Mechanical Integrity of Primary Reformer Hot Outlet Headers

This paper discusses mechanical integrity issues for primary reformer hot outlet headers. These are typically welded assemblies of wrought Alloy 800H or 800HT or cast 20Cr-32Ni-Nb alloy. The typical design and operating conditions for these headers are presented. Problems and failures that have been reported at past Ammonia Plant Safety Symposiums are reviewed and summarized. With this background information, recent failures of hot outlet headers are described along with the identified causes of these failures. The most common failure mechanism is high-temperature creep but the location and causes of creep damage vary. Creep failures have occurred both at weld joints and within base metal. The sources of these problems are discussed and suggestions for mitigation are presented. Based on the findings of the failure and material properties investigations, stress analysis and material damage modeling is used to estimate remaining life.

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Introduction

The purpose of this paper is to review and discuss the primary mechanical integrity issues that are associated with the hot outlet headers of primary reformers. A number of papers presented at past Ammonia Plant Safety Symposiums have addressed various issues with these hot outlet headers. To provide background for recent issues with similar headers, the past work is reviewed and the various types of problems that have been encountered in the past are summarized. Relevant issues on similar headers in hydrogen and methanol reformers are also discussed. In all cases, the hot outlet headers operate in the high-temperature creep range for their material of construction, which is either a wrought Alloy 800H or 800HT or a cast 20Cr-32Ni-Nb alloy. Because of the operating temperature range, failures typically occur by high-temperature creep or creep-fatigue, with the fatigue damage induced by thermal cycling. Thus, creep strength is an important parameter addressed in the review discussion.

Following the background review, examples of recent problems with hot outlet headers are pre-
sented and discussed. The causes of these problems and their relation to past problems are evaluated. The purpose of this evaluation is to help operators of primary reformers minimize the potential of similar problems in future operation of their units and to provide guidance for estimating the safe and useful life of headers that are currently in service. Such guidance will help avoid unplanned shutdowns and schedule repairs or replacements in a timely, cost-effective manner.

**Past Hot Outlet Header Issues**

Steam-methane reformers have either hot or cold outlet headers to collect the process gas from the outlet of the catalyst tubes. Cold headers are made of carbon steel and insulated so that they operate at temperatures well below the creep range, while hot headers are not insulated and operate at high temperatures close to the reformer outlet gas temperature. The focus of this paper is on hot outlet headers that operate in the high-temperature creep range of the materials from which they are constructed. Kawai, et al. [1] point out that there are two basic types of hot header system designs: (1) a hot collector where the catalyst tubes are attached directly to the manifold by means of fittings and the hot gases exit from the manifold via riser tubes and (2) a hot subheader with a cold transfer line where the catalyst tubes are attached to the subheader via outlet pigtails. In either case, the hot header or subheader is cylindrical vessel with numerous welded attachments at holes that allow the hot process gas to flow into the header and central tee section where the hot process gas flows out of the header.

Because the headers operate at temperatures near or greater than 800 °C, they are typically made from either wrought Alloy 800H or 800HT or from cast 20Cr-32Ni-Nb alloy. The fittings and tees are also made using these same alloys. Welding of the headers, tees, and fittings is done with a high-nickel material or a “matching” material. Commonly used welding electrodes include Inco-Weld A, Inconel 182, Inconel 112, and Inconel 117. Commonly used filler metals include Inconel 82, Alloy 625, and Alloy 617. The common “matching” filler metals are Thermanit 21/33, Thermanit 21/33 So, and UTP A 2133 Mn. The final selection of welding material depends on the design and the welding technique that is used.

The primary load on outlet headers is internal pressure but bending loads are often induced by movement of attachments and/or restraints at supports. These system bending loads are related to thermal expansion and complex interactions within the manifold system, making stress analysis to compute their magnitude difficult and adding significant uncertainty to the results of the calculations. Furthermore, the bending loads are often displacement controlled so that stresses induced in the high-temperature creep regime can relax and redistribute with time at operating temperature. In contrast, internal pressure is usually maintained at a relatively constant level and is a sustained load so that stresses induced by internal pressure are relatively straightforward to compute.

This section of the paper reviews hot outlet header issues that have been discussed in previous publications. Issues with primary ammonia reformers as well as issues with similar components in hydrogen and methanol reformers are addressed. In comparing this information, keep in mind that these different types of steam-methane reformers operate in different ranges of temperature and pressure as illustrated in Figure 1. Ammonia reformers operate at higher pressures and lower temperatures than hydrogen or methanol reformers. This difference in operating conditions affects the hot outlet header design. Thus, the outlet headers in ammonia reformers usually operate at higher stresses and lower temperatures than those in hydrogen and methanol reformers.

**Performance of Header Materials**

In 1963, Francis and Glass [2] reported the failure of outlet piping and described high-temperature stress-rupture failures in Type 304 stainless steel
piping. Avery and Valentine [3] reviewed the desired characteristics of materials for outlet manifold systems, while Harnby [4] pointed out that an outlet manifold system made of Alloy 800 had better creep resistance than one made of the HU alloy. Later, Blackburn [5] pointed out that manifolds made of Alloy 800 were less prone to cracking than those made of traditional cast alloys, such as HU, HK, and HT, because of its superior ductility. He proceeded to note that the cast 20Cr-32Ni-Nb alloy was an improvement to Alloy 800 for hot outlet header applications. These papers provide historical background on alloys for use in hot outlet headers.

Figure 1. Typical operating temperature and pressure ranges for ammonia (NH$_3$), hydrogen (H$_2$), and methanol (CH$_3$OH) reformers, from Kawai, et al. [1].

Shibisaki, et al. [6] found that Si, Nb, and C additions had a negative effect on the ductility of 20Cr-32Ni-Nb cast alloys while Mn had a positive effect on their ductility. Parks and Schillmoller [7] indicated that Alloy 800H has a low tendency for embrittlement but noted that the 20Cr-32Ni-Nb cast alloy has higher creep-rupture strength than Alloy 800H. Thomas, et al [8] pointed out a serious embrittlement issue with the 20Cr-32Ni-1Nb cast material and suggested that niobium-free grades should be developed in order to avoid such in-service problems. Hoffman [9] evaluated the aging of 20Cr-32Ni-Nb castings after 21 months of service at 852 °C and found both M$_{23}$C$_6$ carbide and inter-metallic Nb-Ni silicide precipitates present in the aged material. The silicides were mostly G phase plus some η’ phase. They are expected to decrease creep resistance and promote liquation cracking during welding.

Hoffman and Gapinski [10] evaluated samples of a 20Cr-32Ni-Nb cast outlet manifold that had developed fatigue cracks after it had been in service for approximately 192 months at an outlet gas temperature of 871 °C. In addition to carbide and silicide precipitation, they observed carbide coalescence. The observed aging significantly degraded the tensile strength, Charpy V-notch impact resistance, stress-rupture strength and weldability of the material. The carbides and silicides were solutionized by annealing at 1093 °C. However, complete solutionizing of secondary precipitates and spheroidization of the primary carbides required a minimum annealing temperature of 1149 °C. Better stress-rupture properties were achieved by solution annealing at 1204 °C than by solution annealing at 1093 or 1149 °C. The cracked region of the manifold was successfully repaired after in-situ solution annealing at 1149 °C for three hours and operated successfully for four more years before the furnace was rebuilt. The authors recommended keeping Si content as low as possible and maintaining a stoichiometric Nb/C ratio of 7.7.

**Welding and Repairs**

Roach and VanEcho [11, 12] found that weldments generally have lower stress-rupture strength than comparable base metals. Inconel 182 weldments had the lowest stress-rupture strength when compared with Inconel 82, Inconel 112, and Inco-Weld A weldments. Several reformer operators [13-15] have experienced accelerated creep damage, embrittlement, and failures with Inconel 182
weld metal. Failures have been attributed to a loss of rupture strength caused by embrittlement. Inconel 182 is now known to exhibit a loss of rupture strength and ductility at elevated temperatures.


Shibisaki, et al. [16] recommended the use of a high-nickel alloy metal (Inconel 82) for welding 20Cr-32Ni-Nb metals. Kobrin [15] obtained superior results using Inconel 82, Alloy 625 filler metal, and Inconel 112 in place of Inconel 182. Orbons [17] suggested using Inconel 112 in place of Inco-Weld A for applications at temperatures above 816 °C because of its superior resistance to creep damage. Roach and VanEcho [11, 12] also found that Inconel 112 had superior creep resistance to those of Inco-Weld A, Inconel 182, and Inconel 82 weldments. van Wortel [18] noted that filler metals 82 and 617 were found to be susceptible to relaxation cracking. For applications that require the highest strength and corrosion resistance, Inconel 117 electrode and Alloy 617 filler metal are recommended [19] for use at temperatures above 788 °C.

Al-Jubeihi [20] reported making successful repair welds by grinding out cracks, depositing Inco-Weld A stringer beads, and grinding out welds and base metal defects one bead at a time while using dye penetrant tests and ultrasonic examinations to ensure quality repairs. Another successful repair method [21] involves grinding into through-wall cracks to remove 60% of the cross-section and grinding back the surface area by 5 mm to prevent re-cracking associated with embrittlement.

The beveled area is then covered using Inconel 182 electrodes, and the previously ground grooves are filled up by welding following a stringer bead technique. The shortcoming of this repair method is that Inconel 182 welds have poor high-temperature creep strength.

Pande and Swenson [22] successfully completed a preliminary repair by welding a reinforcement cone onto sound material in the tees to extend operating life. Blackburn [5] developed a method to improve weld efficiency using parent metal as filler after completing the root weld with Inconel 82 wire.

Shi and Lippold [23] found that repair practices that lead to failures often involve elevated temperatures throughout welding repairs leading to embrittlement, which in turn gives rise to a ductility dip. When the elevated temperature restraint exceeds the ductility minimum, failure occurs. Another common problem is the mechanism of hydrogen induced crack formation which leads to weld failures. Pattabathula, et al. [24] noted this can be mitigated by increasing the temperature at the weld above dew point, by adding outside surface insulation, and by installing windshields on sides of the furnace.

Another problem with welding operations, pointed out by Shibisaki, et al. [25], involves differences between weld and base metal. The difference in thermal expansion coefficients can lead to elastic-plastic strain of the weld material. Orbons [17] suggested high-temperature preheating around the weld to prevent cracking of the base metal caused by weld shrinkage. Lobley, et al. [26] found that it was necessary to bore machine and solution anneal a 20Cr-32Ni-Nb cast header prior to repair welding because of its severe embrittlement in the aged condition, especially near the inner surface.

Anderson and Bruno [27] reviewed weldability issues that they encountered during the welding of a new cone piece to an existing tee (aged material) in a hydrogen reformer outlet manifold. Both
pieces were 20Cr-32Ni-Nb castings from the same foundry. Metallurgical analysis of samples of the previously installed, aged cone piece on which an Electrode 117 weld bead had been deposited revealed both heat-affected zone (HAZ) liquation cracking (LC) and reheat cracking. As shown in Figure 2, the liquation cracking occurred near the fusion zone (FZ) and was believed to be caused by low melting point silicate precipitates that had formed during service. The reheat cracking was believed to have been caused by a combination of mismatched mechanical properties and high inclusion content. A full solution annealing of the aged material was required to weld it successfully.

Penso and Mead [28] discussed cracking that they found on existing 20Cr-32Ni-Nb statically cast tee pieces when a new centrifugally cast 20Cr-32Ni-Nb reducer cones were welded to them. The location of this cracking is shown in Figure 3. It was found to be associated with intermetallic silicate precipitates in the existing (aged) material. The tees were cut out and solution annealed at 1150 °C, followed by machining out of all of the cracks. Welding was done using a GTAW process with Alloy 617 filler wire, a 50 °C preheat, and a maximum interpass temperature of 150 °C. A buttering layer was first applied using low amperage. The joints were then tack welded, and a root pass was applied. DP inspection was performed following every second fill pass and after blend grinding the cap pass.

Jack [29] reported cracking in a 21/33 weld between a tee and a subheader in a cast 20Cr-32Ni-Nb outlet manifold after 14 years of service at approximately 793 °C. This cracking is shown in Figure 4. Attempts to grind out and repair the cracking were not successful, so tapered sleeves of Alloy 800HT were designed and welded over the cracked weld and similar welds in the outlet manifold.
Recent Hot Outlet Header Issues

To highlight some recent issues with hot outlet headers, three case histories are reviewed. The first two cases cover outlet headers from methanol reformers, while the third case is for an outlet header from a hydrogen reformer.

Case 1. Metallurgical Analysis of a Header to Bull Tee Weld from a Methanol Reformer

A metallurgical analysis was performed on a bull tee from a methanol reformer outlet header. The tee was connected to two header tubes and an increaser cone. The tee was removed when leaks were discovered at the girth welds after approximately 50,000 hours of service.

The header, tee, and increaser cone were made of a cast alloy 20Cr-32Ni-Nb alloy. The welds used to join these components were made using an Inconel 82 root pass, Inconel 82 or Inconel 182 reinforcement, and Inconel 182 for the remainder of the weld. Thus, the majority of the weld filler metal in the joints was Inconel 182.

Approach. The following steps were performed for this analysis. The bull tee was visually inspected and photographed in the as-received condition. Liquid dye penetrant testing was performed on the three large diameter girth welds of the tee. Cross-sections were removed from cracks in the welds, mounted, polished, and etched. Light photomicrographs were taken to document the morphology of cracking and microstructures. Samples for chemical analysis were removed from the reducer cone, header tube, and tee and weld metal between the increaser cone and tee to determine the chemical composition.

Results and Discussion. Figure 5 is a photograph of the bull tee. Pigtails extended vertically from fittings attached to the tee and indicate the top of the tee. Girth welds attached the tee the increaser cone and tee to two header tubes. The outside diameter (OD) of both header tubes was 345 mm.
The wall thicknesses of the header tubes were measured at the 12:00, 3:00, 6:00, and 9:00 o’clock orientations. The average wall thickness of the header tube was 21.4 mm.

Figure 5 also shows the tee after performing liquid dye penetrant testing of the three welds. The red dye shows the locations of indications. Four locations that contain indications were identified. The indications were located at: 1) both header to tee girth welds, 2) a pigtail to header tube girth weld, and 3) the tee to increaser cone girth weld.

Figure 6 is a photograph of the crack-like indication located at the toe of a header to tee girth weld. The indication is approximately 122 mm long and present from the 1:15 to 2:30 orientation. The liquid dye penetrant testing clearly showed the locations of the crack indications at the girth welds.

Figure 7 is a photomicrograph of the mounted cross-section (Mount W3) that was removed a header to tee girth weld (GW 3). The dashed line in Figure 6 indicates the location where the cross-section was removed and the arrow indicates the polishing direction. Cracks are present in the HAZ of the tee, HAZ of the header tube, and in the weld metal. Although difficult to see, a columnar microstructure is also present in the header. Figure 8 is a photomicrograph of Mount W3 showing the HAZ of the tee and the weld metal. Creep cracks are present at the grain boundaries in the HAZ of the tee. Creep voids are present in the weld metal. Figure 9 is a photomicrograph of Mount W3 showing the HAZ of the header tube and weld metal. Relatively wide, and also narrow, creep cracks are present in the HAZ of the header tube. This photomicrograph was taken just below the outside surface where the crack-like indication shown in Figure 6 was located.
Figure 8. Light photomicrograph of the mounted cross-section showing the HAZ and weld metal near the tee side; mirror image of area indicated in Figure 7 (Glyceriga Etchant).

Figure 9. Light photomicrograph of the mounted cross-section showing the HAZ and weld metal near the header tube side; mirror image of area indicated in Figure 7.

Figure 10 is a photomicrograph of the header base metal microstructure. The observed microstructure is consistent with that expected for the 20Cr-32Ni-Nb alloy. The figure shows primary carbides located at the grain boundaries and smaller secondary carbides dispersed in the grains. The finer, secondary carbides are indicative of high temperature exposure. Although it is difficult to be certain since the appearance of the pre-service microstructure is not known, it appears that the network of primary carbides was reduced and that they are slightly blocky, which is also indicative of high-temperature exposure.

The most severe creep damage was on the header tube side rather than the tee side. The intergranular crack path is consistent with a high-temperature creep mechanism. The finer, secondary carbides and blocky primary carbides are indicative of high-temperature exposure.

The results of the chemical composition analysis conducted on a sample removed from the base metal of the header are shown in Table 1. The results of the analyses are consistent with the nominal chemical composition specifications for the alloy at the time of its manufacture.

The results of the chemical composition analysis conducted on a sample removed from the weld metal between the increaser cone and tee are shown in Table 2. Except for the nickel plus cobalt (Ni + Co), manganese (Mn), iron (Fe), and chromium (Cr) content, the results of the analysis are consistent with specified values for Inconel 82 and 182. The Mn and Cr content are consistent with the Inconel 82 specification and not with Inconel 182 specification. The Ni + Co and Fe content are consistent with the Inconel 182 specification and not the Inconel 82 specification.
Table 1. Results of chemical analysis of samples removed from the tee, header tube, and increaser cone compared with the nominal chemical composition for the 20Cr-32Ni-Nb alloy.

<table>
<thead>
<tr>
<th>Element</th>
<th>Tee Wt. %</th>
<th>Header Wt. %</th>
<th>Increaser Cone Wt. %</th>
<th>Nominal¹, Wt. %</th>
</tr>
</thead>
<tbody>
<tr>
<td>Carbon</td>
<td>0.12</td>
<td>0.15</td>
<td>0.14</td>
<td>0.10 – 0.15</td>
</tr>
<tr>
<td>Manganese</td>
<td>1.05</td>
<td>1.01</td>
<td>0.77</td>
<td>1.50 (max)</td>
</tr>
<tr>
<td>Silicon</td>
<td>0.63</td>
<td>0.50</td>
<td>0.65</td>
<td>1.50 (max)</td>
</tr>
<tr>
<td>Chromium</td>
<td>20.62</td>
<td>19.91</td>
<td>20.01</td>
<td>19 – 25</td>
</tr>
<tr>
<td>Nickel</td>
<td>33.03</td>
<td>32.61</td>
<td>33.02</td>
<td>31 – 33</td>
</tr>
<tr>
<td>Niobium</td>
<td>1.17</td>
<td>1.08</td>
<td>1.05</td>
<td>0.50 – 1.50</td>
</tr>
<tr>
<td>Phosphorus</td>
<td>0.019</td>
<td>0.016</td>
<td>0.017</td>
<td>0.050 (max)</td>
</tr>
<tr>
<td>Iron</td>
<td>Balance</td>
<td>Balance</td>
<td>Balance</td>
<td>Balance</td>
</tr>
</tbody>
</table>

Table 2. Results of chemical analysis of a sample removed from the weld between the tee and increaser cone compared with the nominal chemical composition for Inconel 82 and 182.

<table>
<thead>
<tr>
<th>Element</th>
<th>Weld Wt. %</th>
<th>Inconel Filler Metal 82² Wt. %</th>
<th>Inconel Welding Electrode 182² Wt. %</th>
</tr>
</thead>
<tbody>
<tr>
<td>Nickel + Cobalt</td>
<td>65.9</td>
<td>67.0 (min)</td>
<td>59.0 (min)</td>
</tr>
<tr>
<td>Carbon</td>
<td>0.037</td>
<td>0.10 (max)</td>
<td>0.10 (max)</td>
</tr>
<tr>
<td>Manganese</td>
<td>2.74</td>
<td>2.5 – 3.5</td>
<td>5.0 – 9.5</td>
</tr>
<tr>
<td>Iron</td>
<td>8.52</td>
<td>3.0 max</td>
<td>10.0 max</td>
</tr>
<tr>
<td>Sulfur</td>
<td>0.006</td>
<td>0.015 (max)</td>
<td>0.015 (max)</td>
</tr>
<tr>
<td>Silicon</td>
<td>0.17</td>
<td>0.50 (max)</td>
<td>1.0 (max)</td>
</tr>
<tr>
<td>Chromium</td>
<td>20.05</td>
<td>18.0 – 22.0</td>
<td>13.0 – 17.0</td>
</tr>
<tr>
<td>Niobium + Tantalum</td>
<td>2.42</td>
<td>2.0 – 3.0</td>
<td>1.0 – 2.5</td>
</tr>
<tr>
<td>Phosphorus</td>
<td>0.003</td>
<td>0.030 (max)</td>
<td>0.030 (max)</td>
</tr>
</tbody>
</table>

The fractional increase in Ni content would not be expected to have a substantial effect on creep resistance. The differences in the Mn, Fe, and Cr content are likely related to dilution or enrichment that can occur with the various passes of the Inconel 82 and 182 welding materials.

Summary. A summary of the key observations follows:

- Creep voids were present in the heat affected zone (HAZ) and weld metal of the header to tee girth weld but not in the base metal.
- Creep cracks were present in the HAZ and weld metal at one girth weld.
- Intergranular creep cracks penetrated the outer surface in the HAZ and weld metal, near the toe of the girth welds.
- The fine, dispersed secondary carbides are indicative of high-temperature exposure.
- The microstructures of the base metals were consistent with those expected for 20Cr-32Ni-Nb cast alloy.
- Except for Ni compositions for the tee and reducer cone of 0.03 and 0.02 weight % greater than specified, the base metal composition was consistent with specified values for the alloy.
- The composition of the girth weld was consistent with that of Inconel 82 or 182, and in some cases, with both specifications.

The cracking is consistent with a high temperature creep mechanism associated with the low creep strength of the filler material. This type of failure can be mitigated by using a filler metal with improved creep strength in the weld joint such as a “matching” filler or and Inconel 617 filler.

Case 2. Metallurgical Analysis of Outlet Header from Methanol Reformer

A metallurgical analysis was performed on two sections from a methanol outlet header that leaked in-service. One of the sections contained the leak as shown in Figure 11 and the other (non-failed) was located away from the leak in the same header. The header samples were removed from service after approximately the minimum design life of 100,000 hours. The header tube sections were centrifugally castings made of a 20Cr-32Ni-Nb alloy.
**Approach.** The following steps were performed for the analysis. The header tube sections were visually inspected and photographed. A ring section was removed from the through-wall portion of the header tube section; the other header section was already in the form of a ring. The two ring sections were polished and light photomicrographs were taken to document the observed creep damage. The ring sections were then etched in Adlers reagent to reveal their macrostructures and photographs were taken. A small portion of each ring was then removed, mounted, polished, and etched with glyceregia for detailed examination. Photomicrographs were taken to document the microstructure and maximum service temperatures were estimated.

Using elastic finite-element stress analysis, the maximum tensile hoop stress in the region of the header failure was calculated to be 10.2 MPa. Failure stresses for average stress-rupture lives of 100,000 hours (11.4 years) were computed for header operation at various temperatures using the manufacture’s Larson-Miller parameter relationship for the 20Cr-32Ni-Nb alloy. A temperature of 963 °C was calculated to cause stress-rupture failure in 100,000 hours. This was 73 °C above the reported maximum header operating temperature of 890 °C. Based on this result, it is likely that the material was somewhat weaker than indicated by the manufacture’s data and that it was overheated to some degree at the failure location.

**Results and Discussion.** Figure 11 is a photograph of the as-received header tube section (Header Section 1) that failed. The figure shows the through-wall portion of the failure. Secondary cracks are also visible in the vicinity. The cracks were present from the right side of the tube section up to approximately 174 mm from the right side. Also visible is a bulge in the tube where the cracks are present. The maximum diameters of the bulged and non-bulged regions of Section 1 were 174.0 mm and 171.0 mm, respectively.

**Figure 11. Photograph of the header tube section (Header Section 1), as received.**

Figure 12 is a photograph of the ring from Section 1, following polishing and etching. The location of ring Section 1 is indicated in Figure 11. A columnar dendritic macrostructure was visible on both ring sections.

**Figure 12. Photograph of Ring Section 1 from Header Section 1; dashed lines in Figure 11 indicate the location of the ring (Adlers reagent).**

Measured dimensions of the tube sections are listed in Table 3 along with reported nominal dimensions of the tube when it was new. The no-
minal inside diameter (ID) of the new header tube was not reported, so it was calculated based on the reported minimum outside diameter (OD) of a new header tube minus twice the minimum sound wall thickness. The strain values of the header tube sections were based on calculated minimum ID values. There was more strain at the bulge in the header tube than in Header Section 2, which was to be expected. Since baseline values of original dimensions are not available for each tube, the strains are only approximate because of inherent variations in the dimensions of new tubes.

Table 3. Measured dimensions of header tube samples compared to minimum values reported by client.

<table>
<thead>
<tr>
<th>Description</th>
<th>Outside Diameter (OD)</th>
<th>Wall Thickness (WT)</th>
<th>Inside Diameter (ID)</th>
<th>Strain*</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>mm</td>
<td>in</td>
<td>mm</td>
<td>in</td>
</tr>
<tr>
<td>Minimum new tube</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Bulge on Header Section 1</td>
<td>174.0</td>
<td>6.850</td>
<td>15.0</td>
<td>0.592</td>
</tr>
<tr>
<td>Header Section 2</td>
<td>172.0</td>
<td>6.77</td>
<td>15.4</td>
<td>0.607</td>
</tr>
</tbody>
</table>

* Strain based on ID minimum and measured values. ID calculated based on OD and WT minimums and measured values.

Figure 13 is a photomicrograph of Ring Section 1 adjacent to the OD surface. Relatively wide and narrow creep cracks that appeared to follow the dendritic columnar structure were present for the full wall thickness. A low density of creep voids was found in the cross-section. The density of voids was not high adjacent to the ID or OD surface. The absence of creep voids near the cracks is evidence of short-term overheating. Note that evidence of austenite grains and twinning was apparent at the ID where the through-wall cracks were present. This suggests that the ID was cold worked during fabrication. It is not known if this contributed to the failure.

Figure 14 is a photomicrograph of Ring Section 2 adjacent to the OD surface. Aligned creep voids (not linked) were present adjacent to the OD and ID and appeared to be more prevalent adjacent to the OD. The residual life of this tube section is approximately 70% based on metallurgical analysis and guidelines of past studies [33] of reformer tubes. For 11 years of prior operation, this result implies approximately 26 years remaining life, assuming that future operating conditions are the same as those of the past.

Figure 13. Light photomicrograph of Ring Section 1 adjacent to the OD surface, near the through-wall location.

Figure 14. Light photomicrograph of Ring Section 2 adjacent to the OD surface.

Figures 15 and 16 are photomicrographs of the microstructures of Ring Section 1, near to and away from a crack, adjacent to the OD. The photomicrographs were taken at the same magnification. The network (cored structure) of primary
carbides does not appear to be intact near the crack. A cored, lacelike structure was present away from the crack, indicating that lower temperatures were present away from the crack. Also evident near the crack was the presence of secondary carbides and nitrides. The nitrides were not evident away from the crack, which is to be suspected. Nitrides are known to form adjacent to fracture surfaces. Figure 17 is a photomicrograph of the microstructure from Ring Section 2. The cored, lacelike structure and secondary carbides are present as was shown away from the crack in Ring Section 1.

Figure 15. Light photomicrograph of the microstructure of Ring Section 1 near a crack and OD surface (glyceregia etchant).

Based on the microstructures observed in the header samples and the reference material, their maximum service temperatures were estimated to be:

- Ring Section 1: 870°C
- Ring Section 2: 840°C

The reference material had four times as much carbon as the header samples, which made it difficult to estimate these values. Thus, there is uncertainty regarding them.

The amount of niobium (1.91 wt% compared with the specified range of 0.90 to 1.35 wt%) in the header section was greater than the maximum value of the specified range. The amounts of the other elements met the manufacturer’s specification for 20Cr-32Ni-Nb. Higher than desired niobium content may have reduced the creep strength of the 20Cr-32Ni-Nb alloy. This possibility was pointed out in the previous review of published investigations.

Figure 16. Light photomicrograph of the microstructure of Ring Section 1 away from a crack and near the OD surface (glyceregia etchant).

Figure 17. Light photomicrograph of the microstructure of Ring Section 2 (glyceregia etchant).

Summary. A summary of the key observations follows:

- The metallographic evidence (wide creep cracks, low density of voids) suggests that Header Section 1 failed by short-term overheating.
• Ring Section 2 showed metallographic evidence of creep damage. The remaining life of this section was estimated to be 70%, while the maximum service temperature was estimated to be 840°C.

• Using finite-element stress analysis, the maximum tensile hoop stress in the region of the header failure was calculated to be 10.2 MPa. For this stress, a temperature of 963 °C was calculated to cause stress-rupture failure in 100,000 hours, indicating that the material was either overheated, weaker than expected, or a combination of both.

**Case 3. Metallurgical Analysis of Outlet Header a Hydrogen Reformer**

During an inspection, a significant increase in diameter was noted in one of two headers in a hydrogen reformer. The headers were made of the cast 20Cr-32Ni-Nb alloy. The portion of the reformer that fed gas to this header was known to have been operating at higher than desired temperatures. Further inspections revealed the presence of cracks in the header, especially in the around the holes where fittings for the outlet pigtails were attached. As a result, the operator decided to replace the header and cut out two ring sections for metallurgical failure analysis. Ring Section 1 was from the central portion of the header tube where the increase in diameter was the largest and the most material damage was expected to be found. Ring Section 2 was from near the end of the header tube where the increase in diameter was the smallest and the least material damage was expected to be found.

**Approach.** Ring Sections 1 and 2 were visually inspected and photographed and then were polished and photographs were taken to document the observed creep damage. Two portions from Ring Section 1 and two portions from Ring Section 2 were removed, mounted, polished, and etched with glyceregia. Photomicrographs were taken to document the creep voids prior to etching and to document the microstructure after etching.

**Results and Discussion.** Figure 18 is a photograph of Ring Section 1 following polishing. The ring section was approximately 220 mm in diameter by 14.5-mm thick by 64 mm wide. Ring Section 1 contained an inlet port for a pigtail, as indicated in the figure. The port was approximately 31.8 mm in diameter and the wall was thicker at this location, approximately 15.9-mm thick.

![Figure 18. Photograph of Ring Section 1 after polishing.](image)

Indicated and visible on the polished surface is an OD surface breaking crack that is within the limits of the previously mentioned port. Although not visible in the figure, a crack of similar size is located on the ID surface of the port, below the visible crack.

Ring Section 2 was approximately 217 mm in diameter by 14.3 mm thick. The location of interest on the ring had a red marking.

**Metallographic Examination.** Figures 19 and 20 are photomicrographs of Met Section 1a (removed from Ring Section 1) taken adjacent to the OD and ID surface, respectively. Met Section 1a was removed near the port of the pigtail inlet. The residual life of the tube section that Ring Section 1 was removed from is approximately 20% based on the metallurgical analysis and guidelines of past studies [33]. Based on the smaller size of the individual voids in the figures compared to the thicker width of the cracks indicated in Figure 19, it ap-
pears that the material tore after the creep voids aligned. This tearing of the material suggests, but does not indicate, that high-temperature creep was involved. A high density of voids, but no cracking was observed near the ID surface. The aligning and linking up of the voids appears to follow the dendritic columnar structure adjacent to the OD surface. The location of the voids adjacent to the ID surface suggests an equiaxed fine-grained structure. The density of the voids is higher adjacent to the ID surface.

Figure 19. Light photomicrograph of Met Section 1a, taken adjacent to the OD surface and near the pigtail inlet (as polished).

Met Section 1b was removed from Ring Section 1 opposite the port of the pigtail inlet. The density of the voids throughout Met Section 1b was low. Minor aligning of the creep voids was visible adjacent to the OD surface.

Figure 20. Light photomicrograph of Met Section 1a, taken adjacent to the ID surface and near the pigtail inlet (as polished).

Figure 21 is photomicrograph of Met Section 2a (removed from Ring Section 2) taken adjacent to the OD. The residual life of the tube section that Ring Section 2 was removed from is approximately 60% based on the metallurgical analysis and guidelines of past studies [33]. Met Section 2b was removed opposite from the location marked in red on the OD surface. The density of voids in on both met sections, throughout the wall thickness, is uniform and high. The voids adjacent to the OD surface are aligned (as shown in Figure 21) and appear to follow a dendritic columnar structure. The location of the voids adjacent to the ID surface suggests an equiaxed fine-grained structure. Thus, even though Ring Section 2 had less creep damage than Ring Section 1, it still had a significant amount of creep damage.

Figure 21. Light photomicrograph of Met Section 2a, taken adjacent to the OD surface and near the red marking (as polished).

Figures 22 and 23 are photomicrographs of the microstructure of Met Section 1a (removed from Ring Section 1) near the pigtail inlet. The figures show primary carbides located at the grain boundaries and secondary carbides dispersed in the grains. The network of primary carbides is more
intact near the ID surface than near the OD surface which indicates that the OD surface was subjected to higher temperatures than the ID surface.

For the microstructure of Met Section 1a (removed from Ring Section 1), opposite the port of the pigtail inlet, the network of primary carbides is less intact and more secondary carbides are dispersed in the matrix adjacent to the OD surface as compared to the ID surface.

For the microstructure of Met Section 2a (removed from Ring Section 2), at locations 180° from each other, the network of primary carbides adjacent to the OD surface at both locations is less intact than at the ID surface. Again, the structure of the primary carbides indicates that the OD surface was subjected to higher temperatures than the ID surface. The quantity of secondary carbides appears to be similar when comparing the locations adjacent to the OD and ID surface.

**Summary.** A summary of the results follows:

- For Ring Section 1, the width of the creep cracks near the port for the pigtail inlet suggests a high temperature creep mechanism. A decarburized region near the port indicates that the material was locally overheated. The creep
cracks and decarburized region were both located adjacent to the OD surface. Aligned creep voids, but no cracking, were identified in Ring Section 2.

- The remaining lives of Ring Section 1 and 2 were estimated to be 20% and 60%, respectively.

### Creep-Rupture Strength

Jaske [34] has previously reviewed the creep-rupture strength of weldments for hot outlet headers and compared their stress-rupture behavior with that of the cast 20Cr-32Ni-Nb alloy. However, whereas as test data were available for the weldments, only manufacturers’ curves (no data points) were available for the cast base metal. The lack of such published data makes it difficult to assess the cause of the recent base metal failures of hot outlet headers. Some operators and reformer design engineers are of the opinion that the creep-strength of the 20Cr-32Ni-Nb alloy is actually well below the published curves. For this reason, they apply large design margins in establishing stress and temperature limits. Development of reliable creep and stress-rupture data for the 20Cr-32Ni-Nb alloy would be highly desirable.

### Conclusions

Hot outlet headers for steam-methane headers have historically been subject to failure issues. Early failures were related to the use of low-strength alloys until the use of Alloy 800H or 800HT or 20Cr-32Ni-Nb became the construction materials of choice. Then, more failure problems became associated with welded joints and weld metals with low creep strength. The strength and quality of weld joints has increased, but they are still prone to problems, especially when unplanned for bending loads are induced in the header. More recently, several failures have been associated with the cast 20Cr-32Ni-Nb base metal bringing into question its creep-rupture strength.

### References


