized as primary, secondary, and peak. Primary stresses are those stresses which are not self-limiting and are required to balance external forces and satisfy laws of equilibrium. They are induced by mechanical loads which are not relieved as a result of deformation under the loading.

Secondary stresses are those stresses which are self-limiting and which are necessary to satisfy continuity of the structure. They are caused by both mechanical loads, thermal growth restraint, and irradiation swelling restraints.

Peak stresses are highly localized stresses which do not cause structural distortion but may be the source of fatigue failure or crack propagation.

Primary and secondary stresses can generally be considered as membrane and bending stresses.

Membrane stress is the average stress on a section of the component. Bending stress is the stress proportional to the distance from the centroid of a section of a component.

All stresses are calculated on an elastic basis for comparison to limits in this criteria, i.e., strain limits are treated as effective elastic stress limits. In time-dependent stress situations, however, such as caused by swelling, stress relaxation by creep is permitted. This is done to maintain consistency with accepted approaches such as Section III ASME Boiler and Pressure Vessel Code.

G.2 STRESS LIMITS FOR IRRADIATED STAINLESS STEEL WITH LESS THAN 10% ELONGATION

G.2.1 Primary Membrane Stress Intensity

Since this stress by its nature cannot be modified by relaxation and redistribution, it must be limited to avoid rupture or excessive distortion. To provide a margin of safety against rupture, it is limited to

- 50% X the minimum ultimate strength
- or 80% X the minimum creep rupture strength.

To limit the distortion of the stressed part, restrictions must be placed on this stress which is related to the plastic deformation incurred if yield strength is exceeded and also to the plastic deformation which results from material creep.

The stress shall be limited to 67% X the maximum yield strength or to a level which results in 1% creep in the design life of the component.

Recent preliminary data indicates that irradiated stainless steel experiences creep at moderately low temperature (700 to 1000°F). For direct application of the above limits the creep limit will be controlling over most of the temperature range. The 1% creep limit may be exceeded but demonstration will be required that the resulting deformation does not impair the function or jeopardize the safety of the core.

NOTE: The stress limits are for a steady stress. Where the primary stress is not constant over the life of the part adjustments can be made to account for this.

For example in a fuel rod in which pressure stresses are linearly increasing from zero initially to a final stress σ_f for stress rupture at 1250°F. Similarly creep is lower than for a steady stress of level σ_f , and σ_f can be increased to account for this provided the creep strain is shown by analysis not to compromise the function of the component.

G.2.2 Primary Membrane plus Bending Stress Intensity

This stress shall be limited to provide a margin of safety against rupture to

- 75% the minimum ultimate strength
- or 80% the minimum creep rupture strength.*

To limit the distortion and due to the danger in permitting plastic deformation in a quasi-brittle material, the stress shall be limited to

- 100% the minimum yield strength.

To limit distortion due to material creep, a limit must be placed on this stress. The restriction requirements are to avoid deformations beyond a level which jeopardizes the function of the component. Limitation of the deformation to 1% plastic creep strain is an arbitrary level to be used as an objective; however, the acceptable level should be determined based upon the function of the component involved.

*Note that Figure 5-5 contains both a curve for steady-state stress to 80% the minimum creep rupture strength, plus another curve based on a time-dependent stress caused by the bowing load as shown in Figure 5- which is an upper limit for bowing loads for both the GE and PNL correlations.

G.3 SECONDARY STRESSES AND COMBINED PRIMARY AND SECONDARY STRESSES

1) Short term rupture and distortion:

Due to the loss of ductility the limit established for combined stresses is 100% minimum yield strength of the material. (The elevation of yield strength with irradiation may be included since it accompanies the loss of ductility.)

2) Creep considerations:

Creep will relax the secondary stress component but shall be limited to 1% plastic creep strain and ratchet mechanisms will be avoided.

3) Fatigue:

There is no data on which to base a limit on cyclic stress level and number. By staying below yield cycling in the plastic range cyclic stress is avoided but data are needed to go any further in terms of setting limits.

G.4 PEAK STRESSES

Due to the low ductility, the potential for failure at local stress concentrations is high if they are in a high stress field. Every effort must be made to avoid concentrations due to notches and geometry changes, and limit the steady and particularly the cyclic stress components at notches.

Using elastic stress concentration factors and limiting the peak stress intensity to yield is an objective; however, due to the high SCF's at sharp notches, a fracture mechanics approach based upon experimental data on notched specimens and specimens containing welds is required. The details of such tests should be responsive to specific areas of the design where concentration effects cannot be eliminated such as essential welds.

G.5 STRESS LIMITS ON MATERIALS WITH ELONGATION ABOVE 10%

The limits for material which has ductility of 10% or more are based on Section III of the ASME Pressure Vessel Code.

Primary Membrane Stress Intensity

This shall be limited to:

- 67% of minimum yield strength or 90% minimum yield strength for unirradiated material
- 50% of ultimate tensile strength
- 80% of the minimum stress to produce rupture in the design life of the component
- 10% creep strain in the design life.

Primary Membrane plus Bending Stress Intensity

- 100% minimum yield strength or 135% minimum yield strength of unirradiated material
- 75% of minimum ultimate tensile strength
- 100% of minimum stress rupture strength in design life

Primary plus Secondary Stresses

- 2.0 X irradiated yield strength
- 1.0 X ultimate strength
- 1% creep strain

Peak Stress

This shall be limited by fatigue considerations as per Section III of the ASME Boiler and Pressure Vessel Code.

Thermal Ratchet

$$S_{pm} < \sigma_y \frac{-S_{st}}{4}$$

S_{st} = Cyclic secondary stress.

APPENDIX H

TWO-DIMENSIONAL PLATE BENDING ANALYSIS

H.1 PHYSICAL MODEL AND IDEALIZATION

The physical model analyzed consists of a single span along the beam which has a length equal to the rod pitch interval. This does not imply that the behavior at all locations of the grid is approximately the same. A single interval must be isolated (with appropriate boundary conditions) to reflect it being a segment from a continuous beam in order to investigate with rigor a localized behavior. Consider the schematic view looking edgewise at the F9E grid as shown in Sketch H-1.

Sketch H-1

When the central rod A swells so that the relative increase in its diameter is ΔD , it will force deflections of $0.50 \Delta D$ to occur at each of the two mid-height wave crests which it contacts.

When one of the interior rods C swells by ΔD , one cannot correctly assume that it will force deflections on either side of $0.50 \Delta D$. In one direction it expands against a mid-height crest and in the other direction it expands against outside crests, one of which is adjacent to the weld which stays fixed. Furthermore, the rod A is tending to displace the outside crests towards rods C. If the restraint of the weld (which locally suppresses translational and rotational displacements) were not present, there would be no reason to be concerned about the direction in which the rod could expand, for the wave beams would be statically determinate and could displace as a rigid body to the proper location. Before attempting to conclude how rods C deflect, consider rods B.

The edge rod B is restrained by the two outer grid beams. As the outer crests of the edge beam are in contact with the channel wall, no force is being exerted which would cause the edge beam to deflect toward the edge rod. As edge rod B swells, it expands against the mid-height crest of the edge beam and the outside crests of the other beam, one of which is adjacent to the weld which remains fixed. This same latter wave crest is furthermore being forced toward rod B by the swelling of C.

A consistent set of displacements which is compatible with the above discussion is hypothesized as zero displacement at all outer wave crests but $0.50 \Delta D$ at the two mid-height wave crests and at the other six beams. These are not truly the displacements as some flexibility does exist between the weld and the adjacent wave crest. With the exception of the wave crest of the edge beam which contacts the channel wall, some of these wave crests will undergo some slight displacement toward the centerline. A further consideration is that the end view shows six rods and seven beams. Thus, as an average, edge wave beam must accommodate only $6/7$ of average ΔD .

It was concluded in selecting a model that the edge beam would have a displacement of approximately ΔD imposed on the mid-height crest; the central beams bounding rod A would have $0.5 \Delta D$ at the mid-height crests with a small displacement toward the center imposed on the outer crests (these central beams would accommodate somewhat more than $0.5 \Delta D$), and that the beams bounding rods C would have deflections which are a transition between the central and the edge beams. The model selected will produce results that have the most meaningful comparison with the structural criteria and will impose a displacement of ΔD at the mid-height crest and zero displacement at the outer crests.

Plate bending analysis has been performed by formulating the differential equations for the field equation and the boundary conditions in finite difference form and then solving the resulting linear simultaneous algebraic equations by using an existing computer code.

Sketches H-2 and H-3 illustrate the model.

Sketch H-2

Sketch H-3

Points X—zero displacement and rotation. Sides A and B—a recurrence condition whereby all functions of X repeat, i.e., $f(X) = f(X + L_x)$.

The finite difference formulation used a square array interval over the plate surface which subdivided both L_x and L_z into five intervals. The proper aspect ratio of the plate was maintained by formulating the difference equations using a rectangular mesh. To avoid the complication of resorting to either a completely variable mesh or interpolation schemes, the location of the rod-to-grid contact loads were placed at the mesh intervals rather than at the true locations. The following table compares the contact locations of the model with the actual locations.

X-coordinate			
Model	— loads at $\frac{X}{L_x}$	=	0.2, 0.8
Actual	— loads at $\frac{X}{L_x}$	=	0.25, 0.75
Z-coordinate			
Model	— loads at $\frac{Z}{L_z}$	=	0.2, 0.5, 0.8
Actual	— loads at $\frac{Z}{L_z}$	=	0.15, 0.5, 0.85

H.2 MATHEMATICAL MODEL AND METHOD OF SOLUTION

The plate bending differential equation is

$$\Delta^4 w = -\frac{p}{D} \quad (H-1)$$

where

$$\nabla^4 = \left(\frac{\partial^4}{\partial x^4} + 2 \frac{\partial^4}{\partial x^2 \partial z^2} + \frac{\partial^4}{\partial z^4} \right)$$

w = deflection function = $w(x, z)$

p = normal load = $p(x, z)$

$D = \frac{Et^3}{12(1-\nu^2)}$ = flexural stiffness

The boundary conditions are:

At $Z = L_z$

$$\frac{\partial^4 w}{\partial z^2} + \nu \frac{\partial^2 w}{\partial x^2} = 0$$

and

$$\frac{\partial^3 w}{\partial x^2 \partial z} + \frac{\partial^3 w}{\partial z^3} = 0$$

At $z = 0$ (except at points $x=0$ and $x=L_x$)

same as at $Z = L_z$, shown above

At $z = 0$, at points $x=0$ and $x=L_x$

$$w = 0$$

and

$$\frac{\partial w}{\partial z} = 0$$

At $x = 0$ and $x = L_x$, along the z -direction

$$w(x) = w(x + L_x)$$

The last boundary condition, which is essentially a recurrence condition, rigorously considers a continuum of lengths L_x over which the behavior is repetitive.

For a plate subdivided in a rectangular mesh, see Sketch H-4 below.

Sketch H-4

For $\gamma = k/h$, the bi-harmonic operator used in Equation H-1 is represented in Sketch H-5 as in "molecule" form.

Sketch H-5

In application, the molecule is overlaid on a location (or pivotal point) with each balloon being the coefficient which multiplies the variable associated with the position at that balloon. This results in a single algebraic equation with 13 unknowns. Shifting the molecule to a new pivotal point generates a new equation. This operation is repeated for all mesh points in the grid.

Similar molecules were formulated to represent the boundary conditions for a rectangular mesh but are not presented here. In a similar manner they all generate linear algebraic equations.

In applying Equation H-1 in finite difference form, the value of p (the load function) at the point at which the difference equation is formulated represents the ordinate at that point of a distributed variation of load over the plate (the variation being linear from point to point). This is inherent to the order of the difference operator, with the order being used in this formulation being that generated by a fourth order polynomial. Thus, for consideration of a concentrated load at a point, the value of p at the point is the peak value of a tetrahedral load distribution whose volume equals the total load. When the biharmonic difference operator is applied at a point of prescribed deflection and an unknown load, the load replaces as the deflection variable as an unknown in that particular equation.

H.3 RESULTS

H.3.1 Solution to Programmed Problem

The following mechanical properties and dimensions were used for the numerical analysis:

E	=	Young's Modulus	=	30×10^6 psi
ν	=	Poisson's Ratio	=	0.30
t	=	plate thickness	=	0.012 in.
L_z	=	plate depth	=	0.50 in.
L_x	=	plate length	=	0.32 in.

Sketch H-6 shows the resulting deflections when $\Delta D = 0.000250$ inch (0.1% relative strain for a 0.250-inch-diameter rod).

Sketch H-6

The loads on the plate are as shown in Sketch H-7 (for $\frac{\Delta D}{D} = 0.1\%$; to be scaled for F9E values):

Sketch H-7

x --- out of page } force or moment vector
 x --- into page }
 → --- force
 → --- moment, right hand rule

$P_A = 0.29 \text{ lb}$
 $P_B = 0.76 \text{ lb}$
 $P_C = 0.58 \text{ lb}$
 $P_W = 0.23 \text{ lb}$
 $M_w = 0.0176 \text{ in./lb}$

H.3.2 F9E Grid Contact Loads

To obtain the F9E loads at 7.5 inches from the bottom of the core, consider the E.o.L. value of $\frac{\Delta D}{D}$ to be 0.42%; multiply by a factor of 4.2. To consider creep, as the governing creep law is proportional to stress, the percentage of load relaxation remains the same as in the creep analysis performed using the less rigorous beam model. Thus the loads with creep are 44.4% of the elastic value. The F9E contact loads and weld loads are as follows.

Load	Elastic	Creep
P_A	1.21	0.53
P_B	3.22	1.43
P_C	2.42	1.08
P_W	0.98	0.44
M_W	0.0740	0.033

H.3.3 F9E Wave Bending Stress

To obtain the bending stress at mid-height, the average stress along the length will be calculated, rather than at the "point" loading, the same as in the beam analysis.

The bending moment is:

$$M = P_A (0.175 \text{ in.})$$

The average bending stress at mid-height:

$$f = \frac{6M}{(0.32)(0.012)^2} = \frac{(6)(0.175)}{(0.32)(0.012)^2} P_A = 22,800 P_A$$

Using the creep value for P_A , the bending stress in the presence of creep is 12,100 psi.

H.3.4 Rod-to-Channel Relative Growth Induced Grid Bending Stress

Current practice is to consider a coefficient of friction of 1.0 between the rod and the grid at the contact point. Thus, the contact load is also the magnitude of the load causing the bending of interest.

The bending stress can be obtained by scaling the original calculated value by the ratio of the sum of the contact loads. Consider the following computation using contact loads which are calculated considering creep.

Load	Original Beam Formulation	Plate Solution Formulation
P_A	0.26	0.54
P_B	0.26	1.43
P_C	0.52	1.08
Total	1.04	3.05

As discussed in the model description earlier, ΔD loads vary from approximately the full value of the model at the edge to approximately 50% of the calculated by the model at the center, although not necessarily linearly. As an approximation, an average value of 75% of the calculated value from the model will be applied to the beam.

The scaling ratio is:

$$\frac{(0.75)(3.05)}{1.04} = 2.20$$

Thus the bending stress in the grid beam becomes:

$$\sigma = (2.20)(7219) = 15900$$

ADDENDUM

Subsequent to the analysis of the F9E grid structure, experimental data on high burnup (118,000 MWd/Te) have become available. These data indicate the existence of fuel-cladding mechanical interaction. This interaction starts between 40,000 and 90,000 MWd/Te dependent on the smeared fuel density and on the extent of the clad swelling. Precision density measurements were also made on the cladding material to determine the extent of the swelling. The lower end of the swelling measurements corresponds to the PNL swelling correlation while the GE swelling correlation is somewhat higher than the upper end of the data.

In order to evaluate the effect of fuel-cladding interaction, the fuel swelling data have been interpolated to determine the interaction based on the F9E smeared density and the GE and PNL Type-316 stainless steel swelling correlations. The effect of the fuel-cladding interaction is to increase the differential growth of the fuel rod relative to the grid. This results in an increase in the secondary stress in the grid.

Figure 1 indicates how the primary plus secondary longitudinal stress for the grid at 7.5 inches into the core builds up with time. The primary plus secondary longitudinal stress consists of three parts. The primary stresses consist of vibration induced stresses (800 psi) and fuel rod bowing induced stress (32,000 psi for the GE correlation, 19,500 psi for the PNL correlation at 11,280 hours). To these is added the secondary stress of rod-to-grid swelling interaction. This swelling interaction has three separate causes:

1. Fission gas pressure induced radial creep of the fuel rod;
2. Differential thermal expansion and irradiation induced swelling between the rod and the grid, and
3. Fuel rod cladding expansion due to fuel-cladding mechanical interaction.

The first two of these occur and build up throughout life and cause an end-of-life (11,280 hours) stress on the F9E grid of 39,600 psi for the GE correlation and 23,200 psi for the PNL correlation. The third cause of rod-to-grid swelling interaction does not always occur. When it does occur, it is usually at some point after beginning-of-life. The length of time depends on what power the rod is running, its initial theoretical density, and the amount of cold clearance between fuel pellet and rod i.d. Only the center grid at 7.5 inches into the core will see this effect since it is the only grid in the high power region. For the PNL swelling correlation this effect starts at about 8,100 hours into the experiment and for the GE correlation about 9,900 hours. The percentage swelling for both predictions versus time is shown in Figure 2. The result of this fuel-clad mechanical interaction is an additional 10,000 psi for the GE data and 25,000 psi for the PNL data at end-of-life conditions (11,280 hours).

The buildup of all of the above stresses is shown in Figure 1. The figure also contains a plot of the buildup of the above stresses assuming no fuel cladding interaction. In the main text it is stated that for the conditions at 7.5 inches into the core the stress criterion is some 46,000 psi. This being the case, both correlations predict that the criteria will be exceeded at approximately 11,000 hours into the experiment.

This increased swelling will also cause a longitudinal stress increase because of axial rod growth. For the GE correlation, this is a 1.441-pound load causing a secondary stress to 20,000 psi. For the PNL correlation, the load is 2.447 pounds which caused a secondary stress of 33,971 psi. Both of these values are within the criteria.

Since the experiment was planned to achieve a peak burnup of 100,000 MWd/Te in 11,280 hours, the structural criteria can be met with only a minor reduction of irradiation time. The design criteria are not failure criteria. It may not be deemed desirable to reduce the irradiation time depending on the results of further analysis and the interim examination of the assembly.

Figure 1. Longitudinal Primary and Secondary Stress versus Time Grid

Figure 2. Fuel-Clad Interaction Strain

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