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Post-Yield Tubular Design and Material Selection Considerations for Improved Geothermal Well Integrity

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ABSTRACT

Geothermal wells undergo large thermal cycles during their life, impacting both design and well integrity. Literature addressing geothermal well integrity is scant, and the primary basis for design remains working stress design, whereby maximum stresses are within the yield strength of the material. This often drives the designer to higher strength options for well tubulars, which can elevate the risk of low cycle fatigue and brittle failure. More importantly, the implication of using more ductile, lower strength materials on lifetime well integrity is not well understood. As a result, geothermal wells can fail well before reaching the end of their expected life, compromising the economics of the asset.

In this work, we present a post-yield design method for geothermal well tubulars. This method is based on a low cycle fatigue (LCF) approach to assessing well integrity since mechanical failure under high temperature cyclic loading is rooted in fatigue. The approach is based on two key concepts: Critical Strain, a material property, and Ductile Failure Damage Indicator (DFDI), a plastic damage parameter. The new approach accumulates the plastic damage through DFDI, and can handle both cyclic and applied monotonic strains in the plastic region. Since Critical Strain is correlated to microvoid coalescence and incipient fracture due to accumulated plastic damage, it is used as the reference strain in the DFDI damage accumulation. Connection fatigue is included in the LCF design approach via a connection strain localization factor, determined using finite element analyses of the connection under cyclic loading. The paper describes the theoretical and experimental basis of the method, and illustrates its application for geothermal well tubulars. The advantages of this method over the more traditional, Coffin-Manson based LCF models are discussed. The LCF design approach presented in this paper has been used in the past by the authors in the design of cyclic steam injection wells. Since geothermal wells present similar thermal cyclic loading and service requirements, the approach is of interest in geothermal well design.

Well integrity must also cover threats such as corrosion and brittle failure. The LCF method has been integrated into materials selection; in addition to corrosion resistance, fatigue life is utilized for materials selection, especially for corrosion resistant alloys such as stainless steels, titanium and nickel based alloys. The impact of produced fluid chemistry, dissolved acid gases and temperature on material selection and well design is discussed in the context of corrosion and caustic cracking, two of the more important threats to geothermal well integrity. The paper demonstrates how consideration of these aspects in the design of the well should be integrated with mechanical considerations, resulting in a well that satisfies its functional requirements while providing integrity over its expected life. When stainless steel and titanium tubulars are considered for corrosion resistance, the LCF methodology allows comparison of the low cycle fatigue life of various alloys and carbon steels thus ensuring adequate life of the tubular. The authors believe that a formal, rigorous design approach centered on lifetime well integrity is much needed in the geothermal industry, and hope that this work contributes a basis for such an approach.

1. INTRODUCTION

Geothermal wells (whether conventional hydrothermal or enhanced geothermal) are subjected to cyclic thermal loading at temperatures as high as 600°F (316°C). Assuming an initial temperature of 80°F (27°C), the resulting thermal stress[1] alone in an axially constrained cemented tubulars (for typically used OCTG low carbon alloy steel) is 107,640 psi. In order to satisfy traditional Working Stress Design (WSD), the yield strength of the selected tubular should exceed this stress (with a safety margin). Assuming a design factor of 1.25, the minimum yield required in the above example is 134,550 psi, a rather high requirement. Of course, there are no standard API grades satisfying this requirement, although proprietary grade materials are available that have such high yield strength. More importantly, high yield strength tubulars, even if non API, with correspondingly lower ductility, fare poorly under cyclic thermal conditions, especially if plastic strains occur either in the connections or in other areas of strain localization (such as unsupported sections), or sour service conditions are expected. One choice is to use lower yield strength tubulars, and accept the consequence of compressive yielding at high temperatures. For example, an axially constrained L80 tubular initially at 80°F yields in compression when the temperature exceeds 425°F (assuming yield strength deration of 3% per 100°F increase in temperature over room temperature). Using this grade at 600°F therefore implies acceptance of compressive yield during heating. If such yielding is to be acceptable, a post-yield design basis is required to guide appropriate selection of tubulars for thermal service.

[1] Thermal stress is simply $-E\alpha\Delta T$, where E is the elastic modulus, α is the coefficient of thermal expansion, and ΔT is the change in temperature (final – initial). It is useful to note that for low carbon alloys steels, the product $E\alpha$ is approximately equal to 200 psi/°F.

Even though post-yield conditions are inevitable in many geothermal wells given the typically high temperatures, there appear to be no literature or standards that describe a post-yield design approach for geothermal well tubular design. NZS 2403 (Standards New Zealand, NZS 2403:2015), the most commonly used reference for geothermal well design, refers both to working stress (or elastic) design and post-yield (or strain-based plastic) design as applicable design methods. Unfortunately, few details are provided for post-yield design. The document refers to the seminal work by Holliday (1969), and to Industry Recommended Practice #03 (Enform Canada, IRP3:2012), but provides no quantitative basis for the application of post-yield design. One purpose of this paper is to fill this gap in the geothermal literature. A broader goal of this work is to highlight the importance of considering both mechanical and material selection aspects in the design of geothermal wells for lifetime well integrity.

In what follows, a modified Holliday Approach (based on Holliday, 1969) for design of thermal service tubulars allowed to yield in compression is presented. The modifications to the method take into consideration thermal effects on steel tubulars that are not included in the original Holliday work. The method is commonly used in the design of tubulars in the heavy oil thermal recovery industry since at least the 1980s (See Suryanarayana and Krishnamurthy, 2015). It is appropriate for use in geothermal wells, and in line with the NZS 2043:2015 standard. The application of the modified Holliday approach to geothermal well tubular design is illustrated via an example. A Low Cycle Fatigue design approach is also discussed briefly, as an alternative approach to the design of geothermal well tubulars, especially to account for connections, or when it is necessary to predict the number of cycles a given casing and connection can withstand before failing in fatigue. The paper then addresses material selection, particularly in the context of corrosion and caustic cracking. Finally, a general discussion is included on other common threats to geothermal well integrity. The authors hope that this work will provide a rational basis for post-yield design of tubulars for geothermal applications, and for appropriate consideration of corrosion and brittle failure. Ultimately, it is hoped that this paper provides the geothermal well designer with guidance on improving the integrity of the well.

2. THE MODIFIED HOLLIDAY APPROACH

2.1. Holliday's Original Approach

The traditional basis of design for thermal service tubulars is the Holliday approach (Holliday, 1969). Holliday's work was motivated by an investigation of tubular failures in thermal wells, wherein he observed that most failures occurred in tension. Holliday's key insight was that since tensile failure is the prevalent mode, compressive yielding is *not* the cause of failure. In fact, it is more logical if the design approach addresses the *tensile stress upon cool down*. Thus, Holliday allows compressive plasticization due to thermal stress from heat-up of axially constrained tubulars. However, the extent of plasticization (and hence the maximum allowable temperature) is limited such that the resulting *residual* tensile stress on cool down does not exceed the tensile yield stress (or connection joint strength) of the casing. The approach is illustrated in Figure 1.

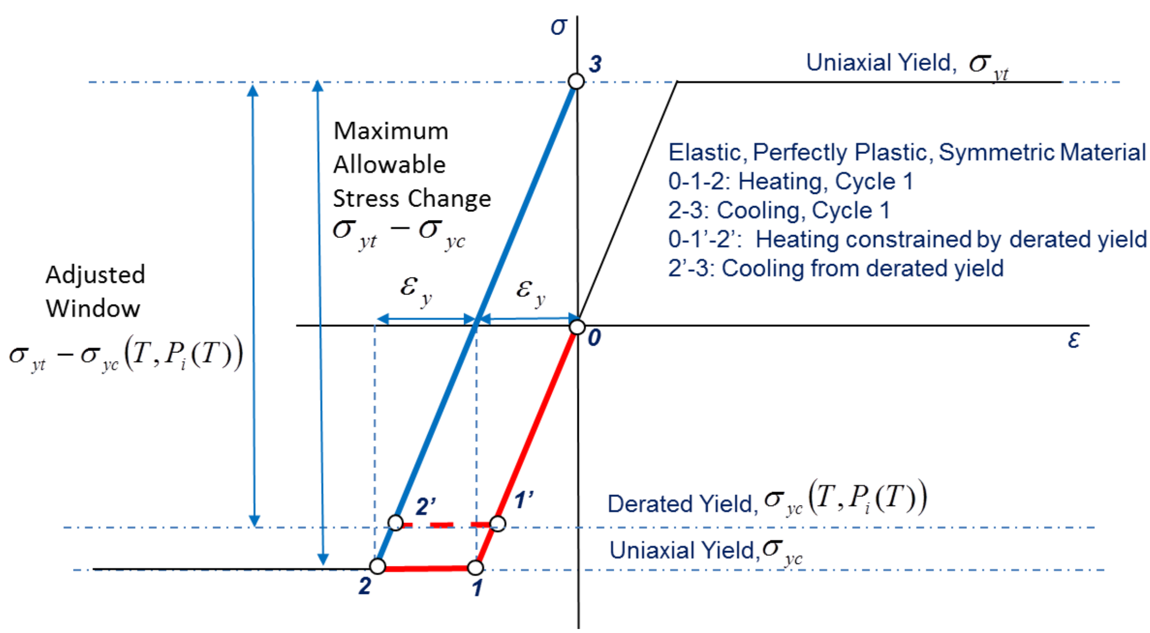


Figure 1: The Original Holliday Design Approach.

In the stress (σ) vs strain (ε) diagram shown in Figure 1, an elastic, perfectly plastic, symmetric material is assumed. The initial condition is nominally at the origin. Upon heat-up (during production), the axially constrained pipe undergoes compression, and the stress-strain behavior follows red curve (along 0-1'). At 1', the axial stress in the tubular just reaches yield. Note that 1' is at a lower stress than uniaxial yield strength of the material (the point "1"). This is because yield strength reduces with elevated temperature, and the internal pressure during production also increases stress (both of which were considered by Holliday in his original work). As a result, yielding occurs before we reach the theoretical limit of "1". The temperature corresponding to 1' is the "yield temperature" for the given conditions, and governs elastic design. Increasing temperature beyond this limit results in compressive yield and post-yield plastic strain. The extent of post-yield plasticization of course depends upon how much higher the temperature is than the yield temperature. If the heat-up is such that the post-yield stress-strain condition reaches 2' (assuming a perfectly plastic material), upon cool-down, the stress-strain curve follows the blue curve, 2'-3, to point "3", which is at uniaxial yield in tension. For an elastic, perfectly plastic, symmetric material, all further cycles progress up and down the blue curve. Thus, we remain within the stress window between tensile yield and compressive yield. Holliday proposes that the temperature corresponding to 2' is the *maximum allowable temperature*, since any higher temperature would result in tensile plasticization upon cool-down. This has come to be known as the "Holliday Stress Window", and is equal to two times the uniaxial yield strenght, if the reduction in yield strength with temperature is ignored. If we account for the reduction in yield due to elevated temperature, as well as for the internal pressure, the window is smaller than two times the yield strength. Thus (referring to Figure 1), the maximum allowable increase in temperature to satisfy the Holliday approach can be calculated as

$$\Delta T_{\max} = \frac{\sigma_{yt} - \sigma_{yc}\left(T, P(T)\right)}{E\alpha} \tag{1}$$

Holliday's design approach thus allows for post-yield loading in the first heat half-cycle, but limits the maximum temperature such that the maximum tensile stress upon cooldown is no greater than the uniaxial yield strength. And in so doing, it allows for much higher temperatures to be applied to tubulars than permitted by working stress (elastic) design. It is also interesting to note that Holliday did not suggest the use of a safety margin in design. At the time it appeared, Holliday's work represented a radical departure from the traditional working stress design approaches as it allowed plastic deformation *by design* (which is now common in most limit-state and strain-based design approaches). Holliday recognized this in his paper: *"It is realized that this concept requires some reorientation of engineering thinking"*.

2.2. Modified Holliday Approach

Ground-breaking as it is, the original Holliday approach ignored several effects of high temperature cycling that change the allowable stress window. The key thermal effects are illustrated in Figure 2. Holliday accounts for **thermal deration of yield**, as discussed above. However, by assuming a symmetric material, Holliday ignores the **Bauschinger Effect,** which reduces the tensile yield strength upon cool down following compressive plasticization. **Thermal Stress Relaxation** (which is also noted in Section 2.10.1.6 of NZS 2403:2015), can result in a reduction in the magnitude of compressive stress during a high temperature "hold", thereby reducing the stress window. Kaiser (2005) provides some experimental results demonstrating thermal relaxation in low carbon steels. Finally, **cyclic strain hardening** can occur on the tension side due to constant-strain cycling (see Stephens, et. al., 2001), whereby the stress at which tensile yield occurs increases with each cycle. This can increase the size of the stress window for later cycles. Most low-yield tubulars used in thermal service are strain hardening.

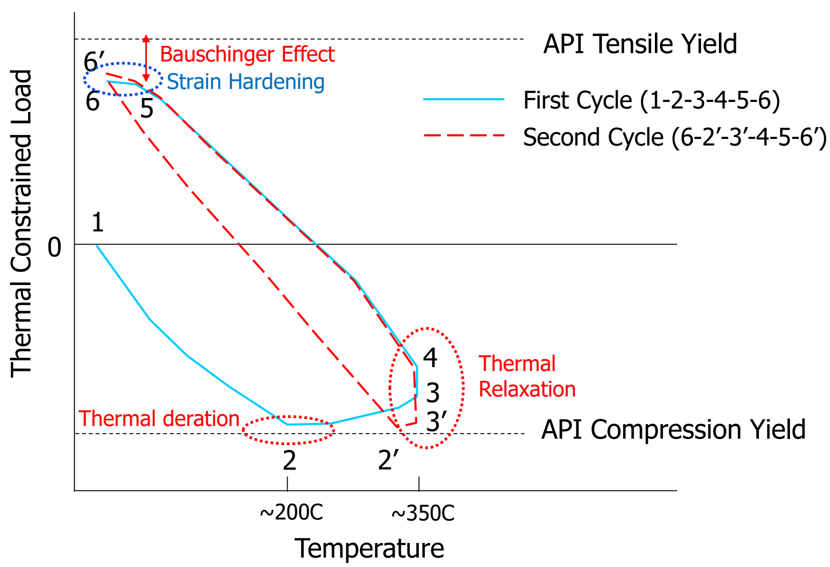


Figure 2. Thermal Effects Impacting Available Stress Window

In the Modified Holliday Approach, the idea is to reduce the available stress window to account for the above effects, and further, to include a margin for uncertainties. Based on experiments and literature on thermal response of steels, the maximum combined impact of the above effects is to shrink the window by 30% to 50%, at the maximum temperatures of interest. Thus, in design, we impose a constraint on the maximum allowable temperature (limit the location of the point *2'* in Figure 1 along the horizontal), so that the material may remain within its elastic region after the first half cycle. Further, rather than base the design on the axial stress, we account for pressure-induced stresses, bending stress (including post-buckling bending stress in unsupported sections), and other stresses by using the von Mises Equivalent (VME) stress in design (referred to as the Triaxial Stress in NZS 2043:2015). It then remains to constrain the extent of post-yield plasticization permitted. We define the Holliday Stress Ratio, *HSR*, as

$$HSR = \frac{\sigma_{VME}}{\sigma_{y,\min}} \tag{2}$$

The magnitude of HSR is then limited by design. The recommended allowable stress ratio is 1.6 for K55 and 1.4 for L80, these two being the most commonly used grades for thermal service. Thus, while the approach allows the calculated VME stress to exceed yield, (hence the name "post-yield" design), it limits the extent of plasticization by design. The VME stress is used in Eq. (2) because the stress state is compressive, and the VME criterion dictates in the compression-burst quadrant of the design envelope (and therefore, its use is conservative). The specified minimum yield strength is used rather than the temperature-derated yield strength in the equation because the allowable stress ratio recommendations already take temperature deration into account (besides the fact it is easier to use the specified minimum yield strength in calculations).

Although the approach is presented in terms of stress (which is easy to calculate and is often calculated routinely in standard well design software programs), the underlying basis of design is strain. By limiting the stress ratio, the location of the point 2' in Figure 1, and hence the total strain, is limited. In general, it can be shown that the total (elastic plus plastic) strain does not exceed 1% with the modified Holliday approach, for an elastic, perfectly plastic material, in the absence of strain localization effects.

It should be remembered that the post-yield design basis is applicable only for the thermal loads- these are loads for which the calculated WSD safety factors in compression and VME are less than unity, but the burst (or collapse) safety factors are much higher than unity. All other (non-thermal) loads should continue to satisfy conventional WSD.

The method is thus very simple to implement in practice. The VME stress is easily obtained from a standard tubular design software program, or can be calculated by hand at any point in the pipe where the internal pressure, external pressure, temperature, and bending radius are known. The HSR can then be easily calculated and compared to the limit.

2.3. Illustrative Example

The Modified Holliday Approach described above is illustrated with an example. We consider a geothermal producer, with a well schematic as shown in Figure 3. The tubular of interest is the 13 3/8", 68 ppf, L80 production tieback. The resource temperature is 246°C (475°F).

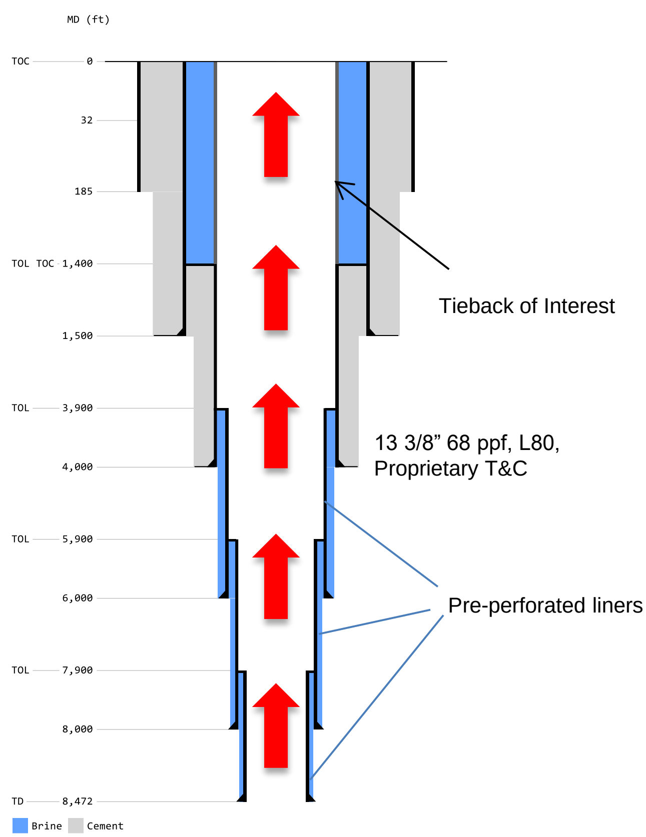


Figure 3. Well Schematic for Example Illustrating the Modified Holliday Approach

Working Stress Design results are depicted in Figure 4, which is the familiar design envelope plot. The inner VME ellipse includes the standard design factor of 1.25. The API polygon shows the burst, collapse and axial force limits, inclusive of customary design factors (See NZS 2043:2015, Table 5 for a list of recommended design factors). The dotted line polygon represents the uniaxial limits of the connection. The figure shows the different design loads considered, including production and quench loads, as load lines in the design envelope. As can be seen, the production load falls outside the VME (and axial limit) envelope. All other loads considered satisfy WSD criteria in this case. The maximum VME stress is 67,900 psi, and the calculated minimum VME (or triaxial) factor of safety for this load case is 1.03 (the safety factor calculation includes deration of yield strength for elevated temperature), which is lower than the recommended design factor of 1.25. If the design approach follows WSD, the primary recourse is increase of grade or yield strength (since wall thickness does not affect thermal stress), and the minimum requirement would be a 110 ksi grade in this case. If the resource temperature is higher, the violation of WSD criteria will be more severe, and even higher yield strength (and more brittle) pipe is required to satisfy WSD. This then, is the dilemma when WSD is used.

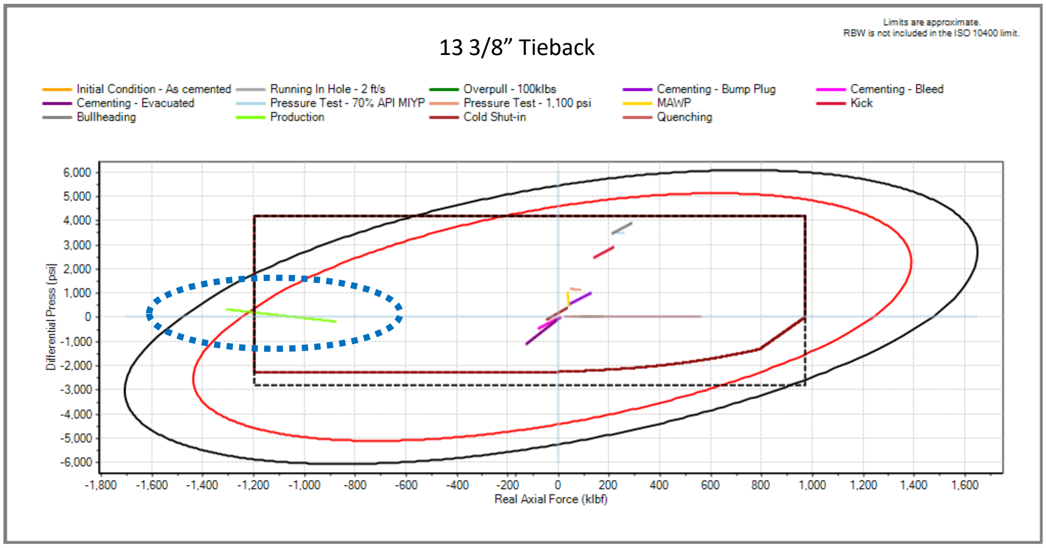


Figure 4. Working Stress Design Results for Illustrative Example

We now consider the Modified Holliday Approach for this load. This load is thermally dominated, as the load line is far from the burst limit (safety factor in burst is much greater than required). Therefore, post-yield design is applicable. The VME stress for this case is 67,900 psi, and the HSR is calculated to be 0.87, much lower than the acceptable limit of 1.4 (for L80). In fact, even with K55, the HSR is 1.23, which is lower than the acceptable 1.6 limit, implying that even K55 is an option for this application (assuming it satisfied WSD criteria for the other loads), if post-yield design is accepted. The primary advantage of using the Modified Holliday Approach is the selection of more ductile materials for a given application. This is an advantage, and is the underlying theme of most structural design codes, where ductility is preferred to strength, all other factors being acceptable.

2.4. Limitations of the Modified Holliday Approach

While able to produce reliable and acceptable designs for geothermal service tubulars, the modified Holliday approach has limitations. These include:

- The allowable stress ratio limit is based on the maximum temperature of interest (in this case, ~700°F), and not the actual temperature of an application. This introduces additional conservatism in the design.
- The assumption of an elastic, perfectly plastic material is a limitation, and not reflective of true material cyclic behavior. The method assumes that when satisfied, it produces only elastic strains after the first cycle.
- Connections are not explicitly considered in the Modified Holliday Approach. In particular, connections may experience strains in excess of pipe body strains for a given cyclic temperature range, which can result in more severe cyclic plasticity.
- Brittle failure is not explicitly included in the design approach.
- Most importantly, when the stress ratio exceeds the allowable ratio, the method implies that the design is unacceptable. This is however, not strictly true. Exceeding the Holliday criteria does not necessarily imply failure. Rather, it may limit the number of cycles that the pipe (and connections) can withstand before failure. Ultimately, failure is governed by fatigue, and it is useful to find the number of cycles to fatigue failure.

The last of the above limitations leads us to a fatigue-based design approach. This is discussed in the next section.

3. LOW-CYCLE FATIGUE APPROACH

Low Cycle Fatigue (LCF) regime applies to cyclic loading beyond yield stress, where plastic strain reversals are present. The use of strain-life methods to predict fatigue life in such cases has been addressed in numerous textbooks (Weronski and Hejwowski, 1991; Stephens, et. al., 2001; Bathias and Pineau, 2011), and is common practice. The application of LCF models to estimate fatigue life of tubulars and connections in thermal wells has received attention in recent times in the literature. Kaiser and Yung (2005) have presented cyclic stress-strain curves for L80, and show how these results may be used in assessing pipe body fatigue life using different hardening rules and LCF models. Wu et. al. (2008) attempted to apply an LCF model to a field problem. Teodoriu, et. al. (2008) and Teodoriu and Falcone (2008) have looked at connection life under cyclic plastic loading, and use both empirical and modeling approaches to establish that the connections can have significantly lower life than the pipe body under a given cyclic thermal load.

Classically, LCF models used in thermal well design use a life model of the Coffin-Manson form (Stephens, et. al., 2001)

$$\frac{\Delta\varepsilon}{2}=\frac{\Delta\varepsilon_e}{2}+\frac{\Delta\varepsilon_p}{2}=\frac{\sigma'_f}{E}\left(2N_f\right)^b+\varepsilon'_f\left(2N_f\right)^c \tag{3}$$

where σ'_f and ε'_f are the fatigue strength coefficient and fatigue ductility coefficient respectively, and correspond to the true stress and true strain at failure, under cyclic conditions. These parameters are obtained from cyclic, strain-controlled experiments. The exponents *b and* c are also experimentally obtained. Details of the experimental approach and methods to determine these parameters can be found in Stephens, et. al. (2001). Once these parameters are known, given an elastic and plastic strain increment, the number of cycles to failure (N_f) can be calculated using the above equation. Damage is accumulated using a standard sum rule, such as the Miner's rule. This is a familiar and much-used strain life model, and many variations of this model have appeared in the literature. In general, the method tends to overestimate fatigue life for thermal service applications. There are two possible reasons.

- The above equation assumes zero mean stress. However, thermo-mechanical response curves (see IRP-3, 2012) suggest that the mean stress is positive and increases with each cycle. Mean stress can be included in the life model, using mean stress corrections proposed by Morrow or Smith, Watson, and Topper (Stephens, et. al., 2001). However, this is rather cumbersome as cyclic data for each cycle are needed, and the mean stress effect is different for each cycle.
- More importantly, the cyclic stress and strain in the connections is not the same as that in the pipe body. Indeed, Teodoriu and Falcone (2008a), and Teodoriu et. al. (2008b) show that the connection fatigue failure precedes pipe failure by a substantial margin. It is therefore important to include connection life in the fatigue model to obtain more realistic life prediction. Some method of accounting for connections is needed.

In the past few years, the authors have developed a consistent and complete LCF approach that addresses the problem differently. It is based on two fundamental concepts- critical strain, and a ductile failure damage indicator (DFDI). The approach is familiar in strain-based design and integrity assessments for pipelines (Gao and Krishnamurthy, 2015). A discussion of ductile failure damage indicators and their calibration is presented by Fischer, et. al. (1995). We believe the approach is easier to implement (for both pipe body and connections) than the Coffin-Manson based strain-life models that have been discussed in fatigue literature, carry lower experimental burden to establish life-model parameters, and give a reasonable estimate of fatigue life. This alternative model is described briefly in what follows, and is discussed in further detail in Suryanarayana and Krishnamurthy (2015).

3.1. Ductile Failure Damage Indicator

The ultimate failure in post-yield cyclic loading is from fatigue, governed by initiation and growth of a crack. Initiation, growth, and coalescence of microvoids form cracks ("ductile plastic damage"), and result in failure due to ductile rupture. For this reason, considerable effort has been made since the mid-1960s [Hancock and Mackenzie (1976), Rice and Tracey (1969), McClintock (1968)] to develop an understanding of both micro mechanisms and the ductile material limit state. McClintock (1968) and Rice and Tracey (1969) established the basic relationship between the growth of a void and imposed stress (and strain) in a triaxial stress field using a micromechanics approach. This strain-limit damage criterion is based on the triaxial stress field, equivalent von Mises stress, and critical strain of the material. The conceptual basis of this criterion is that ductile failure results from initiation, growth and coalescence of voids on a micro scale, and formation of cracks during large plastic deformation. Hancock and Mackenzie (1976) in the mid-70's followed Rice et al's (1969) work, and proposed a reference failure strain, ε_f, a strain limit for ductile failure:

$$\varepsilon_f=1.65\varepsilon_o\exp\left(-\frac{3}{2}\frac{\sigma_m}{\sigma_{eq}}\right) \tag{4}$$

Where the mean stress σ_m is defined as

$$\sigma_m=\frac{1}{3}\left(\sigma_1+\sigma_2+\sigma_3\right) \tag{5}$$

and the von Mises stress (or equivalent stress) σ_{eq} is

$$\sigma_{eq} = \frac{1}{\sqrt{2}}\sqrt{(\sigma_1-\sigma_2)^2+(\sigma_2-\sigma_3)^2+(\sigma_3-\sigma_1)^2} \tag{6}$$

In the above equations, σ_1, σ_2 and σ_3 are principal stresses. The ratio σ_m/σ_{eq} represents the "triaxiality" of the stress field. The parameter ε_o, in Eq. (4), is the *Critical* Strain of the material. Equation (4) implies that the failure strain, ε_f, is not a constant. It is a function of stress triaxiality and the material's critical strain. Critical strain is a material property measured by uniaxial tension testing and is usually in the range of 0.4 to 0.7 for typical casing materials.

Following this concept of strain limit for ductile failure, a non-dimensional plastic damage indicator can be defined for an arbitrarily applied cyclic strain,

$$D_i = \int_0^{\varepsilon_{eq}} \frac{d\varepsilon_{eq}}{\varepsilon_f} \tag{7}$$

where ε_{eq} is the equivalent plastic strain. Equation (7) shows that the plastic damage induced by each increment of the plastic strain is the ratio of the increment to the failure strain. In Eq. (7) principal strains are sometimes used instead of the equivalent strain, although in this work, we use the equivalent strain. Substituting Eq. (4) into Eq. (7), we have,

$$D_i = DFDI = \frac{1}{1.65}\int_0^{\varepsilon_{eq}} \exp\left(-\frac{3}{2}\frac{\sigma_m}{\sigma_{eq}}\right) d\varepsilon_{eq} \tag{8}$$

Equation (8) is the definition of the Ductile Failure Damage Indicator (DFDI) used in this work. The value of DFDI ranges between 0 (undamaged) to 1 (crack). By definition, ductile failure or failure of a component (cracking) will occur when $D_i \geq 1$. Incremental equivalent strains can be accumulated to assess damage in terms of DFDI.

The definition of DFDI includes critical strain, a material parameter required for damage calibration. Critical strain should be obtained precisely using careful experiments. This is discussed in what follows.

3.2. Critical Strain

Critical strain is established from a uni-axial tensile test using the true stress-true strain curve measured in the region beyond necking of a material. As shown in Figure 5 (engineering stress-strain curve for a typical steel), there are two distinct regions on a stress-strain curve. The first knee defines incipient necking, and the stress at this knee is the Ultimate Tensile Strength of the material, a property that is familiar to tubular designers. The second knee defines incipient cracking. The *true strain* at this point (second knee) is the *critical strain* for incipient crack (and the corresponding true stress is the critical stress), and is the first stage for unstable fracture.

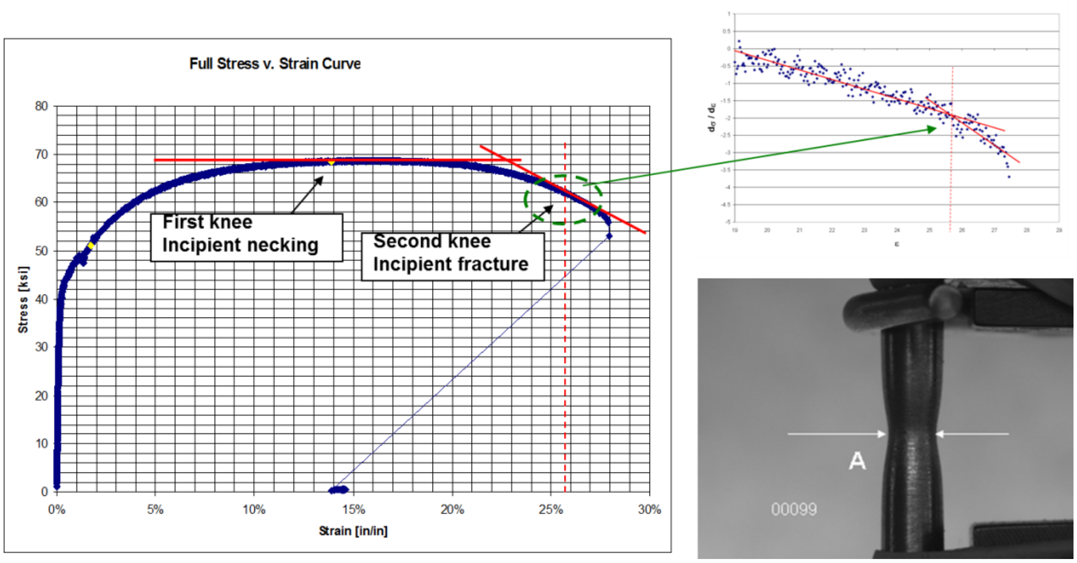


Figure 5: Critical true strain determination

In order to measure critical strain, a true stress- true strain curve has to be obtained. This can be accomplished using a regular uni-axial tensile machine equipped with a high-resolution camera synchronized with tensile data acquisition system for load-displacement and specimen image (diameter). The slope change of the derivative of stress to strain as a function of true strain is utilized to identify critical strain. Experimentally determined critical strains (and stresses) for two commonly used grades are shown in the table below.

Table 1. Critical strain (and stress) for K55 and L80

Grade	**Critical Point**	
	ε'_{crit} **(%)**	σ'_{crit} **(ksi)**
K55	68.4	171.8
L80	60.7	161.9

True-Stress True-Strain Material Curves

In order to calculate strain and stress increments from one point to the next during cyclic loading, we need a material curve that shows the true-stress vs true-strain behavior all the way to failure. The Ramberg-Osgood relationship (Stephens, et. al.2001) is a familiar and commonly used model to represent the true stress – true strain behavior of metals. According to this relationship, the total true strain and the true stress are related by the equation

$$\varepsilon_t = \varepsilon_e + \varepsilon_p = \frac{\sigma_t}{E} + \left(\frac{\sigma_t}{K}\right)^{1/n} \tag{9}$$

In Eq. (3), σ_t is *true* stress, ε_t is *true* strain, E is Modulus of Elasticity and n & K are parameters obtained empirically from true stress – true strain experimental data. In addition to these parameters, the true stress at failure, σ'_f , and the true strain at failure, ε'_f are also obtained from the experiment. It is noted that the form shown in Eq. (9) above is the one used in this work (some textbooks use equivalent forms of the equation, with slightly different definitions of the parameters n and K.). When stabilized cyclic stress-strain curves are available, the same equation, Eq. (9) applies also to strain and stress increments.

The authors have performed limited material tests on some Oil Country Tubular Goods (OCTG) grades commonly used for thermal service. The best-fit Ramberg-Osgood parameters for K55 and L80 are estimated as follows:

Table 2. Ramberg-Osgood Parameters for K55 and L80

Grade	**Ramberg-Osgood Coefficients**	
	n	**K (ksi)**
K55	0.1982	184.15
L80	0.1844	168.16

Note that the above parameters are from monotonic tests. A more correct approach is to use the stable cyclic stress-strain curve to obtain these parameters. An experimental program to obtain cyclic stress-strain curves is ongoing (cyclic test results for OCTG materials are not readily available in the literature).

3.3. Implementation of the LCF Approach

The above approach has been implemented in an Excel^{TM}-based program to illustrate its application. The parameters *K', n',* critical stress and critical strain are determined from monotonic tests, as discussed above. In the absence of cyclic stress-strain data, the monotonic stress-strain curve may be used, as it is a conservative assumption for cyclic strain-hardening materials. However, it is always more appropriate to use cyclic stress-strain data if available, especially for grades known to exhibit cyclic strain softening.

For thermal cycling, the cyclic behavior over two cycles (based on true-stress, true-strain behavior) is illustrated in Figure 6. Key inputs include material properties and information on the thermal cycles (generally the high and low temperature for each cycle), as well as any monotonic strain increments (to account for geomechanical loading, strain localization, etc.). The analysis begins at a known initial condition of true stress and strain. For each half-cycle, the thermal (or any arbitrary) strain increment is first calculated based on the operational specifications and/or models, and from this, the stress increment is calculated using Eq. (9). This establishes the next stress-strain location, from which the next strain increment is applied, and so on. Care must be exercised in conversion of the increment of strain into a point on the stress-strain plane. In each cycle, the plastic strain is calculated and accumulated in the DFDI, as per Eq. (8). The theoretical limit of fatigue life is when DFDI = 1, but in practice, we have used a maximum DFDI of 0.7 to cover for the assumptions made in the implementation and for uncertainties in material properties and loads. Preliminary results from ongoing experimental work on low cycle fatigue testing has demonstrated that the critical strain does not vary with cycles up to 45 and 50 cycles for low carbon steels. This set of data support the DFDI based LCF design and provide more than sufficient safety margin.

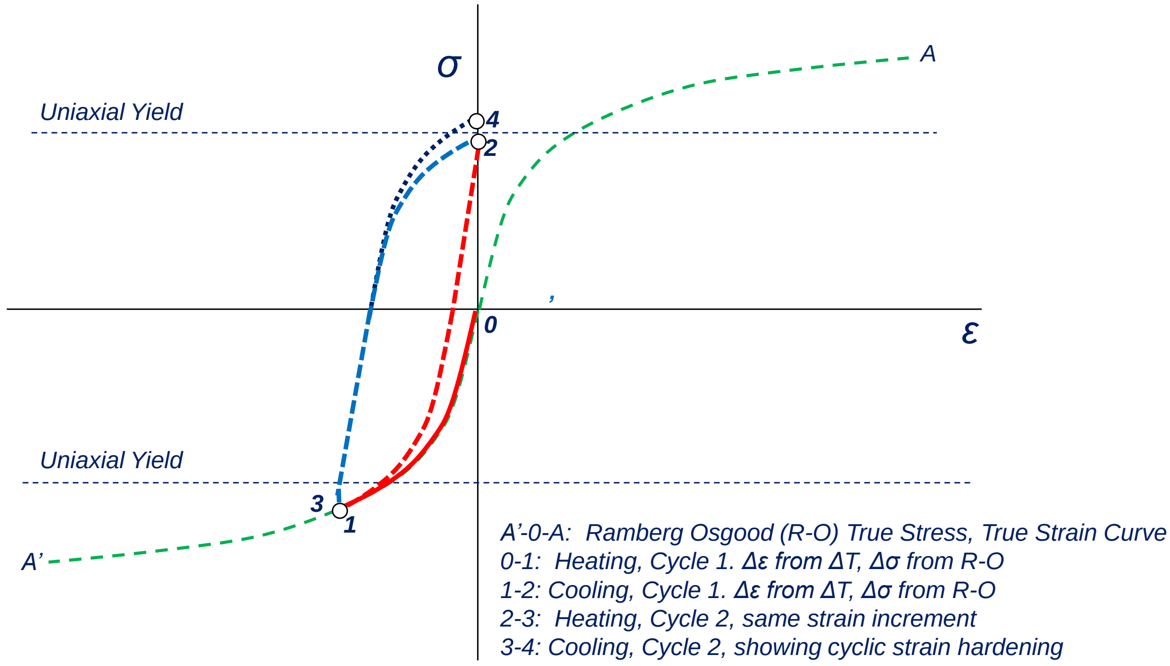


Figure 6. Cyclic behavior under cosnstant-strain thermal cycling

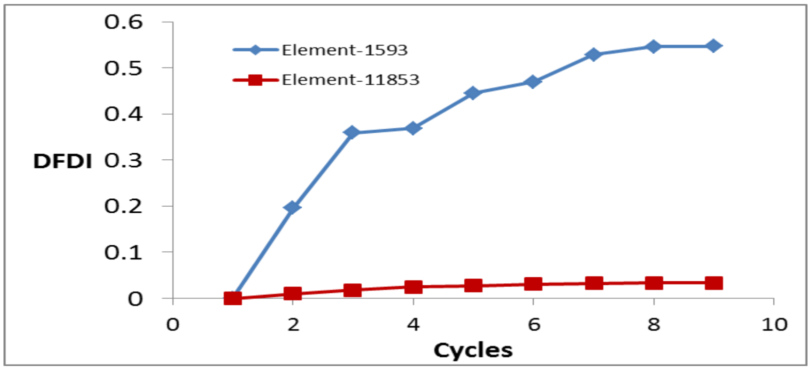
3.4. Incorporation of Connections in the Design Approach.

One of the main advantages of the DFDI approach is that it allows for relatively easy consideration of connections in design. Connections are modeled using finite element analyses (FEA) and damage accumulation is quantified using DFDI. For connections, the triaxial stress in Eq. (8) becomes an important driver. It should be noted that triaxiality in the connections is not negligible, while for pipe-body, it is usually negligible for thermally dominated loads.

In the FEA model, the connection is first made-up using the manufacturer specified make-up practice. The pipe is then subjected to cyclic loading at constant strain amplitude in the range of interest (typically 0.5% - 1.5%). The principal stresses and strains in the pipe body and the connection (both box and pin) are tracked for each cycle. A Strain Localization Factor is then calculated for each cycle, as the ratio of the maximum DFDI in the connection to that in the pipe body. This factor is then used in the LCF model as a strain multiplier for connection fatigue damage accumulation.

FEA for such specific connection analyses is a valuable basis for differentiating between connections, especially in response to post-yield cyclic loading. It is also relatively easy to perform, since we are interested only in the principal stresses and strains in the connection and pipe body. Unfortunately, such analyses require a complete description of the connection geometry, which is not easy to obtain for proprietary connections. For BTC connections, this information is available in the public domain. An example of DFDI accumulation results for an L80 casing is shown in Figure 7. As the figure shows, the DFDI (and accumulated damage) in the connection is greater than in the pipe body. The ratio of the connection DFDI to pipe body DFDI is used as a" strain localization factor" in the LCF model, which then estimates the number of cycles a connection can withstand.

Another alternative for connection selection is to use the ISO:PAS 12835 (2015) connection evaluation protocol. This is a well-defined test and qualification protocol specifically developed for thermal service connections. However, very few connections have been qualified using this protocol, and testing to this protocol remains quite expensive.



Element 1593 is critical location in connection. Element 11853 is in pipe body.
Fully-reversed cyclic strain of 1% at room temperature.

Figure 7. DFDI accumulation in connection and pipe body at 1% cyclic strain, BTC connection

3.6. Using the LCF Approach in Design

In most geothermal applications, the Modified Holliday Approach is likely to be adequate. However, in applications where temperature exceeds 550°F, and with wells where unsupported tubulars or doglegs are present, it is possible that the Modified Holliday Approach cannot be satisfied. In such situations, the number of cycles to failure can be calculated using the above approach and compared to the expected number of cycles during service life for purposes of design. In addition, generation of connection strain localization factors will ensure that well integrity including the connection is assured through its life.

4. MATERIAL SELECTION CONSIDERATIONS IN TUBULAR DESIGN

Mechanical design of tubulars, as discussed in the preceding sections, should be combined with material selection considerations to assure well integrity over the intended service life of a well. In geothermal wells, it is important to consider corrosion and brittle failure.

4.1. Corrosion

Corrosion is a common threat in geothermal wells, with produced brines containing CO_2, H_2S and other acid components. A corrosion analysis is therefore important for all geothermal wells. This will first require a rigorous analytical procedure to ensure characterization of the water chemistry and the concentration of the dissolved gases (Hausler and Krishnamurthy, 2017). Further, in geothermal wells, both single phase water and 2-phase (water and steam) conditions can arise in the well, and common oilfield methodologies for partial pressures estimations are inaccurate. The concentration of acid gases should be estimated with full consideration of the solubility as a function of temperature, pressure and chloride content (Hausler and Krishnamurthy, 2017). This will require sophisticated thermodynamic and process chemistry tools such as OLI.

The corrosion behavior can then be experimentally studied, or more conveniently, modeled using commercial tools like OLI (https://www.olisystems.com/), a program with a thermodynamic framework that simulates multi-phase, multi-component aqueous systems. The OLI suite also provides a corrosion analysis program, particularly for aqueous solutions containing H_2S and CO_2. Typically, the detailed chemistry of the brine is available in most geothermal fields. Given details about the fluid chemistry and thermodynamic state, the program predicts the expected rate of corrosion for a range of metals and metal alloys. This prediction is a reasonable basis for initial design and selection of materials. The program is commonly used in selection of materials for oil and gas applications with corrosion concerns.

As an illustrative example of the process, we consider a geothermal brine with 0.5 mole % of CO_2 and 0.05 mole % of H_2S, with other minor gases including methane. The key components of the brine are 5900 mg/L of Na, 10800 mg/L of Cl^-, 2500 mg/L of Boric acid, 12 mg/L of SO_4 and 25 mg/L of HCO_3^-. This composition is in the range of typical geothermal fluids. The calculated average pH is just over 4, and matches well with the reported pH. It is noted that pH varies with pressure and temperature. Production analysis over a 30-year life indicates that 2-phase conditions are expected in later life of the well. The calculated temperature, pressure, and quality are obtained at various depths. The composition is calculated at these depths along the well, and should cover, at a minimum, the production liner and tieback. The fugacity-corrected partial pressures of H_2S and CO_2 are used, and other gases are ignored. Based on these inputs, the calculated corrosion rate for an L80 material is 20-40 mpy (milli-inches per year), with an average of 25 mpy (or 0.025" wall loss for each year of service). This is a moderate to aggressive corrosion rate (typically, acceptable rates of corrosion range between 2-10 mpy). For a typical 13 3/8", 68 ppf (0.480" wall) tubular, this implies through-wall corrosion in 12-24 years, with an average expectation of through-wall corrosion in 19.2 years. Depending upon the desired service life, this can be a cause for concern.

Analysis of corrosion is an important part of design verification, and should not be ignored in geothermal wells. In general, it helps in appropriate selection of materials, and the design of monitoring and mitigation plans. Corrosion analysis also reveals potential scaling problems. In some cases, a corrosion analysis may in fact show that low carbon alloy steels are adequate, thus eliminating the need for higher cost corrosion resistant alloys. Finally, it is important to note that external corrosion due to the presence of acid zones is also of concern, especially if acid-resistant cements are not used.

There are scenarios where the anticipated corrosion rates will necessitate the use of stainless steel or titanium tubulars. Such materials can provide adequate corrosion resistance for the right environment. However, mechanical considerations other than yield are very important for these corrosion resistant alloys. Despite adequate yield, the material could have low fracture toughness and/or low ductility. This material property has to be accounted for in mechanical design. The LCF design methodology allows all alloys to assessed for fatigue life using critical strain for a particular alloy and quantitatively assess its impact on well integrity. Therefore, in addition to cost, the impact on the total well life can be assessed for a titanium, stainless steel or carbon steel tubulars with considerations to all threats.

4.2. Brittle Failure

The most common brittle failure consideration in oil and gas wells is for sour service conditions (H_2S partial pressure in excess of 0.05 psia). In the presence of H_2S, with low pH and low temperature, hydrogen embrittlement can lead to brittle failure at loads well within the elastic limit. This is often referred to as sulfide stress cracking (SSC). The mechanism is well understood and well documented (see, for example, NACE MR 0175/ISO 15156, 2001). The reference ISO TR 10400 / API TR 5C3 (2008), among others, presents equations to calculate the pressure limit under sour conditions, based on the experimentally determined value of K_{1SSC}, the threshold stress intensity in mode 1 loading of a pre-existing flaw or defect.

However, for thermal service wells, sulfide stress cracking is rarely implicated in brittle failures. First, material selection is usually from lower grade (higher ductility) choices, which conform to the recommendations of NACE MR 0175 / ISO 15156 (2001) for sour service applications. Second, the high temperature during production usually means that K_{1SSC} is very high, and ductile failure will govern. Nevertheless, since the quench load causes reduced temperature, it is worth checking this failure mode, even if it is unlikely to govern design. The limit can be easily checked using the brittle burst equation in ISO TR 10400 / API TR 5C3 (2008). Also as the cyclic loads accumulate, the impact of strain accumulation on hydrogen embrittlement should be analyzed.

A more serious threat to geothermal well integrity is caustic cracking. It is caused by anodic dissolution of high energy sites (such as grain boundaries) in a material, especially at high pH (> 9) and high temperature (100°C or 212°F for low yield materials). Presence of sodium hydroxide, nitrates, and ammonia create favorable conditions for caustic cracking. The failure presents predominantly as intergranular cracking. However, the mechanism is complex, and laboratory simulation is quite difficult.

In geothermal wells, the most favorable site for caustic cracking is actually outside the casing, just above the top of cement, where the cement may not have fully set, leaving some unset cement close to the surface. Depending upon the type of cement used, repeated heating and cooling can then concentrate hydroxide ions, leading to extremely high pH conditions over time. In one example investigated by the authors, due to the requirement to satisfy working stress design, and given the high temperature of the brine (290°C, 554°F), a 150 ksi yield material was used as the production casing. This material is, as can be expected, brittle. Unset cement at the surface created conditions favorable to caustic cracking, and tests and simulations showed that the pH of the unset cement was as high as 12, and local concentration of sodium hydroxide exceeded 20 to 30% at high temperature (225°C). With the high pH and the high yield carbon steel, caustic cracking conditions were present, and with the hoop-stress from the internal pressure during production and shut-in, a crack initiated at the OD at tong marks, and grew to result in a catastrophic failure within a very short period.

Any number of mitigating circumstances could have prevented this failure. Chief among them is the use of a lower grade material (which would have been acceptable according to the post-yield design approaches described earlier in this paper), and a better cement job (which, as is well appreciated in the industry, is critical for geothermal well integrity).

Such cracking threats can result in catastrophic failures, and are due to incomplete quantification of all threats and integrating them into a comprehensive view of well integrity.

5. OTHER THREATS TO WELL INTEGRITY

Other commonly cited threats to well integrity center around cement selection and placement. Mud channeling or excess water in the cement slurry can result in small pockets of trapped fluid adjacent to a casing. The build-up of annular pressure in this trapped pocket during heat-up, if not relieved (for example, by access to surface or to formation outside the cement sheath), can cause extremely high collapse loads on the inner casing (and burst loads on the outer casing). There is, unfortunately, limited engineered mitigation possible for this situation. An obvious mitigation is, of course, proper centralization (taking into account the tubulars and the well trajectory) and proper cement preparation for efficient displacement. An equally serious problem is the presence of short unsupported sections due to cement "holidays", especially shallower in the well. If the unsupported section is short, inelastic buckling can result when temperature is increased, leading to excessive plasticization and ultimately, failure. Once again, avoidance of short cement gaps requires carefully planned and implemented cementing operations. Also, if poor cementation is likely, it is better to leave a long uncemented gap at the surface (assuming it is feasible given the specific well conditions), so that pressure build-up can be mitigated by venting at surface, and any buckling is elastic (that is, unsupported section is "slender", and buckling is helical), resulting in much less plasticization during heat-up. For the production liner and tieback, proper design of cementing operations remain a critical aspect of design.

6. CONCLUDING REMARKS

In this paper, it is fundamentally argued that compressive yield from heat-up of axially constrained tubulars is acceptable, as long as tensile failure (or fatigue failure) are avoided within the service life of the well. Two approaches to post-yield design are described. The Modified Holliday Approach is a notionally strain-based approach that derives from the original work by Holliday (1969), and takes into consideration thermal effects such as yield deration at elevated temperatures, Bauschinger effect, thermal stress relaxation, and strain localization effects. The approach is a logical extension of the familiar Working Stress Design approach, and easy to implement for geothermal wells. An example illustrates the use of this approach. The LCF model described in this paper uses the concept of critical strain, a material property that is measured from uniaxial tension tests, and a Ductile Failure Damage Indicator, through which damage is accumulated. These concepts and their basis are addressed. Ultimately, failure in thermal service tubulars is almost always dictated by plastic damage and fatigue, and LCF based approaches are a necessary step in producing reliable designs that last for the intended service life of modern geothermal wells. LCF based approaches are critical for wells with high alloy tubulars. In the absence of such quantification there is a risk of premature failure despite high corrosion resistance.

The paper also addresses the complementary consideration of materials selection. Corrosion, a major threat to the longevity of geothermal wells, is discussed. Analysis of corrosion and prediction of corrosion rates for use in design are covered. It is argued that brittle failure due to sulfide stress cracking is unlikely in geothermal wells, and the more serious concern with caustic cracking is discussed. The paper ends with a general discussion of other threats to well integrity, and how they may be addressed in design

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