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STUDIES ON LMFBR SUBASSEMBLY BOUNDARY INTEGRITY

by

R. M. Crawford, W. W. Marr,
A. Padilla, Jr., and P. Y. Wang

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ARGONNE NATIONAL LABORATORY, ARGONNE, ILLINOIS

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May 1975

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PREFACE

This is the second in a series of reports on the problem of fuel-failure propagation. The principal objective of these reports is to review and analyze the potential for fuel-failure propagation in an LMFBFR. This is being done to demonstrate LMFBFR safety, even though there has been no evidence of fuel-failure propagation in currently operating reactors. The reports do not comment on current or planned experiments or analytical programs, but review and summarize the results of such programs that have been completed to date.

In order to achieve this objective, a coherent argument is constructed using existing documented results whenever possible and providing results of additional, new analyses where there are gaps or serious discrepancies in the current arguments.

The subject of fuel-failure propagation in LMFBFRs can be conveniently divided into three related areas:

- (1) pin-to-pin failure propagation;
- (2) subassembly-boundary integrity;
- (3) subassembly-to-subassembly failure propagation.

The first area listed above has been discussed in a previous report.¹ This report concentrates only on the second area, and presents a discussion of the initiating events and failure modes which may potentially lead to a breach of the subassembly-boundary integrity. A breach of the subassembly-boundary integrity in this report is defined as a condition in which the subassembly duct is ruptured or severely deformed.

It is concluded that a breach of the subassembly-boundary integrity is not expected to occur for the expected accident sources. However, a breach of the subassembly-boundary integrity might occur due to possible manufacturing defects of the duct or for some undefined failure mechanism. Even in these hypothetical cases, however, it is shown that a breach of the subassembly-boundary integrity does not lead to rapid fuel-pin failure propagation within the accident subassembly. This conclusion is supported by operating experience, experiments, and mechanistic analyses of failure-propagation phenomena presented in this report.

The third area of failure propagation will be discussed in another report.²

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ABSTRACT

In this report, initial faults and their associated failure-propagation modes are identified with the use of safety-assurance diagrams. To put these initial faults into perspective, reactor operating experience is reviewed with particular emphasis on the fuel-melting incident in the Fermi reactor. The ability of the subassembly duct to withstand a pressure-pulse loading is reviewed, including recent analyses of local MFCIs and subassembly-duct mechanical response. In addition, the thermal and mechanical response of the subassembly duct to contact with molten fuel is reviewed and extended; the thermal response (meltthrough) is discussed in the light of more recent analyses, and the mechanical-response behavior (thermal-stress analysis) is extended into the plastic regime. The consequences of a localized failure of the subassembly duct is also analyzed.

I. INTRODUCTION AND PROBLEM DEFINITION

The purposes of this report are to review and summarize previous work, and to present the results of additional analysis of potential mechanisms for violating the integrity of the subassembly boundary. Even though a loss of subassembly-boundary integrity is not expected to occur for plausible initiating events, the potential consequences of such an incident has been investigated. In a previous report on local initiating events,¹ various initial faults were reviewed and analyzed. On the basis of current analyses and experiments, it was concluded that the potential propagation modes for fuel-pin failure resulting from postulated local initial faults were unlikely to occur and were inherently self-limiting. This report discusses the possible damage to the subassembly boundary. The duct integrity is shown not to be threatened by the expected range of initiating faults. However, several hypothetical accidents are postulated in order to determine the capability of the subassembly duct to withstand severe mechanical and thermal loads; in addition, the consequences of a localized failure of the subassembly duct are determined.

This report extends the earlier work of Judd and Bohl³ to include the results of recent analyses and experiments. A considerable amount of analysis has been performed previously on the response of the subassembly duct to pressure pulses and to contact with molten fuel. These analyses established conservative limits on the ability of the subassembly duct to withstand mechanical and thermal loads. In assessing various potential

accident situations, Judd and Bohl concluded, based on pessimistic assumptions, that failure of the subassembly duct by pressure pulses due to a molten-fuel-coolant interaction (MFCI) or by melting due to contact with molten fuel could not be ruled out. These results and conclusions are reviewed and extended in this report.

In this report, initial faults and their associated failure-propagation modes are identified with the use of safety-assurance diagrams. To put these initial faults into perspective, reactor operating experience is reviewed with particular emphasis on the fuel-melting incident in the Fermi reactor. The ability of the subassembly duct to withstand a pressure-pulse loading is reviewed, including recent analyses of local MFCIs and mechanical responses of subassembly ducts. In addition, the thermal and mechanical responses of the subassembly duct to contact with molten fuel is reviewed and extended; the thermal response (meltthrough) is discussed in the light of more recent analyses, and the mechanical-response behavior (thermal stress analysis) is extended into the plastic regime.

Finally, a localized failure of the subassembly duct is postulated in order to analyze the thermal-hydraulic consequences of such a failure.

II. INITIAL FAULTS AND THEIR CONSEQUENCES

In a previous report on local initiating events,¹ various initial faults were identified by means of fault-tree and safety-assurance diagrams, and the potential consequences were assessed. These diagrams will not be discussed again in detail here, but the general conclusions of the assessment will be reviewed.

The initial faults were represented by three categories of accident source: (1) random fuel-pin failure, (2) fuel-pin operation at overpower, and (3) debris in the primary coolant system. Fuel-pin damage and failure-propagation modes associated with these initial faults were analyzed: (1) fission-gas release, (2) local MFCIs, and (3) local blockages in the fuel-pin bundle. Based on these analyses, as well as experimental results and reactor operating experience, it was concluded that fuel pin failure propagation is unlikely to occur and, if occurring, is inherently self-limiting. It is then not expected that severe mechanical or thermal loading of the duct will occur due to these local initiating events. Duct integrity is not threatened under these loads.

In order to determine the containment capability of the duct, the consequences of more severe hypothetical events, in which the integrity of the subassembly boundary may be violated, were analyzed. For example, a local MFCI or fuel debris adjacent to the subassembly wall might cause failure of the subassembly duct in a very localized region. The conditions under which localized failure of the subassembly duct can occur were analyzed, as well as the consequences of the localized failure. The results are presented in the following sections.

The failure-propagation modes associated with these postulated accident sources are presented in Figs. 1-3. Figure 1 shows the accident

Fig. 1. Accident Progression Diagram AP2: Fission-gas Release - Mechanical Damage.
(From Ref. 4).

Fig. 2. Accident Progression Diagram AP7: Release of a Small Amount of Molten Fuel.
(From Ref. 4).

Fig. 3. Accident Progression Diagram AP8: Expulsion of Subassembly Coolant. (From Ref. 4).

progression diagram AP2⁴ for the mechanical damage from a fission-gas release; the applicable line of defense (LOD 6) requires that there be no significant damage to the duct walls from the pressure pulses caused by the gas release. Figure 2 shows the accident-progression diagram AP7⁴ for the release of a small amount of molten fuel, and LOD 4 requires that the pressures generated by a MFCI do not cause damage to the duct. These two diagrams present the two major sources of mechanical loading for the subassembly duct.

The major source of thermal loading of the subassembly duct arises from the sequence of events following expulsion of subassembly coolant, as shown on the accident-progression diagram AP8⁴ in Fig. 3. LOD 5 requires that the temperatures and pressures associated with coolant chugging do not result in damage to the subassembly duct. LOD 7 requires that meltthrough of the duct be prevented by adequate heat-removal capability in the adjacent subassembly or by restoration of flow in the accident subassembly. More local sources of thermal loading, such as a blockage by fuel debris adjacent to the subassembly duct wall, were also considered.

In conclusion, hypothetical accident sources which result in severe mechanical and thermal loading of the subassembly duct have been identified. Although these accident sources are not expected to occur, they are postulated in order to determine the ability of the duct to withstand severe mechanical and thermal loads.

III. REACTOR OPERATING EXPERIENCE - THE FERMI INCIDENT

The fuel-melting incident in the Fermi reactor⁵ is pertinent to this discussion because this is the only reported incident in which failure of the subassembly duct by contact with molten fuel did occur. The Enrico Fermi reactor is a sodium-cooled fast breeder reactor designed for operation at a maximum power of 200 MW(t). The fuel subassemblies consisted of 144 pins in a square array within a square subassembly duct. The fuel pins consisted of enriched uranium-10 w/o molybdenum fuel metallurgically bonded to zirconium clad. The following gives a brief description of the incident and the results of thermal-hydraulic analysis of the incident. For more detailed information, the reader is referred to Appendix A.

On October 5, 1966, the reactor was being brought up to a planned maximum power of 67 MW(t) with two of its three coolant loops in operation. At a power level of 31 MW(t), it was noted that the control rods were excessively withdrawn compared with previous operation, and that the thermocouples over two subassemblies were indicating abnormally high temperatures of 715 and 695°F at the coolant outlet, when the inlet and bulk outlet sodium temperatures were 535 and 600°F, respectively. Alarms were set off by radiation detectors in the containment building and the fission-product-detector building. The power was manually reduced to 26 MW(t) and then the reactor was scrammed.

Subsequent examinations revealed that fuel melting had occurred in two subassemblies, and holes were found in the adjacent faces of the ducts of these two subassemblies. One subassembly had another hole in its duct bordering on a third subassembly whose duct was not penetrated.

Nothing was found in any of the subassemblies that could have caused the fuel melting, but two of the six zirconium segments originally installed on a conical flow guide were found loose in the reactor inlet plenum. It was concluded that these zirconium segments were the only possible source which could have caused the fuel-melting incident.

A thermal-hydraulic analysis⁶ indicated that a blockage at the subassembly inlet by a zirconium segment would reduce the flow to approximately 0 to 3% of nominal in the two subassemblies in which fuel melting had occurred. The third subassembly mentioned above had its flow reduced to somewhat less than 10% nominal.

As the reactor power level was increased, it is postulated that a sodium-vapor bubble filled the central region of the two adjacent blocked subassemblies where the temperatures were above the boiling point of sodium. Within this region, the fuel melted and flowed to the sides and bottom of the bubble, then froze when coming into contact with liquid sodium. Eventually, enough fuel slumped to form plugs which penetrated the subassembly ducts of the two blocked subassemblies.

Although the inlet flow may have been blocked by the zirconium segment and the fuel plugs, sodium could have entered the two blocked subassemblies through the holes in their ducts and provided appreciable convective cooling by two-phase sodium flow through the vapor-bubble region. The sodium between the subassemblies is nominally stagnant, but some leakage flow into this region exists in the inlet nozzles. The absence of even more melting of the ducts of the blocked subassemblies than actually occurred is attributed to cooling from thermally induced natural circulation of the sodium in the gap between the blocked subassemblies.

It is significant that in the Fermi incident failure propagation to adjacent normal subassemblies or even to the adjacent partially-blocked subassembly did not occur. Also, the role of natural circulation of the sodium in the gap between the blocked subassemblies in mitigating the amount of duct melting was concluded to be a very important effect. However, the conclusions of the Fermi fuel-melting incident cannot be applied directly to postulated LMFBR accident situations because the Fermi fuel-pin and subassembly design and materials are quite different from those of current LMFBR designs. Table I summarizes some of these similarities and differences between Fermi and LMFBRs (the FFTF design⁷ is used as an example of current LMFBR design).

From Table I, it is obvious that although the Fermi reactor is of the sodium-cooled fast-breeder type, there are substantial differences with current LMFBR design. With respect to subassembly failure and its propagation, one of the most important differences is the sequence of events in fuel-melting process and the behavior of the molten fuel itself. For LMFBR fuel pins, the melting point of the mixed-oxide fuel is much higher than that of the stainless steel cladding. When the fuel pins are blanketed by sodium vapor, the cladding would be expected to melt and slump downward much before fuel melting begins.

For Fermi fuel pins, the melting point of the metal-alloy fuel (2100°F) is much lower than that of the zirconium cladding (3340°F). In

Table I. Comparison of Fuel Designs

<u>Fuel-pin Design</u>	<u>Fermi</u>	<u>LMFBR</u>
Pin OD, in.	0.158	0.23
Fuel length, in.	30.5	36
Cladding material	zirconium	Type 316 SS
Cladding thickness, in.	0.005	0.015
Fuel material	U-10 w/o Mo	PuO ₂ -UO ₂
Initial fuel-cladding gap, in.	0	0.003
Type of spacers	metallurgically bonded grids	spiral wire wraps
Pin-to-pin spacing, in.	0.041 (square pitch)	0.056 (triangular pitch)
<u>Subassembly Design</u>	<u>Fermi</u>	<u>LMFBR</u>
Number of pins	140	217
Duct configuration	square	hexagonal
Flat-to-flat OD, in.	2.646	4.575
Duct material	Type 347 SS	Type 316 SS
Duct-wall thickness, in.	0.096	0.12
Gap between subassemblies, in.	0.047	0.14
Coolant bulk inlet/outlet temperatures, °F	545/670 [100 MW(t) operation]	600/900 (initial core) 800/1100 (rated core)

the Fermi fuel-melting incident, it was concluded that cladding failure occurred when the fuel temperatures were between 2100 and 2600°F. The dissolution rate of zirconium by molten uranium increases rapidly with temperature, and the cladding would be expected to be completely dissolved by the fuel in about 40 sec at 2300°F or 4 sec at 2550°F. Postincident examination of the blocked subassemblies showed complete fuel-pin melting in the region of the sodium-vapor bubble with little evidence of intact zirconium cladding.

The behavior of the molten fuel in contact with the stainless steel duct wall is quite different for the Fermi metal-alloy fuel versus the LMFBR mixed-oxide fuel. Previous analyses^{8,9} of molten LMFBR mixed-oxide fuel suddenly contacting the duct wall indicated that the duct wall of the accident subassembly would not melt through until 3 to 10 sec, even though there is continual contact between fuel and the steel wall cooled by the intersubassembly sodium. In fact, solid mixed-oxide fuel may tend to form a protective layer of low thermal conductivity next to the duct wall.

Since the melting point of the Fermi metal-alloy fuel is below that of the stainless steel duct, melting of the duct will not occur until the fuel is substantially above its melting point. However, uranium forms a low-melting eutectic with iron at 1337°F. The penetration rate is relatively slow at temperatures between 1337 and 1960°F (<0.00002 in./sec), but is much more rapid (0.0026 to 0.015 in./sec) at temperatures above 1960°F. Thus, the molten metal-alloy fuel could have penetrated the stainless steel duct wall in about 13 sec, even if it was below the melting point of the duct wall.

The amount of molten fuel which can transfer heat to the duct wall may be much different for the Fermi metal-alloy fuel than for LMFBR mixed-oxide fuel. Previous analyses^{8,9} for LMFBR mixed-oxide fuel have used the concept of a maximum nonboiling fuel layer next to the duct wall. The thermal conductivity of the metal-alloy fuel is larger by about an order of magnitude than that of the mixed-oxide fuel, and therefore much more metal-alloy fuel can be in contact with the duct wall if the same criterion of a maximum nonboiling thickness is assumed. This would mean higher heat fluxes might be transferred to the duct wall by the metal-alloy fuel.

It would be difficult to show that LMFBR mixed-oxide fuel is inherently safer than the Fermi metal-alloy fuel. However, one fact might make the possibility of loss of subassembly-boundary integrity and subassembly-to-subassembly failure propagation more likely in the case of metal-alloy fuel: eventual penetration of the stainless steel duct wall can occur at temperatures as low as 1337°F, the lower limit for forming the U-Fe eutectic.

The Fermi incident demonstrated the need to design against large subassembly-inlet blockages, and all subsequent reactor designs have carefully evaluated the potential for a Fermi-type blockage. For example, in the case of FTR, the design of the inlet plenum and of the subassembly-inlet nozzles minimizes the possibility of a complete or nearly complete blockage of flow to the subassembly. Therefore, a complete subassembly inlet blockage is not considered in this report.

IV. ANALYSIS OF POTENTIAL MODES OF FAILURE PROPAGATION

From the previous discussion of the initiating faults, two major areas of concern in determining the integrity of the subassembly boundary are the response of the duct to mechanical and thermal loads.

Much analysis has already been done in order to predict the mechanical response of the subassembly duct to pressure pulses and the thermal response when the subassembly duct is contacted by molten fuel. Some of these previous studies are reviewed and summarized in this section, and the results of more recent studies are presented. The thermal stress analysis of a subassembly duct subjected to severe temperature gradients is chosen for further analysis, since all previous studies ignored the important aspect of asymmetric elastic-plastic behavior of the duct. A preliminary model is developed to predict whether the duct would be expected to fail from thermal stresses before it begins to melt.

Although expected mechanical and thermal loads would not cause the subassembly duct to fail, certain hypothetical dynamic loads are postulated which might cause a localized rupture. The effect of such a localized rupture on the coolant flow and temperature distribution in the accident subassembly is parametrically investigated.

A. Mechanical Loading of Subassembly Duct

The response of the subassembly duct to an internal pressure pulse has been the subject of many analytical^{3,8,10,11,17} and some experimental investigations.^{12,13} As noted in Sect. II on initiating faults, internal pressure pulses may arise from the sudden release of fission gases following fuel-pin failure, from local thermal interactions of the coolant with molten materials, or from sodium hammer on reentry of a sodium slug following sodium voiding and expulsion.

Although some LMFBR fuel pins are designed to accommodate pressures greater than 1000 psia in their gas plenums, the maximum pressure for initial operation of the FFTF reactor is expected to be about 550 psia. The pressure pulse due to a local MFCI has been discussed.¹ For realistic estimates of local MFCIs, the pressures generated may be of the order of 10 atm.¹⁴ In order to provide a conservative estimate of the maximum dynamic pressures associated with a local MFCI, a hypothetical case was selected for testing the integrity of the subassembly duct. This hypothetical pressure pulse had an amplitude of 80 atm with a rise time of 1 msec and a total pulse width of 4 msec. The energy associated with the pressure pulse was slightly less than 100 J (~ 68 in.-lb_f/in. for an interaction zone length of 13 in.).

Several previous analytical studies have resulted in an energy criterion for subassembly-duct failure based upon a static simulation of the pressure pulse loading. The pressure pulse was assumed to have the characteristics of a shockwave (high pressure and brief duration) which impacted the subassembly duct and resulted in an impulsive load. The energy imparted to the subassembly duct was absorbed by the duct as it deformed, and it was assumed that the deformation, energy absorbed, stresses, and strains caused by the shock pulse loading could be predicted by an equivalent static pressure.

Using the above approach, Cole (McWethy *et al.*)¹⁰ concluded that an energy density of 18.8 in.-lb_f/in.² (300 in.-lb_f/in. for an FFTF duct) would offer little threat to the integrity of a duct at 1000°F in early life, although the calculated strain was 4.8%. Near the end of life, with a higher yield strength and lower ductility, he calculated a strain of 2% and concluded that the duct would still have a good chance of survival.

The approach adopted by Coffield and Wattlelet⁸ was similar to that of Cole. They used the FEATS finite-element computer code to estimate how much energy the duct could absorb before reaching its rupture strain and concluded that an FFTF duct at 1000°F could absorb 212 in.-lb_f/in. before reaching its assumed rupture strain of about 2.5%.

Judd³ stated that the use of the energy available in the pressure pulse to determine duct failure was misleading and probably resulted in

pulse energies which are alarmingly low. Instead of an energy criterion, he stated that the response of the subassembly duct should be determined by the pressure-time history of the pulse. For "slow" pulses, which he estimated to be those with rise times greater than about 1 msec, he concluded that the loading could be considered as quasi-static and that the effect could be determined by a static analysis using the peak pressure alone. He then calculated that an irradiated FFTF subassembly duct at 1000°F would fail for a "slow" pressure pulse with a peak of about 800 psi.

For more rapid pulses, Judd considered various triangular pulses and found that the peak pressure required for failure increases as the pulse width decreases. For example, a peak pressure of 1390 psi was required for failure when the pulse width was 0.5 msec and the required peak pressure for failure increased to 2190 psi for a pulse width of 0.1 msec.

McCall¹¹ recently analyzed the response of an irradiated FFTF subassembly duct using the ANSYS finite-element computer code. He assumed that the pressure pulse was "slow" enough to justify use of a uniform static pressure loading and determined that a duct at 1000°F may fail for a pressure pulse with a peak of 555 psi. When the additional strength of the surrounding subassemblies was taken into account, where tight coupling of the ducts by the interassembly gap sodium was assumed, he found that the peak pressure required for duct rupture was increased to 700 psi.

More recent, Kennedy,¹⁵ using the STRAW finite-element computer code, identified the need for dynamic, rather than static, analyses. Using a triangular pressure pulse with varying rise times, he found that rise times of 1 msec and larger yielded results which were nearly static, and that rise times of 0.5 msec and smaller yielded results which were definitely dynamic for a freely standing isolated hexagonal duct.¹⁶ When he accounted for the compressible sodium between the surrounding subassemblies, he found that even pressure pulses of 1-msec rise times would have to be treated dynamically. For a triangular pressure pulse with a rise time of 1 msec and a total width of 4 msec, Kennedy predicted that a pressure of 750 psi was required for failure of a freely standing irradiated FFTF duct at 1000°F.¹⁷ When the compressible sodium in the interassembly gaps and in the adjacent subassemblies was taken into account, a peak pressure of 1200 psi did not result in duct failure, but the calculated strain was 1.00%.¹⁷ It is expected that the ability of the subassembly duct to withstand pressure pulses will be further increased when the resistance to crushing of the fuel-pin bundles inside the adjacent subassemblies is taken into account.

The hypothetical local MFCI selected for this study can now be put into proper perspective in view of the above analytical studies, which are summarized in Table II. When the integrated energy in the pressure pulse is taken over the assumed 9-in.-long MFCI region, the maximum energy deposited in the subassembly duct is about 91 in.-lb_f/in. This is lower by a factor of about 3 than the energy required for duct failure suggested by Cole¹⁰ and Coffield and Wattelet.⁸ If the predicted pressure-time history of the hypothetical local MFCI is considered, the results of Kennedy¹⁷ also indicate that the subassembly duct will not fail. Further improvements in his model are expected to show an increase in the margin of safety for duct integrity.

Table II. Summary of Various Analyses of Mechanical Loading on Subassembly Duct.

Reference	Assumption and Code	Pressure Loading or Energy Density	Duct Geometry d: cross-flat OD, in. h: wall thickness, in.	Material Properties: σ_y : yield stress, psi	Temp, °F	Results	Remarks
7 (GE)	1. Static loading 2. Elastic-perfectly plastic model 3. HEXD computer program	Fuel/clad-sodium thermal interaction on sodium reentry (energy density of 18.8 in.-lb/in. ²)	d = 4.615 h = 0.14	SS 304 a) Early-life $\sigma_y = 14,000$ b) End of life $\sigma_y = 34,000$	1000	a) Max deformation = 0.055 in.; strain of 4.8% b) Deflection = 0.028 in.; strain of 2%	a) Little threat to the integrity of the duct. b) Marginal, still having good chance for survival.
6 (WARD)	1. Static loading 2. Elastic-plastic model with hardening 3. FEATS finite-element computer code	a) 1000 psi b) 400 psi	d = 4.575 h = 0.12	SS 316 at fluence of 2×10^{23} n/cm ² : a) $\sigma_y = 75,000$; rupture strain = 2.5% b) $\sigma_y = 30,000$; rupture strain = 0.5%	a) 1000 b) 1500	a) Max deflection < 0.05 in. b) Max deflection < 0.05 in.	In both cases (a) and (b) no contact of the ducts and without reaching the rupture strain.
2 (ANL)	1. Static loading 2. Elastic-strain-hardening model 3. EPHEX computer program	a) 809 psi b) 287 psi	L (length of flat) = 2.552 h = 0.14	Same as Ref. 6	a) 1000 b) 1500	In both cases (a) and (b), failure strain reached.	Failure predicted for cases (a) and (b)
2 (ANL)	1. Dynamic loading 2. Rigid, perfectly-plastic model 3. DUPLHX computer program	Triangular pulse Pressure (psi) Pulse Duration (µsec) 1120 ∞ 1390 500 1690 200 2190 100 3250 50	L (length of flat) = 2.243 h = 0.14	$\sigma_y = 75,000$	1000	Failure pressure predicted	Max deformation of static case used for failure criterion
8 (WARD)	1. Static loading 2. Elastic-plastic with hardening model 3. ANSYS finite-element computer code	a) 550 psi b) 700 psi	d = 4.575 h = 0.120	20% CW, 316 SS at fluence of 2×10^{23} n/cm ² : $\sigma_y = 57,000$; rupture strain = 1.06%	1000	Possible failure predicted for both cases (a) and (b)	a) Single duct considered in the analysis b) Additional strength due to the presence of neighboring ducts
13 (ANL)	1. Dynamic loading 2. Elastic-plastic with hardening model 3. STRAW finite-element computer code	Triangular pressure pulse rise time: 1 msec total duration: 4 msec peak: 1200 psi	d = 4.575 h = 0.120	20% CW, 316 SS at fluence of 2×10^{23} n/cm ² : a) $\sigma_y = 57,000$; rupture strain = 1.06% b) $\sigma_y = 46,000$; rupture strain = 0.9%	a) 1000 b) 1200	Failure pressures predicted (a) 750 psi (b) 520 psi	Additional strength of duct due to the presence of compressible sodium

The energy associated with this hypothetical local MFCI can also be compared with the experimental data available. PFR-type subassembly ducts were subjected to internal chemical (PETN) explosions, and it was found that 0.5 lb of PETN caused little structural damage, whereas 2 lb of PETN caused substantial damage.¹² If we take an energy equivalence of 1 MW-sec (10^6 J) per 0.5 lb of PETN, the energy in the hypothetical MFCI pressure pulse is lower than the energy required to fail the PFR subassembly duct by a factor of 10^4 .

EBR-II subassembly ducts were also subjected to internal chemical (Bullseye Pistol Power) explosions by Boers.¹³ By use of 125-mg explosive charges, which were reported to have an energy release of less than 0.15 Btu (158 J), a maximum deformation of 0.02 in. was reported, but no duct failures occurred. The EBR-II duct thickness of 0.040 in. is substantially lower than the FFTF value of 0.120 in., and the energy release of 158 J is also substantially higher than the energy in the hypothetical MFCI pressure pulse, again suggesting that the hypothetical MFCI pressure pulse might not cause failure of the FFTF subassembly duct. Unfortunately, the British PFR results and the EBR-II subassembly tests may not be directly applicable for at least two reasons: (1) the magnitude and shape of the pressure pulse from a chemical explosion may be quite different from the MFCI pressure pulse even though there is an attempt to shape the pulse, and (2) ductile materials (unirradiated mild steel) were used in these experiments. However, the difference of four orders of magnitude in the energy required for duct failure is indicative of the very small amount of energy which may result from a hypothetical MFCI pressure pulse.

Since the maximum possible pressure from a sudden fission-gas release (550 psia) is lower than the pressures postulated in the foregoing analyses and experienced experimentally, it is concluded that failure of the subassembly duct due to pressure pulses is not expected. Further dynamic elastic-plastic analysis is required in order to demonstrate adequately the margin of safety.

B. Thermal Loading of the Subassembly Duct

1. Melting of the Subassembly Duct

If the subassembly duct is to melt, it must be contacted by hot fuel above the melting point of stainless steel. Since the presence of liquid sodium would prevent fuel from attaining such high temperatures, such a situation could exist only after sodium voiding has occurred and the subassembly remains voided for a few seconds. In a previous report on local initiating events,¹ it was shown that local coolant boiling due to a local blockage or a local MFCI is not expected to propagate to the entire subassembly and that restoration of the coolant flow would be expected even if only on an intermittent basis. Therefore, it is not expected that any local initiating event in the fuel subassembly will lead to whole subassembly voiding and subsequent meltdown. An initiating fault which could lead to subassembly coolant expulsion is a large subassembly blockage, but this type of occurrence is unlikely for the FFTF subassembly because the inlet plenum and subassembly inlet nozzle are specifically designed to prevent this situation, as discussed previously in Sect. III. Therefore, a complete subassembly inlet blockage is considered to be very unlikely.

Therefore, since no likely mechanism has been identified yet which results in large amounts of hot fuel sufficient to cause duct melting, the existence of molten fuel in the absence of liquid sodium is postulated solely in order to predict the response of the subassembly duct under a severe loading situation. The hypothetical situation where solid and molten fuel is deposited on the inside of the subassembly duct has been analyzed frequently. McWethy *et al.*,¹⁸ considered the case in which a 0.3-in.-thick layer of molten fuel initially at 5000°F is placed against the duct wall initially at 1000°F. The sodium gap between subassemblies was neglected, and the adjacent subassembly duct was assumed to be in contact with the accident subassembly duct. For full-power conditions, the accident subassembly duct was completely melted through in about 15 sec when the calculation was terminated, and it was apparent that substantial melting of the adjacent subassembly duct would also occur.

Coffield and Wattlelet⁸ limited the amount of fuel in contact with the duct by considering only that fuel thickness which is below the assumed fuel boiling point of 6100°F. For realistic ranges of the parameters involved, it was found that the maximum nonboiling fuel thickness at peak power conditions would be limited to 0.1 in. or less. At 5% decay power, the maximum nonboiling thickness was about 4.5 times the full-power value. The results indicated that at full power molten fuel in contact with the duct wall would result in boiling of stagnant sodium in the interassembly gap in about 4 sec. If it is assumed that sodium boiling results in vapor blanketing of the outside surface of the duct, then complete melting of the accident subassembly duct would occur in a matter of a few more seconds. In the shutdown case (5% decay power), it was found that the molten fuel would not completely melt through the accident subassembly duct if it were deposited near the inlet region.

Keaten¹⁹ considered the additional effects of solid-fuel breakaway and molten-steel drainage. Since the sodium velocity in the interassembly gap was assumed to be 2.7% of the normal subassembly coolant velocity, an equilibrium condition was reached in which solidified fuel was in contact with a partially molten duct. Due to deformation of the duct, the solidified fuel was assumed to break off and the molten steel to drain away every 2 or 3 sec, thus allowing fresh molten fuel to come into contact with the solid duct again. The duct-wall thickness was reduced approximately 20% per cycle, and duct failure was estimated to occur after about 3 cycles. The total meltthrough time was estimated to be approximately 15-20 sec.

Pedersen⁹ analyzed the effect of relatively small amounts of molten fuel (less than ~10% of the fuel inventory of a single fuel pin) on the subassembly duct. Figure 4 shows the geometry involved and some of the results obtained. If vapor blanketing occurs in the interassembly gap immediately following the onset of boiling of the stagnant sodium at 943°C (1730°F), the time of complete melting of the accident subassembly duct was 2.9 sec. This meltthrough time is lower by a factor of 2 to 3 than the values obtained by previous investigators, and it is primarily due to the assumptions of higher initial temperature distribution of the duct wall and higher heat flux (~2 times higher than that McWethy used). For diameters of the 0.1-in.-thick UO₂ disc exceeding about 1 in., the meltthrough times at the centerline were no longer affected by the disc diameter.

Fig. 4. Thermal Analysis of Local Melt-through of Subassembly Duct.
ANL Neg. No. 900-1817.

Although cracking and/or breaking away of the solidified UO₂ crust was not considered, it was found that drainage of molten steel at the UO₂-steel interface reduced the meltthrough time to 2.5 sec. When the UO₂ disc diameter was reduced to 1/2 in., the meltthrough time increased to 4.4 sec. For this latter case, increasing the sodium boiling temperature from 943 to 1010°C prevented complete meltthrough of the accident subassembly duct. However, the effect of natural convection of the sodium in the interassembly gap in preventing complete meltthrough was not investigated.

From the foregoing discussion, the major variables and/or parameters which affect the ability of the subassembly duct to withstand melting when contacted by molten fuel are: (a) the amount of molten fuel and its heat-generation rate (full-power versus shutdown condition), (b) the thermal resistances at the fuel-duct wall interface (cracking and breakaway of the solidified fuel crust and drainage of the molten steel), and (c) heat-transfer mechanisms (natural or forced convection in the interassembly gap sodium).

Results of the previous studies are put in perspective in Fig. 5, which compares the steady-state heat-removal capability of the accident and adjacent subassembly ducts with different heat sources. The parameters are for steady-state conditions only and do not account for the energy associated with fuel solidification; in addition, the interassembly gap sodium is neglected. This steady-state condition corresponds to the situation in which molten fuel builds up gradually to a certain thickness, whereas

Fig. 5. Heat-removal Capability of Accident and Adjacent Subassembly Duct.

in the previous studies a layer of molten fuel was assumed to be instantaneously brought into contact with the subassembly duct. For the steady-state full-power conditions, at least some melting of the accident subassembly duct would be expected to occur upon contact with molten fuel, but the duct may not completely melt through if the fuel is deposited in the lower part of the core, where the coolant temperature in the adjacent subassembly is lower. The much higher heat-removal capability of the adjacent subassembly duct would prevent its complete meltthrough, although some melting upon initial contact with molten fuel may occur. Under shutdown conditions, the accident subassembly duct would not be expected to melt through if it can survive the initial contact with molten fuel. The concept of a nonboiling fuel thickness implies the existence of a core of boiling liquid which condenses elsewhere, and the estimated heat fluxes due to fuel-vapor condensation²⁰ can be considerably higher than that due to the maximum

nonboiling thickness. Although these high condensation heat fluxes would be reduced by an order of magnitude by the existence of noncondensable gases, substantial duct melting may occur in the region of fuel condensation.

In conclusion, it is difficult to prevent melting of the accident subassembly duct when it is suddenly contacted by molten fuel unless significant thermal resistances develop at the fuel-duct interface and sufficient convection in the interassembly gap sodium exists. Even for amounts of molten fuel as small as 1/2 in. in diameter and 0.1 in. thick, complete meltthrough of the duct might occur unless boiling of the sodium in the interassembly gap can be prevented by sufficient convection mechanisms. Complete meltthrough of an accident subassembly duct is not expected to occur under certain less conservative assumptions, such as a fuel layer less than the maximum nonboiling thickness or a more gradual deposition of fuel on the duct. The heat-removal capability of the duct wall is large enough to remove the heat generated by the fuel deposited on the inside surface if the initial effects of molten-fuel contact can be overcome and coolant temperatures in the adjacent subassembly can be maintained below about 1200°F. Coolant temperatures in the adjacent subassembly have been investigated in detail by Marr.²

2. Thermal Stress Analysis of Subassembly Duct

When the accident subassembly duct is subjected to a severe temperature gradient, it may fail due to thermal stresses even if melting of the duct does not occur. This case is applicable to situations in which relatively small quantities of molten fuel are deposited on the duct or when local blockages in the region of the duct wall might lead to local boiling or to release of small amounts of fuel debris which lodge against the duct wall.

There have been few analytical studies of thermal stresses in the subassembly duct. Coffield and Wattleet⁸ analyzed the adjacent subassembly duct using the FEATS finite-element computer code when the outside surface was at 2200°F and the inside surface at 1200°F, which was considered to be conservative. It was found that the thermal stresses exceeded the yield strength; therefore cracking of the duct would occur if the rupture strain of 0.5% was assumed. The same conclusion would probably apply if the temperature gradient was from the inside surface to the outside surface.

Kennedy²¹ analyzed a very local hot spot (a square, 0.25 by 0.25 in.) on the subassembly duct using the SLADE finite-element computer code. Figure 6 shows the geometry considered and some of the results obtained. Assuming that the duct material had an extremely low ductility ($\sim 0.2\%$) and that failure occurs when the stresses exceed the yield strength, he found that local failure throughout the entire duct wall thickness would occur when the inside surface reached 1750°F (about the boiling temperature of sodium). The temperature gradient through the thickness of the duct wall was only 275°F, but the temperature gradient in the transverse direction was 650°F on the inside surface and 375°F on the outside surface.

The above studies assumed either all six flats of the duct were subjected to a high temperature gradient or failure occurred when the yield stress was reached. Both assumptions appear to be overly conservative.

To extend the previous work on thermal stresses, a preliminary elastic-plastic model was developed to predict the behavior of the accident subassembly duct subjected to asymmetric thermal loadings.

a. Temperature Gradient

The model consisted of a hexagonal subassembly duct, originally at constant temperature, which is subjected to a uniformly distributed high temperature on the inner surface of one flat and a linear temperature gradient through the thickness of this flat. The outer surface of this flat is maintained at the original interassembly sodium temperature along with the other five flats of the subassembly duct. The subassembly duct was assumed to be very long, thus neglecting longitudinal variations and end effects. An elastic-perfectly plastic stress-strain relationship was assumed for the duct material.

The elastic thermal stress analysis is approximated by using a frame-structure analysis,²² and therefore the thermally induced loading is shared by all the flats of the hexagonal duct. It is assumed

Fig. 6. Thermal Stress Analysis of Local Hotspot on Subassembly Duct.
 (From Ref. 21).

that the membrane stress developed in the flat is negligible. The largest temperature gradient that the subassembly can withstand elastically is

$$\Delta T_y = \frac{60}{19} \frac{\sigma_y}{\alpha E}, \quad (1)$$

where σ_y is the yield stress of the material, α is the thermal-expansion coefficient, and E is the modulus of elasticity. Using values of σ_y , α , and E from the ASME Boiler and Pressure Vessel Code²³ (see Appendix C), the temperature gradient ΔT across the duct wall can be determined for unirradiated stainless Types 304 and 316 for outer surface temperatures up to 800°F as shown in Fig. 7. From these two curves, Type 316 appears to be slightly stronger than Type 304 in resisting thermal loadings.

Fig. 7.
Temperature-gradient Limits for Elastic Behavior of an Unirradiated Subassembly Duct Wall. ANL Neg. No. 900-2269

For temperature gradients higher than those shown in Fig. 7, the duct wall will behave inelastically and a plastic analysis model is required. If the material has sufficient ductility, say greater than 0.4%, it can be shown²⁴ that the inelastic bending moment is given by

$$M = M_{FL} \left[1 - \frac{1}{3} \left(\frac{\Delta T_y}{\Delta T} \right)^2 \right] \quad (2)$$

where M_{FL} is the plastic flow moment denoting the theoretical ultimate plastic carrying capacity of the duct wall. Figure 8 shows how the inelastic bending moment varies with the temperature gradient. It is seen that M approaches very close to M_{FL} ($M \approx 0.99 M_{FL}$) when ΔT is about five times the value of ΔT_y .

Fig. 8.
Variation of Inelastic Bending Moment with Temperature Gradient.
ANL Neg. No. 900-2303

If it is assumed that the heated flat (i.e., the flat subjected to the temperature gradient) fails when $M \approx 0.99 M_{FL}$, then Fig. 9 shows the temperature gradients which produce failure for original duct

Fig. 9.
Temperature Gradient for Plastic Behavior
of an Unirradiated Subassembly Duct Wall
at $M \approx 0.99 M_{FL}$. ANL Neg. No. 900-2294

temperatures from 100 to 800°F for unirradiated materials. In Fig. 9, the "melting line of 316 SS" corresponds to the inside surface being at the melting point and the outside surface at temperature T . Since the curves for the temperature gradient are significantly lower than the melting line, it appears likely that the duct might fail before it reaches the melting point.

For irradiated cold-worked stainless steel Type 316, the material-property data generally show higher yield stress and reduced ductility compared to the unirradiated material^{25,26,27} (see Appendix C). By use of these data, it is possible to compare the maximum temperature gradients at high temperatures for both irradiated and unirradiated ducts in the elastic and inelastic range, as shown in Fig. 10. If the irradiated

Fig. 10.
Effect of Irradiation on Temperature
Gradient for Elastic-Plastic Behavior of
Subassembly Duct Wall. ANL Neg. No. 900-
1820 Rev. 1.

duct is assumed to have an extremely small ductility, which is thought to be a very conservative assumption, then it will fail when the thermally induced moment reaches the limiting elastic moment, i.e., $M = M_y = 2/3 M_{FL}$. For this failure criterion, the irradiated duct can withstand relatively large temperature gradients only for outer surface temperatures below 1000°F. However, failure will still occur before reaching the melting point of the duct.

If the irradiated duct is assumed to have some ductility, as the data indicate, then failure will not occur until significant plastic

flow of the material is established, say $M = 0.9 M_{FL}$. For this case, the irradiated duct has a much higher resistance against thermal loading than the fresh duct, and melting of the duct may occur before its failure due to plastic flow as shown in Fig. 10; however, its strength strongly depends on temperature, total amount of neutron fluence, ductility, and prestraining. More data on the properties of irradiated duct materials are definitely needed for an accurate assessment of the behavior of the irradiated duct.

b. Combined Temperature Gradient and Internal Pressure

If it is assumed that the subassembly duct is under a moderate internal pressure as well as a temperature gradient, the moment at the center of the flat increases as the temperature gradient across the flat is increased, and eventually it exceeds the elastic limit and reaches the elastic-plastic range. Although the moment is not uniformly distributed along the flat, failure can be assumed to occur when the center moment approaches very close to the plastic flow moment.

First, consider the case in which the linear temperature gradient is uniformly distributed along all six flats of the hexagonal duct. For the elastic range, the largest temperature gradient ΔT_y that the duct can withstand can be obtained from the equation

$$\frac{pL^2}{4h^2} + \frac{E\alpha\Delta T_y}{2} = \sigma_y \quad (3)$$

Figure 11 shows ΔT_y for outer surface temperatures T up to 800°F for various values of pressure p assuming $h = 0.12$ in. and $L = 2.641$ in. (thickness of FFTF subassembly duct wall and length of one flat, respectively). Unirradiated material properties of the duct were used in the calculations.

Fig. 11.
Temperature-gradient Limits for Elastic
Behavior of a Type 316 Stainless Steel
Unirradiated Subassembly Duct Wall:
Six Flats Heated.

In the elastic-plastic range, the inelastic bending moment M can be obtained from the equation

$$M = M_{FL} \left[1 - \frac{1}{3} \left(\frac{\sigma_y}{\frac{pL^2}{4h^2} + \frac{1}{2} E\alpha\Delta T} \right)^2 \right] ; \Delta T \geq \Delta T_y . \quad (4)$$

If duct failure occurs when $M \approx 0.99 M_{FL}$, Fig. 12 shows the maximum temperature gradient ΔT for various internal pressure p and for outer sur-

Fig. 12.
Temperature Gradient for Plastic Behavior of a Type 316 Stainless Steel Unirradiated Subassembly Duct Wall at $M \approx 0.99 M_{FL}$: Six Flats Heated.

face temperature up to 800°F. As expected, the internal pressure decreases the allowable temperature gradient, and the duct wall would be expected to fail far below its melting point.

A more realistic case to be considered is when the temperature gradient is uniformly distributed along only one flat of the duct in conjunction with a moderate internal pressure. The largest temperature gradient ΔT_y that the duct can withstand elastically can be obtained from the equation

$$\frac{pL^2}{4h^2} + \frac{19}{60} E\alpha\Delta T_y = \sigma_y . \quad (5)$$

In the inelastic range, the bending moment M is approximated by the equation

$$M = M_{FL} \left[1 - \frac{1}{3} \left(\frac{\sigma_y}{\frac{pL^2}{4h^2} + \frac{19}{60} E\alpha\Delta T} \right)^2 \right] ; \Delta T \geq \Delta T_y .$$

The corresponding temperature gradients in the elastic and inelastic range are shown in Figs. 13 and 14 for various internal pressures using unirradiated material properties for the duct. Since the internal pressure decreases the ability of the duct to withstand thermal loading, it appears that a ductile subassembly will not be able to withstand the combined action of moderate internal pressures and severe temperature gradients without failure.

Fig. 13.
Temperature-gradient Limits for Elastic Behavior of a Type 316 Stainless Steel Unirradiated Subassembly Duct Wall: One Flat Heated. ANL Neg. No. 900-2304

Fig. 14.
Temperature Gradient for Plastic Behavior of a Type 316 Stainless Steel Unirradiated Subassembly Duct Wall at $M = 0.99 M_{FL}$: One Flat Heated. ANL Neg. No. 900-2298

For an irradiated duct at high temperature, Fig. 15 shows that the effect of moderate internal pressures is not significant enough to reduce the maximum temperature gradient completely below the melting line, although the crossover point occurs at lower temperatures. Therefore, in general, melting of the duct wall would be expected to occur before failure.

This preliminary assessment of the behavior of the subassembly duct wall under thermal and internal pressure loadings indicates that the irradiated duct may have significantly stronger resistance against thermal loading than the unirradiated duct. Although the present analysis provides insight into this complex problem, a more refined method (which would almost certainly involve computer implementation) would be required for additional detailed analyses.

C. Coolant Flow Diversion

The integrity of a subassembly duct to maintain normal coolant flow can be violated either by a random duct failure or due to an environmental perturbation such as a large pressure pulse, a severe temperature gradient, or direct meltthrough by molten fuel. Failure of the duct could result in diversion of normal coolant flow out of the subassembly. This might cause a significant reduction of cooling capacity within the

Fig. 15.
Temperature Gradient for Elastic-
Plastic Behavior of an Unirradiated
Cold-worked Type 316 Stainless
Steel Subassembly Duct Wall.

subassembly and might result in local overheating near the rupture site or, for a large rupture, flow starvation in the entire subassembly. Since the problem of local coolant flow diversion out of a subassembly has not been treated previously, there were no available analytical models for handling this problem.

1. Description of Model

The COBRA-III²⁸ computer code for thermal-hydraulic analysis of pin bundles was modified for this study by allowing for flow out of (or into) a coolant subchannel at any specified axial location(s). The case of flow out of the subchannel corresponds to coolant diversion out of the subassembly, whereas flow into the subchannel corresponds to injection of fluid in coolant-mixing experiments. The details of the modification of the equations for continuity, energy, and momentum are contained in Appendix B.

Various sizes of pin bundles, in some cases the typical FFTF 217-pin bundle, were analyzed. Results obtained for smaller bundles were used for extrapolation to the 217-pin bundle. This is necessary because of the excessive computer-core storage and run time required for analyzing the 217-pin bundle. All bundles were subjected to three types of ruptures at one of the corners of the hexagonal duct: a 1-in. rupture at midcore, an 18-in. rupture in the bottom half of the core region (which corresponds to the molten-fuel location in a severe overpower fuel pin), and a 36-in. rupture over the entire core region. Since the flow condition is not much different for an edge subchannel and a corner subchannel in the bundle, the

general conclusion obtained for the rupture at a corner is expected to be applicable to ruptures anywhere around the subassembly duct. The fuel-pin geometry and operating conditions correspond to those of the typical FFTF maximum-power subassembly.⁷ Analyses of the 7- and 19-pin bundles were done for the initial-core conditions. Subsequent interest in the rated core required that different operating conditions be used in the analyses of the 61- and 217-pin bundles. These conditions are listed below along with the COBRA-III parameters used:

Pin OD (in.)	0.23
Pin-to-pin triangular pitch (in.)	0.286
Heated length (in.)	36
Average fuel-pin linear power (kW/ft)	10.16 (7-, 19-pin) 11.06 (61-, 217-pin)
Axial power peaking factor (chopped cosine)	1.24
Nominal inlet mass velocity ($10^6 \text{ lb}_m/\text{hr-ft}^2$)	4.028 (7-, 19-pin) 4.432 (61-, 217-pin)
Coolant inlet temperature (°F)	600 (7-, 19-pin) 792 (61-, 217-pin)
Nominal core ΔT (°F)	290 (7-pin) 336 (19-pin) 366 (61-pin) 395 (217-pin)
Crossflow resistance coefficient, K	0.5
Geometry factor for conduction, kg	2.0
Turbulent-mixing parameters, β	0.025
Axial increment, ΔX (in.)	0.1 (1-in. rupture) 1.0 (18- and 36-in. rupture)

The boundary conditions assumed in COBRA-III are a constant pressure at the top of the fuel bundle and the inlet flow distribution at the bottom. Although pressure boundary conditions both at the top and bottom of the fuel bundle would be more appropriate mathematically, the assumed boundary conditions are adequate, since the differences are expected to be small under normal conditions. For a local disturbance within the fuel bundle, such as a blockage or flow leakage due to duct rupture, the boundary conditions in COBRA-III would still be satisfactory as long as the disturbance is far enough away from the ends of the bundle. This is true for the 1-in. rupture at midcore, where the flow disturbance is limited to a few inches above and below the rupture. There are some errors in the 18- and 36-in. rupture cases because the analysis begins at the bottom of the core region, where the rupture starts, instead of at the bottom of the fuel bundle, approximately 7 in. below the bottom of the core region. However,

the assumption of constant mass velocity at the bottom of the core region is conservative because there would have been more flow at the beginning of the rupture point if the flow diversion from the bottom of the fuel bundle had been taken into account; consequently, the coolant temperatures along the rupture would be expected to be lower than those calculated. The detailed wire-wrap model in COBRA-III was not utilized for calculations; instead an equivalent turbulent-mixing parameter β was used. A few check runs for both the 7- and 19-pin bundles showed that there was no significant difference in the predicted coolant temperature and flow distributions with or without the wire-wrap model.

The total mass flow to each bundle analyzed was assumed to remain constant with and without leakage flow due to subassembly duct rupture. In reality, the rupture might reduce the overall flow resistance of the subassembly and hence, might result in an increase in the inlet flow. In order to know how much flow would be expected to leak out of the subassembly from a duct rupture, the complicated flow paths in the region between the subassemblies must be determined. Consequently, the leakage flow was treated as a parameter in this study instead of assuming various rupture widths. The maximum amount of leakage flow which could be analyzed was limited by the inability of present computer codes to handle zero or reverse flow.

2. Analysis and Results

Figures 16 through 19 show the subchannel layouts for 7-, 19-, 61-, and 217-pin bundles, respectively. The symmetry of the problem permits analysis of only one-half of each pin bundle. Analyses of the 7- and 19-pin bundles were made for a full bundle to assess the effect of the wire-wrap model in COBRA-III. In this latter case, the leakage flow was assumed to come equally from the two subchannels surrounding the ruptured corner, e.g., subchannels 11 and 12 in the 7-pin bundle. Also notice that the subchannel divisions of the 61- and 217-pin bundles are coarser than those of the 7- and 19-pin bundles. This is necessary for the consideration of the requirement of computer-core storage and run time.

a. 1-in. Rupture at Midcore

For the 1-in. rupture at midcore, the axial flow variation in the subchannel adjacent to the rupture (i.e., subchannel 12 in the 7-pin bundle, 26 in the 19-pin bundle, and 31 in the 61-pin bundle) is shown in Fig. 20. For a leakage flowrate of 10.0 lb_m/sec-ft, the maximum decrease in subchannel flowrate is 42% in the 7-pin bundle, 27% in the 19-pin bundle, and 25.5% in the 61-pin bundle. The flow recovers somewhat and settles to a new value after it passes the 1-in. rupture. The temperature variation around the rupture is shown in Fig. 21. The temperature difference between an interior subchannel and an exterior subchannel is greater in a larger bundle. The hotter coolant in the interior subchannel is induced by the leakage to mix with the colder coolant in the exterior subchannel adjacent to the rupture. Downstream of the rupture, however, the coolant temperature increases at a faster pace in the smaller-pin bundles where the decrease in flowrate is higher. At the outlet of the heated section, temperature increase is much larger in a small pin-bundle than in a larger pin-bundle, as shown in Fig. 22.

Fig. 16.
Subchannel Layout of a Seven-pin
Bundle. ANL Neg. No. 900-3081.

Fig. 17.
Subchannel Layout of a 19-pin
Bundle.

Fig. 18.
Subchannel Layout of a
61-pin Bundle.
ANL Neg. No. 900-4047.

Fig. 19. Subchannel Layout of a 217-pin Bundle.

Fig. 20. Local Flow Variation in the Subchannel Adjacent to a 1-in. Rupture at Midcore for a Leakage Flow Rate of 10.0 lb_m/sec-ft in Various Pin Bundles.

Fig. 21. Local Temperature Variation in the Subchannel Adjacent to a 1-in. Rupture at Midcore for a Leakage Flow Rate of 10.0 lb_m/sec-ft in Various Pin Bundles. ANL Neg. No. 900-4051.

Fig. 22.
Variation of Maximum Temperature Increase with Bundle Size for a Leakage Flow Rate of 10.0 lb_m/sec-ft along 1-in. Length at Midcore.

The effect of leakage flowrate on the flow and temperature distributions in the 7-pin bundle was also investigated. The leakage flowrate out of the 1-in. rupture was assumed to be 2.0, 6.0, and 10.0 lb_m/sec-ft corresponding to 30, 89, and 148% of the normal subchannel flow. It is more meaningful to discuss the leakage flow in terms of subchannel (Subchannel 12) flowrates instead of total subassembly flowrates for such a localized situation. For the leakage flowrate of 2.0 lb_m/sec-ft, the subchannel flow drops to 92% of its normal value at the end of the 1-in. rupture and then recovers to 93% after 2 in., as shown in Fig. 23. For a

Fig. 23.
Local Flow Distribution in Subchannel 12 for Various Leakage Flow Rates along 1-in. Length at Midcore in the Seven-pin Bundle.

leakage flowrate of 10.0 lb_m/sec-ft, the subchannel flow drops to 58% of its normal value before recovering to 64% at 2 in. beyond the end of the rupture. The local temperature increase in Subchannel 12 due to these

leakage flows is shown in Fig. 24. The temperature increase at 2 in. beyond the end of the rupture is less than 30°F for the case of 148% sub-channel flow leakage.

Fig. 24.
Local Temperature Increases
in Subchannel 12 for Various
Leakage Flow Rates along
1-in. Length in the Seven-pin
Bundle.

b. 18-in. Rupture in Lower Half of Core

For a leakage flowrate of 1.2 lb_m/sec-ft over the lower half of the core region, where it is postulated that the hypothetical MFCI occurs, the maximum decrease in subchannel mass flowrate as a function of bundle size is shown in Fig. 25. The maximum decrease in subchannel mass

Fig. 25.
Variation of Maximum Decrease in Sub-
channel Flow Rate with Bundle Size for
a Leakage Flow Rate of 1.2 lb_m/sec-ft
along Lower Half of Heated Length.
ANL Neg. No. 900-1053.

flowrate in the 19-pin bundle is about 60%; in the 61-pin and 217-pin bundles it is close to 30%. The maximum temperature increase as a function of bundle size for this case is shown in Fig. 26. Again, it can be seen that the perturbation decreases as the bundle size increases.

Figure 27 shows the effect of leakage flowrate on flow distribution in Subchannel 12 in the seven-pin bundle. The leakage flowrate

Fig. 26.
Variation of Maximum Temperature Increase with Bundle Size for a Leakage Flow Rate of $1.2 \text{ lb}_m/\text{sec-ft}$ along Lower Half of Heated Length.
ANL Neg. No. 900-3435.

Fig. 27.
Flow Distribution in Subchannel 12 for Various Leakage Flow Rates along Lower Half of Heated Length in the Seven-pin Bundle.
ANL Neg. No. 900-3073.

was varied parametrically from 0.4 to $0.8 \text{ lb}_m/\text{sec-ft}$, corresponding to 106 and 212% of the normal subchannel flow (26 and 52% of the total subassembly flow). The axial flow decrease along the length of the rupture was approximately linear, and the amount of decrease was proportional to the magnitude of the leakage flow. The flow recovery at the end of the rupture was much more gradual than for the 1-in. rupture. The axial temperature distribution in Subchannel 12 is shown in Fig. 28. The coolant temperature increase is

Fig. 28.
Temperature Distribution in Subchannel 12 for Various Leakage Flow Rates along Lower Half of Heated Length in the Seven-pin Bundle.
ANL Neg. No. 900-3075.

more than proportional to the leakage flowrate increase since increasing flowrate from 0.4 to 0.8 lb_m/sec-ft increases the coolant outlet temperature from 70 to 220°F above the normal coolant outlet temperature.

Figures 29 and 30 show the across-corners flow and temperature distributions in the 217-pin bundle at midcore and at the core outlet.

Fig. 29.
Effect of Leakage along Lower Half of Heated Length on Flow Distribution across the 217-pin Bundle.
ANL Neg. No. 900-4045 Rev. 1.

Fig. 30.
Effect of Leakage along Lower Half of Heated Length on Temperature Distribution across the 217-pin Bundle.
ANL Neg. No. 900-3444.

Aside from the subchannels adjacent to the rupture, the perturbation in flow and temperature distribution in the remaining subchannels at midcore is quite uniform. At the core outlet, 18 in. above the end of the rupture, the perturbation in flow is quite uniform across the whole bundle, while the perturbation in the coolant temperatures caused by the rupture decreases slightly away from rupture location, but the general shape of both distributions is maintained. Notice that the flow and temperature distributions are not exactly symmetric about the center of the pin-bundle because of the difference in dividing up the subchannels.

c. 36-in. Rupture along Entire Core Length

For a rupture along the entire 36-in. core length, the leakage flowrate was varied parametrically from 0.2 to 0.6 lb_m/sec-ft. This corresponds to 106 to 318% of the normal subchannel flow or 26 to 78% of the total flow in the seven-pin bundle. Figure 31 shows the axial flow distribution in Subchannel 12 in the seven-pin bundle. As in the 18-in. rupture

Fig. 31.
Flow Distribution in Subchannel 12
for Various Leakage Flows along the
Entire Heated Length in the Seven-
pin Bundle.

case, the axial flow decreases almost linearly along the rupture length and the amount of decrease is approximately proportional to the magnitude of the leakage flow. The coolant temperature increase, shown in Fig. 32, also exhibits the same characteristics as the 18-in. rupture, in that the coolant outlet temperature increases faster than the variation in the leakage flow.

Fig. 32.
Temperature Distribution in Subchannel 12
for Various Leakage Flows along the
Entire Heated Length in the Seven-pin
Bundle. ANL Neg. No. 900-3076 Rev. 1.

The axial coolant flow and temperature distributions in various pin-bundles for the 36-in. rupture with a leakage flowrate of $0.6 \text{ lb}_m/\text{sec-ft}$, are shown in Figs. 33 and 34. Figures 35 and 36 show the maximum decrease in subchannel mass flowrate and maximum coolant temperature increase, respectively, as a function of bundle size. As in the other types of ruptures, the perturbation to the subassembly coolant flow and temperature distributions for the same leakage flowrate decreases as the bundle size increases.

3. Conclusions

Results of the present study of coolant flow diversion out of the subassemblies indicate that for the same mass-leakage flowrate, the perturbation to the subassembly coolant flow and temperature distribution decreases as the bundle size increases. Therefore, the results indicate that there is no problem of local overheating for a leakage flowrate up to $10.0 \text{ lb}_m/\text{sec-ft}$ for a 1-in. rupture at midcore, up to $1.2 \text{ lb}_m/\text{sec-ft}$ for an

Fig. 33.
Axial Flow Variation in the Subchannel Adjacent to a 36-in. Rupture along the Entire Heated Length for a Leakage Flow Rate of $0.6 \text{ lb}_m/\text{sec-ft}$ in Various Pin Bundles.

Fig. 34.
Axial Temperature Variation in the Subchannel Adjacent to a 36-in. Rupture along the Entire Heated Length for a Leakage Flow Rate of $0.6 \text{ lb}_m/\text{sec-ft}$ in Various Pin Bundles.
ANL Neg. No. 900-4049.

Fig. 35.
Variation of Maximum Decrease in Subchannel Flow Rate with Bundle Size for a Leakage Flow Rate of $0.6 \text{ lb}_m/\text{sec-ft}$ along the Entire Heated Length.

Fig. 36.
Variation of Maximum Temperature Increase with Bundle Size for a Leakage Flow Rate of $0.6 \text{ lb}_m/\text{sec-ft}$ along the Entire Heated Length.

18-in. rupture in the lower half of the core, and up to $0.6 \text{ lb}_m/\text{sec-ft}$ for a 36-in. rupture along the entire core length.

Although these leakage flowrates correspond to a large fraction of the total flow for a small bundle, the fraction decreases as the bundle size increases. For example, a leakage flowrate of $0.6 \text{ lb}_m/\text{sec-ft}$ for a 36-in. rupture corresponds to 33% of the normal flow to a 19-pin bundle, but would correspond to only 3% of the normal flow of a 217-pin bundle. However, the total leakage flow corresponds to about three times the normal flow in the two coolant subchannels adjacent to the rupture for both 19- and 217-pin bundles.

The results also indicate that a rupture in one of the corners of the hexagonal duct will not produce the hottest coolant temperatures in the region immediately adjacent to the rupture, but in the interior of the bundle, where the hottest coolant temperatures normally occur. This hottest region would be expected to be fairly broad, consisting of many coolant subchannels instead of a localized region.

4. Assessment of Uncertainties

The amount of leakage flow that can be analyzed is limited because of numerical instability problems arising from low or negative flows in the subchannels. To alleviate this problem, extensive modifications to the COBRA-III code would be required.

To ascertain the need for larger leakage flowrates, an analysis of expected leakage flowrates for various rupture widths should be carried out. This would require characterization of the torturous flow paths between the fuel subassemblies from the rupture location to the outlet plenum of the reactor.

For larger flowrates, the resistance of flow in the transverse direction may become large enough compared to the flow resistance in the axial direction to limit effectively the amount of flow which can get to the rupture no matter how large it is. In addition to the numerical instability problem mentioned above, the COBRA-III code may not be valid for analyzing such large transverse flows because the model assumes that the crossflows are small in comparison with the axial flow. Therefore, if the need for much larger leakage flowrates is indicated by the analysis of the flow paths from the rupture to the outlet plenum, a new model to predict the transverse pressure drop within the fuel bundle may be required.

V. SUMMARY AND CONCLUSIONS

Based on a consideration of potential initiating faults and previous reactor operating experience, the effect of various mechanical and thermal loads on the integrity of the subassembly duct was assessed.

The severity of pressure pulses from expected fission-gas releases or a local molten-fuel-coolant interaction (MFCI) are well within the capability of the subassembly duct to withstand. For a hypothetical local MFCI, improved dynamic elastic-plastic analytical models are expected to show that subassembly-duct failure is not expected.

Although subassembly-duct failure is not expected, various local ruptures in the subassembly were postulated in order to determine the perturbation in the coolant flow and temperature distribution resulting from coolant flow diversion out of the subassembly through the rupture. For the range of rupture flowrates studied, it was concluded that there will be no local overheating due to a local rupture in the fuel subassembly.

Gross fuel melting arising from a severe loss of flow to a single subassembly is not considered in this report for the FFTF reactor because the subassembly and reactor design prevents the occurrence of large sudden blockages. However, it is still possible for failure of the subassembly duct to occur if a local region of the wall is contacted by molten fuel greater than about 0.1 in. thick and about 1 in. in diameter. However, the heat-removal capability of the adjacent subassembly would prevent additional fuel or control-subassembly failure.

A model for stress analysis of the subassembly duct subjected to severe thermal gradient was developed. The results indicate that local failure of an unirradiated subassembly duct would be expected before the duct starts to melt, but that an irradiated subassembly duct might start to melt before failure due to thermal stress. The much higher resistance of the irradiated subassembly duct to thermal loading is highly dependent on the material properties of irradiated stainless steel.

In conclusion, on the basis of current analytical and experimental results, it appears that the subassembly-duct integrity will not be violated for expected mechanical or thermal loads; for hypothetical accidents which result in severe mechanical and thermal loading, failure of the subassembly duct would probably occur.

APPENDIX A

The Fuel-Melting Incident in the Enrico Fermi ReactorA. Description of the Incident⁵

On October 5, 1966, the Fermi reactor was being subjected to a controlled, slow increase in power in order to investigate anomalous thermocouple readings which had been obtained at the outlets of several subassemblies during previous operation. At a power level of 31 MW(t), it was noted (at about 3:05 PM) that the control rods appeared to be withdrawn further than normal for this power level. The reactor power increase was interrupted, and the thermocouples at the core outlet were checked. Abnormally high sodium temperatures at the outlet were indicated by the thermocouples over subassemblies M-140 and M-098, which were about 115 and 80°F higher than normal for those respective locations.

Shortly thereafter, at 3:09 PM, radiation alarms were set off by detectors in both the containment building and the fission-product-detector building. A fission-product detector was also provided to monitor the primary argon gas for the decay products xenon and krypton, because the presence of these gases would indicate fuel failure. In fact, cover-gas samples were drawn from the reactor vessel and the primary pump tanks, but the detection of fission products involves a significant delay time. The reading for the cover-gas sample exposed between 2:58 and 3:01 PM was found to be off scale, and a gain check indicated no malfunctioning of the detector. Shortly after this gain check, the fission-product-detector building was automatically isolated from the reactor building by the radiation alarm. The reactor was manually scrammed at 3:20 PM.

Seven subassemblies were removed from the reactor after the incident and subjected to destructive examination. Only two subassemblies, M-127 and M-098 (see Fig. A.1 for their locations), had suffered extensive fuel melting, and holes were found in the adjacent faces of their ducts. Two other subassemblies, M-122 and M-140, had suffered some overheating, but their fuel pins and ducts were intact. Nothing was found in any subassembly that could have caused the fuel melting, but two of the six zirconium segments originally installed on a conical flow guide were found loose in the reactor inlet plenum. A series of hydraulic tests showed that one of the zirconium segments could have simultaneously produced nearly complete blockage (~3% of normal flow) in subassemblies M-127 and M-098, and partial blockage in the two other subassemblies (~10% in M-122 and ~30% in M-140). Abnormally high sodium temperatures at the outlet were not indicated by thermocouples over subassemblies M-127 and M-122.

B. Thermal-hydraulic Analysis of the Incident⁶

A thermal-hydraulic analysis showed that reduction of the subassembly flow to approximately 3% of the normal value would lead to sodium boiling and fuel melting. Based on this analysis and all of the available evidence, the following sequence of events was suggested.

KEY:

CR Control Rod
 SR Safety Rod
 OR Oscillator Rod
 CS Core Shim Subassembly
 CF Core Fuel Subassembly
 BF Blanket Fuel Subassembly
 CT Control Filter, Tube-rod, Dummy Core Subassembly
 NA Sodium Worth Subassembly
 NS Neutron Source

MS APDA Materials Surveillance Subassembly
 M Subassembly Manufactured by D. E. Makepeace Co.
 S Subassembly Manufactured by Sylvania Division, Sylvania Electric Products Co.
 M 001 - M 206 Core Subassemblies
 M 301 - M 400 Inner Radial Blanket Subassembly
 S 500 - S 798 Outer Radial Blanket Subassembly
 M 801 - M 1000 Outer Radial Blanket Subassembly

Units shown without prefix are dummy outer radial blanket subassemblies.

Fig. A.1. Fermi Reactor Loading on October 5, 1966.

- A zirconium segment from the conical flow guide broke loose and produced nearly complete blockage of M-127 and M-098 (the accident subassemblies), and partial blockage of M-122 and M-140 (the adjacent subassemblies).

- As the reactor power level was increased, a sodium-vapor bubble filled the central region of the two accident subassemblies, where the coolant temperatures were above the boiling point of sodium.

- Fuel melting started at a reactor power level of 9 to 18 MW(t), and clad failure occurred at temperatures of 2100 to 2600°F.

- Within the region of the sodium-vapor bubble, the molten fuel flowed to the cooler sides and bottom of the bubble, and froze when coming into contact with the liquid sodium.

- Eventually, enough fuel slumped to form plugs in the accident subassemblies, which penetrated the duct walls.

- Sodium in the gap between the two accident subassemblies, which was nominally stagnant, entered the two subassemblies through the holes in the duct walls and provided appreciable convective cooling by two-phase flow through the vapor-bubble region. This convective cooling prevented additional fuel melting and failure propagation.

- The flow of liquid sodium induced in the interassembly gap provided cooling of the duct walls of the two accident subassemblies and prevented further penetration of the duct walls by molten fuel.

APPENDIX B

Modification of COBRA-III Computer Code for Leakage or Injection Flow

COBRA-III²⁸ is a thermal-hydraulic computer code which calculates the transient coolant flow and temperature distribution in nuclear fuel bundles by considering turbulent and diversion crossflow mixing between the coolant subchannels.

To allow for leakage or injection flow into a subchannel, the following equations were modified:

A. Continuity Equation

For a control volume that consists of a segment of subchannel i connected to an arbitrary subchannel j , the continuity equation in COBRA-III (Eq. A.2 in Ref. 28) is modified to become

$$A_i \frac{\partial \rho_i}{\partial t} + \frac{\partial m_i}{\partial x} = -W_{ij} - W_{i,R} \quad (B.1)$$

where $W_{i,R}$ is the leakage flow or injection flow, depending on whether its value is positive or negative, i.e., a positive $W_{i,R}$ denotes a leakage flow out of subchannel (see accompanying diagram). By considering all adjacent subchannels and taking W_{ij} to be positive for flow from i to j , Eq. B.1 can be written as

$$A_i \frac{\partial \rho_i}{\partial t} + \frac{\partial m_i}{\partial x} = - \sum_{j=1}^N W_{ij} - W_{i,R} \quad (B.2)$$

$$i = 1, 2, \dots, N.$$

B. Energy Equation

With leakage (or injection) flow, the energy equation can be written as

$$\frac{1}{u_i''} \frac{\partial h_i}{\partial t} + \frac{\partial h_i}{\partial x} = \frac{q_i'}{m_i} - (T_i - T_j) \frac{c_{ij}}{m_i} - (h_i - h_j) \frac{W_{ij}'}{m_i} - h^* \frac{W_{ij}}{m_i} - \frac{1}{m_i} \frac{\partial m_i}{\partial x} h_i - h_R \frac{W_{i,R}}{m_i}, \quad (\text{B.3})$$

where h_R is the enthalpy carried by the leakage (or injection) flow, i.e., for leakage flow out of the subassembly duct, it is the same as the local coolant enthalpy of subchannel i . u_i'' is the effective enthalpy transport velocity. For homogeneous two-phase flow or for single-phase flow, $u_i'' = u_i$, the axial velocity in subchannel i .

In the accompanying diagram, $\bar{q}_i'$ represents the heat-transfer rate from the fuel surface, q_i' , and the thermal-conduction term between subchannels i and j . By using Eq. B.1 and neglecting $\partial \rho_i / \partial t$, and by considering all adjacent subchannels, Eq. B.3 becomes

$$\frac{1}{u_i''} \frac{\partial h_i}{\partial t} + \frac{\partial h_i}{\partial x} = \frac{q_i'}{m_i} - \sum_{j=1}^N (T_i - T_j) \frac{c_{ij}}{m_i} - \sum_{j=1}^N (h_i - h_j) \frac{W_{ij}'}{m_i} + \sum_{j=1}^N (h_i - h^*) \frac{W_{ij}}{m_i} + (h_i - h_R) \frac{W_{i,R}}{m_i}, \quad (\text{B.4})$$

which, for leakage flow where $h_R = h_i$, is identical to the energy equation used in COBRA-III (Eq. A.8 in Ref. 28).

C. Axial Momentum Equation

The axial momentum equation used in COBRA-III (Eq. A.15 in Ref. 28) is modified to become

$$\begin{aligned} \frac{1}{A_i} \frac{\partial m_i}{\partial t} - 2u_i \frac{\partial \rho_i}{\partial t} + \frac{\partial p_i}{\partial x} = & - \left(\frac{m_i}{A_i} \right)^2 \left[\frac{v_i f_i \phi}{2D_i} + A_i \frac{\partial}{\partial x} \left(\frac{v_i'}{A_i} \right) \right] \\ & - g \rho_i \cos \theta - f_T \sum_{j=1}^N (u_i - u_j) \frac{W'_{ij}}{A_i} \\ & + \sum_{j=1}^N (2u_i - u^*) \frac{W_{ij}}{A_i} + (2u_i - u_R) \frac{W_{i,R}}{A_i} \end{aligned} \quad (B.5)$$

(see accompanying diagram).

The choice of u^* , the effective axial velocity carried by the diversion crossflow, must presently be assumed because there is insufficient data to define it accurately. Fortunately, various assumptions of u^* have been found to cause only small to moderate changes in the solutions for subchannel flow. In the present study, u^* was assumed to be the same as u_i , the axial velocity of the coolant in the donor subchannel. For a leakage flow, the effective axial velocity u_R was also assumed to be the same as u_i .

APPENDIX C

Material Properties for Thermal Stress Analysis of a Subassembly Duct

Since there is a relatively large uncertainty in the mechanical properties of the subassembly duct, particularly at high irradiation levels, they are tabulated here for reference.

Table C.I. Properties of Austenitic Steels 304 and 316
(From ASME Boiler and Pressure Vessel Code, Sect. III)

Temp, °F	Yield Stress σ_y of 304 SS, ksi	Yield Stress σ_y of 316 SS, ksi	Coefficient of Thermal Expansion (α), 10^{-6} in./in./°F	Modulus of Elasticity (E), 10^3 ksi
100	30.0	30.0	9.16	28.1
200	25.0	25.8	9.34	27.7
300	22.5	23.3	9.47	27.1
400	20.7	21.4	9.59	26.6
500	19.4	19.9	9.70	26.1
600	18.2	18.8	9.82	25.4
700	17.7	18.1	9.93	24.8
800	16.8	17.6	10.05	24.1

Table C.II. Properties of Austenitic Steels at High Temperature
(From HEDL-TME 71-32)

Temp, °F	Coefficient of Thermal Expansion (α), 10^{-6} in./in./°F	Modulus of Elasticity (E), 10^3 ksi	Yield Stress σ_y of 304 SS (Unirradiated), ksi	Yield Stress σ_y of 316 SS (Unirradiated), ksi
600	9.83	25.4	18.2	18.9
700	9.95	24.7	17.5	18.0
800	10.07	24.1	17.0	17.4
900	10.19	23.4	16.3	17.1
1000	10.30	22.6	15.6	17.0
1100	10.40	21.8	15.0	16.6
1200	10.46	20.9	14.4	16.1
1300	10.53	20.0	13.4	15.4

Table C.III. Properties of Type 316 Cold-worked Stainless Steel
After Irradiation^a (From BMI-1900)

Test Temp, °F	Yield Strength, ksi	Total Elongation, %
600	116	8.5-19.5
700	109	
800	103	
900	95	
1000	85	
1100	68	
1200	44	
1300	38	

^a Irradiated at 482-662°F to total neutron fluence of
 3.6×10^{22} n/cm²; 15% cold work.

Table C.IV. Properties of Type 316 Cold-worked Stainless Steel
After Irradiation^a (From GEAP-10062)

Test Temp, °F	0.2% Offset Yield Strength, ksi	Total Elongation, %
932	94	11
1112	56	19
1202	44	18
1292	40	11

^a Irradiated at 350°C (662°F) to 5×10^{22} n/cm² total; 17%
prestrained.

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