LMFBR SUBASSEMBLY RESPONSE TO LOCAL PRESSURE LOADINGS - AN EXPERIMENTAL APPROACH

By

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LMFBR SUBASSEMBLY RESPONSE TO LOCAL PRESSURE LOADINGS - AN EXPERIMENTAL APPROACH

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1. Introduction

Analyses of off-normal, accidental events that may occur within a given subassembly of a Liquid Metal Fast Breeder Reactor (LMFBR) is based on the determination of possible initiating events and the study of subsequent effects. Of prime interest in these efforts is the determination of whether or not these local events can propagate from fuel pin to fuel pin and thence, from subassembly-to-subassembly. Initiating events which have been identified include spontaneous pin failure and fission release, failure of an overenriched, misloaded fuel pin and local or full subassembly blockages. The problem of pin-to-pin failure propagation has been addressed elsewhere [1] and will not be discussed here, however, the possible subassembly failure propagation that could occur as a result of pressure pulses is of interest here as well as fuel meltthrough of subassembly walls. Of prime concern in this effort is the study of propagation phenomena connected with local pressure pulse generation in and the structural response of fuel subassemblies. The problem of propagation due to thermal loadings will not be considered at present but is being addressed in a similar experimental effort [2].

The problem of determining the response of fuel subassemblies to locally generated pressure pulses and possible propagation of damage to surrounding subassemblies or central rod positions is difficult to approach from either an experimental or purely analytical point of view. Several factors which must be considered in the study of such events, as mentioned above include: (i) definition of subassembly local accident which may result in significant pressure pulses and this damage not only to the accident subassembly but also its neighbors, (ii) definition of subassembly duct material properties at various times in the core life under operating or accident conditions; (iii) propagation phenomena, including subassembly fracture modes and locations and (iv) geometry of the subassembly structure including intersubassembly coolant and the surrounding subassemblies, their internals and other structures such as central rod guide tubes.

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The definition of local subassembly accidents poses one of the most difficult areas to resolve, since a wide range of initiating conditions may be postulated followed by various sequences. These pressure pulses may well range from those expected when one or more end-of-life fuel pins fail, releasing high-pressure fission gas (a type of event with a relatively high probability of occurring) and coolant interaction. The latter event is still of very low probability, however, because current reactor designs preclude subassembly blockages and understanding of the fuel-coolant-interaction problem indicates that this event has a low likelihood of occurrence. The intensive studies underway attempting to describe accident sequences should lead to the future definition of realistic pressure-time sources for use in subassembly damage evaluation.

End-of-life material properties of the stainless steels used in subassembly ducts are not well known. There is an effort to determine these properties and improvements in the mathematical description as well can be expected. At present, the mechanical properties such as ductility, yield strength and ultimate strength are only nominally known and may be subject to significant variations. Of interest too are the strain rate sensitivities of these materials as well as their fracture behavior under dynamic loading conditions. These could be significant in defining adequate safety margins.

Given the above significant uncertainties in the pressure pulse and material properties of subassembly ducts, an experimental program to determine hexcan response to local accidents is difficult to define. Fortunately, preceding the experimental effort an analytical program based on finite element techniques has resulted in the development of computer codes which can also describe these phenomena. The overall approach then, is to devise experiments which can be used to verify and where necessary, modify and extend the computer codes and analysis. The codes would then be used to evaluate in-reactor events as pressure pulses and material properties become known.

The main goal of the experimental effort described here is to perform well-defined and instrumented experiments in order to obtain sufficient data to support the computer code development.
The codes being developed include the STRAW [3] and SADCAT [4] codes which are two and three-dimensional, explicit codes which not only take into account the accident subassembly but also the intersubassembly coolant and the surrounding one or two rows of subassemblies including some representation of the subassembly internals. Since the experimental program is designed to verify the calculational model which describe the subassembly response, the following approach is being taken. First, quasi-static experiments are performed to define the gross response of subassembly ducts to internal and external hydrostatic pressures. Measurement of strains and mid-flat deflections will give initial confidence in the code and will help to eliminate the uncertainties which may exist in material properties. Secondly, after the development of a calibrated and verified pressure-time source, dynamic tests on single isolated hexcans are to be performed in which pressures, strains and deflections are measured. Finally, experiments are to be performed to investigate the effects of intersubassembly coolant, and subassembly cluster tests to evaluate subassembly-to-subassembly damage propagation phenomena.

Hexagonal ducts used in the experiments are those which have current design dimensions and materials. This is adequate for code calibration since it is only necessary to know the room temperature material properties. In order to approximate the low ductility of irradiated hexcans two approaches will be taken. The first will be centered around using ducts with various degrees of cold-working since it was determined that uniform properties could be maintained throughout the hexcan. Initially, annealed and 50 percent cold-worked material will be used. Subsequently, use will be made of a specially treated material to approximate the low ductility of irradiated stainless steels.

After adequate verification and possible modifications of the codes to correlate with experiments has been achieved, the codes will then be used to calculate actual in-reactor subassembly response to local accidental events. By this means, reliance upon out-of-pile experiments to bound the energy source will be avoided since complete simulation would be difficult to achieve. The overall program sequence and approach is depicted in fig. 1 After the codes have been verified and calibrated with an experimental out-of-pile effort, subsequent determination of pressure pulses, material properties and configurations will be used to calculate in-reactor subassembly-to-subassembly damage propagation phenomena.

2. Finite Element Structural Dynamics Codes

2.1 Two-Dimensional Code

The basic structural computer code used in the analysis of hexcan deformations is STRAW, a nonlinear, transient finite element program. The code is two-dimensional in the r - θ plane essentially, and has the capability of including not only the accident subassembly but also the surrounding subassemblies and the intersubassembly coolant. Since the code is of a relatively simple nature, repeated computational trials to study parameter changes can be quickly and economically run. The mathematical model includes the equations for large displacement and small strain theory, which for normally brittle in-reactor materials is adequate. However, part of the experimental effort is geared to performing tests in annealed, high ductility, stainless steel so that some judgment on the range of applicability of the code can be made. The codes uses an explicit numerical integration scheme but modifications and improvements have been incorporated to include an implicit technique to allow treatment of longer duration events. [5] The code also includes treatment of hydrodynamic finite elements and a representation of axial flow in order to improve representation of energy particularly effects.

2.2 Three-Dimensional Codes

Since many in-core structures are composed of plate and shell components, a general purpose code SADCAT has been developed for transient analysis of these three-dimensional structures.

This code uses a lumped-mass system and an explicit time-integration procedure. In addition, it treats large displacements and elastic-plastic behavior of material. The code, therefore, is especially suited for structures subjected to high loading rates and undergoing large displacements.
The formulation within the code is based on the finite-element procedure, in which the
discrete model of the structure consists of an assemblage of flat, triangular plate elements inter-
connected by nodes. Each element is associated with a convected coordinate system which is attached
to the element. Thus, the convected coordinate system moves with the element and approximates the
rigid-body motion of the element. In addition, each node is associated with a set of body coordinates
which rotate with the node and coincide with the principal axes of the mass moment of inertia of the
node.

The lumped masses and rotational inertias of the nodes are obtained as follows:
The mass and moments of inertia of each element is calculated and then subdivided into three equal
parts, each associated with the respective nodes of the element. This procedure is applied to every
element so that the final mass and moments of inertia at each node consist of contributions from all
adjacent elements. The principal moments of inertia are then determined at each node; the associated
principal directions constitute the initial body coordinates. At the boundary the moments of inertia
are made isotropic and the body coordinates are prescribed so that any required rotation can be
imposed.

The total displacement of the element is decomposed into a rigid-body motion and a deforma-
tion. The rigid-body motion is defined by the motion of the convected coordinate system; the defor-
amation is the displacement relative to the convected coordinates. Rotations of the nodal coordinates
are described by Euler angles; the nodal coordinate components are updated at every time step.
Plate elements are assumed to have linear in-plane displacements and cubic transverse
displacements. The deformation displacements are computed from the relative displacements and
relative rotations of the nodes. Strains are computed in the convected coordinate system: the first
derivative of the in-plane displacement provides the membrane component of strain, and the second
derivative (curvature) of the shape function yields the flexural component of strain.

The tensile data of true stress vs engineering strain are approximated by a multi-linear
curve with isotropic strain hardening. Yielding in the biaxial state of stress is handled by the
von Mises yield criterion and a flow rule is assumed in the plastic regime. The plastic components
of stress are evaluated by an algorithm introduced by Hartzmann and Hutchinson [5].

Internal nodal forces and moments are derived using a five-point trapezoidal integration
across the element thickness and a three-point integration over the element plane. The transverse
nodal forces are obtained from the requirement that the nodal forces of an element must be self-
equilibrated.

In some problems in which the residual membrane forces are large and no external loads
are present there is a tendency for certain nodes to oscillate in a snap-through mode. This effect
is eliminated by a membrane artificial viscosity which can be specified as a percentage of critical
damping.

J. Material Properties

Of particular importance for prediction of hexcan deflections under a specified loading is the
description of the material properties as input data to the codes. Stress concentrations in the
corners of the hexcan will cause plastic flow for even relatively small deflections of the midflat.
This necessitates an accurate knowledge of the yield point, for the plastic hinge effect causes the
midflat deflection to increase sharply when the elastic limit is exceeded. Because of the sensitivity
of deflection to the plastic-flow material properties, the stress-strain relationship cannot be
linearized in the yield region.

A stress reversal in the outer layer at the corner, shown in fig. 2, gives rise to a further
difficulty in the material description. The material is first compressed until plastic flow and
strain-hardening take place. With further deflection, the strain direction reverses, and elastic
tensile straining proceeds until yielding in tension occurs. The new tensile yield point depends
upon the earlier compressive strain-hardening effect. An example path of possible stress-strain states is shown in fig. 3. Precisely where to expect the new yield point is not known. There are several theories for the strain-hardening effect. For the conventional uniaxial stress-strain diagram the "kinematic hardening" effect assumes an unchanging magnitude for the elastic range; "fixed hardening" assumes an invariant yield stress point. The sample computation shown in fig. 3 illustrates the possible importance of the choice of models. The isotropic model is stiffer, and for the example shown in fig. 3, the isotropic assumption indicates that the hexcan would sustain a pressure of 2000 psi. However, under the assumption of kinematic hardening the tensile yield stress is lowered and the hexcan cannot sustain the same pressure of 2000 psi, but expands out to failure. Based upon indications from other experiments [6], the correlations in this study assume the yield lies between the fixed and kinematic values.

Another source of some difficulty for the correlation of the computations with the test results is that the two-dimensional deformation of the hexcan section remote from and effects is in a plane-strain mode. Axial displacements are constrained, hence, flexural beam elements in the code are in a biaxial state of stress. The stress-strain data are from uniaxial tests and must be corrected to apply to the plane-strain problem. The von Mises yield ellipse is used in this approximation. The most direct correction is for the isotropic hardening model, which ignore the Bauschinger effect, and the yield ellipse simply enlarges without translation (in the principal stress space). For kinematic hardening, the yield ellipse is translated but without change of size. For yield points between these extremes, the yield surface both changes size and is translated in the principal stress diagram.

In order to minimize, as much as possible, material property uncertainties when doing experiments and comparisons, actual measurements were made of samples taken from tested ducts. Samples were machined from sections taken from both the axial and transverse directions of the drawn hexagonal ducts in order to detect possible differences due to the drawing operation. The wall thickness of the duct limits the size of the specimen to a rectangular area of only 0.03 sq. in. The gage length of the specimen is 1.0 in. which makes the nonuniform strain distributions occurring near the ultimate point difficult to measure. Early tensile tests performed in the program relied on the cross head displacement for strain measurements. It was found, however, that these results were unreliable since apparently, the stiffness of the machine, including the specimen grips, was of the same order as the specimen. Further, there was still a difficulty in determining the strains near the necking points. Somewhat satisfactory analytical techniques were devised to resolve this problem and they are described elsewhere [7].

The more direct approach is to mount a strain gage with a one inch gage length directly on the sample. Measurements indicated the point of uniform strain and the specimen cross sectional area was measured after fracture. In this manner the true engineering versus the engineering strain data for each sample was determined. Since the specimens were taken directly from the ducts, some nonuniformities in the area of the specimen were found both axially in each sample and from sample to sample. Overall material properties were then determined by averaging over the several samples tested and then running sensitivity studies. It was found that in some cases at least ten percent variation was justified.

Further modification of the data was needed in order to apply it directly to the STRAW code in which plan-strain curve was adjusted by a simple method [8] in which the Young's modulus is increased by a factor of $1/(1 - v^2)$, and the yield stress of a factor of $1/(1 - v + v^2)$, where $v$ is Poisson's ratio.

The adjustment for the plastic region consisted of increasing the slope of the stress-strain curve by a factor of 4/3.

Further tests were performed to ensure that the material properties were uniform over a hexcan flat. It was found from previous comparisons [7] that strong variations in cold work, for example, had
a profound effect on the deflections. In order to resolve this issue and remove another uncertainty, diamond point hardness tests were performed. These determinations were made from the mid flat region through to the corners and also through the thickness of the flat at about five points. The diamond point hardness can also be correlated with cold work level. In general these tests demonstrated that properties were indeed quite uniform.

4. Hydrostatic Pressurization of LMFBR Hexcans

Several experiments have been performed on the hydrostatic pressurization of subassembly hexcans. The main reasons for this approach were to determine the gross response of hexcans to static internal pressurization, and to provide reliable pressure, deflection and strain measurements. Since there was a need to limit the length of the hexcans used in each experiment, it was found by separate calculations that a one foot length, corresponding to an L/D ratio of about 3 would be adequate. Tests described later, also showed that this length would be adequate.

The apparatus used in both the static internal and external pressurization experiments is shown in fig. 4. When performing the internal pressurization tests the heavy outer ring, surrounding the hexcan, is removed so that instrumentation can be placed. The hexcan to be tested was welded to rigid end rings which were, in turn, bolted to thick end plates. Since plane-strain conditions were required during the tests, the end motions of the hexcans were restricted by twelve one-inch diameter bolts which were arranged around the edge of the thick plate. The cross sectional area of the bolts was such that even under a pressurization of about 7000 psi end motion would be small. This was also verified by measurement during some tests.

Measurements taken during each test included the internal (or external) pressure, mid-flat and corner displacements and mid-flat, internal and outer surface strain. Strain gages placed axially in the test section also demonstrated that plane strain conditions existed during the test. The deflections were measured with sensitive linear potentiometers which were spring loaded to maintain contact with the hexcan surface. Further, in each test, measurements were taken on several flats and corners in order to provide sufficient redundancy and to detect asymmetric deformation that may be due to nonuniform material properties.

In order to demonstrate that a hexcan with a length of one foot was sufficiently long to provide plane-strain conditions with a negligible amount of end effects, a hexcan with a length of two feet was also tested. Each hexcan was made of solution annealed 316 stainless steel and had uniform properties throughout. Figure 5 shows the deflection of the two-foot long hexcan as a function of axial distance and internal pressure. Superimposed upon the two-foot section data are the deflections from the mid flat region of the one foot long hexcan test. Further, fig. 6 shows the midaxial deflections of both hexcans while fig. 7 shows the corner deflections. As can be seen from these comparisons the agreement is quite good and it is felt that this adequately demonstrates that the one-foot long duct section to (1) minimize end effects and (2) provide a central position of the hexcan where plane-strain conditions exist.

4.2 Comparison of Test and Calculational Results

Several tests were run using hexcans made of type 316 stainless steel at room temperature conditions. The ductility of the ducts was varied by using ducts manufactured with different degrees of cold working. The hexcans listed had an initial inside flat-to-flat measurement of 4.335 in (11.01/cm) with a wall thickness of 0.120 in (.3048 cm). There was some variation in thickness, but it was small.

The first series of hexcans tested were solution annealed. Solution annealing provided hexcans with uniform properties throughout, thus eliminating difficulties experienced with as received ducts. The material is very ductile and thus provides a test in which a large amount of deformation could be expected. This was desirable since one objective of the test series was to determine the range of applicability of the codes. In general, the analytical models used were based on small strain, large deflection theory. However, it was never clear from analysis what would truly be "small".
For irradiated ducts, it should be noted, with an expected ductility (total elongation) of about 2 - 6 percent, it was assumed that this theory would be applicable. However, this type of test should define the region where small strain, large displacement theory breaks down for a hexcan.

In fig. 8 is depicted the partial stress-strain relationship used to describe the annealed 316 stainless steel. Curve "A" is the basic data while Curve "B" represents the curve corrected for plane-strain conditions in the STRAW code. The material is quite ductile. Figure 9 shows the comparison of the STRAW code calculation and the average mid-flat displacements from the experiment. The comparison is made through an internal pressure of about 3000 psi (207 bar). At this point, the full instrumentation was disconnected in the test and the can was pressurized to failure at about 4500 psi (310 bar). Figure 10 shows the average mid-flat strain for the test while fig. 11 shows the corner displacement as a function of internal pressure. The agreement is quite good over the entire range of comparison. Referring again to fig. 9, the mid-flat displacement several observations can be made about the shape, or mode, of deflection. Elastic displacement occurs at pressures below about 250 psi (17.2 bar) while plastic flow occurs in the corner, and indeed throughout the entire beam, for pressures up to about 1750 psi (121 bar). At pressures higher than about 1800 psi (124 bar) the hexcan deforms into a cylindrical shape and, in fact, deformations would be calculated perhaps using thin shell cylinder theory.

In order to gain some insight into the sensitivity of the calculations to material properties by up to 10 percent. The results of these calculations are shown in fig. 12. Here mid flat deflection is shown as a function of pressure. Agreement is quite good to pressures of about 1750 psi (121 bar) where strong variations occur.

Tests with nominal 50 percent cold worked type 316 stainless steel were also performed. This material has a somewhat lower ductility, a much higher yield and uniform stress. The stress-strain curves are shown in fig. 13, including not only the median of the tensile specimens but also the corrected curve and a curve nominally 10 percent higher. The mid-flat deflection is shown in fig. 14. Here better agreement was achieved with the stress-strain curve 10 percent above the median. These increased values may be justified in fact, since some variations may be caused by some nonuniformities, other than necking, in the tensile specimens. The 50 percent cold worked duct failed at about 3050 psi (20 bar) while the hexcan flats were still in the bending mode. The inner and outer surface strains are compared in fig. 15 and fig. 16, respectively. Again, better agreement was achieved with the increased stress-strain curve. In general, agreement still was quite good even for the nominally determined values. Mode shapes, an important quantity were in excellent agreement.

Some comparisons were also made using the SADCAT three-dimensional finite-element code. Of interest in these calculations were the end effects. As was noted previously, a one-foot (30.48 cm) duct was of sufficient length for the mid flat deflection to be independent of the radial constraints imposed by the heavy end plates. Unlike the radial constraints, axial end constraints at the ends may very stiffen the entire can against expansion. This stiffening effect would be independent of the can length. At the end region, the axial deflection profile is nonuniform and computations require a three-dimensional code. The boundary condition imposed by the end flange constrains the slope as well as the displacement, i.e., the flange imposed a bending moment to the end as well as a radial force component. For the prescribed end conditions of zero slope and radial displacement, the deflection profiles shown in fig. 17, were computed with SADCAT. The bending moment induced an inflection point at the flange and a slight bulge appeared near the end. The maximum deflection occurred at the bulge rather than at the midsection as might otherwise be expected. This bulging phenomena has been previously described for thin, right circular cylinders based on elasticity theory [9]. The bulge produced an additional deflection of about 10 percent above the uniform deflection at the sections more remote from the ends. The stiffening effect of the axial constraint to also shown in fig. 17. The computation indicates, for example, that at an internal pressurization of 2000 psi, the deflection is reduced 20 mils, or 10 percent less than would occur if the duct were permitted to contact freely.
in the axial direction.

The effect of axially constraining the hexcan is shown in fig. 18 for a loading rate of 400 psi/msec (27.6 bar/msec). There was little influence at the smaller pressures, aside from a small dynamic effect, but at the higher pressures above 1300 psi (90 bar) expansion caused an axial contraction. At an internal pressure of 3000 psi (207 bar) the length was reduced by 0.6 in. (1.52 cm). As a result the expansion of the duct was much greater when the ends were free to contract than for cases with constrained ends. At 3000 psi (207 bar), freezing the ends increased the maximum midplane deflection by 27 percent from 300 to 380 mils (0.76 to 0.97 cm).

5. Dynamic Pressurization Experiments

Of course, the more interesting cases in accident analysis occur under dynamic loading conditions. To this end a source was needed, which would duplicate a pressure pulse which could be experienced by a subassembly duct. As was pointed out in the beginning of this article this is a somewhat fruitless task since a wide range of pressure pulses, which are sequence dependent, may well be expected. Even so, it may be postulated that these pulses may be of a generic nature in that they may well have characteristic rise time ranges and pulse durations if not absolute pulse magnitude. With this in mind a case was delineated which gave a pressure pulse peak of about 100 bar with a rise time of 1 msec and a total pulse duration of 5 msec. The case in which this pulse was defined assumed the failure by melting of one or several, misloaded overenriched fuel pins and a subsequent molten fuel-coolant interaction. The conservative nature of this event was noted earlier.

5.1 Pressure Source Development

The pressure source developed is based on the controlled expansion of high pressure gaseous products from a low-density high explosive detonated inside a vented steel canister. The high explosive is a mixture of PETN (pentaerythritol tetranitrate) and either hollow glass or plastic micro-balloons. The hexcan experiments, the pressure source consisting of a stack of slotted steel rings replaces the fuel pins and coolant within the hexcan thus providing an axisymmetric dynamic pressure loading. In this configuration, after detonation of the charge mixture, the high pressure detonation products fill the canister and vent through the slotted steel rings into the space between the rings and the hexcan wall. These gaseous products then vent to the atmosphere through axial parts at each end of the hexcan. With this arrangement the pressure rise time is controlled by the cross-sectional area of the slots in the canister rings while the axial venting controlled the peak pressure, decay rate and total pulse duration. The source canister could be varied in total length but as in the quasi-state tests it was 1-foot long (30.48 cm). Since the source was required to provide an axially uniform load it was tested in a calibration device which had a sufficient number of pressure transducers axially located to determine the pressure profile.

The details of the theoretical development have been presented elsewhere [10] and will not be repeated here. The overall procedure was to (1) develop a theoretical base for designing the pressure source, (2) test the pressure source in a calibration canister, and (3) after calibration use the source in appropriate hexcan experiments. The source calibration canister is shown in fig. 19. The inside cross sectional area of the rigid steel cylinder which surrounds the source canister corresponds to the total cross-sectional area of an LMFBR hexcan. Model 603H Kistler gages were used to measure the axial pressure distribution at six locations and also the pressure at the end plate of the calibration device. The rigid cylinder was used to minimize any radial expansion effect on the pressure pulse. However, since gaseous products are used there would be relatively minor effect even for the much larger deflections expected in a hexcan.

The results of the source development effect are essentially summarized in fig. 20. Here is presented a comparison between the theoretical model and actual pressure pulse measured at gage 4 - the axial midplane of the source canister. Other measurements along the axial direction showed a gradient of perhaps 7 percent; not enough variation to cause an unacceptable amount of variation in axial deflections.
5.2 Dynamic Tests and Calculations

The test series on the dynamic loading of single hexcans using the pressure source as described in the previous section will include essentially the same materials used in the hydrostatic tests. Tests performed to date have included a similar series to show the adequacy of using a one-foot (30.48 cm) hexcan as opposed to a two-foot (60.96 cm) long duct. In these tests the dynamic strains as well as deflections and pressures are being measured. Other problem areas which are being addressed include the determination of strain-rate effects on material properties and the diversing of appropriate models for inclusion in the calculations.

The dynamic response of the hexcan to a pressure pulse is compared in fig. 21 to the response under a quasi-static pressurization. At the same pressure, the dynamic deflection lags behind the hydrostatic deflection due to inertial resistance. However, as the peak of the pressure pulse is reached, inertia causes an overshoot in the deflection. These dynamic inertial effects can exert a strong influence upon the final deformation, as is illustrated in fig. 13. For a 1200-psi pressurization at a rise time of 0.5 msec, the peak midflat deflection (280 mils) occurs at 0.6 msec, shortly after the peak pressure is reached. This is an expected response behavior. However, a qualitatively different response behavior was computed when the pressurization was increased to 2000 psi for the same rise time. The expected overshoot appeared with a peak of 360 mils, a peak above the hydrostatic deflection (320 mils), but the return to this hydrostatic position was abruptly stopped, and a further jump in the deflection advanced to a final permanent deformation of 500 mils. This apparently anomalous response behavior is caused by the plastic tensile flow, permitting easy extension, while compressive strain is strongly resisted by the large elastic modulus. Consequently, the deflection can occur in a series of alternating steps in which outward motion of the midflat accompanied by a relatively stationary corner is followed by an outward motion of the corner while the midflat region holds stationary.

6. Conclusions

An experimental program has been initiated to determine the response of LMFBR-type subassemblies to local subassembly accidents. The approach taken in this program is to perform well-defined experiments in which pressures, deflections, and strains can be measured. An effort is also being made to determine material properties of the hexcans and to assure that the material properties are uniform throughout a specimen. Results from the tests are then compared with computer-code calculations in order to check the validity of the analysis and perhaps modify models for more meaningful correlations. The codes then will be used to calculate the response of subassembly ducts under in-reactor accident conditions.

Comparisons to date show good agreement between calculations and experimental results for annealed and 50 percent coldworked Type 316 stainless steel ducts under hydrostatic loading conditions. These tests and comparisons are quite helpful in gaining knowledge and insight into the way in which hexcan distort under internal and external pressures. Possibly these tests can be used to extrapolate directly to tests in which the rise time of the pressure pulse is significantly longer than the natural period of the hexcan. The natural period is about 0.3 sec based upon clamped-clamped elastic beam theory and some dynamic calculations using the STRAW code.

Several items were determined in the tests and comparisons which will be helpful in performing and interpreting dynamic tests. It has been demonstrated that one-foot-long hexcans are adequate for single hexcan tests. Material properties for annealed and coldworked Type 316 stainless steels have been determined. Diamond point hardness tests have demonstrated that the material properties should be uniform both along the hexcan and through its thickness – an important point if adequate modeling is to be performed.

A good pressure source has been developed which duplicates the general characteristics of a local accident. A basic theory has also been developed which can be used as a tool to design other pressure sources. Limitations, of course, may exist in the design of the source canister rings, but in any
event peak pressures could be varied over a significant range. The pressure source is reproducible and does not exhibit a strong axial gradient which should not affect needed experimental results needed for code calibrations and comparisons. Further effort will be needed to define the strain-rate characteristics of the materials used in the tests.

Future work will be concerned with testing ducts with material properties more closely approximating those of end-of-life ducts. Although these materials will have low ductility and yield and ultimate strength approximating end-of-life ducts, they are not intended to be a strict simulant. The purpose is to check the modeling in the computer code using material properties more closely approximating those expected under reactor operating conditions. Propagation phenomena and the effect of intersubassembly coupling in damage propagation will also be investigated.

7. Acknowledgments

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References


Figure Captions

Fig. 1. Development of Predictive Analysis Method for Subassembly Response.

Fig. 2. Examples of Stress Reversals at Outer Corner Surface and Inner Midflat Surface for Annealed, Room Temperature, LMFBR Hexcans.

Fig. 3. Comparison of Stress-strain States for Isotropic and Kinematic Hardening.

Fig. 4. Schematic of Apparatus Used for Single Hexcan Tests.

Fig. 5. Comparison of 1-foot versus 2-foot long Hexcans Showing End Effects on Results.

Fig. 6. Comparison of Midplane Midflat Deflections for 1-foot and 2-foot long Ducts.

Fig. 7. Comparison of Midplane Corner Deflections for 1-foot and 2-foot long Ducts.

Fig. 8. True-Stress-Engineering-Strain at Room Temperature of Annealed Type 316 Stainless Steel.

Fig. 9. Comparison of Experimental and Calculated Midflat Displacement versus Internal Pressure.

Fig. 10. Comparison of Outer Midflat Strain versus Internal Pressure for Annealed 316 Stainless Steel Hexcans.

Fig. 11. Comparison of Experimental and Calculated Corner Displacement versus Internal Pressure for Annealed 316 Stainless Steel Hexcans.

Fig. 12. Sensitivity of Midflat Deflection Calculations to Variations in Material Properties.

Fig. 13. True Stress versus Engineering Strain for 50 percent Coldworked 316 Stainless Steel.

Fig. 14. Midflat Deflection versus Internal Pressure for a 50 percent Coldworked Hexcan.

Fig. 15. Outer Surface Strain versus Internal Pressure for a 50 percent Coldworked Hexcan.

Fig. 16. Inner Surface Strain versus Internal Pressure for a 50 percent Coldworked Hexcan.

Fig. 17. Computed (SADCAT) Deflections in End Region Showing Effects of Axial End Constraints.

Fig. 18. Effects of End Constraints Upon Midflat Deflections as Shown by SADCAT Calculations.

Fig. 19. Pressure Source and Calibration Apparatus.

Fig. 20. Comparison of Theory and Experiment of Dynamic Pressure Pulse.

Fig. 21. An Example of a Dynamic Calculation Using the STRAW Code.
SUBASSEMBLY RESPONSE ANALYSIS DEVELOPMENT

SUBASSEMBLY RESPONSE OUT-OF-PILE EXPERIMENTAL PROGRAM

DEFINITION OF IN-REACTOR SUBASSEMBLY ACCIDENTS P(t), P(v), MAT’L PROPS.

MODIFICATION VERIFICATION AND ANALYSIS BASED ON EXPERIMENTAL COMP.

VERIFIED ANALYSIS USED TO PREDICT IN-REACTOR SUBASSEMBLY RESPONSE
(a) INNER MIDFLAT SURFACE

(b) OUTER CORNER SURFACE

STRESS, ksi

STRAIN

EXPANSION

COMPRESSION

EXPANSION

COMPRESSION
43.5 ksi

H = HARDENING

-41.6 ksi = \( \sigma_y \) (YIELD)

ISOTROPIC MODEL

KINEMATIC MODEL
ALL DIMENSIONS IN inches
I

V)
a
D
u
1
2
3
4
5
6
7
8
9
10
11
12
13

-1
—
—
—
—

\( p = 500 \text{ psi} \)

\( \rho = 1000 \text{ psi} \)

\( \rho = 2000 \text{ psi} \)

\( \rho = 2800 \text{ psi} \)

Midflat Deflection, \(10^{-3} \text{ in.}\)
"A" Annealed 316 SS Data

"B" Corrected for Plane-strain Conditions

True Stress, psi

Engineering Strain, in./in.
STRAW Calculation

Avg Displacement from Test

Midflat Displacement, $10^{-3}$ in.

Internal Pressure, psi

0 1000 2000 3000
**STRAW Calculation**

- **Outside Midflat Strain, %**
- **Internal Pressure, psi**

- **Avg Strain from Test ST107I**
DECREASE IN STRESS

PLAN STRAIN COMPTUATION

10% DECREASE IN STRESS DATA

PRESSURE, psi

MID-FLAT DEFLECTION, mils.
Plane-strain Correction for Median Data

Median of Tensile Test Data Range

10% above Median
Calculation Based upon Median Stress-Strain Data

Test Points (SRI Test ST1061)

Midflat Deflection, mil

Stresses 10% above Median Data

Internal Pressure, psi

0 1000 2000 3000
Calculation Based upon Median Stress-Strain Data

Stresses 10% above Median Data
1.0 Test Points (SRI Test ST106I)

Stresses 10% above Median Data

Calculation Based upon Median Stress-Strain Data

- Inner-surface Strain at Midflats, %.
- Internal Pressure, psi

0 0.0 1.0

0 1000 2000 3000
EXPERIMENTAL DATA

DISTANCE FROM SUPPORT FLANGE, in.

- NO AXIAL CONSTRAINT
- ENDS FIXED AXIALLY
- HEXCAN FLANGE
- SUPPORT FLANGE

MID-FLAT DEFLECTION, mils

- 1000 psi
- 2000 psi
- EXPERIMENTAL HYDROSTATIC DATA

NO AXIAL CONSTRAINT

ENDS FIXED AXIALLY

400 psi/msec

MID-FLAT DEFLECTION, mils

PRESSURE, psi
EXPLOSIVE CHARGE (65/35% PETN/µBalloons)

PL II DETONATOR

KISTLER GAGE
MODEL 603 H
PRESSURE SOURCE
VENT RING

RIGID STEEL CYLINDER
4.55-inch ID
6.5-inch OD

12"
1-1/2"
2"
2"
2"
2"
2"
1-1/2"
CHARGE: PETN/μ Balloons
CHARGE MASS: 58 g

THEORY

EXPERIMENT G104, GAGE 4
Pressure, psi

2000 psi, 0.5-msec Rise Time

Hydrostatic (2000 psi)

1200 psi, 0.5-msec Rise Time

Midflap Radial Deflection, mil

Time, msec

0 0.20 0.60 1.00 1.40