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ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
Argonne, Illinois 60439

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FUSION-COMPONENT LIFETIME ANALYSIS

by

Richard F. Mattas

Fusion Power Program

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LIFETIME ANALYSIS OF FUSION REACTOR COMPONENTS

by

Richard F. Mattas

ABSTRACT

A one-dimensional computer code has been developed to examine the lifetime of first-wall and impurity-control components. The code incorporates the operating and design parameters, the material characteristics, and the appropriate failure criteria for the individual components. The major emphasis of the modelling effort has been to calculate the temperature-stress-strain-radiation effects history of a component so that the synergistic effects between sputtering erosion, swelling, creep, fatigue, and crack growth can be examined. The general forms of the property equations are the same for all materials in order to provide the greatest flexibility for materials selection in the code. The individual coefficients within the equations are different for each material. The code is capable of determining the behavior of a plate, composed of either a single or dual material structure, that is either totally constrained or constrained from bending but not from expansion. The code has been utilized to analyze the first walls for FED/INTOR and DEMO and to analyze the limiter for FED/INTOR.

1.0 INTRODUCTION

Lifetime analyses for fusion reactor systems have been performed over the years as part of the design studies for the different reactor systems that have been proposed. Most of these analyses have emphasized particular aspects of the component lifetimes. For example, early studies emphasized radiation effects, particularly swelling and ductility loss.^(1,2) The design limits on swelling range from ~ 2-10%, and the limits on uniform elongation range from ~ 0.5 to 2%. The radiation limits were used since they depended only on the operating temperatures and neutron fluence, and they did not require detailed analysis of the material stresses or the operating scenario. Other studies have emphasized the mechanical property limitations, such as fatigue, thermal creep, and creep-fatigue interactions.^(3,4) For example, the stresses and strains associated with cyclic operation were calculated, and the fatigue lifetime was determined by comparison with laboratory generated fatigue curves. In work by Mattas and Smith, both radiation effects and mechanical property effects were considered. However, individual property changes were considered to occur independently of other property changes, and component design dependence was not considered in the analysis.⁽⁵⁾ A study by Cramer, et al, also examined both radiation effects and mechanical property changes.⁽⁶⁾ In addition, component design thermal and structural analysis was an integral part of the study.

More recent studies have examined the synergistic effects that occur during operation.⁽⁷⁻¹⁰⁾ In these studies the interaction of properties such as swelling, radiation creep, crack growth, and fatigue have been analyzed in considerable detail. The material most often studied is austenitic stainless steel because it is the most likely structural material in near term devices and it has the largest data base from which to draw. The most complete work in this area was done by Watson,⁽⁹⁾ who examined 20% cold-worked Type 316 SS. In addition to the properties mentioned above, the effect of surface erosion and the effect of radiation on crack growth behavior were also incorporated into the model.

A major consideration of any lifetime study is the choice of the design and end of life criteria. The design code most often employed in fusion component analysis is Section III, Division I of the ASME boiler and Pressure

Vessel Code (Code Case 1592 for Class I components for elevated temperature service). The code is designed to protect components from six failure modes: 1) ductile rupture, 2) gross distortion from buckling and incremental collapse, 3) fatigue failure, 4) creep rupture, 5) loss of function due to excessive dimensional changes, and 6) creep buckling.⁽⁸⁾ The ASME Boiler and Pressure Vessel Code does not, however, contain guidelines for radiation damage, and at this time there are no general guidelines that have been established for radiation effects in fusion components. The end of life criteria are the values for the minimum acceptable materials properties and the maximum allowable changes allowed in component performance. Properties and changes included in the end of life criteria are mechanical strength, ductility, dimensional change (swelling and creep), fatigue damage accumulation, and creep failure limits. The end of life criteria would be used to establish operating lifetimes so that components could be replaced prior to catastrophic failure. Again, there are presently no generally accepted guidelines for the end of life criteria.

The present work was undertaken to study the behavior of first wall and limiter/divertor components. The approach used is to calculate the synergistic effects of the various materials properties in a manner similar to previous studies. In this way, the time evolution of the component stresses and strains along with changes in the materials properties could be studied. Two major modifications have been made to this type of materials modeling. First, the model is designed to analyze the behavior of several different materials rather than just a single material. The material property equations have been formulated to be flexible enough so that only the coefficients need to be changed for different materials. Second, the model was designed to examine both single and duplex structures. Many first wall and limiter/divertor designs use a duplex structure where a non-structural material, exposed to the plasma, is attached to a heat sink. The response of such a structure to the fusion environment is expected to be considerably different from a single material. These two modifications allow for greater flexibility to the user in analyzing various fusion components.

The paper is divided into three major sections. The first section discusses the different areas that impact component lifetimes. These areas are the reactor operating conditions, the materials properties, the component geometry, and the design and failure criteria. The second section describes

the model structure and the materials property equations. The third section presents the results of model calculations for the FED/INTOR and DEMO first walls and for the FED/INTOR limiter.

2.0 LIFETIME CONSIDERATIONS

In general, fusion component operating lifetimes depend on the interactions of the:

- 1) Reactor operating conditions
- 2) Materials properties
- 3) Component geometry and constraints
- 4) Design and failure criteria

All of these areas must be included in any lifetime determination. This section examines each area in detail.

2.1 Reactor Operating Conditions

The relevant reactor operating conditions include the neutron wall loading, the surface heat flux, the fluxes and energies of particles escaping the plasma, the burn cycle frequency, the coolant selection, and the coolant temperature. The expected range of operating conditions for FED, INTOR, DEMO, and STARFIRE are presented in Table 1.⁽¹⁰⁻¹³⁾ The temperature distribution through a particular component is determined by the neutron volumetric heating, the surface heating, the coolant temperature and heat transfer characteristics, and the materials' thermophysical properties. The neutron current also produces lattice displacement damage which can lead to swelling, hardening, and embrittlement of the materials. The particles impinging on the components exposed to the plasma are responsible for sputtering and erosion of the surfaces. The burn cycle characteristics and frequency determine the operating stress range and fatigue requirements. Table 1 indicates that the neutron wall loading and surface heat flux increase in going from a near term device (FED) to a commercial reactor (STARFIRE). The integrated neutron dose also increases. It is hoped that commercial devices will operate in a steady state mode in order to alleviate the fatigue problem.

There are a number of off-normal events, such as disruptions and loss-of-coolant accidents, which can reduce the lifetimes of components. The unpredictable nature of these events makes them difficult to characterize. However, in the case of disruptions, an attempt has been made to establish their frequency, energy density, and time duration. The reference disruption conditions for INTOR are shown in Table 2.⁽¹⁴⁾ Disruptions can result in

Table 1. Operating Parameters of Proposed Fusion Reactors

Parameter	FED		INIOR	DEMO	STARFIRE
	8 Tesla	10 Tesla			
Neutron Wall Loading (MW/m ²)	0.4	1.0	1.3	1.8	3.6
Surface Heat Flux (MW/m ²)					
First Wall	0.04	0.05	0.11	0.25	0.85
Limiter/Divertor	<2.5	<3.1	<2.4	<2.4	<4.0
Plasma Edge Temp (eV)	400	390	150	150	1200
Particle Current to First Wall/Limiter (s ⁻¹)	5 x 10 ²³	8 x 10 ²³	3.3 x 10 ²³	6.7 x 10 ²³	3.75 x 10 ²²
Lifetime Neutron Fluence (MW-y/m ²)	~0.6	~0.6	~6	~10	~20
Coolant	Water	Water	Water	Water	Water
Coolant Temperature (°C)	<100	<100	<100	280	300
Burn Cycle Characteristics					
Ramp Time (s)	12	12	11	~ 20	1440
Burn Time (s)	100	50	100/200	Cont.	Cont.
Dwell/Shut Down Time (s)	40	40	45	—	1440
Total Reactor Cycles	2.5 x 10 ⁵	2.5 x 10 ⁴	7 x 10 ⁵	<10 ³	<10 ³

Table 2. INTOR Disruption Conditions

	Limiter	Reference Divertor	1st Wall	Limiter	Worst Case Divertor	1st Wall
Major Disruptions						
Energy (J/cm ²)	270	230	175	535	230	175
Time (ms)	20	20	20	5	5	5
Vapor Shield	Yes	Yes	Yes	No	No	No
Minor Disruptions						
Energy (J/cm ²)	170	170	-	170	170	-
Time (ms)	20	20	-	5	5	-
Vapor Shield	Yes	Yes	-	No	No	-

Reference Frequency

	Major Disruptions	Minor Disruptions
Stage I (y ⁻¹)	151	300
Stage II (y ⁻¹)	32	155
Stage III (y ⁻¹)	63	310

vaporization and melting of exposed surfaces leading to accelerated erosion rates. The effects of disruptions are greatest for the lower melting point materials such as Be and stainless steel, and the lifetime of the first wall/impurity control components can be greatly reduced. Other off-normal events have not been sufficiently characterized to estimate their influence on lifetime. Less obvious factors that can influence lifetimes are the length and frequency of maintenance operations and the procedures used for ramping the reactor up and down from full power.

Not only is it important to know what reactor events can influence lifetimes, but it is also important to know when these events occur in the reactor history. For example, a temperature transient that occurs early in the reactor history is likely to have less effect on the materials than the same transient later in the reactor history. Highly irradiated structures have a lower ductility and are less able to accommodate strains prior to failure.

2.2 Material Properties

There are a large number of material properties which can influence the lifetime of reactor components, as shown in Table 3. The properties included are thermophysical, mechanical, neutronic, corrosion, and sputtering. In general, the various properties are not completely independent, and therefore synergistic effects must also be considered. No analysis to date has included all of the properties shown in Table 3. In some cases, a particular property is believed to be of secondary importance, and in other cases, the relevant property data do not exist. Often large extrapolations of existing data or the use of properties data from a similar material may be required to make a lifetime estimate. When no data exist, a property may be modeled theoretically or a best estimate of the property values must be made. Of particular importance is the lack of data on the effects of 14 MeV neutrons. At present, radiation effects data are drawn from fission reactor experiments and ion implantation experiments, and thus the influence of the fusion neutron spectrum can only be estimated. Most of the existing irradiation data have been obtained using fission reactor materials such as Types 316 and 304 austenitic stainless steels and advanced cladding and duct materials such as HT-9. The amount of radiation data for other fusion candidate materials are

Table 3. Properties Influencing Materials Lifetimes

Property	Important Variables	Influence on Lifetime
Thermal Conductivity	Temperature, fluence	Temperature distribution
Thermal Expansion	Temperature, fluence	Stress distribution
Specific Heat	Temperature	Rate of temperature and stress change
Poisson's Ratio	Temperature	Stress distribution
Elastic Modulus	Temperature, fluence	Stress distribution
Electrical Resistivity	Temperature, fluence	Electromagnetic forces
Yield Strength	Temperature, fluence, strain rate	Stress distribution and ability to withstand off normal events
Ultimate Tensile Strength	Temperature, fluence, strain rate	
Uniform Elongation	Temperature, fluence, strain rate	
Total Elongation	Temperature, fluence, strain rate	
Fracture Toughness	Temperature, fluence, strain rate	Dimensional changes and stress changes during operation, creep rupture
Swelling	Temperature, fluence, neutron spectrum	
Creep	Temperature, fluence, stress, flux	
Fatigue	Temperature, fluence, stress, burn cycle frequency, strain range	
Crack Growth	Temperature, fluence, stress intensity, burn cycle frequency	Crack propagation and failure
Corrosion	Temperature, fluence, chemistry, environment	Flow blockage, wall thinning, embrittlement
Stress Corrosion Cracking	Temperature, fluence, chemistry, stress, stress intensity, microstructure	Crack propagation and failure
Transmutations	Neutron spectrum, neutron fluence	Potential influence on other properties
Sputtering	Temperature, particle species, particle energy	Wall thinning and defect initiation

sparse. The lack of a comprehensive material data base imposes a significant uncertainty in the lifetime estimates.

2.3 Component Geometry and Constraints

The component design will influence the temperature and stress distributions in the material and thus will influence component lifetimes. The implications of this statement are that the materials lifetime can be enhanced by judicious design, and, moreover, that the optimum design for one material may represent a poor design for another material. The lifetime analysis must therefore consider a specific design, and the estimated lifetime is expected to be valid only for that design. A comprehensive analysis should examine the parameter and property variations in three dimensions, and the interactions of different reactor components should also be considered. For example, the creep and swelling experienced by one component may result in unintended contact with a second component which could reduce the operating lifetime of either or both.

2.4 Design and Failure Criteria

Present reactors are designed to insure that designated stress limits are not exceeded. For this purpose, design stress codes have been developed to serve as guidelines in the stress analysis. Fusion reactors will ultimately be designed using such a stress code, and several fusion reactor analyses have used the methods of ASME pressure vessel code case 1592 as the basis for their stress and lifetime estimates.^(4,15,16) This section will address the questions of stress analysis and the use of pre-existing stress codes in performing the lifetime analysis.

The ASME Code Case 1592 for Class 1 Components in Elevated Temperature Service provides design criteria for components in service at temperatures where creep and other time dependent effects may be significant.⁽¹⁷⁾ The code case is intended to cover a wide variety of design configurations and service conditions and the design criteria are therefore usually quite conservative. The various possible modes of failure which confront the designers are:

- 1) Excessive elastic deformation and elastic instability.
- 2) Excessive plastic deformation.
- 3) Brittle fracture.

- 4) Stress rupture/creep deformation
- 5) Plastic instability - incremental collapse.
- 6) High Strain - low cycle fatigue and creep fatigue.
- 7) Stress Corrosion
- 8) Corrosion fatigue

The code imposes various limits on stresses and strain in order to avoid the above modes of failure. The designer must consider, in addition to setting limits for allowable stress, some adequate and proper failure theory in order to define how the various stresses in a component react and contribute to the strength of that part.

Since different types of stress have different degrees of significance, the code assigns a different allowable stress for each stress classification. The allowable stresses are given in terms of the stress intensity, S_{mt} , which is calculated from the tensile and creep properties of the structural materials. The criteria for calculating S_{mt} are given in Table 4.⁽¹⁵⁾ At lower temperatures the value for S_{mt} is a fraction of either the tensile yield or ultimate stress, and at high temperatures S_{mt} is the stress necessary to produce a given amount of creep during the expected component lifetime.

The classes of stresses that are considered by the design codes are the primary stresses, secondary stresses, and peak stresses.⁽¹⁸⁾ A primary stress is a stress developed by the imposed loading which is necessary to satisfy the laws of equilibrium between external and internal forces and moments. The basic characteristic of a primary stress is that it is not self-limiting. If a primary stress exceeds the yield strength of the material, the prevention of failure depends entirely on the strain hardening properties of the material. The primary stresses considered are the primary membrane stress, P_m , local primary membrane stress, P_{ℓ} , and the primary bending stress, P_b . A secondary stress is a stress developed by the self-constraint of the structure. It must satisfy an imposed strain pattern rather than being in equilibrium with an external load. The basic characteristic of a secondary stress is that it is self limiting. A peak stress is the highest stress in the region under consideration. The basic characteristic of a peak stress is that it causes no significant distortion and is objectionable mainly as a possible fatigue source. Fatigue analysis is included in the design code and consists of the

Table 4. Structural Design Criteria

Time-independent Allowable Stress: S_m (other than bolting)	Lowest value of	$\left\{ \begin{array}{l} 1/3 S_u \text{ at room temperature} \\ 1/3 S_u \text{ at temperature} \\ 2/3 S_y \text{ at temperature} \end{array} \right.$
Time-dependent Allowable Stress: S_t	Lowest value of	$\left\{ \begin{array}{l} 2/3 \text{ of minimum stress to} \\ \text{cause creep rupture in} \\ \text{time } t \\ 80\% \text{ of minimum stress to} \\ \text{cause tertiary creep in} \\ \text{time } t \\ \text{Minimum stress to produce} \\ 1\% \text{ strain in time } t \end{array} \right.$
General Primary-membrane Allowable Stress: S_{mt}	Lesser of S_m and S_t at temperature and time.	

S_y = Material Yield Strength

S_u = Material Ultimate Strength

calculation of the induced strain per cycle, the number of cycles expected for the component during its lifetime, and finally a comparison of those values with fatigue curves given in the code. Safety factors of two in stress or twenty in cycles to failure are built into the code design curves.

The potential failure modes and various stress categories are related to the code provisions as follows:

- 1) The primary stress limits are intended to prevent plastic and/or creep deformation and to provide a nominal factor of safety on the ductile burst pressure and creep rupture.
- 2) The primary plus secondary stress limits are intended to prevent excessive plastic deformation leading to incremental collapse or ratchetting, and to validate the application of elastic analysis by ensuring shakedown when performing the fatigue evaluation.
- 3) The peak stress limit is intended to prevent fatigue failure as a result of cyclic loading.
- 4) For elevated temperature designs the code requires special considerations to prevent failure by creep-fatigue interaction.
- 5) Special stress limits are provided for elastic and inelastic instability.
- 6) Protection against brittle fracture is provided by material selection, rather than by analysis.
- 7) Protection against environmental conditions such as corrosion and radiation effects are the responsibility of the designer, but procedures for protecting against these effects are not provided in the code.
- 8) In addition to stress limits, the code also requires the maximum accumulated inelastic strain not to exceed prescribed values.
- 9) The code allows the same stress limits for welds as for the base metal. However, the accumulated inelastic strain limit for welds is prescribed to be half of that of the base metal.

The maximum allowable stress intensities for the various categories of stress are given in Table 5.⁽¹⁸⁾ The primary membrane stress is limited to a value of S_m , whereas the sum of the primary and secondary stresses at low temperatures, including thermal stresses is limited to a value of $3S_m$. The value of $3S_m$ has been suggested assuming that the structure will shakedown to elastic-strain shortly after startup operation of the component. When peak stresses are included, the allowable stress is $2S_a$, where S_a is the allowable

Table 5. Stress Categories and Maximum Allowable Stress Intensities Current Design Codes

Category	Included in Category	Maximum Allowable Stress Intensity
S_1	P_m	S_{mt}
S_2	$P_m + P_b + Q_m(s)^*$	$(3/2)S_m$ $K_t S_t$
S_3	$P_m + P_b + Q_m(s) + Q_m(t) + Q_b(s) + Q_b(t)$	$3S_m$
S_4	$P_m + P_b + Q_m(s) + Q_m(t) + Q_b(s) + Q_b(t) + F_t + F_c$	$S_r = 2S_a$

* $P_\ell = P_m + Q_m(s)$ = Primary local membrane stress

P_m = primary mean stress.

P_b = primary bending stress.

$Q_m(s)$ = secondary mean stress due to sustained loads.

$Q_m(t)$ = secondary mean stress due to temperature.

$Q_b(s)$ = secondary bending stress due to equivalent linear temperature distribution.

F_t = peak stress due to temperature--free thermal stress.

F_c = peak stress due to highly localized stress concentration, or highly localized hot spots.

S_a = allowable stress amplitude for any given number of cycles, N

S_r = $2S_a$ = stress range

$K_t = 1 + K_s \left\{ 1 - \frac{P_m + Q_m(s)}{S_t} \right\}$
($K_s = 0.25$ for shell-type structures)

stress amplitude calculated from the design fatigue curves. It should be noted that neither corrosion nor radiation effects have been specifically considered in establishing these stress limits. In most cases, the safety margins built into the design codes will allow for changes due to corrosion or radiation damage, but in some cases the allowable stress limits could be non-conservative. The design codes also do not consider crack growth from a pre-existing defect or crack. For some designs, crack growth is expected to limit the lifetime of a reactor component. Although such an approach is highly desirable, the applicability of linear elastic fracture mechanics to analyze crack growth in the thickness direction of a thin creeping solid is questionable. The elastic-plastic fracture mechanics approaches for crack growth are still undergoing development and are not at a stage to be used in design.

One effect of radiation that has been included in the design codes is the irradiation-induced changes in the materials' fracture toughness.⁽¹⁸⁻²⁰⁾ Several alloys, including HT-9, are known to exhibit a ductile-brittle transition temperature (DBTT) below which the material acts in a brittle fashion and which has been shown to be increased by irradiation.⁽²¹⁾ The brittle behavior of these materials can be described by the change in the tensile, the impact, or the crack growth properties, and the design codes provide guidelines for each of these three properties. In all cases, the allowable limits are related to the nil ductility temperature (NDT) which is defined as the temperature at which the fracture stress for specimens with small flaws is equal to the yield stress for specimens with no flaws. When the tensile properties are used for the prevention of brittle fracture the guidelines are:⁽¹⁸⁾

- 1) For $T \leq \text{NDT}$, the maximum allowable stress is 41 MPa (6 ksi).
- 2) For $T > \text{NDT} + 17^\circ\text{C}$ (30°F), the maximum allowable stress is 1/2 the yield strength.
- 3) For $T > \text{NDT} + 33^\circ\text{C}$ (60°F), the maximum allowable stress is the yield strength.
- 4) For $T > \text{NDT} + 67^\circ\text{C}$ (120°F), the maximum allowable stress is the ultimate stress.

The impact properties of materials are usually determined using the Charpy V-notch impact test that measures the energy required for rapid fracture of

standardized specimens. The design code for pressure vessels specifies that at $T = NDT + 33^{\circ}\text{C}$ (60°F), the impact energy should be at least 67.8 J (50 ft-lbs) and that the lowest service temperature should be $> NDT + 56^{\circ}\text{C}$ (100°F).

If crack growth is to be used as a design guideline, the ASME Pressure Vessel Code prescribes procedures to be used for evaluating the stress intensity of an assumed defect. The calculated stress intensity must be less than the stress intensity at the temperature of operation that is given in a design curve that incorporates the data of several pressure vessel steels. At the NDT the calculated stress intensity must be $< 44 \text{ MPa}\cdot\text{m}^{1/2}$ to prevent brittle failure. The assumed effect of radiation for all types of analysis is to increase the NDT while leaving the shapes of the design data curves unaffected. Thus as the NDT increases, the maximum allowable stress, impact energy, or stress intensity at a given temperature will decrease.

A design code for fusion reactors is likely to contain several changes from the present design codes because of the additional influence of radiation on several properties. Long term dimensional changes are considered in Code Case 1592 to occur via thermal creep. However, dimensional changes in fusion components will occur primarily via swelling or radiation creep, and significant changes are expected to occur at temperatures and stresses far below those where such changes would occur by thermal creep. Restriction of the stresses to values that produce a strain allowed by the design codes could result in a low value for the allowable primary stress. Radiation damage will also result in significant changes in the yield and ultimate strengths of the materials. It is not obvious that the allowable stresses which are based upon tensile strength values should be allowed to increase with time. Fatigue of structural materials may be influenced by radiation. In the high cycle range where the strain is elastic, radiation may increase the number of cycles to failure, while in the low cycle range where the strain is both elastic and plastic, radiation may reduce the number of cycles to failure.⁽²²⁾ In the absence of a design stress code for fusion reactors, present codes such as ASME code case 1592 provide useful guidelines for component design. It should be recognized, however, that in some cases use of the present design codes may result in overly conservative designs while in other cases their use could result in non-conservative designs. The addition of radiation effects as an

integral part of the stress analysis will be essential for the realistic appraisal of fusion reactor structural components.

Failure Criteria

The lifetime failure criteria are the limits imposed on the materials properties beyond which a component is considered to fail. Previous design studies have identified four modes of failure:⁽²³⁾

- Leaks - Coolant penetration into the plasma which results in the introduction of excessive impurities into the plasma. The mechanical properties that are usually important are crack growth rate, fatigue and creep rupture.
- Fracture - Gross rupture of the wall producing disruption of coolant flow, loss of vacuum, or flow of coolant into adjacent areas. In general, fracture is most likely to occur during an off-nominal situation such as a plasma disruption which produces high loads in fractions of a second. The important mechanical properties are tensile and shear strength, ductility and fracture toughness, and creep-rupture.
- Deformation - Structural material exceeds a total allowable strain as defined by structural symmetry or a design code such as the ASME Nuclear Pressure Vessel Code. Important mechanical properties are thermal creep and irradiation creep; however, the amount of swelling or irradiation creep that can be tolerated without causing problems has not been defined.
- Instability - General instability or buckling of structural components, local buckling, or wrinkling of structural members. Important mechanical properties are the moduli of elasticity and shear (rigidity).

In most cases, cracking leading to leaks is predicted to be the predominant failure mechanism. The cracking problem is a particularly important consideration for reactors which operate in the cyclic mode. Fracture failure is of greatest concern during off-normal events. Irradiation-induced loss of ductility will have a major impact in the analysis of fracture failure during such off-normal events. While it is obvious when a component has failed by cracking or fracture, it is less obvious when a component has failed due to deformation. The allowable dimensional change is determined by how much change can be accommodated before reactor operation is affected or it becomes difficult or impossible to replace that component. The allowable dimensional change is therefore likely to be related to the design

and function of the various components. One component may be able to tolerate only a 1% dimensional change, whereas another component may be able to tolerate a 10% change.

To date, a set of failure criteria has not been established for fusion components. This statement reflects the early stage of development of fusion devices. As more detailed designs are developed, as normal and off-normal conditions are more firmly established, and as additional material property data become available, then more reasonable estimates for the failure criteria can be made. The establishment of both design and failure criteria will be an evolutionary process which is likely to continue through to the operation of commercial reactors.

3.0 LIFETIME MODEL

3.1 Introduction

The analysis of component lifetimes must incorporate the operating and design parameters, the material characteristics, and the appropriate failure criteria. It should also incorporate the time-temperature-property history of the component so that synergistic effects between erosion, swelling, creep, fatigue, and cracking can be examined. This section describes a one-dimensional lifetime model which incorporates these quantities. The model was developed specifically for first wall and impurity control components.

3.2 Model Overview

The major emphasis of the modeling effort was to examine component response for a wide variety of operating conditions and materials. For this purpose, a one-dimensional model of a plate is believed adequate, although the model can, in principle, be adopted to 2 or 3 dimensional calculations. The material property equations are the same for all materials in order to provide the greatest possible flexibility for materials selection in the code. Only normal operating conditions (burn cycle and down periods) are included in the model.

The flow diagram for the lifetime code is shown in Figure 1. The code first calculates the temperature, stress, and strain distribution through a plate based upon the selected operating and design parameters. Property changes are then determined for a specific time increment, and the effects of those changes on the initial distributions are evaluated. This process is repeated until failure occurs or the desired lifetime is reached.

The basic inputs to the code are the operating and design parameters. The design parameters include component material(s) selection, plate thickness, coolant characteristics, primary stress level, initial crack length, and plate constraint. The operating parameters include the burn cycle characteristics (ramp time, burn time, dwell time), down period duration and frequency, surface heat flux, neutron wall loading, and surface erosion rate. Finally, the failure criteria for swelling, deformation, and ductility are also provided.

Figure 1. Lifetime Code Flow Diagram

Either a single or dual material plate can be analyzed by the code. The dual material plate is representative of impurity control components which are likely to have a low-Z material bonded to a structural material. The materials properties which are considered are the thermophysical, mechanical, swelling, and neutronic properties as shown in Table 6. Temperature is the primary parameter considered for the thermophysical properties. Radiation effects are also considered in the cases of the thermal conductivity and the elastic modulus. The mechanical properties considered are the tensile, crack growth, fatigue, and creep properties. A bi-linear elastic-plastic behavior is assumed for the tensile properties. Temperature, stress, fluence, flux, stress and strain ranges, and stress intensity are the parameters included in determining the mechanical behavior. Radiation swelling is dependent on the temperature and fluence, and the neutronic properties depend on the neutron flux and fluence.

A simple thermal-hydraulics subroutine calculates the temperature distribution through the plate at various times during the burn cycle. The temperature distribution is then used to calculate the thermal strain distribution, which is used as input to calculate the stress distribution. The stresses can be determined for a plate which is either totally constrained from expansion, allowed to expand but not bend, or is unconstrained. These three conditions span the possible range of component constraint in the reactor.

Once the temperature, stress, and strain distributions are determined, the long term response of the material to the reactor environment can be evaluated. The code determines the swelling change, creep change, fatigue damage, and crack growth for a time increment Δt . The swelling and creep which occurs during the period Δt results in a modified strain distribution that is used to calculate the changes in the stresses. The code also determines the amount of surface erosion during the time increment. The reduction in plate thickness results in a modified temperature distribution. The property change calculations are then repeated using the modified distributions until either the failure criteria are met or the component reaches the goal lifetime. For calculational purposes, the temperature and stresses are assumed to remain constant during the time increment. Therefore, Δt must be chosen such that the stress change is small compared

Table 6. Properties Considered in Lifetime Analysis

Property	Parameters
Thermal Conductivity	Temperature, Swelling
Specific Heat	Temperature
Density	Temperature
Thermal Expansion	Temperature, Fluence
Young's Modulus	Temperature, Fluence
Poisson's Ratio	Temperature
Uniform Elongation	Temperature, Fluence
Yield Strength	Temperature, Fluence
Ultimate Tensile Strength	Temperature, Fluence
Crack Growth	Stress Intensity, Temperature, Stress Ratio
Fatigue	Strain Range, Temperature, Fluence, Average Stress
Creep	Stress, Temperature, Fluence, Flux
Swelling	Temperature, Fluence
Nuclear Heating	Flux
Helium Production	Fluence
Displacements Per Atom	Fluence

with the total stresses in order for this approach to reasonably approximate in reactor behavior.

3.3 Materials Properties

The properties modeled by the code are the thermophysical, tensile, fatigue, crack growth, and swelling. These properties are represented by empirical equations that incorporate the temperature, fluence, and stress dependencies. Whenever possible the predicted property values are based upon available experimental data. In many cases, the experimental data are sparse, and therefore data from similar materials or best estimates of property values are employed. The lack of data for many materials means that there is considerable uncertainty built into the model, and the predictions from the model should be viewed as showing qualitative trends rather than as an accurate representation of component behavior. The material property equations will be updated on a continuing basis as additional information becomes available.

3.3.1 Thermophysical Properties

The thermophysical properties are represented by linear temperature equations.

$$\begin{aligned} Y &= Y_0 + Y_1 T \\ \alpha &= \alpha_0 + \alpha_1 T \\ \nu &= \nu_0 + \nu_1 T \\ k &= k_0 + k_1 T \\ C_p &= C_0 + C_1 T \\ \rho &= \rho_0 + \rho_1 T \end{aligned} \tag{1}$$

where Y is Young's modulus, α is the thermal expansion coefficient, ν is Poisson's ratio, k is the thermal conductivity, C_p is the specific heat, and ρ is the density. Values for the thermophysical properties are generally well known, and general references are usually used to obtain the property data. (10,14,15,24,25)

The effect of radiation on the thermophysical properties is believed to be important in the case of thermal conductivity, Young's modulus, and thermal

expansion. The thermal conductivity is known to be reduced by material swelling or porosity.⁽²⁶⁾ The reduction takes the form

$$\frac{k_f}{k} = (1 - \Delta v/v)/(1 + \beta \Delta v/v) \quad (2)$$

where k_f is the reduced conductivity, $\Delta v/v$ is the amount of swelling, and β is a constant $\simeq 2$.

The effect of radiation on the other properties is specific to graphite.⁽¹²⁾ The derived equations are:

$$\begin{aligned} k_f &= (k-30) \exp(-\phi t) + 30 \\ Y_f &= Y (1 - .25 \Delta v/v) \\ \alpha_f &= \alpha (.75 + .75 \exp(-(\phi t - 2)^2/3.65)) \end{aligned} \quad (3)$$

where k_f , Y_f , and α_f are the radiation effected thermal conductivity, Young's modulus, and thermal expansion respectively, and ϕt is the neutron fluence in units of dpa.

3.3.2 Tensile Properties

Three tensile properties have been considered - uniform elongation, 0.2% yield strength, and ultimate tensile strength. The effect of temperature and radiation are included in the evaluation of these properties.

In the unirradiated condition, the uniform elongation, ϵ_u , is assumed to be

$$\begin{aligned} \epsilon_u &= (\epsilon_1 \exp(-\epsilon_2 T) + \epsilon_3 + \epsilon_4 T) \\ &\times (\exp((T - T_{DBT}) / \Delta T_{DBT}) / (1 + \exp((T - T_{DBT}) / \Delta T_{DBT}))) \\ &\times (1 / (1 + \exp((T - T_H) / \Delta T_H))) \end{aligned} \quad (4)$$

where ϵ_1 , ϵ_2 , ϵ_3 , ϵ_4 are materials constants, T_{DBT} is the low temperature ductile-brittle transition temperature, ΔT_{DBT} is the temperature range over which the ductility changes from brittle to ductile behavior, T_H is the high temperature embrittlement temperature, and ΔT_H is the temperature over which the high temperature ductility changes from ductile to brittle behavior.

The general behavior of Equation 4 along with the expected effect of radiation on ductility is illustrated in Figure 2. Ductility can be low at

Figure 2. General Behavior of Uniform Elongation Modeled by Lifetime Code

low temperatures due to a change in fracture modes from ductile to brittle failure. This behavior is most common in bcc metals, and T_{DBT} is below room temperature for standard structural alloys. Over the operating temperature range, the uniform elongation is high, but it generally decreases with increasing temperature. At very high temperatures, where void formation and growth under an applied stress is the predominant failure mechanism, the ductility decreases rapidly. The effect of radiation is to reduce the ductility at all temperatures. At low temperatures, T_{DBT} can increase with radiation damage. The behavior of the transition temperature is assumed to be

$$T_{DBT} = T_{DBT_f} - (T_{DBT_f} - T_{DBT_1}) \exp(-\epsilon_5 \phi t / T) \quad (5)$$

where T_{DBT_1} is the ductile-brittle transition temperature in the unirradiated condition, T_{DBT_f} is the temperature at which the ductile-brittle transition saturates with radiation, and ϵ_5 is a constant. Ductility at high temperatures can be reduced by the formation of helium during irradiation. The high temperature transition temperature is

$$T_H = T_{H_f} - (T_{H_f} - T_{H_1}) \exp(-\epsilon_6 C_{He}) \quad (6)$$

where T_{H_1} is the high temperature embrittlement temperature in the unirradiated condition, T_{H_f} is the temperature at which helium embrittlement saturates with radiation, C_{He} is the helium concentration, and ϵ_6 is a constant. At intermediate temperatures, ductility is reduced by radiation hardening. The reduction in ductility is assumed to be

$$\epsilon_u (IRR) = \epsilon_f - (\epsilon_f - \epsilon_u) \exp(-\epsilon_7 \phi t / T) \quad (7)$$

where ϵ_f is the saturation ductility with radiation, ϵ_u is the unirradiated uniform elongation, and ϵ_7 is a constant.

The equations used to describe the yield strength and ultimate tensile strength in the unirradiated conditions are:

$$\begin{aligned} YS &= Y_1 \exp(-Y_2 T) + (Y_3 + Y_4 T) / (1 + \exp((T - Y_5) / Y_6)) \\ UTS &= U_1 \exp(-U_2 T) + (U_3 + U_4 T) / (1 + \exp((T - U_5) / U_6)) \end{aligned} \quad (8)$$

where the Y's and U's are materials constants. The equations used to describe the effects of radiation are those developed by R. L. Simons for 20% cold worked type 316 stainless steel.⁽²⁷⁾

$$\begin{aligned}
 YS (IRR) &= YS + Y_7 (1 - \exp (-\beta_1 \phi t)) \exp (-\alpha_1 \sqrt{C_{He}} \phi t) \\
 &- Y_8 (1 - \exp (-\gamma_1 \phi t)) \\
 UTS (IRR) &= UTS + U_7 (1 - \exp (-\beta_2 \phi t)) \exp (-\alpha_2 \sqrt{C_{He}} \phi t) \\
 &- U_8 (1 - \exp (-\gamma_2 \phi t))
 \end{aligned} \tag{9}$$

where

$$\begin{aligned}
 Y_7 &= A_1 T - B_1 \text{ for } T < T_1, \text{ and } Y_7 = A_1 T_1 - B_1 \text{ for } T \geq T_1 \\
 U_7 &= A_2 T - B_2 \text{ for } T < T_3 \text{ and } U_7 = A_2 T_3 - B_2 \text{ for } T \geq T_3 \\
 \alpha_1 &= E_1 \exp (-F_1/T), \beta_1 = \exp (C_1 T) / D_1 \\
 \alpha_2 &= E_2 \exp (-F_2/T), \beta_2 = \exp (C_2 T) / D_2 \\
 Y_8 &= J_1 (T - T_2) \exp (-M_1 (T - T_2)) \text{ for } T > T_2 \text{ and } Y_8 = 0 \text{ for } T \leq T_2 \\
 U_8 &= J_2 (T - T_4) \exp (-M_2 (T - T_4)) \text{ for } T > T_4 \text{ and } U_8 = 0 \text{ for } T \leq T_4
 \end{aligned} \tag{10}$$

Note that the above equations are capable of describing the combined effects of neutron displacement damage and helium generation.

3.3.3 Creep

Creep strain will tend to reduce the stresses through the plate during normal operation. Both radiation induced creep and thermal creep can contribute to creep strain, but under normal operating temperatures and stresses, radiation creep will dominate the creep process. The materials model considers only in-reactor creep which includes a temperature and fluence dependence in the creep rate, but does not consider thermal creep per se. The equation describing the in-reactor creep rate, $\dot{\epsilon}_{cr}$, is

$$\dot{\epsilon}_{cr} = CR \sigma^{n_c} \phi \tag{11}$$

where σ is the applied stress, n_c is the stress exponent, and CR is the creep rate coefficient which includes the temperature and fluence dependence.⁽²⁸⁾

$$CR = (G1 + G2 \phi t) FF/PU \quad (12)$$

where G_1 , G_2 , and PU are constants, and FF is described by

$$\begin{aligned} FF = & CT \times G3 + CT \times G4 (T - T_{cr}) / T_{cl} + G5 \exp (G6 - G7 T) \\ & + CT \times G8 \exp (G9/RT) / (1 + H1 \exp (- H2/RT)) \\ & + CT \times H3 \exp (- H4/RT) \end{aligned} \quad (13)$$

The last term represents the rapid increase in creep at high temperatures, and the preceding term represents a swelling related effect on creep. In-reactor creep is assumed to be non-damaging, and therefore there is no intrinsic materials limit placed on the amount of creep strain allowed. This assumption is in contrast to thermal creep tests of irradiated specimens which can fail at creep strains $\leq 1\%$.

3.3.4 Fatigue and Crack Growth

The fatigue lifetime is described by a modified universal slopes equation⁽²⁹⁾

$$\Delta \epsilon_t = \frac{FS (UTS - \bar{\sigma})}{Y} N_f^{-.12} + (FE \times \log D)^{.6} N_f^{-.6} \quad (14)$$

where $\Delta \epsilon_t$ is the total strain range, N_f is the number of cycles to failure, $\bar{\sigma}$ is the average stress level during the cycle, FS and FE are constants, and D is the ductility parameter given by:

$$D = \frac{1}{1 - RA} \quad (15)$$

where RA is the reduction in area during a tensile failure. Since the reduction in area is not known in most cases, D has been approximated by

$$D \approx \frac{\epsilon_{u_i}}{\epsilon_{u_i} - \epsilon_u} \quad (16)$$

where ϵ_{u_i} is the initial uniform elongation of unirradiated material, and ϵ_u is the uniform elongation in the irradiated condition. It is recognized that the use of ϵ_u to describe the ductility parameter D , may not provide the most accurate representation of fatigue behavior, but its use should illustrate the general trends expected due to the loss of ductility

during irradiation. The influence of temperature and radiation is included implicitly in Equation 14 through the parameters UTS, Y, and ϵ_u . The ASME safety factor of 2 in strain or 20 in cycles to failure can be included in calculating the fatigue lifetime, if desired.

Fatigue damage accumulation is accounted for in the model with the simple equation

$$\text{Fatigue damage} = \sum_i \frac{N_i}{N_{fi}} \quad (17)$$

where N_i is the number of cycles during a time increment step, and N_{fi} is the predicted number of cycles to failure for the conditions present during the time increment. Creep-fatigue damage accumulation is not considered in the model, since radiation creep is assumed to be non-damaging.

Crack growth is described by linear elastic fracture mechanics. The stress intensity, K, is

$$K = K_{cr} \sigma_p \sqrt{a} \quad (18)$$

where σ_p is the peak applied stress, a is the crack length, and K_{cr} is a factor which depends on the component and crack geometry and is ≈ 2 . For cases where the minimum stress is not equal to zero, the stress intensity is better described by an effective stress intensity, K_{eff} , where

$$K_{eff} = K (1 - R)^m \quad (19)$$

where R is the minimum stress divided by the maximum stress during the cycle, and m is a parameter that is usually independent of temperature and has been measured experimentally to range from 0.5 to 0.7.⁽³⁰⁾ The rate of crack growth is then described by

$$\frac{da}{dN} = \left(\frac{B_k K_{eff}}{Y} \right)^{n_k} \quad (20)$$

where B_k and n_k are materials constants. The temperature dependence of crack growth is included in the temperature dependence of Young's modulus, Y.⁽³¹⁾ The effect of radiation on crack growth has not been included at this time due

to the very large uncertainty in its influence. The work by Watson⁽⁹⁾ indicates that, theoretically, radiation could have a large effect on crack growth rate, resulting in reduced lifetimes. There has been little experimental work in this area, however.

The fatigue and crack growth analyses are really describing the same phenomenon, namely the fatigue of a structural material under a cyclicly applied load. Fatigue analysis includes crack initiation in the lifetime determination, whereas a starter crack is assumed to exist for linear elastic fracture mechanics. Either analysis is available in the model. If the initial crack length is assumed to be zero, then crack initiation is determined by fatigue analysis, and crack growth analysis can then be used to follow the long term growth and possible failure of the plate.

3.3.5 Swelling

Radiation induced swelling can result from the formation of voids or gas bubbles. The equation used to describe void swelling is⁽³²⁾

$$\frac{\Delta V}{V} = RR \left\{ \phi t + \frac{1}{\alpha} \ln \left[\frac{1 + \exp (\alpha (\tau - \phi t))}{1 + \exp (\alpha \tau)} \right] \right\} \quad (21)$$

where α and τ are materials constants and RR contains the temperature dependence.

$$RR = W_1 \exp (-W_2 (T - W_3)^2) + W_4 \exp (-W_5 (T - W_6)^2) \quad (22)$$

where the W's are constants. Note that Equation 22 for the temperature dependence allows for the incorporation of two swelling peaks.

Helium bubble formation can also lead to material swelling. The equation describing swelling is⁽³³⁾

$$\frac{\Delta V_{He}}{V} = \frac{A_{He} (C_{He})^{1.5}}{\sqrt{N_{He}}} \quad (23)$$

where A_{He} is a constant, C_{He} is the helium concentration, and N_{He} is the bubble nucleation site concentration which is dependent on temperature.

$$N_{He} = N_0 \exp (Q_{He} / RT) \quad (24)$$

where N_0 is a constant; Q_{He} is an activation energy, and R is the gas constant.

The total swelling is assumed to be the greater of $\Delta V/V$ and $\Delta V/V_{He}$. Applied stress has been shown to influence the degree of swelling.⁽³⁴⁾ This effect has not been included in the model, however, because of the very large uncertainties in this area.

3.3.6 Neutronics

The nuclear heating rates, atomic displacement damage rates, and gas production rates have been calculated for a number of elements, as shown in Table 7.⁽³⁵⁾ The neutron spectrum is that predicted for the fusion DEMO reactor, and the results have been normalized to a 1 MW/m^2 wall loading.

3.4 Temperature Distribution

The lifetime code calculates the steady state temperature distribution during the burn cycle for any selected set of operating parameters. During the dwell period, the temperature of the plate is assumed to return to the coolant temperature. The physical and mechanical properties are then determined from these distributions. The steady state temperatures are believed to be reasonable approximations of the time dependent response of the thin wall structures being considered.

The thermal-hydraulics model used is that of Haines taken from Ref. 14. The plate configuration considered is shown in Figure 3. The heat transfer equations for a one dimensional plate with volumetric heating are:

$$\frac{d}{dx_1} \left[k_1 (T) \frac{dT}{dx_1} \right] + q_{v_1} = 0, \text{ for } 0 < x_1 < t_1 \quad (25)$$

$$\frac{d}{dx_2} \left[k_2 (T) \frac{dT}{dx_2} \right] + q_{v_2} = 0, \text{ for } 0 < x_2 < t_2 \quad (26)$$

where x_1 and x_2 are defined in Figure 3, T is the temperature, $k_1 (T)$ and $k_2 (T)$ are the thermal conductivities of the heat sink and surface material respectively, and q_{v_1} and q_{v_2} are the nuclear heating rates in the heat sink and surface material respectively.

Table 7. Heating Rates, Displacement Rates, and Gas Production Rates for Different Elements Normalized to 1 MW/m² Neutron Wall Loading

	Heating Rates (W/cm ³)	dpa/y	Gas Production Rates (appm/y)	
			Helium	Hydrogen
Be	7.8	-----	3,543	71.9
B	15.0	-----	3,343	196.3
C	6.9	8.9	2,172	~ 0
Al	7.33	11.71	351.6	745.3
Ti	5.60	-----	120.9	178.2
V	7.43	12.69	54.23	273.4
Cr	10.62	10.62	117.5	356.5
Fe	11.24	10.22	120.4	517.0
Ni	7.68	10.55	429.6	1208.3
Cu	14.70	13.23	106.3	578.1
Nb	9.33	6.29	32.4	114.7
Mo	13.37	6.31	-----*	-----*
W	23.01	2.97	3.4	8.9
Ta	18.96	3.39	-----*	10.8

* Small values.

Figure 3. Thermal Model of Surface Material and Heat Sink

Equations 25 and 26 are non-linear because of the temperature dependence of the thermal conductivity. They can be transformed into linear differential equations by means of the Kirchoff transformation by introducing new variables U_1 and U_2 defined by:

$$\frac{dU_1}{dx_1} = k_1(T) \frac{dT}{dx_1}, \text{ for } 0 < x_1 < t_1 \quad (27)$$

$$\frac{dU_2}{dx_2} = k_2(T) \frac{dT}{dx_2}, \text{ for } 0 < x_2 < t_2 \quad (28)$$

Substituting Equations 27 and 28 into Equations 25 and 26 yields:

$$\frac{d^2 U_1(x_1)}{dx_1^2} + \dot{q}_{v_1} = 0, \text{ for } 0 < x_1 < t_1 \quad (29)$$

and

$$\frac{d^2 U_2(x_2)}{dx_2^2} + \dot{q}_{v_2} = 0, \text{ for } 0 < x_2 < t_2 \quad (30)$$

Equations 29 and 30 are subject to the following boundary conditions:

$$U_1(x_1 = 0) = 0$$

$$\left. \frac{dU_1}{dx_1} \right|_{x_1=0} = \dot{q}_s + \dot{q}_{v_1} t_1 + \dot{q}_{v_2} t_2 \quad (31)$$

$$U_2(x_2 = 0) = 0$$

$$\left. \frac{dU_2}{dx_2} \right|_{x_2=t_2} = \dot{q}_s$$

where $\dot{q}_s$ is the heat flux to the plasma side surface. The solutions to Equations 29 and 30 are:

$$U_1(x_1) = -\frac{\dot{q}_{v1} x_1^2}{2} + x_1 (\dot{q}_s + \dot{q}_{v1} t_1 + \dot{q}_{v2} t_2), \text{ for } 0 < x_1 < t_1 \quad (32)$$

and

$$U_2(x_2) = -\frac{\dot{q}_{v2} x_2^2}{2} + x_2 (\dot{q}_s + \dot{q}_{v2} t_2), \text{ for } 0 < x_2 < t_2 \quad (33)$$

The values for U_1 and U_2 can be determined at the discrete nodes within the plate, as shown in Figure 3. The temperature difference from one node to the next is given by

$$T_{n+1} = \frac{U_{n+1} - U_n}{k (\bar{T}_n)} + T_n \quad (34)$$

where $\bar{T}_n$ is the mean temperature between T_n and T_{n+1} . Equations 32, 33, and 34 provide a means for determining the relative temperature profile through the plate.

The absolute temperatures can be determined once the temperature at the coolant-heat sink interface is established. The power balance equation at this location is

$$\dot{q}_s + \dot{q}_{v1} t_1 + \dot{q}_{v2} t_2 = h_c (T(x_1 = 0) - T_0) \quad (35)$$

where h_c is the coolant heat transfer coefficient, and T_0 is the coolant temperature. The heat sink temperature is then given by

$$T(x_1 = 0) = \frac{\dot{q}_s + \dot{q}_{v1} t_1 + \dot{q}_{v2} t_2}{h_c} + T_0 \quad (36)$$

The code first determines $(T(x_1 = 0))$ and then progresses from one node to the next until the top surface temperature is determined.

The temperature drop across the interface between the surface material and the heat sink can be determined from the power balance relation

$$\dot{q}_s + \dot{q}_{v2} t_2 = C (T(x_2 = 0) - T(x_1 = t_1)) \quad (37)$$

where C is the thermal conductance at the interface.

At high material temperature, a significant amount of heat can be radiated from the top surface, reducing the amount of heat conducted through the plate. The radiation heat flux is

$$\dot{q}_{\text{rad}} = \epsilon \sigma T (x_2 = t_2)^4 \quad (38)$$

where ϵ is the effective emissivity and σ is the Stefan-Boltzman constant. The effective surface heat flux is then $\dot{q}_s - \dot{q}_{\text{rad}}$. The code calculates the effect of surface radiation with an iterative procedure. The node temperatures are first calculated to get $T (x_2 = t_2)$, and the radiation heat flux is calculated. The new effective heat flux is then used to re-calculate the temperatures until the changes between iterations become acceptably small, e.g., 0.1%.

3.5 Stress Analysis

3.5.1 Material Tensile Behavior

Component materials are assumed to follow a bi-linear, elastic-plastic behavior, as shown in Figure 4. The material is assumed to be elastic up to the point of the 0.2% yield strength. Linear plastic behavior is then assumed up to the ultimate tensile strength, and the material is assumed to fail at stress greater than the ultimate tensile strength. The strain to failure is assumed to be the uniform elongation.

3.5.2 Stresses in a Totally Constrained Plate

At each node within the plate (Figure 3), the total strain, ϵ_n , is calculated. The total strain consists of thermal stress plus the accumulated plastic strain from previous operating cycles. The plastic strain includes swelling, radiation creep, and previous thermal strain in excess of that required to reach the yield stress. The stress at the node, σ_1 , is simply that required to return the strain to zero.

$$\sigma_1 = \frac{Y_1 \epsilon_1}{(1 - \nu_1)}, \text{ for } \sigma_1 < \sigma_{ys} \quad (39)$$

and

$$\sigma_1 = \sigma_{ys1} + \left(\frac{\epsilon_1 - \epsilon_{ys}}{(1 - \nu_1)} \right) \frac{(\sigma_{UTS} - \sigma_{ys})}{(\epsilon_u - \epsilon_{ys})}, \text{ for } \sigma_1 > \sigma_{ys}$$

Figure 4. Bi-Linear Elastic-Plastic Behavior of Materials

where Y_n is the elastic modulus, ν_1 is Poisson's ratio, σ_{UTS} is the ultimate tensile strength, σ_{ys1} is the yield strength, ϵ_u is the uniform elongation, and ϵ_{ys} is the elastic strain at the yield strength.

The stress free temperature, i.e., temperature at which the thermal strain is zero, is assumed to be the coolant temperature. In reality, the stress free temperature is probably the average temperature of the component. For relatively thin plates bonded to more massive structures, the average temperature should be close to the coolant temperature.

3.5.3 Stresses in a Plate Allowed to Expand, but Not Bend

The stresses within a plate which is allowed to expand but not bend are due to the deviations from the average plate strain. The average stress through the plate must be equal to the primary stress level, σ_{pr} . The thermal stress, σ_{th_i} at any node, is given by

$$\sigma_{th_i} = \frac{Y_1 (\bar{\epsilon} - \epsilon_1)}{(1 - \nu_1)}, \text{ for } \sigma_{th_i} < \sigma_{ys} \quad (40)$$

where $\bar{\epsilon}$ is the average strain through the plate, and

$$\sigma_{th_i} = \sigma_{ys} + \frac{(\bar{\epsilon} - \epsilon_1 + \epsilon_{ys}) (\sigma_{UTS} - \sigma_{ys})}{(1 - \nu_1) (\epsilon_u - \epsilon_{ys})}, \text{ for } \sigma_{th_i} > \sigma_{ys} \quad (41)$$

The sum of the node thermal and primary stresses are subject to the following boundary condition:

$$\frac{\sum_1 \sigma_i}{n} = \sigma_{pr}, \quad (42)$$

When the difference between the average strain and node strains include only elastic strains, the total node stress is simply

$$\sigma_i = \sigma_{th_i} + \sigma_{pr} \quad (43)$$

When the difference between the average strain and the total node strain includes both elastic and plastic strains, the code goes through an iterative procedure until both Equations 40 and 41 are satisfied.

4.0 RESULTS

4.1 Introduction

Applications of the lifetime code to first wall and limiter designs are presented in this section. The cases considered are the first walls for FED/INTOR and for DEMO and the limiter for FED/INTOR. The relevant parameters for the calculations are shown in Tables 8 and 9. The structural material for the first wall is 20% cold worked Type 316 stainless steel. For FED/INTOR the first wall is uncoated, and for DEMO the first wall is coated with 2 mm of Be. Useful heat is extracted only in DEMO as reflected by the differences in the coolant temperatures. Another major difference in the two designs is the high number of burn cycles in FED/INTOR compared with DEMO which operates in the steady state mode.

The limiter structural material is V-15Cr-5Ti, and both Be and graphite are considered as surface materials. V-15Cr-5Ti is an advanced structural material that is capable of withstanding relatively high heat loads and exhibits good resistance to radiation damage. Be and graphite are leading candidate low-Z materials for impurity control applications. Graphite has potential problems with accelerated sputtering, but its unique properties make it an interesting case to consider. The limiter coolant is at a low temperature of $\sim 90^{\circ}\text{C}$. The surface heat flux is relatively high, however, at 2.4 MW/m^2 . The erosion rates chosen are representative of those calculated for physical sputtering using the REDEP code.⁽³⁶⁾ The interface contact between the structure and surface materials is assumed to be perfect in terms of thermal conductance and stress distributions.

4.2 Base Properties

This section summarizes the base properties for 20% cold worked Type 316 stainless steel, V-15Cr-5Ti, Be, and graphite. The properties of interest are the thermophysical, tensile, creep, fatigue, and swelling properties. The property values shown are those calculated from the computer model.

4.2.1 Thermophysical Properties

The values for the thermophysical properties are taken from Refs. 13-15, 24, 25, 37, and 38, and they are tabulated in Table 10. The thermal

Table 8. Design and Operating Conditions for FED/INTOR and DEMO First Walls

Parameter	FED/INTOR	DEMO
Material	20% CW Type 316 SS	Be (70% TD) on 20% CW Type 316 SS
Thickness	12 mm	2 mm (Be) 4 mm (SS)
Coolant Temperature	90°C	280°C
Surface Heat Flux	.11 MW/m ²	.25 MW/m ²
Neutron Wall Load	1.3 MW/m ²	2.1 MW/m ²
Availability	50%	50%
Lifetime Goal	10 y	10 y
Lifetime Cycles	7 x 10 ⁵	250
Lifetime Fluence	52 dpa	84 dpa
Burn Time (s)	200	6 x 10 ⁵
Dwell Time (s)	40	3.6 x 10 ³
Down Periods/Year	6	2
Stress Condition	Free to expand but not bend	Free to expand but not bend
Primary Stress Level (MPa)	0	50
Erosion Rate (m/s)	3 x 10 ⁻¹¹ m/s	1 x 10 ⁻¹¹ m/s

Table 9. Design and Operating Conditions for FED/INTOR Limiter

Parameter	Value
Material	
Surface	Be or Graphite
Structure	V-15Cr-5Ti
Thickness	
Surface	10 mm
Structure	1.5 mm
Coolant Temperature	90°C
Surface Heat Flux	2.4 MW/m ²
Neutron Wall Load	1.3 MW/m ²
Availability	50%
Lifetime Goal	2 y
Lifetime Cycles	1.3 x 10 ⁵
Lifetime Fluence	~ 13 dpa
Burn Time	200 s
Dwell Time	40 s
Down Periods/Year	6
Stress Condition	Allowed to expand but not bend
Erosion Rate	3 x 10 ⁻¹⁰ m/s

Table 10. Thermophysical Properties of FW-Limiter Materials

	Be	C	V-15Cr-5Ti	20% CW 316 SS
Thermal Expansion ($10^{-6}/K$)				
300 K	11.5	3.8	9.1	16.1
700 K	15.3	4.3	10.1	17.7
Thermal Conductivity (W/mK)				
300 K	188	149	21.7	14.0
700 K	128	101	27.1	20.8
Specific Heat (J/kg K)				
300 K	1923	782.5	460	503
700 K	2499	1210.5	548	557
Elastic Modulus (GPa)				
300 K	303.4	6.5	130.3	208.1
700 K	206.1	7.2	117.4	182.3
Poisson's Ratio				
300 K	.06	.15	0.3	.27
700 K	.06	.15	0.3	.29
Density (kg/m^3)				
300 K	1810	1891	6157	8032
700 K	1775	1880	6072	7845

conductivity and elastic modulus of graphite will be altered by neutron irradiation, as described in Section 3.

4.2.2 Tensile Properties

The unirradiated yield strengths of 316 stainless steel, V-15Cr-5Ti, Be, and graphite are illustrated in Figure 5.^(12,14,32,39,40) Type 316 stainless steel exhibits the highest strength, whereas graphite exhibits the lowest strength. It should be noted that the strength of graphite can vary over a considerable range, and the unique tensile behavior of graphite makes it difficult to define tensile parameters in the usual sense applied to metals. The effect of irradiation is generally to increase the strength of materials at low temperatures, but the strength can be either increased or decreased at high temperatures, depending on the material and the original thermo-mechanical treatment. The effect of neutron irradiation on the strength of 20% cold worked type 316 stainless steel is shown in Figure 6. Below ~ 700 K the strength increases, and above ~ 700 K the strength decreases.⁽²⁷⁾

Radiation will cause a reduction in ductility as gauged by the uniform elongation shown in Figures 7 and 8. In the case of 316 stainless steel, the ductility is reduced quickly at very low and very high temperatures. At high fluence levels, the maximum ductility is predicted to occur at $\sim 600 - 700$ K. The effect of radiation on the ductility of other metals at 750 K is shown in Figure 8. Be is predicted to show the sharpest loss of ductility, and V-15Cr-5Ti is predicted to show the lowest decrease.^(5,14,41)

4.2.3 Creep Properties

In reactor creep of the first wall-limiter materials is illustrated in Figure 9.^(28,42) Because of the lack of data for V-15Cr-5Ti and for Be, the creep rates are assumed to be the same as HT-9 at low temperatures.⁽²⁸⁾ The high temperature creep rates have been adjusted from HT-9 creep by the ratios of the absolute melting points. At low temperatures, creep in Be and V-15Cr-5Ti is predicted to be approximately a factor of three less than creep in 316 stainless steel. Graphite is predicted to have the highest radiation creep rate.⁽⁴²⁾

Figure 5. Unirradiated Yield Strength of First Wall - Limiter Materials

Figure 6. Tensile Strength - 20% CW 316 SS

Figure 7. Uniform Elongation - 20% CW 316 SS

Figure 8. Effect of Radiation on the Uniform Elongation of FW/Limiter Materials at 750 K

Figure 9. Predicted in Reactor Creep for First Wall and Limiter Materials

4.2.4 Fatigue

The fatigue properties of the first wall-limiter materials at 750 K are shown in Figure 10.^(5,14,43) In the cases of Be and graphite, the fatigue curves are based upon very sparse data, and they are subject to large uncertainties.⁽¹⁴⁾ The vanadium alloy exhibits the greatest high cycle fatigue life.

4.2.5 Swelling

The swelling behavior of the metals is shown in Figure 11.^(5,14,32) Be exhibits the greatest degree of swelling due to the high helium generation rate. Type 316 stainless steel has a swelling peak at ~ 850 K. The high temperature swelling shown for both V-15Cr-5Ti, and Type 316 stainless steel is due to helium bubble formation. No void swelling is predicted for V-15Cr-5Ti at 50 dpa.⁽⁵⁾

The swelling behavior of graphite is shown in Figure 12.⁽¹²⁾ Graphite initially densifies and then begins to swell at a fluence level of 9-18 dpa. The point at which the volume change returns to zero decreases with increasing irradiation temperatures.

4.2.6 Discussion

The property values used here represent the present state of knowledge for the different materials. In the case of Type 316 stainless steel, the data base is relatively extensive, and therefore the property predictions are not likely to be too far from reality. There are some questions, however, concerning the role of high helium generation rates on the swelling and mechanical properties. For the other three materials, the data base is much less extensive, and the uncertainties are therefore much greater. It is anticipated that the model equations will be continually updated as new information becomes available.

Figure 10. Fatigue Life of FW/Limiter Materials at 750 K

Figure 11. Predicted Swelling at 50 dpa

Figure 12. Swelling Behavior of Graphite

4.3 First Wall Response

4.3.1 FED/INTOR First Wall

The FED/INTOR first wall is composed of 20% cold-worked Type 316 stainless steel. The plate facing the plasma is 12 mm thick and it is backed by a thin corrugated stainless steel panel. The coolant is water at 90°C. The reactor operating conditions are presented in Table 8.

The temperature distributions through the first wall at several times during the reactor lifetime are shown in Figure 13. The surface of the plate exposed to the plasma for this and all other relevant figures in this section is the zero point of the abscissa. The temperature at the plasma side surface is 500 K, and it drops to 373 K adjacent to the coolant. The temperature gradient deviates from linearity because of the temperature dependence of the thermophysical properties and the significant contribution from nuclear heating. As the plate thickness is reduced by surface erosion, the surface temperature decreases such that after 15 y of operation the peak temperature is only 435 K.

The thermal gradients through the plate are largely responsible for the generation of the operating stresses. The stress distributions for a plate that is allowed to expand but not bend are shown in Figure 14 for various times during the reactor life. Early in life ($t = 0$), the stress is compressive on the plasma side and tensile on the coolant side during the burn. The stress levels are near zero during the dwell period. The peak thermal stress of ~ 400 MPa is well within the $3 S_{mt}$ design guideline. Radiation creep results in stress relaxation during the burn period such that the stress levels are near zero after ~ 1 y of operation. The stress levels during the dwell cycle move towards the opposite of the initial stress distribution during the burn. As the plate erodes, the peak stress levels are reduced ($t = 15$ y). Void swelling is predicted to reach only .02% after 15 y and hence it has no impact on the first wall stress distribution.

A major concern of a high cycle device like FED/INTOR is the possibility of fatigue or crack growth failure. For the present analysis, the initial crack length is assumed to be zero, so that only fatigue damage accumulation was evaluated. The increase with time in the fatigue damage factor (a value of 1 means failure) for the plasma side of the plate is shown in Figure 15.

Figure 13. INTOR First Wall Temperature Distribution

Figure 14. INTOR First Wall Stress Distribution

Figure 15. INTOR First Wall Fatigue Damage Accumulation

It should be noted that a safety factor of two in fatigue strain lifetime or a factor of 20 in fatigue lifetime was used in the calculations. The results indicate that the FED/INTOR first wall is not expected to crack during its lifetime. On the other hand, if stress relaxation and erosion were not included in the analysis, a failure would be predicted in ~ 10 y.

Loss of ductility is another major concern for the first wall structural material. The change in the uniform elongation in the FED/INTOR first wall is shown in Figure 16. The uniform elongation is predicted to reach a level of only 1% after ~ 10 y. The DEMO first wall (see below) will reach the same level after ~ 4 y of operation. The loss of ductility to these levels must be considered as being serious, but there are no established guidelines for acceptable ductility values. A major effort is required to develop such guidelines.

4.3.2 DEMO First Wall

The DEMO first wall employs a duplex structure with 2 mm of Be attached to 4 mm of 20% cold-worked Type 316 stainless steel. Both 70% theoretical density and 100% theoretical density Be have been analyzed. The major differences between the two forms of Be are a reduced thermal conductivity and an assumed absence of helium swelling in the 70% TD material. The first wall is thinner than the FED/INTOR first wall because of the anticipated lower erosion rates resulting from a reduced number of disruptions during the lifetime. The DEMO first wall operates at a neutron wall loading of 2.1 MW/m^2 and a surface heat flux of 0.25 MW/m^2 . The water coolant is assumed to operate at 280°C . Because of the pressurized water conditions, the first wall is assumed to operate at a modest primary stress level of 50 MPa. Other operating conditions are given in Table 8.

The temperature distributions through the first wall using 100% TD Be at several times during the reactor lifetime are shown in Figure 17. The peak temperature is 630 K. The thermal gradient is low in the Be due to its large thermal conductivity compared with Type 316 stainless steel. Erosion during the reactor life leads to a reduced surface temperature in a similar fashion to the FED/INTOR first wall. The temperature distributions for the first wall with 70% TD Be is similar to the distribution shown in Figure 17, but the

Figure 16. Loss of Ductility in 20% CW Type 316 SS

Figure 17. Temperature Distribution in the DEMO First Wall

peak surface temperature is calculated to be ~ 5 K higher with the low density material.

The stress distribution in the first wall using 70% TD Be is presented in Figure 18. The most obvious difference between the DEMO and FED/INTOR first walls is the large stress discontinuity that occurs at the Be-stainless steel interface. The stress in both the Be and stainless steel are below the ASME guidelines for thermal stresses and they are in fact considerably lower than the stresses in the FED/INTOR first wall. (Note that the stress free temperature, i.e., the temperature where the thermal mismatch between the Be and stainless steel is zero, is assumed to be room temperature.) The stresses relax towards the primary stress after ~ 1 y of operation. The average stress level increases with time since surface erosion reduces the thickness of material that can carry the forces from the pressurized water.

The stress calculations presented in Figure 18 assume that no swelling occurs in the 70% TD Be. Radiation experiments on 100% TD Be indicate that rapid swelling occurs because of the production of large amounts of helium. The effect of helium swelling on the first wall stresses is shown in Figure 19. Over a period of 0.5 y, the stress levels in 70% TD Be remain relatively constant, whereas there is a rapid decrease in the stresses in 100% TD Be due to swelling expansion. The 316 stainless steel substrate acts to restrain the swelling and puts the Be under a high compressive stress. Radiation creep in Be is not predicted to occur at a rapid enough rate to alleviate the stresses. Therefore, it is concluded that any Be surface layer used in fusion reactors should contain enough porosity to accommodate the swelling due to the neutron generated helium.

The loss of ductility in the 316 stainless steel is shown in Figure 16. The reduction in uniform elongation occurs approximately a factor of two more rapidly than in the FED/INTOR first wall. Further work is required to evaluate the impact of this ductility loss.

4.4 FED/INTOR Limiter

The FED/INTOR limiter employs a duplex structure consisting of 10 mm of Be (100% or 70% TD) or graphite on 1.5 mm of V-15Cr-5Ti. The limiter operates with a surface heat flux of 2.4 MW/m^2 , and it is cooled by water at 90°C . A summary of the operating parameters is given in Table 9.

Figure 18. DEMO First Wall Stress Distribution

Figure 19. Swelling and Stress in Be for DEMO First Wall

4.4.1 Be - V-15Cr-5Ti Limiter

The temperature distributions through the limiter at initial startup and after a year of operating are presented in Figure 20 for both the 100% TD and 70% TD material. The temperature gradients are approximately linear through the plate, and the surface temperature drops by ~ 160 and ~ 100 K for the 70% TD and 100% TD material respectively due to surface erosion.

The stress gradients through the plate during the burn are shown in Figure 21. The stresses during the burn are largely compressive, whereas the stresses during the dwell period are largely tensile. The yield stress of the Be is exceeded from the top surface to ~ 4.5 mm below the surface. Initially, the yield stress of the vanadium is also exceeded, but it shakes down to the elastic regime after a few cycles. The large amount of thermal strain in the Be results in a prediction of cracking in the top 4 mm shortly after startup. The crack is not predicted to penetrate into the vanadium alloy structure, however.

A major concern with the high stresses present in the limiter structure is the potential for fatigue failure in the V-15Cr-5Ti structure. The time evolution of fatigue damage accumulation for two stress distributions is illustrated in Figure 22. In one case, the cracking in the Be is assumed not to affect the stress distribution through the plate, and in the second case, the effect of cracking on the stress distribution by assuming that the net section stress through the intact thickness is equal to zero (no net primary stress). Thus, as the Be cracks, the stresses are relieved in the V-15Cr-5Ti structure. Figure 22 indicates that in both cases, no fatigue failure is predicted, but the fatigue damage factor reaches a value of 0.46 (1.0 indicates failure) when cracking stress relief is not taken into account.

The effect of radiation on the mechanical properties of V-15Cr-5Ti is illustrated in Figure 23. The uniform elongation is sharply reduced, and the yield strength is substantially increased by neutron irradiation. The changes in the mechanical properties are large because of the relatively low operating temperatures. Some vanadium alloys are known to exhibit low temperature hardening and embrittlement due to plastic instability at low fluences.⁽⁴⁴⁾ The changes in the mechanical properties would be considerably less at higher operating temperatures.

Figure 20. Temperature Distribution for INTOR Be-V Limiter

Figure 21. Initial Stress Distribution for the INTOR Limiter

Figure 22. Fatigue Damage Accumulation in Vanadium Heat Sink (Be 70% TD)

Figure 23. Vanadium Yield Strength and Ductility Changes in INTOR

The swelling behavior of 100% TD Be is shown in Figure 24. Total swelling is predicted to remain below 1% due primarily to the low temperature of operation. It is assumed that the 70% TD material exhibits no swelling.

4.4.2 Graphite - V-15Cr-5Ti Limiter

The temperature distribution through the graphite coated limiter is shown in Figure 25. The effect of the radiation induced reduction in thermal conductivity is clearly illustrated by the temperature increase after 0.5 y of operation.

Surprisingly, the large changes in the thermal gradient have little impact on the stress distribution through the limiter plate. The initial stress distribution is shown in Figure 21. The stress level in the graphite is very low due to the low values for the thermal expansion, the elastic modulus, and the tensile strength. The low stresses in the graphite result in low stresses in the vanadium alloy, and it is predicted to operate successfully for the 2 y operational life.

Swelling in the graphite near the top surface is shown in Figure 24. During the 2 y life, the graphite is predicted to densify rather than swell. The high degree of densification results in a net tensile stress in the graphite, but again the stress levels are predicted to remain low for the reasons stated above.

In spite of the low operating stresses, the graphite is predicted to crack early in the operating life. Figure 26 illustrates the interrelationships between the stresses, swelling, and fatigue damage accumulation for the top surface of the graphite. As the graphite densifies by ~ 1%, the stress during the burn cycle increases from -5 to 10 MPa. The increase in the average stress during each fatigue cycle results in an acceleration in fatigue damage, and a fatigue crack is predicted to form at the top surface after ~ 0.065 y of operation. The crack is not predicted to penetrate into the V-15Cr-5Ti structure, however.

Figure 24. INTOR C and Be Limiter Swelling - Top Surface

Figure 25. Temperature Distribution for INTOR C Limiter

Figure 26. Interaction of Swelling, Stress, and Fatigue Damage Accumulation at the Top Surface Layer of the INTOR Graphite Limiter

5.0 SUMMARY AND FUTURE WORK

5.1 Summary

The results presented in the previous section can be summarized as follows:

1) First wall designs for near term devices appear to meet the design lifetimes for normal operating conditions. The operating stresses are well within the limits imposed by the ASME boiler and pressure vessel code, and total swelling and creep are estimated to be low. The surface material, Be, must be used in a porous form so that neutron generated helium can be accommodated. The major concern for Type 316 stainless steel is the loss of ductility which could be a factor in a failure during an off-normal event. In the case of a high cycle device like FED/INTOR, the possibility of fatigue failure is also a concern.

2) The successful operation of the limiter in near term devices is more difficult to assess. Significantly higher stresses are predicted because of the much higher heat loads to the limiter surface. The limiter with a graphite coating is predicted to exhibit considerably lower stresses than the Be coated limiter. Cracking of both the Be and C surface layer is predicted shortly after startup. It appears that the surface cracking could help alleviate stresses in the structural material. A potential problem with the surface cracking is the propagation of the cracks into the heat sink material. In the present cases, however, the cracks are not predicted to propagate into the V-15Cr-5Ti substrate. A concern in the use of V-15Cr-5Ti is the loss of ductility that occurs during neutron irradiation at the relatively low operating temperatures.

The validity of these results is dependent on a number of factors. First, the calculations are only as accurate as the materials property data used as input. Unfortunately, with the exception of austenitic stainless steel, the materials data base is inadequate. Therefore, the results presented should be viewed as illustrative of trends that are likely to occur in fusion reactors rather than accurate predictions of component behavior. Second, there are several effects which have not yet been incorporated into the model. Disruptions could have a significant effect on the stress distribution and near surface materials properties. The actual amount of

material lost during a disruption is also not accounted for. The effect of off-normal events, particularly temperature excursions, would affect the stress distribution and the accumulated radiation damage. If the temperature should rise to the alloy annealing temperature, the stresses would quickly relax, and the radiation damage would anneal out of the material. Helium that was generated during irradiation would probably migrate to the grain boundaries resulting in severe embrittlement. Another possibility is a phase change occurring at elevated temperatures. An example of this phenomenon is temperature excursion of HT-9. If HT-9 were raised above the austenitizing temperature, a martensitic transformation would occur upon cooldown. The transformation would produce a very hard and brittle structure that would be unacceptable for further operation. Finally, the strength and heat transfer properties of the bond between the surface and structural material are not considered. In an actual structure, failure of the bond would be a major concern.

5.2 Future Work

The present model will be used to perform parametric studies for various materials combinations and conditions. The results of these studies will provide information concerning materials limitations and design limitations in a fusion environment. Failure modes for the different materials will be identified. The model will also be used to examine possible variations in materials response as a function of the assumed properties. As an example, there is considerable uncertainty in the swelling behavior of Type 316 stainless steel in a fusion environment (i.e., under irradiation by 14 MeV neutrons). The model will be used to predict first wall response to several potential swelling scenarios for fusion conditions ranging from those in next generation devices (FED, INTOR) to those in commercial devices (STARFIRE). The relative importance of the different swelling scenarios on first wall lifetime will be assessed. The goal of the parametric studies is to establish design and end of life criteria for fusion components.

There are several areas where the code can be upgraded.

- 1) The property parameters will be continually upgraded as improved materials data become available.

2) The properties of the interface bond will be incorporated into the model. Bond strength and radiation degradation are the most important properties to be examined.

3) Upgrading of the model to incorporate two- and three-dimensional component designs will be examined. Ultimately, it would be desirable to incorporate a time dependent model, such as this, into a finite element structural code so that it can be used as a general tool for component design.

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