TRANSLATING ELEVATED TEMPERATURE MATERIAL PROPERTIES INTO RULES FOR STRUCTURAL DESIGN

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ABSTRACT
This paper describes how existing data on austenitic type stainless steel material were utilized to establish structural design rules for the Fast Flux Test Facility (FFTF). These rules govern the design of all heat transport system pressure boundaries, including the core support and core holddown structures. In addition to limiting design against material degradation from mass transfer and irradiation damage, the rules are intended to guard against structural failure from ductile rupture, ratchetting, gross deformation, buckling instability and creep-fatigue interaction.
I. SUMMARY

This paper describes how the structural design rules for elevated temperature components in the Fast Flux Test Facility (FFTF) evolved from the available materials data on Types 304 and 316 austenitic stainless steel. Specific application of these rules are to all pressurized boundary components in the heat transport system (including the core support and core hold-down structures but excluding fuel cladding), which operate at temperatures in excess of 800°F (427°C). In guarding against the various failure modes, the FFTF rules have resulted in a significant change from previous ASME Code guidelines for design by analysis. Thermal shock loads and other transient effects are emphasized along with strain and deformation limits. Inelastic analysis methods are considered as a logical development in design if simple screening tests, which use limits based on elastic calculations, are not met.

Safer and more reliable FFTF component design should evolve from applying these rules since each rule is directly related to a material failure mode.

**Ductile Rupture** - A stress intensity limit is used where values for a given temperature depend on time of loading. The stress intensity is not expected to exceed: (a) a fixed percentage of the rupture stress; (b) a fixed percentage of the stress associated with a tertiary creep initiation; and (c) a fixed percentage of the stress associated with 1% lifetime strain. Cumulative loading effects are considered by a linear time-fraction limit.

**Ratcheting** - A limit on lifetime inelastic strains is imposed. For membrane sections, base material is restricted to 1% and weld regions to 1/2% total strain.

**Creep-Fatigue** - A modified linear damage rule is used where cumulative cycle fraction evaluates fatigue damage and a cumulative time fraction evaluates the damage due to creep holdtimes.

**Buckling** - Minimum factors of safety are given for theoretical and experimental methods.

**Gross Deformation** - If a region cannot endure arbitrary strains of 1%, then separate functional deformation limits must be specified for the designer-analyst.

**Irradiation Effects** - Highly stressed parts must maintain 10% residual total (tensile) ductility at end of life.

**Mass Transfer Effects** - If diffusion effects can change the alloy compositions outside the normal specified limits, then the effects on properties must be reflected in the design procedures.
II. INTRODUCTION

Power reactors in the United States have consistently been designed to the rules of construction codes recognized by professional engineering societies. With water-cooled power reactors, the need for improved design analysis methods for pressure boundary structures eventually led to the present nuclear codes such as Section III \(^{(1)}\)^\textsuperscript{*}. With the advent of liquid-metal-cooled fast reactors (LMFBR's), new structural design problems are outside the treatment of Section III.

The authors became involved in this area because of conceptual design of the Fast Flux Test Facility (FFTF), a 400 MW test reactor for LMFBR fuels and technology. These early efforts at setting structural design rules revealed several new problem areas:

**Service Life** - In existing ASME design rules \(^{(2)}\) for elevated temperatures, the expected time of service for a component was not considered in the limits on allowed loads. A component with one year of service life would have limits based on \(10^5\) - hour material properties.

**Loading Parameters** - The FFTF novel design problems included severe thermal shock loadings. Existing ASME rules \(^{(1, 2)}\) did not set loading limits by using material properties related to similar rates and times of loading. This was an appropriate stance for Section III rules since no time-dependent material phenomena were involved, but elevated temperatures needed new rules, particularly in the evaluation of fatigue.

**Strain** - In Section III\(^{(1)}\), materials were not expected to undergo inelastic strains after the initial loading cycles. Limits for strains were needed for elevated temperatures where creep strain would be a feature throughout the service life.

**Welds** - Existing codes \(^{(2, 3)}\) contained many fabrication rules for welds, but no guidelines were stated for design by analysis. Weld zones obviously have different material properties than the base material.

\*Superscript numbers refer to numbered references at the end of the paper.
Material Property Degradation - Mass transfer effects can leach carbon and nitrogen from certain structural alloys exposed to sodium. In addition, the FFTF contained long-lived components near the core, exposing them to a neutron flux. Existing codes provided no guidelines to the designer for these effects.

Clearly, the FFTF has required a re-evaluation of the philosophy of structural design at elevated temperatures. Massive experimentation or proof testing could not be used since the expected service life of most FFTF components was ~20 years. Any new guidelines for analysis would be based primarily on existing materials data.

This paper describes the background and development methods for the FFTF rules which:

- provide guidelines for design by analysis at elevated temperatures
- give safer and more reliable component service
- provide guidelines for reactor safety calculations
- provide a viable starting point for future LMFBR design
- provide rules that anticipate, as much as possible, the rules to be issued at some future time by the ASME Code

Each mode of failure for the FFTF is treated separately in this paper.
III. PROPERTIES RELATED TO DUCTILE RUPTURE

1. Background and Development

Ductile rupture is the most commonly understood failure mechanism. At lower temperatures, the failure of a structure can conceptually be related to the short-term tensile specimen. At elevated temperatures, the stress-rupture (or creep-rupture) test gives an easily referenced material property. In existing ASME Codes for elevated temperature, primary loads (e.g., pressure and weight) were limited by rules involving limits on stress intensities. The stress limit was related to the following material properties:

- average and minimum stress to cause rupture in $10^5$ hours under a constant load, uniaxial test. [Test data at $10^5$ hours were extrapolated from actual test data at lower times of $2 \cdot 10^4$ hours.]

- average stress causing a secondary creep rate of 0.01% in 1000 hours in a constant load, uniaxial specimen. [Test data at the low creep rate are extrapolated from actual data at stresses several times the reference level.]

Both of these properties could be conveniently determined by the creep-rupture testing of a uniaxial specimen under constant load, and most of the recorded data from such tests consist of: (a) load stress, (b) secondary creep rate, (c) temperature, and (d) time-to-rupture. Once the two above criteria were in place, the majority of the subsequent elevated temperature tests were the same tests producing more of the same data. Few efforts were made to assess the characteristics of the entire creep curve - particularly the primary creep portion, which was so difficult to measure experimentally.

In the FFTF, the emphasis was on stresses near discontinuities since pressures in sodium-cooled systems tend to be low. The FFTF re-evaluation of the primary load limits needed first to consider component life. Some LMFBR plants were expected to serve useful lives longer than $10^5$-hours, the previous base time for material properties. Secondly, the re-evaluation must account for transients since earthquake and thermal shock transients are the most severe loadings on most components.

By May 1969, persons working on the draft of the LMFBR Piping Design Guide document (by C. F. Braun and United Nuclear Corp.) suggested that the FFTF use two stress intensity limits: one for long term loads (based on $2 \cdot 10^5$-hour properties) and another for transients of less than a few hour's duration. A similar procedure was adopted in the first comprehensive recommendations for FFTF structural criteria. Based on an evaluation of sparse data at various strain rates by Moen, there was an initial belief that the extremely rapid loadings might be based on equivalently fast strain rate tensile tests. The hope was that, as the testing strain rate increased, the stress needed to achieve a given strain would also increase—as shown in Figure 1. As par
FFT development, high strain rate tests were performed (Figure 2) by Stelchen, which showed that the use of stress values higher than the normal 0.2% offset yield stress are unjustified.

In the ASME, persons on the Subgroup on Elevated Temperature Design (particularly W. J. O'Donnell) favored the use of stress limits based on material properties characteristic of the same time as the duration of loading. This viewpoint was adopted by the FFTF rules. Details of the primary membrane stress intensity limit, $\sigma_{\text{lim}}$, are discussed below.

2. Limit Stress Intensity for Primary Loads

The definition of "primary" loads on the FFTF includes the same loads defined as primary in Section III (Reference 1). However, at elevated temperatures the possibility of creep deformation has required the designer to consider a new source of primary stress -- thermal expansion caused stress where structural action (elastic followup) keeps stresses high even if small inelastic deformations occur. For all structures, primary loads include these thermal stresses when averaged across a structural section. The result is equivalent to a net shear or normal force.

Vessel-nozzle regions also receive a different kind of primary thermal stress when the attached piping induces a net section bending stress due to thermal expansion. However, the piping currently does not consider these same thermal expansion induced bending loads as primary in nature. The past experience in piping design seems to vindicate the viewpoint that any inelastic strains from such stresses is distributed throughout the piping runs instead of concentrating in one element (e.g., an elbow).
For the primary general membrane stress intensity, the limiting quantity is $S_m^*$, which is the lowest stress value from several material property criteria. First, the time-independent criteria used for the $S_m$ of Section III were extended into the higher temperature range (above 800°F [427°C] for austenitic stainless steels). The criteria are:

1. One-third of the minimum specified tensile strength at room temperature
2. One-third of the tensile strength at metal temperature times 1.10
3. Two-thirds of the minimum specified yield strength at room temperature
4. Ninety percent of the yield strength at metal temperature

The procedure used to obtain minimum expected ultimate tensile strength and yield strength at elevated temperatures was simply to use the ratios of strength at temperature to strength at room temperature provided in Reference 4. These ratios, when multiplied by the minimum expected yield and ultimate strengths at room temperature (shown in the material specification), provide the minimum expected strengths at temperatures up to 1500°F. The appropriate criteria of 1/3 on ultimate tensile strength or 90% on yield strength then produce the tensile segment of the $S_m$ curve (Figure 3).

The use of "minimum" in Reference 4 is defined as a statistical procedure where a multiple of the standard deviation was subtracted from the mean of the observed (e.g., visual extrapolation of log-log plots for individual heat data) values. In particular, the minimum values should be exceeded by 95% of all test values. An earlier goal was to define minimum as 95% confidence that 95% of the test values would exceed the stated minimum property, but the lack of data organized in that manner precluded the use of that definition for FFTF.

Although the yield strength in Reference 4 is defined as the 0.2% offset yield stress, the authors have suggested that the use of a fixed strain value would eliminate some of the practical difficulty of establishing the slope of the stress-strain curve. With austenitic stainless steel at elevated temperatures, the strain value would be ~0.27% to match the Reference 4 values at 800°F.

Second, the use of uniaxial constant-load, stress-to-rupture data was considered to be a time-dependent stress criterion somewhat analogous to the ultimate strength at lower temperatures.

1. Two-thirds of the minimum stress to cause rupture at time $t$ at the metal temperature.
APPLICABLE TO
GRADE 304 OF:
SA-182
SA-213
SA-240
SA-249
SA-312
SA-376
SA-430
SA-452
SA-479

FIGURE 3. $\bar{\sigma}_m$ Values versus Temperature - 304 Austenitic Stainless Steel
Even though other ASME Codes\(^{(2)}\) use 80% of the minimum stress-to-produce-rupture in \(10^5\) hours, these same codes also had a precedent of using \(\approx 1/3\) of the minimum ultimate strength at lower temperatures. The 2/3 factor, like many factors in structural design codes, was a compromise among differing technical views.

The establishment of minimum stress-to-rupture for any given time required considerable extrapolation beyond the reference values provided in Reference 4 (e.g., minimum and average values at \(10^5\) - hours and average values at \(10^4\) - hours). To develop a minimum stress-to-rupture curve from 100 hours to 200,000 hours, the following steps were taken:

**Step 1** - On a log stress versus log time plot, shown in Figure 4, place values from Reference 4 corresponding to: \(A = \) average stress-for-rupture in \(10^5\) hours; \(B = \) minimum stress-for-rupture in \(10^5\) hours; and \(C = \) average stress-for-rupture in \(10^4\) hours.

**Step 2** - Using the data plots from Reference 4, obtain a visual average through the data for rupture in 100 hours.

**Step 3** - Place the 100-hour rupture strength values for each temperature on a log stress versus temperature plot shown in the insert, Figure 4. Draw a straight line through the individual data points to "normalize" the stress-to-rupture in 100 hours.

**Step 4** - At a given temperature, place the "normalized" value determined in Step 3, above, on the log stress-log time plot of Figure 4 - point D.

**Step 5** - Connect points D and C with a straight line and pass another straight line from point C through point A out to point E at 200,000 hours. This forms a continuous average stress-to-rupture curve from 100 hours to 200,000 hours for a given temperature.

**Step 6** - Draw a second curve through point B, parallel to the "average" curve constructed in Step 5, above. This represents the minimum stress-to-rupture for a given temperature and time from 100 to 200,000 hours.
FIGURE 4. Derivation of Stress-to-Rupture Curves
Step 7 - The above sequence is repeated for each temperature from 1000°F to 1500°F at 50°F increments (e.g., from 538 °C to 815°C in 20°C increments).

Step 8 - To extrapolate minimum stress rupture strengths below 1000°F (538 °C), the procedure shown in the inset of Figure 4 is used plotting log stress versus temperature for each given time-to-rupture.

The third criterion was related to the time for the onset of tertiary creep, another property from the uniaxial, constant-load creep tests at a given temperature (see Figure 5).

. Eighty percent of the minimum stress to cause initiation of tertiary creep at time "t" and at the metal temperature.

The tertiary creep regime is treated as undesirable for material used in critical structures. Many alloys have been identified as having a tertiary creep stage characterized by internal cracks, sub-surface grain boundary separation and other phenomena which invalidate the use of usual analytical modeling procedures for the material(6).

Often, materials properties data will correlate better with initiation of tertiary creep than with time-to-rupture. This can be seen on Figure 6.

In evaluating materials data for this criterion, the existing creep data was mainly in the form of secondary creep rates and time-to-rupture. In Reference 6, the tertiary creep initiation point (point A in Figure 5) was described indirectly by the fraction of total life that the material spent in primary-plus-secondary creep. This work provided the major data for the evaluation of the criterion.

Determination of the minimum stress for the onset of tertiary creep starts with use of the minimum stress-to-rupture curves described previously. Using the fraction, \(F_s\), described by Leyda and Rowe(6)(that portion of the rupture life which is expended at the point of departure from secondary creep), the minimum stress-to-rupture curve is shifted to the left by that amount - Figure 7.

This creates a curve which gives the minimum stress for the onset of tertiary creep, as a function of time. The \(F_s\) values used by the authors for Types 304
FIGURE 5. Creep Rupture Test Definitions
FIGURE 6. Uniaxial Creep of Annealed Type 304 Stainless Steel in 1800°F Sodium and Helium Environments - 21,000 psi Stress

FIGURE 7. Derivation of Values for Initiation-of-Tertiary Creep
and 316 stainless steel, are an average of Leyda and Rowe $F_S$ values for Types 304, 316, 321 and 347 stainless steel.

When trying to select point A of Figure 5 from an actual creep curve, the authors found it difficult to determine the exact point of initiation of tertiary creep. The analogous situation arose in picking a yield stress, so the authors recommend a similar remedy. By drawing a line parallel to the secondary creep portion of the curve, but offset by perhaps 0.2% strain, then the point B (Figure 5) will be easier to identify. In any event, those who perform creep tests should recognize the technical community's interest in more of the creep curve than has been reported in the past.

The fourth criterion is related to the total strain of uniaxial tensile specimens under constant-load creep tests at a given temperature.

Eighty percent of the average stress which produces 1% total strain after time "t" at the metal temperature.

The above criterion represents a compromise of opinions among people on the ASME Subgroup on Elevated Temperature Design. The criterion was conceived as a means of limiting the deformations that could arise from equilibrium stresses. The basis for the 1% is somewhat related to a criterion used in Section VIII(2) (0.01% secondary creep strain in 1000 hours) which implied 1% total secondary creep strain after $10^5$-hours (roughly the FFTF lifetime at elevated temperatures). In addition, this strain value was believed to be a reasonable limitation for deformations in the type of structures used for nuclear reactor pressure boundaries.

Determination of the average stress-to-produce 1% total strain was a much more difficult process than either of the two previously described properties. Ideally, the procedure is as follows:

Step 1 - Obtain a series of representative creep curves for each temperature and a range of stress levels as depicted in Figure 7.

Step 2 - Using the family of creep curves, obtain the points necessary to construct the isochronous stress-strain curves shown in Figure 8.
FIGURE 8. Derivation of Isochronous Stress-Strain Curves
Step 3 - Drawing a vertical line through a value of 1% strain on the isochronous stress-strain curve, at each temperature permits one to construct a family of curves shown on Figure 9 of log stress versus temperature for 1% strain in 10-to-\(5 \times 10^5\) hours.

Step 4 - At each temperature of log stress versus log time plot is constructed for the stress to produce 1% total strain - Figure 10.

The design values obtained by this method are generally considered invalid at the lower temperatures (less than 1000°F) and lower stress levels. At the lower stress levels, there is always considerable uncertainty in measuring the strains necessary to construct the initial creep curve. At higher stress levels (e.g., shorter times) there is practical difficulty of experimentally measuring the primary creep strains, which represent a significant portion of the total strain.

The procedure actually used for FFTF involved starting with isochronous stress-strain curves developed by Smith in Reference 7. The stress to produce 1% total strain was then obtained as outlined in Steps 3 and 4.

In comparing the above elevated temperature criteria to those used in Section VIII(2), two are absent from the FFTF rules. The use of average stress-to-rupture data is no longer used since design against ductile rupture should use minimum properties of a given alloy grade where enough data exist. Finally, the creep rate criterion of Section VIII was deleted as it: (a) did not directly relate to a specific failure mechanism, (b) was covered by the tertiary creep and 1% total strain criteria, and (c) was difficult to measure in actual tests at the stress ranges of interest. Smith has discussed the problem in detail in Reference 8.

Like Section III, uniaxial data provided the base material properties for FFTF rules, and the flow rule of Tresca was used to relate uniaxial properties to multiaxial stress fields. This procedure has a long history of usage in Section III.(1) For analysis, the Von Mises approach (effective stress) is also acceptable (Figure 11).
FIGURE 9. Extrapolation Technique for 1% Total Strain Values

FIGURE 10. Stress to Produce 1% Total Strain at Temperature, T
3. Cumulative Loading and the Life Fraction Rule

The use of stress limits on primary loads are straightforward for a single long-term load at fixed temperature, but reactors have cycles of both temperature and stress which require the stress limits of the previous section to be able to handle cumulative loads.

For a given loading cycle, the times can be broken down into a variety of "constant-load-at-temperature" time periods. The time "t" in the $S_m$ criteria then is considered to apply to the total accumulated service at a given load and temperature. These primary stresses are then weighted by a linear time fraction summation to properly weight each different load-at-temperature condition:

$$\sum \left( \frac{t}{t_m} \right) \leq 1.0$$

where the time "t" under the $i^{th}$ load is compared to the $t_m$ values from the lower of the two time-dependent $S_m$ criteria, tertiary creep initiation and minimum time-to-rupture (under similar stress and temperature conditions).

Although this summation has the appearance of a rupture damage summation, the intent is only to reasonably control the primary stress intensity at elevated temperatures, where time at load is important.
IV. PROPERTIES RELATED TO STRAIN AND DEFORMATION

1. Background and Development

In Section III\(^1\), the basic material assumption is that a material is ductile. There are no stated limits on the strains but the limitations arise because of stress limit criteria and the design rules themselves. For example, the key premise of the design rules is to assure that, after a few load cycles, no additional inelastic strains will occur. Stresses are expected to cycle between tensile and compression yield strength (Primary-plus-Secondary Stress Limit), thus confirming that a material should have ductility at end of life and that fatigue analysis can use test data in which no gross inelastic strains occur.

Section III does contain a few rules on ratchetting based on one-dimensional models\(^9\). An elevated temperature code case of Section III, #1331 allowed one additional approach -- inelastic analysis. However, no guidelines were included.

Fatigue rules of Section IV have always used an equivalent stress range, even though the low-cycle fatigue data were originally generated by strain range control. In Code Case 1331, the low-cycle fatigue data were extended to 1200°F (650°C), but no consideration of hold time or creep effects was included.

The rules for buckling instability, the most complicated failure phenomena to analyze, are still being developed even in Section III. Present rules apply only to elastic material model analysis.

From this background of ASME Codes, the FFTF rules needed to resolve several key questions about philosophy of rules for design:

- Could elastic material models be used to successfully define the inelastic phenomena? (If so, then limits could be specified.)
- With the current level of analysis practiced by U. S. industry, was it realistic to require some detailed inelastic analysis?
- If the materials data are sparse and do not clearly define the failure mechanism, then should the rules be limited only to words of general guidance for the designer?

These three questions have permeated every discussion between groups of people working on FFTF design rules. The position is not completely clear even in early 1971. However, the largest step has been made -- the FFTF rules recognize that, without an analysis using a realistic and complicated material model, the tests based on elastic material models will not cover all components.

Inelastic analysis will be performed wherever required, on all FFTF critical components prior to reactor operation. If vendors cannot perform these analyses, then other direct government subcontractor organizations will perform them.

Limits or rules will apply to inelastic analysis and the rules will be specific.
FFT-F limiting rules may have large errors, especially when long term loads
can accentuate the errors of strain histories. However, the use of specific
limits will provide FFTF with a starting point for evaluating all FFT-F compo-
nents -- both in design and in operation. The design limits also provide a
more effective way to limit or instill new methods of design. For example, a
better design should result if weld regions are given lower allowable strains
than those in base material -- rather than stating guidance such as, "... the
designer shall consider the lower ductility in weld regions and try to keep
welds out of highly loaded regions."

With the lack of basic materials data for the remaining modes of failure, the
above philosophy decisions should explain some of the arbitrariness of
decisions discussed in the next sections.

2. Material Data for Inelastic Material Models

Requirements on strains from inelastic analysis were deemed feasible because
of recent advances in finite element structural analysis computer programs in
the U.S. In these programs, elastic, plastic and creep behavior effects (on
stress and strain fields) are evaluated for a time increment. With the output
strain history very dependent on material model, the FFTF has also attempted
to specify the material model for inelastic analysis. Only by establishing
a reference constitutive equation for Type 304 or 316 stainless steel material
behavior could one maintain the desired consistency for design analysis on the
entire FFTF. The major problem encountered in this exercise was deriving a
constitutive equation which was compatible with the requirements of the
computer programs. Failure to keep the equation within the constraints can
lead to instability of the output stress and strain and, incidentally, add
much cost to the design analysis efforts.

At the time of writing, the reference equations for the materials of interest
were still being upgraded for FFTF use by a group of researchers at WADCO.
Materials data for these efforts have been from uniaxial tests. Further tests
are continuing at several AEC-supported laboratories.

3. Functional Limits

Certain components (e.g., flanges, valve bodies, in-reactor control rods)
may lose their functional ability from deformation long before the material
actually fractures. This mode of failure is design-dependent, and the buyer
must state these functional limits (to the designer) in the Equipment Specifi-
cation document. In the FFTF, the maximum strain philosophy has been incor-
porated for other failure modes so that functional limits need be mentioned
only if they are less than an already-imposed limit.

For example, 1% total membrane strain is the allowable lifetime limit called
out for primary loads and strain limits. For a region with functional limits,
inelastic analysis can be avoided by assuming that 1% strains occur and in the
most detrimental manner consistent with the loading directions. If the result-
ant deformations meet the functional limits, then the requirement is satisfied.
Otherwise, the functional limit would require the designer to perform further calculations using inelastic material models.

4. Limits on Total Inelastic Strain

From an inelastic analysis, the most direct limit would be on strains, so strain limits are most convenient for elevated temperature rules. However, this same mode of failure has been covered indirectly by one of the criteria for primary loading stress limit and by rules on creep-fatigue interaction (next section). The use of a separate limit reflects a need for:

- controlling strains in weld regions
- analyzing by a more accurate model than the primary stress limit calculations, which can use elastic material models
- backing up for possible unconservative errors in the creep-fatigue rules
- convincing designers to recognize total strain as the key design parameter at elevated temperatures
- relating failure modes to a measurable quantity which can be monitored throughout the life of the components

Setting the actual strain limits depended less on actual data than on a "meeting of the minds" from people around a room. Blackburn's data (Figure 12) shows that long-term service at creep temperatures will lower observed tensile ductilities. The differences between creep and tensile ductilities indicate that time of loading should be important to available ductility. However, the current FFTF position is that a short-term event can set up high residual loads which can lead to long-term creep strains.

The actual limits in use are as follows:

- Total membrane strain limit = 1%
- Limit on total surface strain (from bending plus membrane) = 2%
- Limit on total strains at a local discontinuity (geometric or otherwise) = 5%

These limits reflect categories similar to the stress categories in Section III (e.g., primary, secondary and peak).

Several finite element analyses of typical weld regions have disclosed the significant metallurgical stress concentration at weld regions. Material characterization of welds is very sparse and is complicated by the many additional parameters introduced by the welding process. In the FFTF, considerable effort is being expended to specify weld processes which will give matching weld-base metal properties. The failure mode normally exhibited by welds is failure by tearing in the adjacent base material (e.g., heat-affected zone). The higher strength of the weld is the usual cause for this mode of failure.

Rules for FFTF weld regions state that base material properties will be used for all calculations of strains. However, total strains will be limited to one-half the base material values (shown above). Supporting data are shown by
FIGURE 12. Effect of Prior Creep Deformation on Tensile Elongation of 304 SS
Figure 13 which was obtained from cross-weld-region specimens and should reflect a gross regional behavior corresponding to the analytical model.

In practice, the restrictions on weld regions should emphasize their key role in nuclear reactor structures by forcing careful selection of weld processes and by encouraging the designer to keep welds out of high strain zones.

The FFTF rules also recognized that use of inelastic analysis on a wide scale would be expensive. Simple screening tests were sought to keep the number of inelastic analyses at a minimum for a given component. For the strain limit, the elastic tests depend mainly on the work of Burgreen and Bree which used the plot of Figure 14 for defining the safe region "E." The theory behind these tests assumes that multiaxial strain fields are related to uniaxial test results by "effective" strain concepts. This assumption is more open to question than the use of similar procedures on stress. This is the major input of material property data to the simple screening tests. To be effective in reducing the number of inelastic analyses, the screening tests should allow more than the area "E" of Figure 14. The FFTF rules will utilize such a test as soon as justification is available. The authors recommend Reference 10 a review of these screening tests.

5. Rules for Combined Creep Fatigue

Fatigue cracking at lower temperatures must consider the effects of hold time at elevated temperatures. Most recently, this was illustrated by plotting low-cycle fatigue data generated by Conway, Berling, et al, at GE-NPS (Figure 15)(11).

In February 1969, the first proposed rules for the 1000°F (538°C) FFTF reactor vessel included the familiar linear creep-fatigue relation.

\[ \sum_i \left( \frac{t_i}{\tau_{\text{rupture}}_i} \right) + \sum_j \left( \frac{n_j}{n_{\text{failure}}_j} \right) \leq 1 \]

The first term represented rupture damage; and the second, fatigue damage. Note that there was no dependence on the order of events or dependence on nonlinear effects.
FIGURE 13. Variation of Creep Ductility with Rupture Life for Selected Austenitic Stainless Steel Submerged-Arc Weldments

FIGURE 14. Stress Regimes for One-Dimensional Element
FIGURE 16. Limits for Creep-Fatigue Interaction Using Inelastic Analysis

\[
\sum_{i=1}^{p} \left( \frac{\varepsilon_i}{\tau_{D_i}} \right) \text{ OR } \sum_{k=1}^{q} \left( \frac{n_k}{N_k} \right)
\]

FIGURE 17. Basic Fatigue Data Plot
A later equation expressed the above creep-fatigue interaction in the more general form\(^{(12)}\):

\[
\Sigma_i \left( \frac{n}{N_i} \right)^u_i + \Sigma_j \left( \frac{t}{t_{r,j}} \right)^v_j \leq \kappa_k
\]

where the possible beneficial (recovery processes) or detrimental interaction of creep and fatigue is handled by the constant, \(\kappa_k\), and the exponents, \(u, v\). The rate, cyclic shape and holdtimes are to be evaluated by the constants, \(\alpha, \beta\) in the equation.

For the FFTF, the general form was simplified to the following:

\[
\sum_i \left( \frac{t}{t_{r,i}} \right) + \sum_j \left( \frac{n}{N_{d,j}} \right) \leq D
\]

where \(D\) (Figure 16) was to remove the unconservatism of the linear equation due to nonlinear creep-fatigue interaction effects. The \(t_{r}\) term is related to 100% of the stress-to-rupture. \(N_d\) was to provide the design fatigue curve -- and in this case, several curves in order to incorporate dependence of fatigue damage on strain rates.

Code Case 1331-4 of Section III provided fatigue design curves for use in elastic analysis. To perform an inelastic analysis for FFTF, fatigue design curves of total strain range versus cycles to failure, as a function of strain rate, were required. Using the same body of fatigue data as used by the ASME Code groups, plots of temperature versus log cycles-to-failure were prepared for each material and strain rate, as a function of axial strain range (Figure 17). Data from a family of curves such as shown on Figure 7 were used to construct "representative" fatigue curves (log axial strain range versus log cycles to failure) for each family of materials, at each strain rate and at each temperature, Figure 8. The customary factors of safety used by the ASME Code groups, e.g., 20 on cycles to failure and 2 on axial strain range, were used to produce the "design" fatigue curve for the family of materials, strain rate and temperature, shown also on Figure 18. For each temperature a series of these curves were combined to show the effect of strain rate as shown on Figure 19.
**Figure 18.** Fatigue Design Curve Generation

**Figure 19.** Design Curve for Fatigue
6. Buckling Instability Limit

The rules of past nuclear codes (1, 2) were recognized as limited to time-independent buckling modes. With creep and stress-relaxation, the initial shape and load distribution in the structure will change during life. Buckling instability failure depends on both quantities, so an analytical approach must consider the entire load history with inelastic material models. If geometry and load distributions can be estimated by inelastic material models, then the buckling instability analysis, using elastic material models, could be made for any particular stage of life.

The complexity of this theoretical approach again needed to be offset by an alternative approach. For this, an experimental approach was the only reasonable alternative with a stated factor of safety. Currently, the FFTP project is using the factors of safety shown in Table 1.

<table>
<thead>
<tr>
<th>Type of Material Model</th>
<th>Theoretical Load F.S.</th>
<th>Experimental Load F.S.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Elastic</td>
<td>3.0</td>
<td>2.5</td>
</tr>
<tr>
<td>Elastic-Plastic</td>
<td>2.5</td>
<td>2.0</td>
</tr>
<tr>
<td>Elastic-Plastic-Creep</td>
<td>2.5</td>
<td>2.0</td>
</tr>
</tbody>
</table>

(and F. of S. = 10 on time) (and F. of S. = 10 on time)

No other novel material test data are used for this failure mode.
V. IRRADIATION CONSIDERATIONS

All structural components within the FFTF core (e.g., fuel clad, fuel duct, control rod) are relatively short lived components (~1 year) and rules for their design have been outside the main efforts of the authors. Other papers at this conference will describe guidelines for design with irradiation-induced creep and swelling.

Suffice it to state that design of in-core structures have placed major emphasis on strain and deformation limits ... stress limits have been relatively unimportant.

Irradiation-induced effects quickly decrease outside the core area because of lowered neutron fluence -- particularly high-energy fluence. In this environment, several important structural components (core lateral restraint device, duct support pads, core support plate, core hold down structure) must have lives comparable to those of primary coolant system components. The FFTF has concentrated on the material property of ductility to provide the measure of irradiation damage.

Ductility indicates to the structural designer the ability of the metal to flow plastically before fracture. "Although ductility measurements are not used qualitatively in design, a high tensile ductility shows that the metal is "forgiving" and likely to deform locally without fracture should the designer err in the stress calculation or the prediction of service loads\(^\text{[13]}\). Therefore, adequate ductility is an essential requirement for primary coolant boundary materials. For austenitic stainless steels, a residual total elongation capability of 10\% in a uniaxial test is arbitrarily defined as a point below which the materials no longer behave in a ductile manner when subjected to multiaxial stresses. The decision to select 10\% total elongation as an irradiation damage limit was derived from the reference\(^\text{[14]}\) which classified metals into three categories on the basis of total elongation: adequate - 10\% to 15\%; limited - 1\% to 10\%; and brittle - less than 1\%.\)
VI. MASS TRANSFER CONSIDERATIONS

Sodium coolant in a reactor heat transfer loop sets up a diffusion and mass transfer mechanism whereby carbon and nitrogen are diffused from the structural material in the hotter parts of the loop and deposits these elements onto structural elements in colder regions of the loop. With carbon (and perhaps nitrogen) being an important strengthening agent in stainless steels and alloys, the designer must consider these phenomena in design rules for hot components in the loop (15). Furthermore, these elements may degrade the particular cold leg components when the carbon (in form of chromium carbide) and nitrogen deposit on the surfaces of the structures.

For carbon diffusion, the present assumption is that the sodium of the FFTF primary loops are carbon-saturated systems due to the highest temperatures occurring in the large exposed surface area region in and above the core. In saturated sodium, the bulk of the diffusion occurs where the fluid temperatures are increasing (e.g., the core) and hot enough to have an appreciable diffusion coefficient (Figure 20). In FFTF, the core structure is comprised of short-lived structures at temperatures up to \( \sim 1150^\circ F \) \( (\sim 621^\circ C) \). The only other rising fluid temperature region is the long-lived structure at, and immediately downstream of, the outlet nozzles where bypass coolant flow re-enters the main coolant stream (temp = 1050°F). For these regions, the only way to keep from degrading the normal design values for 304 stainless steel material is to insure high enough initial carbon content so that the wall-averaged carbon content will not fall below the minimum specified for the alloy grade (0.04% for 304 S.S.). The hot leg piping for the FFTF would be able to avoid any decrease in design values by specifying carbon range above 0.06%.

Nitrogen diffusion phenomena are relatively untested as to their effect on strength of 304, 316 S.S. Since the alloy grade specification does not have a minimum nitrogen content, any diffusion of nitrogen does not degrade the minimum properties specified for design -- thus, the FFTF design will not degrade design properties. The core, which is the principal area of expected diffusion, has structural material already specified as low nitrogen (<0.01%). The reason is unrelated to nitrogen diffusion since the specification was intended to reduce gas formation from the interaction of nitrogen and neutron flux. The major impact on FFTF design from nitrogen diffusion has been the restrictions on the use of nitrided hard-facing surfaces in the hot coolant regions of the primary loop.

In the cold leg of the primary loop, carbon (in the form of chromium carbide) is expected to deposit on the heat exchanger tubes. Although some loss of impact strength is anticipated, there is little evidence to evaluate the important area of fatigue strength effects. At present, the FFTF design rules give no guidance in this area. Experimental investigation of the phenomena is recommended for this problem.
FIGURE 20. Diffusion Coefficient for Carbon into Liquid Sodium
VII. FUTURE DEVELOPMENTS

When the FFTF begins operation, strain limits will make a convenient reference point for measuring the effects of any unusual accident. Provided one knows the temperature and load history during the event, the limits on strain have "a priori" set the standard for answering two key questions:

. Is it safe to again use the component?
. If so, for how long is it safe?

One area not touched in the FFTF structural design rules was the use of fracture mechanics in initial design. Fracture mechanics are relied upon only after a crack is found in an operating component.* The design rules assume no cracks exist -- an obvious simplifying assumption that was made for one or more of the following reasons:

. Past ASME Codes made the same assumption.
. Crack propagation at elevated temperatures in multiaxial stress fields are inadequately described by theory and data.
. Certain officials are hesitant to make the assumption that cracks may exist even at start of life in their carefully designed, fabricated, and tested materials.

In developing the structural design rules for FFTF, it became clear that the present design rules for many material phenomena could benefit from further study. Materials information development programs have already started in the U.S. to achieve the following goals:

. Establish more accurate constitutive equations for structural materials.
. Enlarge the data basis for the stress and strain criteria (for base material).

*In the FFTF, fracture mechanics has been used to try to answer an important design and safety question: "Given a small crack in piping, will it become large enough so that FFTF instrumentation can detect the sodium leak before the crack undergoes rapid propagation?"
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. Expand upon the sparse data now available on weld properties.
. Improve the capacity and capability of computer codes used for three-dimensional structural analysis.
. Confirm the appropriateness of computer codes and material models by performing reference tests which give strain histories on actual structural shapes under cyclic and thermal loads.
. Investigate the areas of ratchetting and creep-fatigue to develop new simplified test methods.

In the future -- perhaps by 1980 -- elevated temperature structural design rules can be limited to inelastic analysis of strains in a few key regions using an alloy's constitutive equation and the expected histogram for the component. The computed strain history can then be evaluated against the strain and deformation limits. The redundancies between creep-fatigue rules and the two limit areas of primary stress and total strain will be minimized because of improved confidence in the rules for evaluating creep-fatigue.

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The authors would like to express their appreciation to members of the ASME Subgroup on Elevated Temperature and to Messrs. J. C. Tobin, D. P. O'Keefe, W. F. Brehm and A. L. Snow of the Westinghouse professional staff for their willingness to assist the authors in understanding the many technical areas encompassed by FFTF structural design rules.
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2. ASME B & PV Code, Section VIII, Division 1, Pressure Vessels, American Society of Mechanical Engineers (1971 Edition).


DISCUSSION

A. PHILLIPS, U. S. A.
In the creep curve the initiation of tertiary creep initiates also the destruction of the material and together with a maximum strain is the design criterion.

M. T. JAKUB, U. S. A.
We seem to agree. Both factors were used for criteria in establishing the allowable limits for primary stresses. Further, limits on total accumulated (lifetime) strains are imposed on all structural materials.

K. R. KNOWLES, U. K.
Stress concentration, introduced by an earlier questioner, is catered for in ASME Section III code as a peak stress in fatigue criterion. Now in fatigue, the degree of roughness of the surface has a known and large effect on the initiation and propagation of cracks. However, the code makes no special allowance for the improved fatigue resistance arising from a surface made specially smooth, even polished. Did these considerations influence your settling of the criteria?

M. T. JAKUB, U. S. A.
Unlike low-temperatures fatigue, the fatigue data at high temperatures did not allow any conclusions pertaining to the effects of surface finish. As a result, such effects are assumed to be covered by the customary factors of safety which are applied to average fatigue data curves in order to establish "design" fatigue curves.