MASTER

IMPLICATIONS OF METAL SWELLING IN FAST REACTOR DESIGN

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IMPLICATIONS OF METAL SWELLING IN FAST REACTOR DESIGN

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I. CHARACTERISTICS OF FAST REACTORS

It is useful for scientists, working in the highly-important field of fast-neutron radiation effects, to have a general understanding of the impact of metal swelling on fast-reactor design. To convey such an understanding, it is first necessary to introduce some of the important characteristics of fast reactors.* Since this introduction assumes minimal prior exposure to the subject, and the emphasis is on simplicity rather than thoroughness, readers who already possess a journeyman's understanding of fast reactors should skip this Section.

Figure 1 shows the core and environs of a typical liquid-metal cooled, fast-breeder reactor (LMFBR). In the core, fission heat is generated in long, slender, hermetically-sealed rods called fuel elements. A cross-sectional view of a typical fuel element, through its fueled region, would show a stainless steel tube (cladding) of 0.25 in. outside diameter and 0.015 in. wall thickness, containing fuel in the form of close-fitting pellets. The fueled length, corresponding to the height of the reactor core, is typically 3 ft and is sandwiched between 1.5 ft long axial-blanket sections containing natural UO_2 all within the same cladding. The function of the axial blankets is to capture neutrons that leak from the core in the axial direction, and convert ^{238}U to

* Throughout this paper, the reactor coolant is assumed to be sodium, and the reactor fuel is assumed to be $(\text{U,Pu})\text{C}_2$. Fast reactors can be designed with other coolants and other fuels, but the problems of metal swelling are not avoided by these changes alone.

Fig. 1

Typical LMFBF Core and Environs

²³⁹Pu. The other major contributor to fuel-element length is a 3 ft storage plenum for fission gas (not shown in Fig. 1). This capacity permits the fuel element to operate to an economical level of burnup ($\sim 100,000$ MwD/tonne) without undue risk of cladding failure from over-pressurization. Thus, a typical fuel-element length, including top and bottom end plugs welded to the cladding, is about 9 ft.

Averaged over the entire core at full reactor power, fuel elements generate heat at the rate of about 8 kw/ft. This is about half of the rate that would produce central melting, an undesirable occurrence. Assuming a core height of 3 ft and a plant thermal efficiency of 40%, a 1000 Mwe plant requires about 100,000 fuel elements.

For convenience in handling, fuel elements are grouped into larger units called fuel assemblies. Figure 2 shows a typical fuel-assembly cross section. This assembly contains 217 fuel elements arranged in a regular triangular pattern, and has a hexagonal stainless steel housing (wrapper tube) of about 5 in. width and 0.140 in. thickness.

Sodium flows upward through the vertically standing assembly and through the fuel-element bundle, at a velocity of about 25 ft/sec, while acquiring a temperature rise of about 300°F and a dynamic pressure loss of about 60 psi. Adequate and uniform cooling of all fuel elements in the assembly requires reliable support of the individual elements and preservation of uniform spacing between them. The fuel-element support method (not shown in Fig. 2) may consist of a helical wire wrap on each element or a system of intermittent grids somehow affixed to the wrapper tube.

Fig. 2
TYPICAL LMFBR FUEL ASSEMBLY

The core of the 1000 Mwe plant, therefore, contains about five hundred fuel assemblies and generally about one-tenth as many channels (guide tubes) for control assemblies. The in-core portions of the guide tubes are externally identical fuel assemblies but are hollow and aligned with vertical drive shafts. These drive shafts are frequently protected in above-core tubular extensions of the guide tubes as shown in Fig. 1. The drive shafts, and affiliated mechanisms atop the reactor vessel, move control material (nuclear poison) into and out of the core, in response to operator action or automatic signal. The controlled movement of this nuclear poison which, like fuel, is contained in assemblies for handling convenience, permits the reactor to be started up, shut down, and operated for long periods at constant power level. Surrounding the fuel and control assemblies are, typically, two rows of radial-blanket assemblies and one row of assemblies that serves as a radial reflector and thermal shield. The radial blanket has a composition and function similar to that of the axial blanket previously described. The radial reflector, composed chiefly of stainless steel, further reduces loss of neutrons by radial leakage, and protects the reactor vessel from high temperature and excessive neutron damage during its 30-year lifetime.

In total, the typical 1000 Mwe fast reactor contains in the order of one thousand assemblies -- fuel, control, radial blanket, and radial reflector. Their combined weight, in the order of 200 tons, is borne by a support structure consisting of two horizontal grid plates shown in Fig. 3. In addition to bearing the massive load, the grid plates provide the principal vertical orientation of each assembly. This is achieved by plumb alignment of upper-plate and lower-plate holes that are closely bored to the size of the tubular nozzle at the lower end of the assembly.

HANDLING HEAD

Fig 3

TYPICAL LMFBF CORE-SUPPORT CONCEPT

HANDLING HEAD

Fig. 3

TYPICAL LMFBR CORE-SUPPORT CONCEPT

The geometric array of assemblies (typically triangular) and their center-center spacing are also established by the holes in the upper and lower grid plates. Some clearance between neighboring assemblies is desirable to facilitate fuel handling which is done, one assembly at a time, with an in-reactor machine. The handling machine raises, lowers, and carries the assembly by the handling head at its upper end. A nominal clearance of 0.050 in. between 5 in. assemblies is considered an adequate provision for fuel-handling convenience. Since the handling heads normally are submerged under many feet of opaque sodium, the fuel handling operation is done blindly, and the absolute location of a given handling head must be known with good accuracy.* Obviously, this task becomes more difficult with structural material that is not dimensionally stable.

The total length of the reactor assemblies, from the bottom of their nozzle to the top of their handling heads, is about 14 ft. To avoid vibration and gross distortion from the temperature, temperature gradients, and radiation effects, the assemblies are supported at more places than merely the upper and lower grid plates. Figure 3 shows a typical design in which a support plane is created at the upper end of the assemblies by spacer pads affixed to the wrapper tubes, and another plane of support is accomplished near the midlength using spacer pads. Spacer pads at these intermittent elevations may be of the full-circumference type (shown in Figure 3) or they may be installed only at corners. With these two planes of tightness, the location of the individual assemblies is much better assured than would otherwise be the case.

* In existing fast reactors, the allowable radial uncertainty in handling-head location is 0.25 to 0.5 in. Improved designs are being developed to increase the allowable uncertainty to 1 in. or more.

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Positive, reliable, and reproducible fixation of the fuel relative to the reactor's geometric center is highly important because of the neutronic sensitivity of fast reactors to dimensional changes. This sensitivity can be expressed in the following relationships which are typical for fast reactors:

$$\frac{\Delta k}{k} \sim - 0.5 \frac{\Delta r}{r} \quad (1)$$

$$\text{and } \frac{\Delta k}{k} \sim - 0.3 \frac{\Delta h}{h} \quad (2)$$

where: $\frac{\Delta k}{k}$ is the departure from nuclear criticality (reactivity)

r is the core radius

h is the core height

Interpreting equations 1 and 2, a 0.7% change in core radius or a 1.2% change in core height gives $\frac{\Delta k}{k}$ a value of 0.0035.* In a plutonium- fueled fast reactor, this amount of reactivity is defined as "1 dollar", which is the reactivity equivalent of the delayed-neutron fraction. Since the delayed-neutron fraction represents the margin of safe control of the nuclear chain reaction, anomalous reactivity effects that are a small fraction of 1\$ are closely watched during reactor operation and must be explained. Uncertainties of stainless steel swelling confound the predictability of reactivity trends during the life of a core (~ 2 year) and hinder confident operation accordingly.

* Core contraction causes positive reactivity and core expansion causes negative reactivity.

II. IMPACT OF SWELLING ON SELECTED CORE COMPONENTS

Although core-metal swelling is a problem that can affect the design strategy of the entire plant, it is convenient to examine separately the effects of swelling on: (a) fuel elements; (b) fuel-element support; and (c) core structure. Items (a) and (b) represent the internals of the fuel assembly which has a short service life compared to that of the plant (i.e. 2 year/vs. 30 year). Since fuel assemblies are expendable and have no inherent design permanence, the plant owner can capitalize on scientific breakthroughs and evolutionary advances in materials science during the plant lifetime. Item (c), however, represents or implicates core hardware or design approaches that are not easily changed during the life of a particular plant. Therefore, the approach to core support and restraint, and the design strategy to accommodate core metal swelling become permanent features of the plant and are among the most important design decisions.

A. Fuel Element

Stainless-steel swelling was first observed on fuel-element claddings in the UK fast-reactor program[1]. That, plus the fact that the US fast-reactor program was committed to fuel-element development more than to any other single endeavor, made it inevitable that fuel-element lifetime be the first subject of concern. Specifically, the need to discount the successful irradiations of oxide fuel elements in the Dounreay Fast Reactor (DFR) and the Experimental Breeder Reactor II (EBR-II) was widely discussed, as in the following excerpt from the Edison Electric Institute's Fast Breeder Reactor Report[2]:

"The implication of these [indications of stainless-steel swelling] results is that the burnup capability of stainless steel clad oxide fuels -- as demonstrated by radiation tests in DFR, EBR-II, and thermal reactors -- must be re-evaluated. These radiation tests were interpreted to indicate a demonstrated burnup capability of at least 50,000 Mwd/T. In the carrying out of these experiments, it was acknowledged that the clad did not receive a total fluence typical of that which would be obtained in the large fast reactor. It, however, was assumed that this discrepancy, about a factor of 3 to 4 in the clad neutron dose, would have no significant effect on the overall behavior of the fuel. With the observation of possible significant stainless steel clad swelling, this assumption is now questionable, and until such time as radiation tests can be carried out under more typical conditions, the demonstrated burnup capability is only equivalent to about 15,000 to 25,000 Mwd/T consistent with the total neutron dose obtained on the clad."

Illustrating this point, Fig. 4 shows the effect of stainless steel swelling on typical mixed-oxide fuel elements in three fast reactors.* Identical fuel elements in terms of size, composition, power rating, and burnup, experience vastly different amounts of cladding dilation from the metal-swelling source. The ratio of cladding dilation in EBR-II, the Fast Flux Test Facility (FFTF), and a commercial LMFBR power plant, is about 1:5:7.

In Figure 4, and throughout this paper unless otherwise noted, the basis for swelling predictions is the August, 1969 BNWL-WARD expression for cold-worked 316 stainless steel [3]. This expression is given below in equation form and in Fig. 5 in graphical form:

$$\frac{\Delta V}{V} = 1.01 \cdot 10^{-36.0} \times (\phi t)^{1.69} \left(\exp \frac{-7808}{RT} - 5480 \exp \frac{-25300}{RT} \right) \quad (3)$$

where $\frac{\Delta V}{V}$ = fractional volumetric change

ϕt = fluence (neutrons/cm²) of E > 0.1 MeV

T = temperature in °K

R = 1.987

Fig- 4

CLADDING DILATION FROM RADIATION-INDUCED SWELL
 IN THREE FAST REACTORS

Fig. 4

SWELLING DILATION FROM RADIATION-INDUCED SWELLING
IN THREE FAST REACTORS

Fig- 4

CLADDING DILATION FROM RADIATION-INDUCED SWELLING
 IN THREE FAST REACTORS

ASSUMPTIONS

PEAK LINEAR POWER = 16 kW/ft
 ϕ_{max} (EBR-II) = 2.5×10^{15} (75% > 0.1 MeV)
 ϕ_{max} (FFTF) = 7.0×10^{15} (70% > 0.1 MeV)
 ϕ_{max} (LMFBR) = 10.0×10^{15} (50% > 0.1 MeV)
 FUEL-ELEMENT OD = 0.25 in.
 CLADDING THICKNESS = 0.015 in.
 SMEARED DENSITY = 9.35 g/cc (85% TD)
 CLADDING TEMP. AT PLACE OF MAX.
 SWELLING = 1000°F
 $\Delta D/D = 1/3 \Delta V/V$

Fig- 4

CLADDING DILATION FROM RADIATION-INDUCED SWELLING
 IN THREE FAST REACTORS

SWELLING ESTIMATES OF TYPE 316
STAINLESS STEEL 20% COLD WORKED-FFTF
INTERIM DESIGN BASIS (AUG. 1969)

It is not self evident, however, that extra cladding dilation due to radiation-induced swelling increases the probability of cladding failure. Some investigators regard this phenomenon as a potential promoter of higher burnup [3,4,5,6]. This is derived from the thesis that dilation of the inside diameter of the cladding makes it possible to accommodate more fuel expansion. If this thesis is correct, cladding dilation due to swelling would permit a higher smeared density of fuel (helpful to breeding gain and Doppler effect), and increase the intrinsic burnup capability of fuel elements to a limit not yet defined.*

The present view of fast reactor core designers seems to be that metal swelling should not be interpreted as a "cladding problem" or a "fuel-element problem," as was first done. There is some chance that cladding swelling may actually allow more burnup before cladding failure, although it will be several

years before this thesis can be evaluated experimentally.** Until then, it is a safer speculation that core lifetime will be limited by something other than the intrinsic burnup capability of high-quality fuel elements. If this speculation proves correct, the emphasis in fuel-element development should gradually shift toward higher smeared density and the lowering of fabrication costs by eliminating requirements that purchase burnup capability beyond the usable range.

* The limit might be imposed by a void content at which thin-walled metal tubing is no longer an acceptable pressure vessel material capable of surviving thermal shocks and other duty indigenous to fuel-element cladding. This limiting void content has not been established empirically except that it is evidently higher than the maximum thus far seen on unfailed fuel elements ($\sim 10\% \Delta V/V$). Reference 7 derives the estimated effect of swelling on tensile properties, using an analogy with powder-metallurgy technology which may be a useful first approximation until more empirical data are reported.

** The most unambiguous test of the thesis requires testing to failure of many fuel elements in two fast reactors with substantial difference in flux (e.g., FFTF and EBR-II) but other parameters held constant.

B. Fuel-Element Support

The helical-wire wrap, with advantages of low cost, moderate pressure drop, and good mixing potential, was the most popular fuel-element support system in fast reactors built or committed before the discovery of metal swelling. However, several of the AEC-sponsored 1000 Mwe LMFBR design studies [8-12] chose intermittent grids, partly to realize a small improvement in breeding gain from the lower mass of stainless steel. Since the discovery of metal swelling, core designers have debated whether grids are required (because of metal swelling) for early cores in near-term high-flux LMFBRs (FFTF and the 300-500 Mwe Demonstration plants), or whether their development can be deferred to an era when major fast reactor problems are under control and research and development emphasis can focus on modest economic gains.

The potential problems of the wire wrap are: (a) it offers little compliancy to accommodate planer differential swelling between the fuel-element bundle and the wrapper tube, and (b) differential swelling between cladding and wire could produce either wire breakage or slackness. Even if all the metallurgical determinants of swelling are known and held constant, differential swelling between the fuel-element bundle and its wrapper tube and between the cladding and its helical wire are inevitable because of temperature differences (see Fig. 5). Figure 6 shows typical average axial-temperature profiles for cladding, helical wire, and wrapper tube. The wire temperature is less than that of the cladding by the amount of the film ΔT plus half the cladding ΔT . The wrapper tube is still cooler due to the characteristic excess flow area at the bundle periphery. This excess area is normally less for a wrapped bundle than for a gridded bundle.*

Even when loose packing is provided to accommodate differential swelling, the inevitable non-straightness of fuel elements will cause the bundle to spread within the assembly, with the wire wraps of the edge elements contacting the wrapper tube and initial clearances distributed throughout the interior of the bundle [13]. If it is desired to further reduce wrapper-tube overcooling, a subsized wire can be employed on the edge elements.

Fig. C

TYPICAL AXIAL TRACE OF AVERAGE
METAL TEMPERATURES IN AN LMFBR
FUEL ASSEMBLY

Based on the temperature estimates in Fig. 6, the swelling predictions in Fig. and 1.5×10^{23} n/cm² ($E > 0.1$ MeV) as the peak fluence in early cores of near-term LMFBRs, a 1.5% planar differential linear expansion between a wire-wrapped bundle and its wrapper tube should be anticipated. This estimate includes an allowance for cladding strain caused by mechanical loading from the continued fuel. A 1.5% differential $\Delta L/L$ can be accommodated by clearances (porosity) in the as-fabricated fuel assembly but the upper limit of this design tactic, before problems of temperature uncertainty and vibration become excessive, has not been established. There is little doubt that the grid, properly designed to offer compliant fuel-element support, is better able to cope with a large or uncertain swelling differential between the fuel-element bundle and the wrapper tube.

Figures 5 and 6 can also be used to estimate differential elongation between the cladding and its wire wrap. At 1.5×10^{23} nvt ($E > 0.1$ MeV), that differential appears to be about 0.4 %. Allowing for differential thermal expansion and cladding strain from fuel growth, the wire-length deficit becomes about 0.5%. Since the wire wraps the fuel element along its entire length, of which the core accounts for only 33% (see Sect. I), the actual deficit in wire length is only 0.18%.

Thus, an initial slackness amounting to 0.18% of the wire's total length would prevent end-of-life strain in the wire. Initial slackness is probably preferable to an initially tight wire which would subsequently suffer a low average strain that could be seriously magnified at a weak spot near the end welds. Wire-wrapped test elements, irradiated in EBR-II to $> 100,000$ MwD/tonne, have been deformed by the tight wire into a helix 180° out of phase with the wire[14]. While undesirable and easily avoided, this condition has not been blamed for any failures to the U.S. fuel-development program.

Throughout this evaluation it has been assumed that swelling will obey equation 3 (Fig. 5) and that metallurgical factors affecting swelling are common to all of the stainless steel in the fuel assembly. There are, unfortunately, fundamental problems with these assumptions. Swelling data are very scattered and cold work may be difficult to match in dissimilar pieces. If the wrapper tube, cladding, and helical wrap follow swelling laws that differ from one another in the adverse direction, metal swelling could impose a burnup limit on the helical wrap concept as low as 30,000 MwD/tonne in a high-flux LMFBR [15]. For this reason, LMFBR core designers are not unanimous in their opinions about fuel-element support, though most agree that fuel-element support is unlikely to emerge as the burnup-limiting component in near-term cores. Fortunately, the choice is not an irrevocable 30-year decision, but one that can be reconsidered for follow-on cores when metal swelling and other pertinent behaviorisms are better characterized. With such reasoning as this, the FFTF Project has chosen the wire wrap for initial use, but the US LMFBR Program is supporting a modest development of industrially-favored grid designs.

C. Core Structure

In fast reactor cores of conventional design, metal swelling will cause the wrapper tubes of fuel and blanket assemblies, and the guide tubes for control and safety assemblies, to elongate and dilate (enlarge in cross section) during service. Also, when swelling of wrapper and guide tube material is not circumferentially uniform, bowing will occur and other forms of distortion (e.g. twist) are possible. The supporting structure for the core and blanket assemblies (grid plates), and the structure over the assemblies (instrument or holddown plate), are generally considered permanent installations. Both components are typically exposed to temperatures in the swelling regime (700-1100°F), and must be examined for the possibility of excessive swelling during a 30 ± 10 year lifetime.

The Atomics International 1000 Mwe LMFBR Plant Design [16], with certain simplifications, was chosen as a basis for calculations of the impact of metal swelling on the core structure in commercial fast reactors. The reference core has two radial enrichment zones and is 43 inches high. The diameter of Zone-1 is 35.4 in., and Zone-2 is 51.0 in. There are two radial blanket zones of one assembly thickness (5.6 in. each) having average outer radii of 56.6 in. and 62.0 in respectively. A stainless steel reflector surrounds the blanket radially. In the axial direction: the upper surface of the grid plate is 50.5 in. below the core mid-plane; there is a 15-in. lower axial blanket, and a 12-in. upper axial blanket; the top of the fuel assembly is 142 in. from the grid plate (91.5 in. from the core mid plane); and an instrument plate is located at the top of the assemblies. The residence times were taken as 2 yr. for the core assemblies, 5 yr for the radial blankets, and 30 yr. each for the radial-reflector assemblies, the grid plates, and the instrument plates. Other design parameters were taken directly from the published study [16].

Elongation: The end-of-life elongation of opposite walls of each assembly are shown in Fig. 8. It is noted that the inside walls elongate more than the outside walls due to temperature and flux gradients. Average elongations of the core assemblies range from 0.822 to 1.673 inches from their original lengths of 142 inches. The large elongation in the first row of radial blanket assemblies is due to their extended residence time.

Elongation of assemblies, and differences in elongation, will complicate the design of the fuel handling machine somewhat. More flexibility is required than when the elevation of the handling head is unchanging. Also, some reactors employ a sweep arm or similar device for assuring that assemblies are properly seated in the grid plate. A 1 to 2 inch change in assembly length will make such an operation more difficult, particularly in a core containing both old (long) and new (short) assemblies. On the whole, however, elongation appears to be the least serious manifestation of swelling of wrapper and guide tubes, although it is not trivial.

7
 FIG. 8- ELONGATION OF WRAPPER-TUBE WALLS DUE TO SWELLING
 ASSEMBLY ROWS NUMBERED FROM CORE CENTER
 (END OF LIFE)

Dilation: The radial dilation of each row of assemblies (assuming no restraint) is shown in Fig. 8 at end of life. The total diametral dilation would be twice the amount shown. The maximum diametral growth of any assembly during its expected lifetime is about 0.230 in. The plane of maximum dilation occurs about seven in. above the core mid plane, this being the location of the worst combination of flux and temperature in this design. Clearly, the nominal 0.050 in. gap between assemblies is insufficient to accommodate swelling-induced dilation. The necessary gap is about 0.280 in. i.e. the maximum diametral growth of any assembly plus a fuel-handling allowance of 0.050 in.

Bowing: Differential swelling of opposite sides of the wrapper tube can be expected to produce an irreversible bowing of the assembly. The elongation of opposite walls of each assembly is shown in Fig. 7, from which it is evident that significant elongation differentials exist at the end of life, and that these differentials are especially large near the core edge. Differential elongations shown in Fig. 7 correspond to free-bowed shapes (neglecting mechanical interferences with neighbors) shown in Fig. 9.

If the outward free bowing were suppressed by top-end restraint, the bowing profiles would be as shown in Fig. 10. This figure shows an inward bow of the fueled length of about 0.46 in. in the outer core assembly and more in the inner blanket assembly (0.88 in.). This inward movement of fuel represents a mechanism for slow but substantial reactivity addition (see Sect. I). For reasons such as this (originally to preclude positive

22

DISTANCE BETWEEN VERTICAL LINES = 0.05 in.

Fig. 8

DILATION OF WRAPPER TUBES DUE TO SWELLING
(END OF LIFE)

25
35-

35-25

DISTANCE BETWEEN VERTICAL LINES = 100 in.

$F_{3.10}$ BOWING PROFILES OF ASSEMBLIES WITH TOP-END RESTRAINT
(END OF LIFE)

reactivity

feedback from thermal bowing), spacer pads were located in the core zone.

Figure 11 shows the effect of locating the spacer pad, as was done by AI, 10 3/4 inches above the core midplane. Although this is effective in counteracting positive reactivity effects, relatively high bending stresses are implied by the shapes indicated in Fig. 11, and spacer pads placed along the fueled length interfere with the accommodation of dilation.

Except for the influence of creep*, a fuel assembly removed from the restraining effect of its neighbors will tend to assume the free-bowed shape depicted in Fig. 9. This could create considerable difficulty if subsequent operations (intra-site transport, cleaning, decay storage, shipping, etc.) are designed to accommodate only straight components as was the practice before metal swelling was discovered.

Grid Plate and Instrument Plate: To define the swelling problem with respect to "permanent" components at the fuel-assembly extremities (grid plates and instrument plate), a number of S_6 transport theory [17] calculations related to the AI 1000 MWe design were performed. To improve the general usefulness of the study, axial blanket lengths were varied from 10 to 20 inches. Figure 12 presents the high-energy neutron flux at the top and bottom of the upper grid plate and the instrument plate as a function of axial blanket thickness, with these fluxes expressed as percentages of the high-energy flux at the core midplane.

* Both conventional "thermal" creep and radiation-enhanced creep, about which more will be said later.

27
-56-

DISTANCE BETWEEN VERTICAL LINES = 1.00 in.

DISTANCE BETWEEN VERTICAL LINES = 1.00 in.

Fig. 2 BOWING PROFILES OF ASSEMBLIES WITH TOP-END RESTRAINT AND IN-CORE SPACER PADS (END OF LIFE)

~~29~~ = 28

Fig. 14

$\phi_1 / \phi_{1,MAX} \%$

Fig. 12

$\phi_1 / \phi_{1,MAX}$ FOR GRID PLATE AND INSTRUMENT PLATE VERSUS AXIAL BLANKET THICKNESS

Despite higher operating temperatures, it appears that swelling of the instrument plate will be less than that of the upper grid plate using the same lengths for upper and lower blankets and current swelling estimates. The long upper plenum in the AI design causes a ten-fold reduction in instrument-plate flux relative to the upper grid plate. Figure 13 presents swelling estimates of the upper grid plate as functions of service lifetime and axial-blanket length. The BNWL-WARD 1969 swelling estimates for solution-treated type 304 stainless steel*[3] were taken as the basis for these calculations. (Unlike smaller core components which can be fabricated in an annealed or cold-worked condition, the massive grid plate is conventionally specified to be annealed). It appears, from Fig. 13, that a 15-inch blanket offers adequate shielding for the upper grid plate, although this conclusion is sensitive to the questions of whether swelling depends on flux as well as fluence, and whether such dependence works to the advantage or the disadvantage of long-lived components. Differential swelling can be a serious problem in grid plates**, and the only protection is to keep the absolute swelling of the top surface very low. The grid-plate design concept that uses a high-flux-region insert (as in the Fermi reactor) has merit in view of the prevailing uncertainties concerning long-term, low flux swelling effects. Not only is replacement facilitated by this design, but buckling due to radial swelling variations can be avoided.

*
$$\frac{\Delta V}{V} = 4.32 \times 10^{-49} (\phi t)^{1.71} \exp \left(\frac{3.567 \times 10^4}{T} - \frac{1.379 \times 10^7}{T^2} \right) \quad (4)$$

** A 1% differential volume growth on opposite faces of a grid plate that is 10 ft. in diameter and 4 inches thick produces a free bow of 1.5 inches.

VOLUMETRIC EXPANSION OF UPPER
 GRID PLATE

III. IMPACT OF SWELLING ON SOME EXISTING REACTORS

Operational fast reactors, like EBR-II and the Enrico Fermi Atomic Power Plant in the US, were designed with no knowledge of, or deliberate provision for, stainless steel swelling. Fast reactors now in the latter stages of construction, like the Prototype Fast Reactor (UK) and the BN 350 (USSR), had little forewarning of the phenomenon during their design periods. Brief comments on the impact of swelling on these reactors are offered below. A more extensive discussion is given in Reference 7.

A. Experimental Breeder Reactor-II

Although EBR-II was designed [18] 10 years before stainless steel swelling was recognized, swelling has caused no serious problem in seven years of operation, and plant factor has increased steadily. Low flux and low burnup combine to limit the removal fluence of driver fuel assemblies to a value at which swelling is negligible. The use of EBR-II to irradiate ceramic fuel elements to high burnup and structural materials to high fluences has occasionally resulted in swelling-provoked deformations that hinder removal of assemblies from the core and fuel elements from assemblies. Recently removed control and safety-rod guide tubes (thimbles) have survived the highest fluences to date--nearly as high as FFTF driver assemblies at goal burnup and ~ 60% of the lower end of the range of interest for commercial reactors (Fig. 14). Guide tube temperatures, however, cover only the lower half of the range of commercial interest. Data on swelling and mechanical properties from these thimbles are expected in the second half of 1971.

FAST NEUTRON FLUENCE AND REQUIRED TEST TIME VERSUS PROGRAM OBJECTIVES

Fig. 14

The EBR-II assemblies employ hexagonal wrapper tubes 2.290 in. across outside flats, with a nominal cold gap between assemblies of 0.030 in. One 3/8 in. OD spacer pad (produced by indenting the inside surface of the wrapper tube) is provided in the center of each flat, at an elevation about 1 in. above the core mid-plane. Nominal height of each spacer pad is 0.014 in., so the nominal cold gap between pads is 0.002 in. No plane of support is provided at the top ends of the assemblies, thus they are free to splay outward (away from the core centerline) from differential thermal expansion and differential swelling.

Except at the spacer pads, dilation of the wrapper tubes can occur freely until the 0.030 in. gap between assemblies is consumed. That amounts to about 1.3% $\Delta L/L$ or 3.9% $\Delta V/V$ swelling, consistent with the response of cold-worked cladding at 10% burnup of fully enriched mixed-oxide fuel elements under test in the EBR-II neutron flux. The mechanical loads on the spacer pads are not precisely known but they have proved sufficient to produce creep and/or swelling suppression such that the distance across pads remains relatively unchanged while normal dilation occurs just 1/2 in. away. This behavior has kept the reactor from being seriously limited by the presence of in-core spacer pads.

B. Enrico Fermi Reactor

The Enrico Fermi Atomic Power Plant was designed [19] in the same era as EBR-II but has many distinguishing features, some of which may affect how the reactor responds to stainless steel swelling [20].

The fuel elements and separate upper and lower axial-blanket elements are housed within a square wrapper tube 2.646 in. across outside flats. The wrapper tube is Type 347 cold-worked stainless steel, nominally 0.096 in. thick. Vertical orientation of reactor assemblies is provided conventionally by a double grid plate which receives the tubular nozzles in close-fitting holes. A plane of spacer pads exists 4.5 in. above the midplane of the 30.5 in. long core. These pads, made of Colmonoy, are welded to the corners of the wrapper tubes and machined to close tolerances.

The handling head of each core and inner-radial-blanket assembly is captured by a stiff hold-down finger projecting downward from a horizontal hold-down plate. This finger prevents lifting from hydraulic forces and also precludes radial displacement of the assembly's upper end from differential thermal expansion or differential swelling. The center-to-center spacing of hold-down fingers is slightly less than the center-to-center spacing of grid plate holes. Accordingly, when the hold-down plate is lowered (as it always is before reactor startup) and the handling heads are captured by their respective hold-down fingers, an inward gathering of assemblies occurs and all clearance at the spacer-pad plane is eliminated. This design strategy is commonly called the "tight-core concept" and it produces very dependable reactivity effects.

The design, based entirely on thermal-effect considerations,

must be analyzed for the effects of swelling. Because of low burnup expectations and low temperatures of the metallic Core A fuel, very little swelling will be experienced until the proposed UO_2 core is installed and operated well into its life expectancy. At a peak total flux of $\sim 6.5 \times 10^{15}$ n/cm^2 sec ($\sim 70\% > 0.1$ MeV), the oxide core will have a burnup to fluence (swelling) ratio similar to that of future LMFBRs.

Elongation of the wrapper tube due to swelling can be accommodated by the feature originally provided to accomodate thermal expansion, i.e., the nozzle spring. Dilation accommodation, in this reactor with in-core spacer pads, would be problematical except for the encouraging performance of EBR-II. Assuming assistance from radiation-enhanced creep and/or retardation of swelling by compressive stress in the spacer pad region such that the full 0.047 in. nominal gap between assemblies can be claimed as dilation provision, a peak $\Delta V/V$ of $\sim 5.3\%$ can be permitted. In the diagram of dilation accommodation (Fig. 15), note that the corner fuel element is intentionally omitted (in the oxide core design) since the fuel-element support grids are affixed to the wrapper tube by set screws into corner pags. The plane of the spacer pads does not coincide with a grid.

The effects of swelling-produced bowing (differential elongation) are shown qualitatively in Fig. 15. The S-shaped thermal-bow should gradually become more pronounced with swelling. This is because very little swelling will occur below the spacer-pad plane (~ 700 F). With a higher inlet temperature, the S-shaped thermal bow could reverse. The limiting factor in the ability of the Fermi design to accommodate bowing is the

REACTOR
CENTERLINE

Fig. 29¹⁵

SWELLING ACCOMMODATION IN FERMI OXIDE CORE

necessity of the handling head to mate with the holddown finger when the holddown plate is lowered prior to startup. The nominal allowable mismatch is $1/4$ inch although with adverse maximum tolerances, this is reduced to about $1/16$ inch. It can be surmised that the feasibility of high-fluence operation will be greatly assisted by radiation-enhanced creep.

C. Prototype Fast Reactor

The British Prototype Fast Reactor [21-23] was under construction when the phenomenon of stainless-steel swelling was first observed. At that point, obviously, options were limited for core redesign in response to this newly-discovered property. In synthesizing the reference design, however, the engineers at Risley had made certain decisions that appear fortunate in terms of giving the core a capability to accommodate some metal swelling.

The distinguishing feature of the PFR core plan (Fig. 16) is the rosette pattern of fuel and non-fuel assemblies in the core. It is intended that each fuel assembly, in groups of six, lean on a central structural member externally similar to the fuel-assembly wrapper tube. In most cases, the "leaning post" also serves as a safety-rod or control-rod guide tube. Contact between a fuel assembly and its assigned leaning post is brought about by an out-of-plumb alignment of the two nozzle support points. Contact is established at a spacer-pad plane nine inches below the bottom of the 36 in. long core. A bending bar concentrically located in the fuel-assembly nozzle deflects elastically under the force needed to insert the nozzle

FUEL ASSEMBLY
(LEANING TO RIGHT)

SAFETY MEMBER

CONTROL MEMBER

IRRADIATION TEST

PFR CORE PLAN

into its receiving socket^{*}, producing a contact force at the spacer pads.

With the below-core spacer pads being the only intentional region of contact between a fuel assembly and its leaning post, the full gap between adjacent wrapper tubes is available to accommodate dilation in the fueled length. This represents a potential for accommodating swelling of up to 8.7% $\Delta V/V$ (0.16 in. gap out of a 5.7 in lattice spacing). Clearance between the upper and lower guide tubes is ample to accommodate elongation of the latter. No other problems from swelling-induced elongation are apparent.

Swelling-induced bowing in this unrestrained design is apt to produce greater problems than either dilation or elongation. Without restraining stresses, radiation-enhanced creep can not play a role in maintaining wrapper tube straightness. Excessive bowing from swelling differentials will presumably be prevented by frequent rotation which serves to equalize swelling circumferentially.

D. BN 350

Like the British PFR, the Russian prototype LMFBR, BN-350, was well under construction when metal swelling was discovered, reported, and confirmed in the western world. It is believed that no last-minute alterations were made as a result of this discovery, so the core would be expected to have only limited capability to accommodate swelling.

^{*} It is intended that this force be no more than the assembly's dead weight.

The BN-350 core [24] is essentially a hybrid between EBR-II and Fermi. The fuel assemblies are hexagonal, free to bow at the top, and radially restrained only by the surrounding fence of radial blanket and reflector assemblies. Spacer pads are located in the fueled zone--at wrapper-tube corners.

The wrapper tube is 3.780 inches across outside flats with a nominal cold gap between assemblies of 0.079 in. (See Fig. 17). The spacer pad occupies only 0.059 inch of the gap between assemblies, leaving a cold clearance in the spacer pad plane of 0.020 in. (compared with 0.002 inch in EBR-II). Thermal expansion to operating temperatures would still leave a gap of at least 0.016 inch although this would disappear with metal swelling after a peak burnup of about 30,000 MwD/tonne based on Fig. 5. Further burnup requires that the wrapper tube distort around the spacer pads, assisted by radiation-enhanced creep. As an upper limit, assuming the 0.079 inch gap is fully effective in accommodating dilation, the core could survive metal swelling of ~6% $\Delta V/V$, equivalent to a peak burnup of about 70,000 MwD/tonne based on the swelling relationship plotted in Figure 5.

As is evident in Fig. 17, wrapper-tube dilation will produce pad-on-wrapper contact instead of the conventional pad-on-pad contact. In addition to possibilities of damage to the stainless-steel wrapper tube, removal of a swollen assembly is made more difficult by this design approach. If, for example, a wrapper tube has dilated more than 0.020 inch in any plane below the spacer pads, it becomes trapped by its six neighbors and must be forcefully extruded through the constriction formed by the adjacent assembly's spacer pads. Full core refueling, removing assemblies

- 41 -
- 90 -

WRAPPER TUBE
(OUTSIDE FLAT)

BN-350 CORE-ASSEMBLY PATTERN

Fig. 17
3F

starting from the outside, appears to be the only way to utilize the full 0.079 inch gap and thereby to achieve a respectable burnup in this design.

IV. STRATEGIES OF SWELLING ACCOMMODATION

Since the first reported indication of the stainless steel swelling problem in late 1967 [1], fast reactor core designers have examined numerous design solutions in varying levels of detail. This Section reviews the progress in this area of activity.

A. FFTF Approach

On the FFTF Project, considerable effort was devoted to interpreting the design implications of swelling, evaluating engineering solutions to the problem, and developing specialized analytical tools for predicting the response of the core to metal swelling. Most of the early contributions to the design literature [25-29] resulted from the special timeliness of this project. It is to be expected, therefore, that a current favorite among design strategies for accommodating metal swelling would be the FFTF approach including its close variations.

The design strategy for accommodating metal swelling in the FFTF can be summarized as follows:

- (1) The fuel elements (217 per assembly) are housed within conventional rigid wrapper tubes. Flexible or articulated wrapper tubes were considered but not chosen because extensive development was deemed necessary.
- (2) Wrapper-tube dilation is accommodated by large inter-assembly gaps.

- (3) Spacer pads are located above the core, and the inter-assembly gap is unobstructed over the high-fluence region of the wrapper tube.
- (4) Fuel-bundle swelling is accommodated by wrapper-tube dilation plus porosity within the as-fabricated fuel assembly.
- (5) A peripheral core clamp suppresses bowing due to differential elongation, and maintains tightness at the above-core spacer pad planes.
- (6) Radiation-enhanced creep is depended upon to promote reasonable assembly straightness for refueling, and to relax high bending stresses effected by the core clamp.

The mechanical design of the core clamp has not been disclosed in detail. Its function is to maintain tightness at the spacer pad planes when engaged, and provide clearances for fuel handling when disengaged.

In the FFTF or clamped-core approach, differential-thermal elongation, differential-swelling elongation, and radiation-enhanced creep interact in a highly complex manner as shown in Fig. 18 taken from Ref. 29. When only thermal strains are considered, the bowing profile of all assemblies except the one at core centerline is qualitatively represented by Fig. 18a. Differential swelling, by itself, would produce shapes like the one in Fig. 18b and the additive effect of thermal and swelling strains is represented by Fig. 18c. In suppressing free bowing in the direction that would be produced by differential thermal elongation and differential swelling elongation, the core clamp imposes high bending stresses. The relaxation of these stresses, by radiation-enhanced creep, alters the bowing profile markedly (Fig. 18d). Note that the shapes

INWARD DEFLECTION ← | → OUTWARD DEFLECTION

Fig-18
**ASSEMBLY DEFLECTION PATTERNS IN THE
 CLAMPED-CORE APPROACH**

qualitatively depicted in Fig. 18 are a function of time, location in the reactor, and the design expressions used to describe swelling and creep.

When the core is unclamped for fuel handling and the above-core restraints (See Fig. 3) are removed, the assemblies assume shapes of a basically different character, as shown in Fig. 19 which is also taken from Ref. 29. Again, it is convenient to analyze separately the three factors responsible for distortion. With the reactor at zero power, the thermal strains are removed and the assemblies would be straight (Fig. 19a) if no other distortion mechanisms were operative. Differential swelling is not reversible, however, so the upper ends would tend to splay outward (Fig. 19b and c, also see Fig. 9) when freed of restraint. With sufficient stress relaxation at power, however, the profile at zero power can actually be reversed (Fig. 19d). Note that the radial position of the top end, which must be known with fair precision for blind fuel handling, is a strong function of swelling and radiation-enhanced creep.

B. Other Approaches

Other design strategies for accommodating stainless-steel swelling or otherwise mitigating the principal problems of swelling have been examined, though generally not in the detail of the "FFTF Approach". Some of these alternate methods have been documented in open literature [30-35]. Reference 7 describes many alternatives and analyzes their relative effectiveness, economic penalties, and developmental requirements. This Section is a compendium of the open literature in this area.

Extrapolated EBR-II: In this approach [30], the basic simplicity of

Fig. 19
 ASSEMBLY DEFLECTION FOLLOWING
 REMOVAL OF RESTRAINT

46-94

the EBR-II design is retained to the maximum possible extent. A thick spacer pad is added at the top end of the assembly to limit free-end deflection, but no peripheral core clamp is used. Spacer buttons on the wrapper tube flats are retained in the high-fluence region, although some design modification is suggested to facilitate their collapse under the simultaneous effects of swelling and creep. In another modified EBR-II approach, the spacer buttons are moved to the lower-axial-blanket region [7].

Cold-Wrapper Tube: This design [31] evolves from the observations that: (1) most of the problems caused by metal swelling are associated with dimensional instability of the wrapper tubes (see Sect. II); and (2) metal below a certain threshold temperature is dimensionally stable. The swelling threshold appears to be $\sim 660^{\circ}\text{F}$ [36], a temperature not appreciably below typical LMFBR sodium-inlet conditions. Therefore, a dimensionally stable wrapper tube is preserved by channeling reactor-inlet sodium past the inside surface of the wrapper tube. This cold bypass stream mixes with the hot main stream above the high-fluence region of the wrapper tube, and reactor outlet temperature is not degraded significantly.

Articulated Assembly: Articulating* the assembly has the desirable effect of reducing deformation and straining forces compared to rigid members with equivalent amounts of differential swelling. Designs employing one, two, or three joints have been critiqued [7].

Circular Wrapper Tube: Since wrapper tubes will be dimensionally unstable under the influences of swelling and creep, one should consider making them in the most fabricable and structurally optimum shape, i.e., a circular tube. These tubes, oriented by a grid plate into the usual

*That is, providing joints or localized areas of increased or complete flexibility.

triangular array, could be held in line contact with their six neighbors by means of a simple peripheral clamp as in SEFOR [37]. Space for dilation accommodation is conveniently afforded by the cuspidated areas between three tubes, provided that radiation-enhanced creep is a large and dependable effect at high fluences.

Bowing-Minimization Features: Noting that most of the free radial displacement of core-assembly upper ends is caused by the portion of wrapper-tube above the core region (see Fig. 9), the problems associated with suppressing bowing are reduced if this length can be reduced. Placing the fission-gas storage plenum beneath the fuel and housing the upper axial blanket in a separate assembly have been proposed as a means of accomplishing this goal [32].

The ²³³U-Th Cycle: Among the 5 or 6 "neutronic methods"* that have been proposed to combat the metal swelling problem [7], the use of the ²³³U-Th cycle is the least obvious but the most interesting. Substituting ²³³U for ²³⁹Pu as the principal fissile isotope has two effects that are beneficial in terms of reducing the swelling to burnup ratio. First, ²³³U has a higher fission cross section and can therefore produce the same fission rate with a lower neutron flux. Secondly, the ²³³U-Th fuel system has greater spectral flexibility. That is, swelling could be reduced by lowering the average neutron energy without a serious diminution of the breeding ratio.

Refractory Metals: In principle, the metal swelling problem can be solved by replacing stainless steel, entirely or strategically, with

* Methods of decreasing high-energy neutron fluence on core and core-proximity hardware.

material that does not swell at fast reactor conditions. Complete substitution and various schemes of partial substitution have been evaluated [33], using molybdenum as the replacement material. Apparently, molybdenum swells only at temperatures above the range of design interest [38]. This desirable property, along with its high-temperature strength must be balanced against penalties in neutronic performance [39-40] and increased fabrication costs [41] inherent in most refractory metal candidates.

Periodic Rotation of Assemblies: Rotating assemblies 180° periodically during their in-reactor residence, reduces elastic bending deflection (bowing) by averaging the fluence and swelling on the wrapper tube circumference. This is the reference strategy in the Gas Cooled Fast Reactor [34] and is compatible with a number of other partial solutions [32] in evolving a composite strategy for coping with metal swelling.

Periodic Annealing: In the "Calder Hall Class" of British gas-cooled reactors, the problem of radiation-induced swelling of graphite is solved by periodic in-situ annealing. A brief high-temperature ($\geq 1400^\circ\text{F}$) annealing of irradiated type 304 stainless steel has been found to sharply reduce the void content [42]. It has been proposed [35], therefore, that periodic in-situ annealing be considered for future reactors as a way of coping with swelling. Obviously, considerable design and materials work would be needed to demonstrate feasibility. If the design problems associated with in-situ annealing prove too formidable, periodic ex-reactor annealing (which requires more fuel handling and a larger on-site fuel inventory) could be considered.

V. EMPIRICAL ASSESSMENT OF SWELLING AND RADIATION-ENHANCED CREEP FROM EBR-II CORE COMPONENTS

Contemporary LMFBR core design requires reliable expressions for swelling and creep as well as an understanding of integral swelling-creep effects. Core components removed from EBR-II, at fluences approaching near-term targets (see Fig. 14), exhibit deformations that are attributable to these irradiation effects. In many ways, these components offer the best current opportunity to assess the reliability of contemporary swelling and creep estimates at high fluences, and to gain insight into the response of stainless steel components to the fast-reactor environment.

Argonne National Laboratory is coordinating the effort to derive maximum engineering value from the EBR-II core-component measurements. The EBR-II Project provides the neutron fluxes [43], temperatures [44], and nondestructive measurements employed in the analyses. Destructive measurements are obtained from EBR-II and other projects within ANL, and from other participating U.S. Laboratories.

Analytical efforts to date have been directed toward understanding

(a) wrapper-tube bow, (b) "moat" formation near wrapper-tube spacer pads, and (c) fuel-element-cladding bow. With regard to wrapper-tube bow, a number of discharged tubes have permanent convex curvature (with respect to the core vertical centerline) presumably due to transverse differential swelling. Figure 20 compares measured permanent transverse displacements of the experimental-assembly-XA08 wrapper tube, with displacements calculated by several different procedures. The XA08 assembly held a number of mixed-carbide fuel elements that achieved about 10 percent burnup of heavy atoms, and the wrapper tube had a peak exposure of about 8×10^{22} nvt.

To obtain the measured displacements (shaded area) in Fig. 20, positive transparencies of two photographs [45] of opposite sides of the assembly were appraised with an EPO LP-2 Profile Projector. Displacements were measured at each side of the assembly in each transparency, producing a total of four displacement measurements at each elevation. Corrections were necessary because the planes of an included ruler (and reference straight edges), assembly, and photographic plate evidently were not parallel. This skewed condition affected the local focus of the transparencies, and both factors contributed to the wide scatter band of the measured displacements. Currently, deformations of EBR-II components are being measured directly and more accurately (see below).

The other curves in Fig. 20 were calculated with various modifications of the GROW code [32], which essentially integrates the equation

$$\frac{d^2x}{dz^2} = \frac{1}{R} = \frac{\Delta(\Delta L/L)}{D}$$

RELATIVE TRANSVERSE DISPLACEMENT, mils

RELATIVE TRANSVERSE DISPLACEMENT VERSUS
AXIAL DISTANCE FROM TOP OF WRAPPER TUBE FOR EBR-II

FIG. 20

AXIAL DISTANCE FROM TOP OF WRAPPER TUBE

RELATIVE TRANSVERSE DISPLACEMENT VERSUS AXIAL DISTANCE FROM TOP OF WRAPPER TUBE FOR EBR-II ASSEMBLY

FIG. 20

where x is transverse displacement, z is axial position, R is radius of curvature, $\Delta(\Delta L/L)$ is difference in "strain" from one side of the wrapper tube to the other (a linear transverse strain distribution is assumed), and D is the distance across the wrapper tube. The curve labeled "Harkness-Li" is based on $\Delta L/L$ values due to steel swelling alone, as found from an empirical fit of results from the SCIM code [46]:

$$\% \Delta V/V = 2.054 (10)^{-20} (\phi t)^{0.955} [-1 + 6 (10)^{-3} t - 1.15 (10)^{-5} t^2 + 7.16 (10)^{-9} t^3]$$

where ϕt is total EBR-II fluence and t is temperature in $^{\circ}\text{C}$. The curve labeled "WADCO" is based on a swelling correlation in Ref. [47]. Data from both sources are intended to apply to solution-treated Type 304 stainless steel. The XA08 wrapper tube was predominately of that material, although the corners had about 10% cold work.

The curve labeled "Density data" is based on density measurements gathered from four sources [48-51]. The data appear to be mutually consistent, and indicate that the density varies about linearly from one side of the wrapper tube to the other. Maximum $\% \Delta V/V$ on the side of the wrapper tube nearest the core vertical centerline was found to be 3.65% and on the side farthest from the centerline, 2.55%. The axial position of these maximum values was about 1.5 in. above the core horizontal center-plane.

Tentative conclusions that can be drawn from Fig. 20 are: First, the WADCO swelling correlation appears to give better differential-swelling predictions than the SCIM-code fit, for the conditions under which the XA08 assembly was irradiated. Second, creep must have taken place to

keep the wrapper tube straighter than the density data would indicate.

This occurrence of creep is not surprising, for the calculated displacements of Fig. 20 produce unrestrained assembly upper-end deflections in the range of 0.3-0.6 in., and the initial upper-end clearance between XA08 and the relevant adjacent (never-swollen) assembly was only about 0.02 in., according to calculations that considered thermal deflections and elastic interactions of the assemblies [52]. Because no public code exists that accounts for both creep and interactions of assemblies, the BOW-V [53] and CRASIB [54, 55] codes are being combined.

Directly measured permanent displacements of safety- and control-rod thimbles 3A1, 5A3, and 5D1 were no larger than the XA08 wrapper-tube displacements, even though the thimbles had peak fluences 1.7-2.1 times greater [56].* Calculated maximum displacements, using the WADCO correlation, are as large as 0.8 in. This must mean either (a) that swelling saturates, or (b) that even the relatively "loose" EBR-II core provides enough restraint, and thereby enough creep occurs, to keep the thimbles straight. In view of the large amounts of creep described elsewhere in this section, the latter alternative currently seems more likely. Density measurements yet to be made on the thimbles should resolve this uncertainty.

With regard to moat formation near wrapper-tube spacer pads, the distances across the stamped spacer pads of the XA08-assembly wrapper tube remained essentially unchanged because of pad loads, even though nearby the tube was swelling about 20 mils across flats [30, 49]. Figure 21 compares measured permanent displacements near a pad, with displacements

* The transverse thimble-tube dimensions are identical to those of the wrapper tubes.

PERMANENT DISPLACEMENT VERSUS RADIAL DISTANCE FROM SPACER-PAD CENTER FOR EBR-II ASSEMBLY XA08 WRAPPER TUBE

FIG. 21

RADIAL DISTANCE FROM PAD CENTER,

PERMANENT DISPLACEMENT VERSUS RADIAL DISTANCE FROM SPACER-PAD CENTER FOR EBR-II ASSEMBLY XA08 WRAPPER TUBE

FIG. 21

2

calculated by the AXICRP code [57] using a centrally loaded, circular, flat-plate (disk) representation and a WADCO creep correlation for solution-treated stainless steel [58]. The conditions for Curve A are (a) the disk edge is prevented from rotating (by application of a suitable pressure distribution) but is free to move radially; (b) a 25-lb pad load is maintained, as suggested by BOW-II calculations [52]; (c) 4600 psi are added to the edge pressures; and (d) the elapsed time is 9580 hr. (Effective full-load exposure time of the pads was about 10,300 hr.) The added pressures were intended to simulate compressive stresses produced by swelling. These stresses could have been as high as 200,000 psi at the end of exposure, if the wrapper-tube corners had been fixed and no creep occurred, but of course neither condition was maintained. In addition, the compressive stresses exist circumferentially only, in a wrapper tube free to bow, so that the edge pressures on the simple disk representation should properly simulate average rather than maximum compressive stresses.

Conditions for the other four calculated curves are similar to those for Curve A, with the following exceptions:

Curve Letter	Pad Load, lb	Added Edge Pressures, psi	Elapsed Time, hr
B	50	4200	8460
C	50	9000	8480
D	50	4200	10700
E	60	9000	10640

None of the calculated curves reproduces the localized moat formation that occurred. The 50 and 60 lb pad-load curves do show total displacements as large as measured, but according to recent BOW-V calculations the pad loads should be less than 25 lb, rather than more [53]. No additional calculations of this type are planned until either (a) AXICRP has been modified to account for local swelling effects that have been observed [49] (and that might influence moat formation), or (b) additional evidence accumulates that indicates the creep correlation employed generates low values.

With regard to fuel-element-cladding bow, almost all fuel elements discharged from experimental assembly X040A had permanent concave curvature (with respect to the assembly axial centerline) [51]. The elements had reached about 5.8 percent maximum burnup of heavy atoms and 4.3×10^{22} nvt maximum total fluence. Their curvature was probably due to creep resulting from transverse differential thermal stresses. The explanation for these thermal stresses is as follows: Between the peripheral elements and the wrapper tube, the coolant subchannels have larger equivalent diameters and flow areas than the subchannels inside the element bundle. Therefore, the exterior sides of the peripheral elements are exposed to higher unit coolant flowrates than other element sides in the bundle. Consequently, the coolant

temperatures in the peripheral subchannels are lower than in interior subchannels, and temperature gradients, across the elements, result that are believed to increase as the periphery of the bundle is approached. Restraint of the elements by the wrapper tube and element spacer wires produces axial thermal bending stresses in the element claddings.

Figure 22 compares measured permanent transverse displacements, for several of the X040A elements, with displacements calculated by using the CRASIB code [54] in conjunction with the WADCO swelling correlation for solution-treated austenitic stainless steel [47] and the LIFE-I (NOH = 0 option) creep correlation [59]. (The element claddings are of 0.290 in. OD $\times$ 0.020 in. wall, "annealed" (Rockwell B 85), Type 304L stainless steel.) The elements were assumed to be restrained transversely at 29 different axial positions; that is, an exaggerated amount of transverse restraint was imposed. One check calculation was made in which the element was restrained at only 11 axial positions. This reduced restraint, which is probably less than that actually imposed by the 6-in.-pitch helical spacer wires on the fuel elements, decreased calculated permanent displacements by only 0.1 percent.

The "reference" temperatures for the X040A assembly indicate that the gradients across the peripheral elements are substantially greater than for the elements of the second row in from the periphery (450°F/in. vs. 50°F/in. , at core outlet). As a result, the calculated displacements based on the reference temperatures are substantially greater for the peripheral elements than for the "second-row-in" elements (Fig. 22). However, the measured displacements for the peripheral elements are only modestly greater than for the second-row-in elements. Accordingly, it

PERMANENT TRANSVERSE DISPLACEMENT vs DISTANCE FROM UPSTREAM END OF ELEMENT FOR FUEL ELEMENTS FROM EBR-II ASSEMBLY X040A

FIG 22

was adjudged that the coolant "mixing coefficient" used for calculating the reference temperatures might have been too small. To demonstrate this possibility, the reference temperatures were modified rather arbitrarily so that the gradients across the peripheral elements were closer to those across the second-row-in elements (135°F/in. vs. 70°F/in. , at core outlet). Calculated displacements based on the modified temperatures can be seen to be in reasonable agreement with the measured displacements, although interesting discrepancies still remain.

Similar calculations were also performed using the WADCO creep correlation [58]. This correlation, which includes irradiation-induced creep only, gave maximum displacements 7 to 13 percent less (and 0-5 in. farther downstream) than the LIFE-I correlation, which includes both thermal- and irradiation-induced creep.

Fuel elements discharged from a number of other experimental assemblies have been found to be deformed in the same manner as those from assembly X040A [56].

VI. EFFECT ON FAST-REACTOR ECONOMICS

The impact of metal swelling on energy costs is difficult to assess at this time but it is reasonably clear that the phenomenon will tend to raise those costs somewhat. The design strategies described in

Sect. IV all contain the potential for increases in capital cost, fuel cycle cost, or both, though not by so much as to undermine the role of the fast breeder reactor as a major future energy source for the world.

Potential design solutions have been evaluated more in terms of fuel-cycle costs than capital costs [7]. In most solutions, including the clamped-core concept, decreases in breeding gain and specific power are virtually inevitable relative to "pre-swelling" estimates. These decreases follow directly from the decrease in fuel volume fraction caused when core space must be assigned to accommodate metal swelling. The sensitivity of fuel cycle costs to breeding ratio and specific power is shown in Fig. 23. Included there is a thumb-shaped zone that covers the designs proposed for large oxide cores in the past seven years. The 1000 MWe Follow-On designs are plotted specifically. None of these studies allowed for metal swelling. The leading portion of this "thumb", i.e., high specific power and high breeding ratio, is achieved through sophisticated design features such as thin non-free-standing wrapper tubes, negligible gap between assemblies, and other promoters of high fuel fraction. It now appears that this region is tenuous because of the imperatives of accommodating metal swelling. Pending further development work, the shaded zone of the "thumb" must be viewed as combinations of breeding gain and specific power possibly unattainable in a design that allows for the amount of swelling now being projected (Fig. 5). Part of this loss is recoverable through an increase in fuel smeared density, the feasibility of which ironically capitalizes on cladding swelling to afford more space for fuel swelling (See Sect. IIA.)

EFFECTS OF SPECIFIC POWER AND BREEDING RATIO ON FUEL CYCLE COSTS, AND POTENTIAL IMPACT OF SWELLING

Fig. 16

Fig. 23

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