

Failure assessment of seam-welded pipe under fatigue and thermal loading

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ARTICLE INFO

Keywords:

Failure analysis
Welded pipeline
Finite element analysis
Fractographic analysis

ABSTRACT

This paper presents an assessment of the failure of a seam-welded pipeline under fatigue and thermal loading. The investigation concentrated on analyzing several factors potentially influencing pipeline failure. Both experimental analysis and numerical simulations utilizing the finite element method (FEM) were conducted to understand potential failure modes of a seam-welded SS304L pipeline. The experimental and FEA analyses investigated various failure factors including weld defects, fatigue and thermal loading, residual stresses, and overloads. The study concludes that substandard welding procedures and inadequate post-weld treatment, coupled with elevated residual stresses in the heat-affected zone (HAZ) of the weld, were the primary contributors to failure. The paper systematically outlines the utilization of experimental and FE analyses in examining practical engineering failure instances. This approach strengthens the ability to avert failures and navigate risks with precision. Furthermore, the results highlight the critical significance of adhering to code and standard guidelines concerning pipe seam welds and post-weld treatment practices.

1. Introduction

Pipeline transportation systems is one of the most efficient and reliable transportation modes and play a critical role in various industries, such as oil and gas transportation, petrochemical industries, and power plant and power generation industries [1–3]. A good portion of these pipeline systems are made of carbon and stainless steels with optimum mix of strength and toughness certified and classified by the American Petroleum Institute (API) [2]. These steels provide good combination of strength and toughness to carry various fluids and gases under practical ranges of pressure and temperature. Fusion welding has been the preferred joining process during the fabrication of these long pipeline systems. Submerged arc welding (SAW) and gas metal arc welding (GMAW) have been traditionally used for the welding process [3]. Another commonly used method of fabrication used in oil and gas industry is the Electric Resistance Weld (ERW) in which the edges of the seam are heated up electrically to the coalescence temperature and then joined by mechanical pressure without the addition of extraneous metal [4]. The welding process creates a challenge on the reliability and structural integrity of the pipeline and places an important role on the assessment and testing of the mechanical properties of parent metal and welded joints for good service life [5–7].

There are many common issues and problems that can result in pipeline leaks and failure. These include mechanical damage, seam

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weld defects, stress corrosion cracking (SCC), hard spots, fatigue, corrosion, dents and thickness reduction. Additionally, operating stresses resulting from live loads play a critical role on the integrity of the pipelines. These loads may include pressure, fatigue, thermal, and other loads. However, material and weld failure constitute the largest percentage of failures in transmission pipelines. As indicated above, the most common method of fabrication for these pipeline systems is to bend sheet metals and use seam weld along the axis of the pipe. The longitudinal seam weld along the axis of the pipe is oriented perpendicular to the main hoop stress and provides a fertile area for weld defects and discontinuities that can greatly affect the pipeline integrity. In most pipeline failures, this area is the site for crack initiation and propagation.

There are many guidelines and standards that ensure producing a reliable and robust weld. Factors like material compatibility, joint design, welding procedures and techniques, post welding treatments, welding inspection and testing, and welders training are carefully considered and discussed in details in these standards to produce seamless welds with exceptional strength and durability. The most commonly used standards in industry are: the AWS D1.1: *Structural Welding Code*, published by the American Welding Institute, AWS [8], and the British Standard BS EN ISO 15614: *Specification and Classification of Welding Procedures for Metallic materials* published by the British Standards Institute, BSI [9]. Additionally, the NACE MR0175 / ISO 15156 standards concentrate on the selection of materials, procedures and testing for welds in oil and gas production services with special attention to preventing sulfide stress cracking, stress corrosion cracking, and other forms of corrosion in these environments [10]. Quality control measures, including non-destructive testing, are implemented to guarantee that the weld meets the stringent criteria for pipeline construction, safeguarding against structural flaws and potential hazards.

Adhering to the guidelines in the above standards will normally produce a reliable weld connection. However, weld defects are generally inevitable and eliminating weld defects completely is a difficult and costly process. Weld defects in seam-welded pipes can significantly compromise the structural integrity and reliability of the pipeline, potentially leading to failure. Common defects include lack of fusion, incomplete penetration, porosity, and cracks. These defects create weak points in the weld, reduce load-bearing capacity, and act as potential sites for crack initiation. These cracks will eventually propagate under operating stresses and lead to leaks, ruptures, or complete failure [11–14]. Overloading frequently occur due to fluctuation of the operating pressure, severe variation in operating temperatures, and plugging of the pipeline during operation. Such overloads may lead to localized plastic deformation especially at the weld toe and the separation line between the weld and the parent metal. Eventually these localized plastically deformed areas will create localized grooves that will act as stress risers and crack initiation sites. It is worth noting that, to minimize the above problem, the existence of filler material (weld material) and weld separation lines in these pipe sections is considered non-compliant with Clause 6.1.3 of ASTM A312 which states that “*welded pipe shall be made using an automatic welding process with no addition of filler material during the welding process*” [15].

Residual stresses in the Heat-Affected Zone (HAZ) of seam-welded pipes is another important factor that can significantly impact the failure and overall integrity of the pipeline. The welding process essentially heats up the metal locally to a liquid or highly plastic state, fused, and then allowed to cool. The cooling and contraction process creates a zone of residual stresses that extends out into the heat affected zone (HAZ) with stresses that may be up to the yield stress of the material [16–20]. In the case of pipelines, operating stresses from pressure, suspended loads, vibrations, and other sources would be directly added to the existing residual stresses. These stresses, if not properly managed, creates weaker areas that will be potential initiation points for fatigue cracks and stress corrosion cracking and escalate the rate of crack initiation and propagation. Welding residual stresses are inevitable and various post-welding treatments to control or eliminate them are costly and may not be feasible. Pipe sections fabricated by rolling and seam welding longitudinally may have residual stresses from both the forming to the cylindrical shape and the welding of the pipe along the seam. Generally, the significant impact of weld residual stresses on the failure of seam welded pipes emphasizes the critical importance of careful welding procedures and comprehensive post-weld treatment practices in ensuring structural integrity and preventing failures

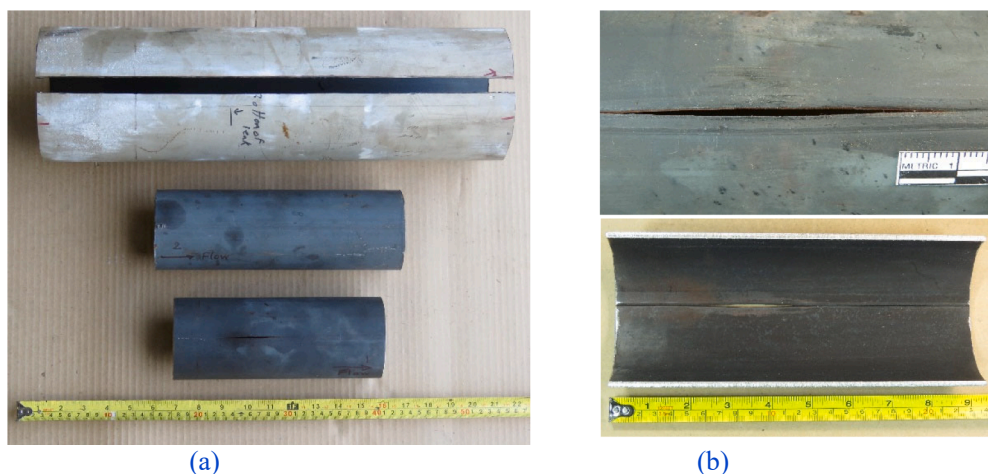


Fig. 1. (a) Failed and intact sections of the pipe, and carrier pipe, (b) Closer view of the crack seen at the toe of the weld on OD (top) and seen from the root of the weld on ID.

[16–22].

The objective of this paper is to systematically investigate and outline the utilization of experimental and FE analyses in examining a practical engineering failure case. The present study considers the effect of weld imperfection and residual stresses on the integrity and reliability of seam welded pipes. An actual failed portion of a pipeline is thoroughly examined experimentally to assess the mode and cause of failure. A three-dimensional finite element model is then created to simulate the failure process and the conditions of failure. Results from the experimental study and the numerical simulation are compared and discussed.

2. Experimental study and Methodology

2.1. Introduction and Background of the Failure

A leak and a long penetrating crack have been detected on a 3.5" outer diameter (OD) stainless steel pipe carrying Ammonium Nitrate (AN). The pipeline section is constructed from AISI 304L stainless steel manufactured to ASTM A312 Specification [15]. It was situated inside a 5" carrier pipe, where the annulus between the failed line and the carrier pipe contained steam at a temperature of approximately 175Å°C and a pressure of approximately 750 kPa. The leak was discovered through high readings shown on the steam conductivity monitoring program, where an ensuing investigation indicated that AN leaked into the steam jacket. The failed AN line operated at a temperature between 144 °C and 153 °C at an internal normal operating pressure of approximately 1,013kPa. Both the failed and the intact sections of the pipe were cut-out nearby the weld and the carrier pipe was split and opened. Fig. 1a shows an overview of all sections just before carrying the investigation.

The objective of the experimental investigation is to carry out:

- Visual examination of external and internal surfaces of the fracture surfaces.
- Performing X-Ray diffraction (XRD) analysis to check for crystalline corrosion products.
- Performing scanning electron microscope (SEM) and energy-dispersive X-ray spectroscopy (EDS) examination.
- Preparing metallographic cross-sections (micros), and evaluating material microstructure of failed and intact area micros, transversely across the weld cross-section.
- Perform hardness measurements as per NACE MR0175/ISO 15,156 [10] on failed and intact area micros, transversely across weld cross-section.

2.2. Visual Inspection

Fig. 1b shows a closer view of the failed samples just before carrying the investigation focusing on the crack area from outside (OD) and inside (ID) of the pipe. The samples were split in the longitudinal direction at the 3o'clock and 9o'clock locations away from the failure and weld. The following describes the results of the visual examination on the failed sample:

- The length of the examined part or the failed sample was approximately 23 cm. The OD of the sample was 3.5" (88.9 mm). The wall thickness (WT) of the sample was 0.120" (3 mm) on average.
- The location of failure was along the weld with evidence of a bulge and a slight "fish mouth" rupture appearance from the external surface indicating presence of plastic deformation. The length of the ruptured crack/opening was approximately 60 mm on the outside diameter, Fig. 1b (top).
- The crack partially penetrated the wall thickness of the pipe along the weld. The length of this non-penetrating crack was approximately 170 mm long on the ID and extended on both sides of the rupture/opening location, indicating initiation from the ID, Fig. 1b (bottom).
- A linear groove was evident on one side of the weld along the full length of the sample, seen from the ID.

Table 1
XRD Quantitative Results*.

Mineral Name	Compound Name	Chemical Formula	Weight %	
			Failed Sample	Un-failed Sample
Gwhibaite	Ammonium Nitrate	(NH ₄)(NO ₃)	/	62.0
Hematite	Iron Oxide	Fe ₂ O ₃	83.9	27.4
Quartz	Silicon Oxide	SiO ₂	0.6	1.2
/	Zinc Oxide	ZnO	/	0.6
Rutile	Titanium Oxide	TiO ₂	0.5	0.3
/	Chromium Iron Nickel	Cr _{0.05} Fe _{0.90} Ni _{0.05}	/	0.5
Troilite	Iron Sulphide	FeS	1.0	0.9
/	Aluminum Oxide	Al ₂ O ₃	/	2.3
/	Chromium Iron Oxide	Cr _{1.6} Fe _{0.4} O ₃	7.7	4.7
Bohmite	Aluminum Oxide Hydroxide	AlO(OH)	6.2	/

* Complete EDS and XRD spectrum graphs are given in Appendix-A.

- The internal surfaces were uniformly covered with a dark well-adhered scale, but no evidence of any significant corrosion or metal loss was observed on internal surfaces or at the rupture/opening location.

2.3. XRD and EDS Analysis

Corrosion product/scale and deposits were taken from the internal surfaces of the failed and intact samples to perform X-ray diffraction (XRD) and energy-dispersive X-ray spectroscopy (EDS) analyses. The XRD quantitative results are shown in Table 1.

The XRD results shown in Table 1 provide valuable information about the compounds present and their percentages. However, these results are not sufficient to conclude on the mechanism and reason for failure until it is combined with fractographic analysis using scanning electron microscopy (SEM) and possibly carrying slow strain rate mechanical testing (SSRT), to assess the susceptibility of the material to specific fracture modes like stress corrosion. Nevertheless, the following important observation that will be helpful in finding out the mode of failure may be deduced from the table:

- Fe, Ni, Cr, and Si: Likely originated from the 304L SS base metal. Iron (Fe) constitutes the base element of stainless steels. Chromium (Cr), Nickel (Ni), and Silicon (Si) are common alloying components of 304L stainless steel.
- Zn, Ti, and Al: These elements are present in small amounts in the XRD results. They may originate from alloying or inclusions in the stainless steels pipe, although 304L SS manufactured to specification does not contain these alloying elements. Alternatively, these elements may originate from the aqueous phase of the AN solution.
- The iron and chromium iron oxide scales were protective to the base metal pipe since no significant wall thinning or metal loss were found underneath, on both the failed and intact un-failed samples [23–25]. Similarly, iron sulphide (FeS) is known to form a tenacious protective scale layer and the presence of FeS on both the failed and intact samples is consistent with the base metal pipe experiencing no significant wall thinning or metal loss [26].
- The Ammonium Nitrate was only found in the intact sample. This is a remnant of the dominant AN in the internal media of the pipe and is consistent with the AN solution still being present in the intact pipe after failure. In contrast, the AN solution leaked completely into the steam jacket at the failed pipe section, explaining why it was not found in the corresponding XRD results.

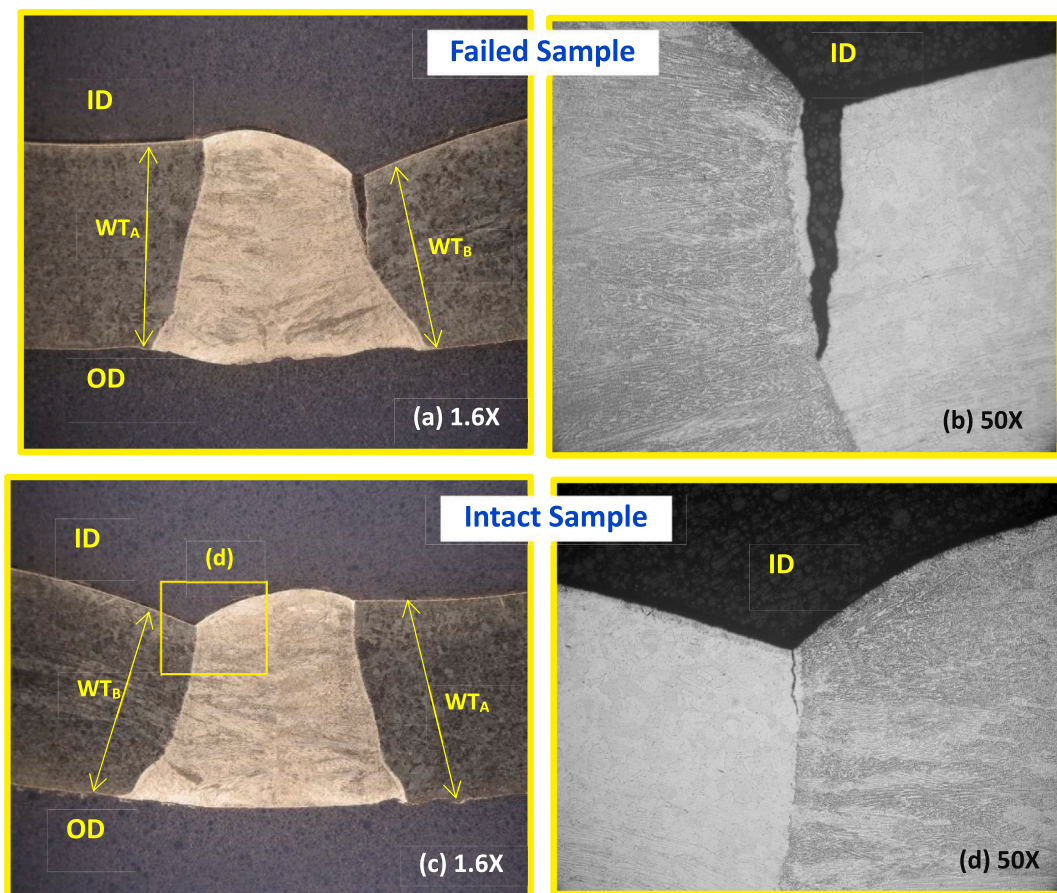


Fig. 2. (a, c) Crack Starting at the root of the weld and propagating in the base metal, Mag. 1.6X; (b, d) Crack propagating in the base metal and reaching weld metal, Mag. 50X.

- No chlorides (Cl , Cl_2), sulphates (SO_3), or phosphates (PO_3) were seen in the XRD results, which is consistent with the absence of such compounds in the internal media and the base metal. This finding is important in identifying the specific degradation mechanism of the pipe as discussed in the following sections.

2.4. Metallographic/Microstructural Examination

2.4.1. Microscopic Examination

Two metallographic cross-sections (micros), one for the failed and the other for the intact sample, were examined microscopically. Results of the examination are shown in Fig. 2. The following observation may be drawn from the figure:

- A crack penetrating approximately half of the wall-thickness (WT) was clearly visible at low magnification (1.6X) in the failed sample. Similar smaller crack was only seen at 50X magnification for the intact sample.
- Both cracks initiated from the ID at the root of the weld where a groove was found between the weld and the pipe wall and the pipe wall thickness (WT) was noticeably smaller on the crack-side of the weld compared to the opposite un-cracked side.
- Both cracks initiated and propagated in the base metal very close to the weld line in the HAZ area indicating possible high tensile stress in the area. This may be attributed to high residual stresses due to the lack of post-welding treatment to relieve or reduce residual stresses.
- There was no significant corrosion product build-up found inside the crack and smaller secondary cracks were seen emanating out from the main crack. This secondary cracking does not appear to be branching in the transgranular stress corrosion cracking (TG-SCC) sense [27–29].
- The crack propagation and the crack tip ended at the separation line between the weld metal and the pipe. This behavior is expected due to hardness variations as will be explained in the following sections.

2.4.2. Metallographic Examination

The fracture faces of the failed sample were separated and prepared for fractography and EDS examinations. The fracture face was cleaned using a procedure involving boiling Alconox, inhibited hydrochloric acid (HCl), and mechanical brushing with baking soda. Using a Zeiss Stemi 2000-C stereo-microscope, the cleaned fracture face was analyzed in four sections as shown in Fig. 3 which shows

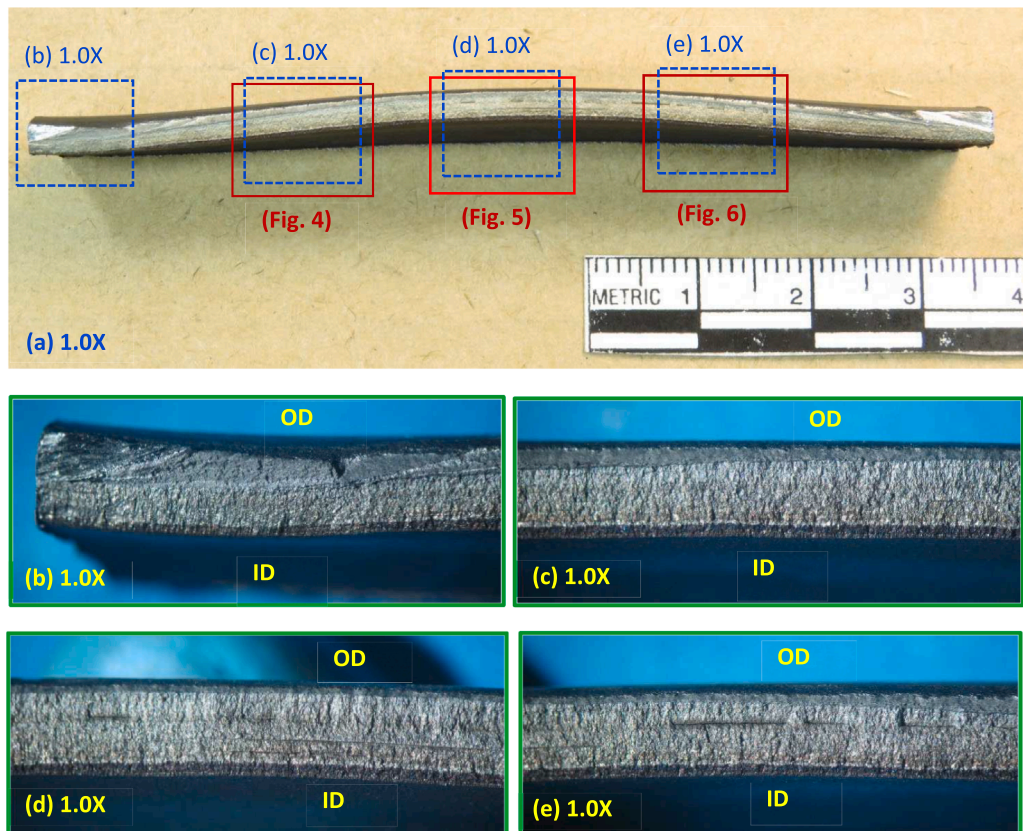


Fig. 3. (a) Fracture surface cleaned and ready for SEM examination – Mag. 1.0X. (b) Detailed view of left-most area of fracture face, (c) Detailed view of left-center area, (d) Detailed view of central area, and (e) Detailed view of right-center area (e).

the initial investigation with magnification 1X. Figs. 4 and 5 show the fractography from the SEM. The following observations are concluded from Fig. 3:

- The left-most area (Fig. 3b) of the sample showed a distinct two-feature fracture face with a flat light-colored area on the bottom which is characteristic of cleavage due to crack propagation, and a protruding darker-colored area above which is characteristic of the plastic deformation caused by ductile overload.
- The center-left and right areas (Fig. 3c, e) and the center-most area (Fig. 3d) of the sample show similar trend but with smaller plastic zone.

2.4.3. SEM Examination

Microscopic examinations at higher magnifications were conducted under a scanning electron microscope (SEM) using secondary electron images and backscatter electron techniques. The SEM images further confirmed the observations and findings from the lower magnification images discussed above. As shown in Fig. 3, the SEM examination was divided into three sections at and around the center of the fracture face. Figs. 4-6, show overviews of the three examination areas at various magnification factors using Secondary Electron Detection (SED). In addition to the validation of above-mentioned points, the SEM images indicate the following:

- Microvoid features in the protruded “shear lip/bevel” type region of the fracture face near the OD (Fig. 4a, 5a, 6a). Microvoid formation and coalescence is characteristic of ductile overload at the final stage of fracture.
- Striations in the circumferential direction were clearly visible in the cleavage region of the fracture face closer to the ID (Fig. 4c, 4d, 6c, 6d). Striations are characteristic of fatigue failures where crack propagation occurs intermittently under cyclic tensile loading.
- Secondary cracking of small and large sizes, with lengths in the circumferential direction and penetration in the axial direction, were seen throughout the cleave region of the fracture face closer to the ID (Fig. 4c, 4d, 5d, 6c, 6d).

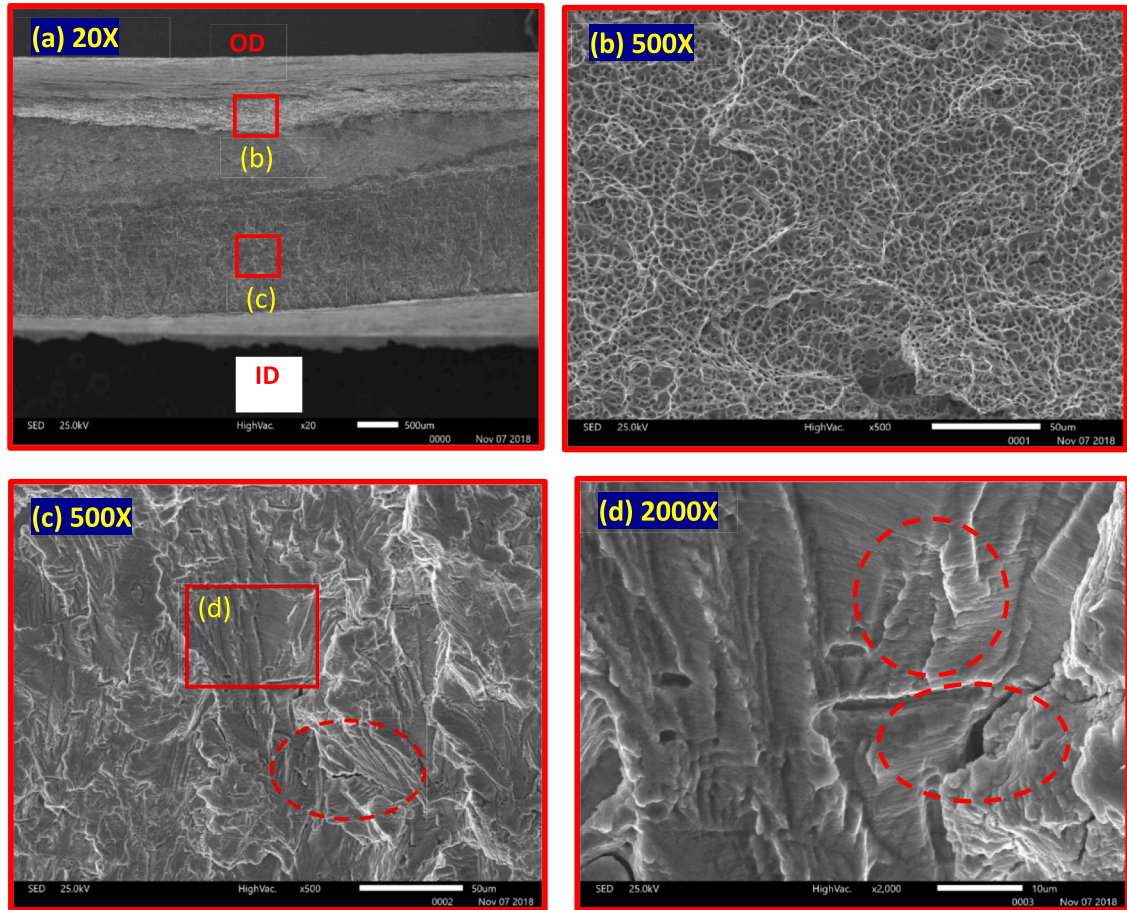


Fig. 4. (a) SEM Image of left-center area of Fig. 3a showing cleavage (bottom) and ductile overload (top) features; (b) SEM image of ductile overload area showing microvoid; (c) SEM image of cleavage area showing flat ridges and secondary cracks; and (d) Higher magnification SEM image showing striations and secondary cracks.

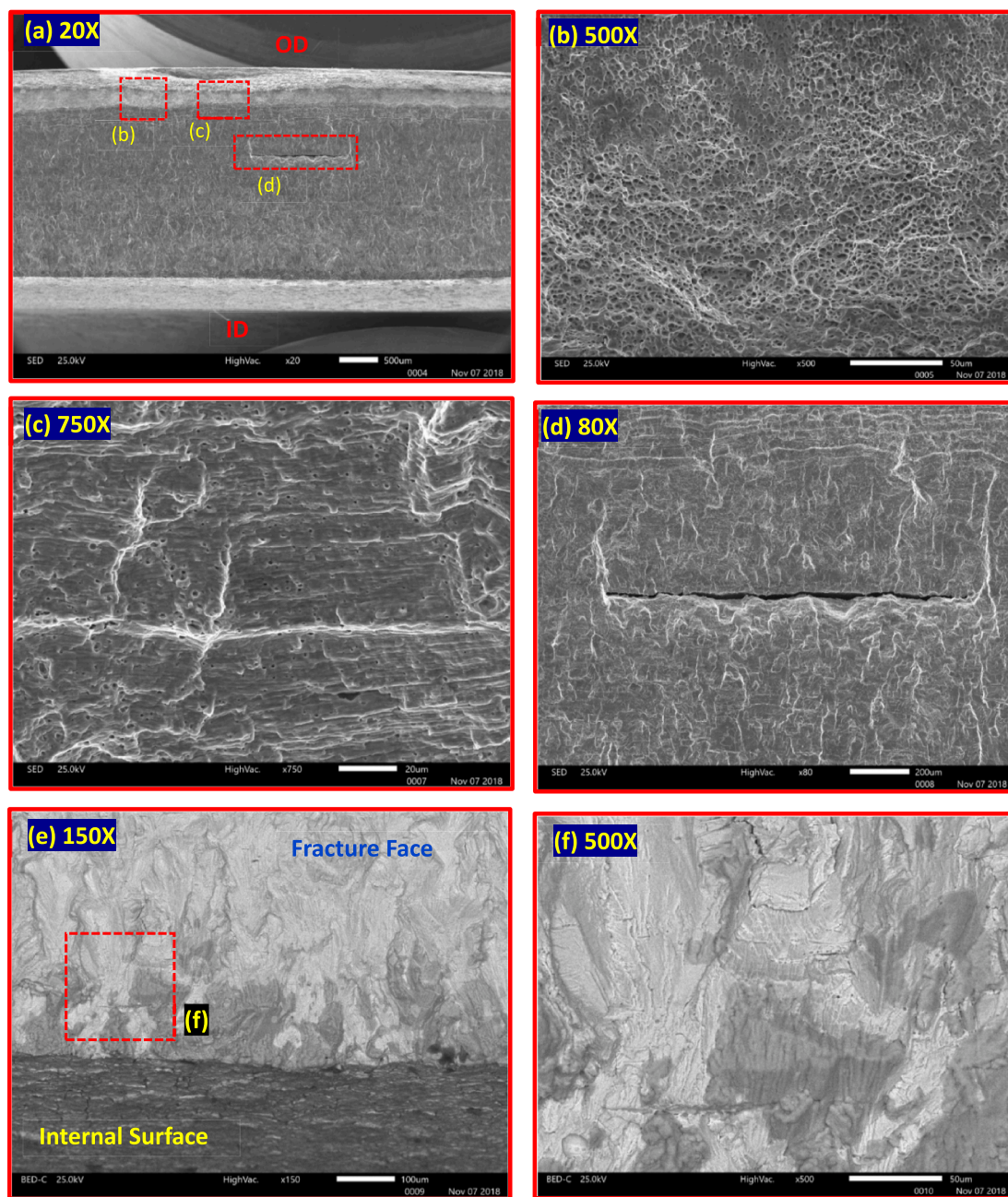


Fig. 5. (a) SEM Image of center area of Fig. 3a showing cleavage (bottom) and ductile overload (Top) features; (b) SEM image of ductile overload area showing microvoid; (c) SEM image of cleavage area showing striations; (d) SEM image of linear crack; (e) SEM image (using backscatter electron detection for contrast) of tenacious scale near ID not removed through cleaning; and (f) Higher magnification SEM image of Fig. 5e.

- The thin band of tenacious corrosion product/scale was observed at the ID edge of the sample using Backscatter Electron Detection (BED) for higher image contrast and feature visibility (Fig. 5e, 5f). This region was investigated with EDS that is discussed in the following section. Nonetheless, it is confirmed here that this thin band near the ID indeed contained a more tenacious scale that was not removed through cleaning unlike the remaining fracture faces

2.5. EDS Analysis

The elemental composition at different locations of the fracture face, specifically the base metal and remaining corrosion product/scale not removed during cleaning, were determined under an SEM using an EDS technique. It should be noted that the EDS

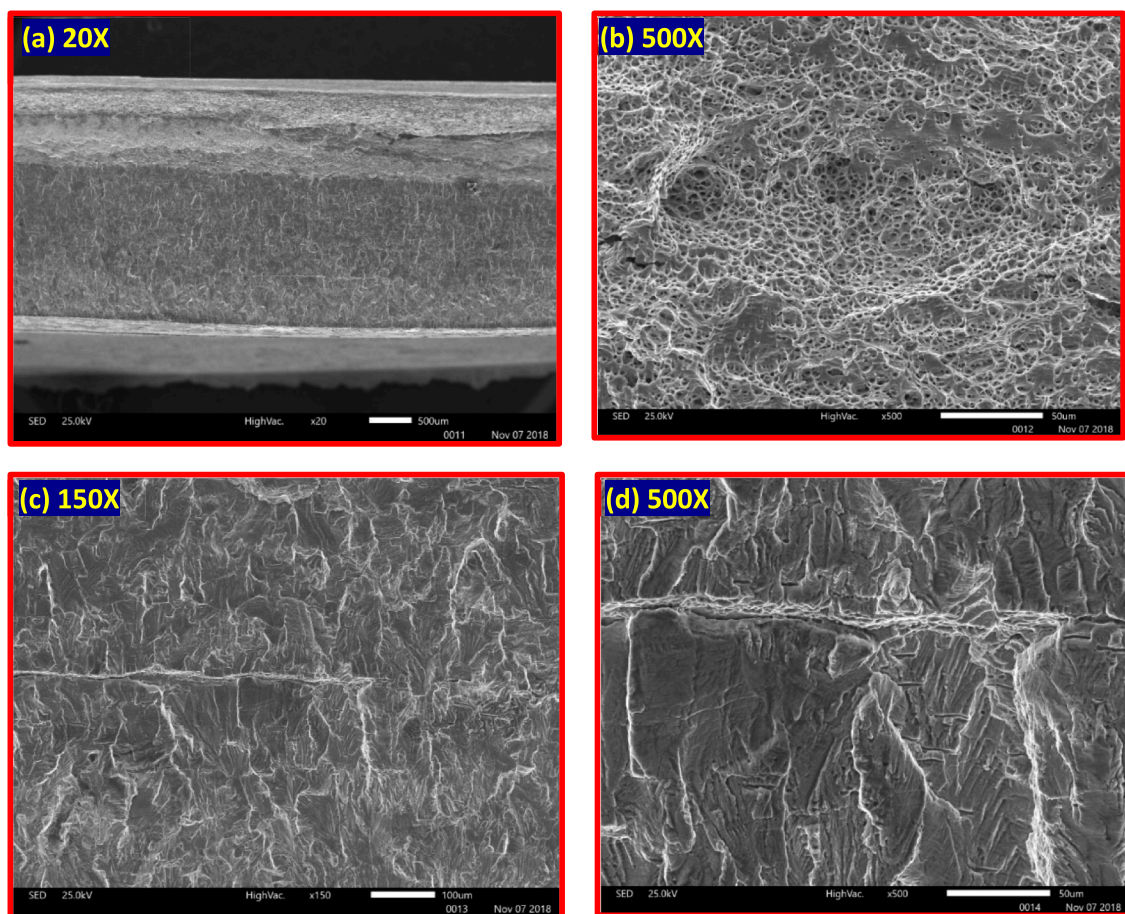


Fig. 6. (a) SEM Image of right-center area of Fig. 3a showing cleavage (bottom) and ductile overload (top) features; (b) SEM image of ductile overload area showing microvoid; (c) SEM image of cleavage area showing flat ridges and secondary cracks; (d) Higher magnification SEM image of Fig. 6c showing striations, flat ridges and secondary cracks.

examination cannot determine elements lighter than carbon (e.g., hydrogen). Results for elements with low atomic numbers (e.g., carbon) and/or with low concentrations are semi-quantitative and therefore were not provided. Table 2 shows results of elemental composition of the base metal measured in the cleavage feature area and the microvoid feature area. Comparisons are listed for the

Table 2

EDS of base metal in weight %, compared to 304L SS specification*.

Element	Base Metal		304L SS
	Cleavage Feature Area	Microvoid Feature Area	
Fe	61.67	61.34	Balance
Cr	18.42	18.69	Min: 18.0; Max: 20.0
Ni	8.56	8.79	Min: 8.0; Max: 12.0
O	4.76	4.40	/
Na	3.56	4.03	/
Mn	1.29	1.15	2.0 max
P	0.69	0.76	0.045 max
Si	0.56	0.65	0.75 max
Cu	0.30	/	/
Al	0.19	0.19	/
S	/	/	0.03 max
N	/	/	0.10 max
C	Excluded	Excluded	0.03 max

* Complete EDS and XRD spectrum graphs are given in Appendix-A.

corresponding specification for SS grade 304L.

The examination of Table 2 shows that:

- Chemical compositions of both areas examined are consistent with 304L stainless steel specifications [30].
- Sodium (Na) was found in concentrations of approximately 4 wt% in both locations examined, although 304L SS should not contain any. Since sodium is in small amounts at both locations, it is most likely from remnants of the cleaning agents used (sodium silicate and sodium carbonate in Alconox, and sodium bicarbonate or baking soda).
- Phosphorous (P) was found in concentrations of approximately 0.7 wt% in both locations examined, although 304L SS should only contain a maximum of 0.045 wt%. Since phosphorous is in equal amounts at both locations examined, it is likely to be from remnants of the cleaning agents used (complex phosphate in Alconox). Alternatively, it could be from phosphorous in the 308L filler wire/rod or equivalent metal used in welding the 304L pipe.
- Copper (Cu) was found in a concentration of approximately 0.3 wt% in the cleavage fracture area. This is likely from copper in the 308L filler wire/rod or equivalent metal used in welding the 304L pipe.
- Aluminum (Al) in a concentration of 0.19 wt% was found in both locations examined. It is likely present in small quantities as an alloying component in this 304L SS pipe.

EDS spectra were also taken at the band on fracture face near the ID where corrosion product/scale was still present. The results didn't reveal any new findings and are similar to those discussed above.

2.6. Hardness Testing

Hardness at different locations was measured on the surface of the metallographic cross-section of the failed and intact section micros after etching. The locations are based on the butt weld survey method prescribed in ANSI/NACE MR0175-ISO 15,156 [10]. Micro hardness readings were measured by Shimadzu hardness Tester Type M, under the load of 500 g (HV500g). Vickers hardness readings were converted to Rockwell C hardness in accordance with the ASTM A370 [31]. Table 3 compares the hardness of the different locations and the following observations are concluded:

- The highest hardness was measured near the crack tip of the failed sample. This is expected due to high plastic deformation around the crack tip. The second highest overall hardness region was in the left toe/root. This is where localized WT reduction (necking) was observed. Thus, it is expected to have a higher hardness due to plastic deformation of the area overall and around the crack in specific.
- All the base metal hardness results were well below the maximum hardness specified for 304L SS (approximately 200 HV).
- Aside from the increased hardness around the crack, no significant differences are found in hardness results between failed and intact samples.

It should be noted that NACE MR0175 indicates that the material limits for austenitic stainless steels shall have a maximum Rockwell C (HRC) of 22. No conversion between HRC and the HV used in the present examination is available for austenitic materials in ASTM E140, yet a conservative value based on carbon-manganese steel is 250 HV. The highest hardness value measured in Tables 3 above is well below this conservative limit.

3. Remarks on failure Mechanisms

From the above experimental study, several possible mechanisms for the observed failure in the 304L stainless steel pipe were considered:

- Stress corrosion cracking (SCC) and corrosion fatigue.
- Weld defects or violations of the approved welding procedure, and post weld treatment.
- Failure due to internal overpressure
- Fatigue failure due to cyclic loads, vibration, and high residual stresses combined with initial imperfection around the seam weld.

Table 3

Vickers micro hardness (HV500g) comparison.

Location	Failed Sample Hardness, HV _{500g}	Intact Sample Hardness, HV _{500g}	Note
Pipe Base Metal	158 ± 12	157 ± 12	Close to average hardness
Left Toe/Root	212 ± 5	185 ± 20	Close to average, scattered hard spots (crack)
Right Toe/Root	183 ± 6	175 ± 19	Close to average, scattered hard spots (crack)
Weld Metal	194 ± 21	187 ± 13	Close to average, scattered hard spots
Fusion Zone	195	177 ± 6	Close to average, increased hardness
Crack Tip	226	—	Highest hardness point on micro

SCC and corrosion fatigue were discarded as the failure mechanism:

- 304L SS is an austenitic SS, which is not susceptible to corrosion or SCC from the internal media (AN solution) or the external media (steam) of the present situation [27–29].
- Although the internal media contained an aqueous phase, dissolved chlorides were absent as indicated by the XRD results. Austenitic SS will not experience SCC in water without dissolved chlorides at the service temperature of the present situation.
- Cracking at and near the failure location was in singular transgranular (TG) form. Characteristic SCC is typically either branched TG or intergranular (IG) form, depending on the service conditions and material [28].

Weld defects or violations of the approved welding procedure were discarded as the primary failure mechanism; however, improper post-weld treatment is likely to be one of the main factors contributing to the failure:

- The fracture did not occur or pass through the weld metal, and did not initiate at the separation line between the weld and the pipe, indicating no apparent lack of fusion.
- The use of low carbon base metal variants (i.e., 304L SS) minimizes the susceptibility of weld decay, sensitization, or cold-cracking. Additionally, the maximum hardness in the weld and surrounding regions, even at locations plastically deformed, was less than a reference conservative material limit of 250 HV (or 22 HRC) established by NACE MR0175.
- A filler material (weld metal) was used in the weld of both samples analyzed. This is clear from the differing microstructures on the base metal versus the weld metal. The existence of a filler material in these pipe sections is non-compliant with Clause 6.1.3 of ASTM A312 [15] which states that “*welded pipe shall be made using an automatic welding process with no addition of filler material during the welding process*”.
- Another important reason for considering weld procedure and post weld treatment to be a contributing factor to the failure is the existence of main crack leading to failure as well as secondary crack in the HAZ area very close to the weld line where high residual stresses are expected if no post weld treatment is performed [16–20]. These residual stresses are tensile stresses that can reach up to the yield point of the material. This point will be investigated more in the finite element simulation section to follow.
- The existence of linear groove on both samples near the weld line and the reduction in wall thickness may have resulted from weld procedures. These cause stress concentration and present a potential source for crack initiation.

Internal overpressure was discarded as the main failure mechanism:

- The fracture surface at the failure location exhibited both cleavage and ductile overload features. The ductile overload feature was approximately 10 % of the WT, suggesting the pipe could not contain the pressure when the crack grew to a critical length, not due to overpressure.
- It is possible that the localized WT reduction at the crack initiation points occurred due to an overpressure causing plastic deformation. This may have also been due to improper weld procedure, inadequate post weld-treatment, and other manufacturing defects.

Based on material evidence, fatigue from pressure cycles was also found to be a contributing factor to the failure:

- Fatigue results in a single, non-branched, transgranular crack perpendicular to the surface, consistent with what was found at the failed location.
- Striations in the axial direction were clearly visible in the cleavage region of the fracture face closer to the ID. This is characteristic of fatigue failures where crack propagation occurs intermittently under cyclic loading.
- Cyclic tensile stresses could be the result of pressure variations in the line due to pump pressure pulsations and mechanical vibrations.
- No significant corrosion products were found on the fracture faces or embedded in the cracks in the micros. However, high stresses at the crack tip are expected to affect the protectiveness of the oxide film generated on stainless steel surfaces, in a mechanism known as film-rupture.
- A groove was present at the weld toe before the crack initiation and propagation. The internal surfaces of the groove exposed to media developed a much more tenacious corrosion product/scale, compared to the fracture faces gradually exposed afterwards. There are a few possibilities which can explain the existence of this groove prior to crack initiation, listed below in order of likelihood:
- A pre-existing manufacturing artifact of the butt-weld end preparation prior to the actual welding. The WT ratio for both the fractured and intact samples was approximately 10 % less in WT than the nominal. This may be a result of butt-weld end preparation, prior to bending the plate for welding.
- An internal overpressure event, possibly from previous plugging of the line plastically deformed the area near the weld seam, leaving behind the groove.
- The groove near the weld toe created the necessary stress concentrations to initiate the fatigue crack. In the ASME B31.3 code for pressure piping [32], the pressure design thickness for a straight pipe under internal pressure is calculated using the following equation:

$$t = \frac{PD}{2(SEW + PY)} \quad (1)$$

Where: $D = 88.9 \text{ mm}$ is the outside diameter of the pipe, E is the quality factor from Table A-1A [32], $P = 1.323 \text{ MPa}$ is the maximum operating internal pressure, S is the allowable stress value for material from Table A-1 [32], $t = 2.55 \text{ mm}$ ($0.85 \cdot \text{WT}$, based on 15 % WT groove) is the design thickness, $W = 1$ (for 3xx SS at temperature $< 300\text{F}$) is the joint strength reduction factor, $E = 0.80$ (single butt seam), and $Y = 0.4$ is the coefficient from Table 3 04.1.1 [32]. Solving equation (1) for the stress value, we get $S = 28.1 \text{ MPa}$.

- Based on an allowable stress value of 16.7 ksi or 115 MPa for 304L SS pipes [32] and tubes at 150 °C or 300°F, the ratio of nominal stress to maximum stress is:

$$R_{\sigma_nom} = \frac{\sigma_{nom}}{\sigma_{max_allow}} = \frac{28}{115} \cong 25\% \quad (2)$$

However, in the presence of a crack, and using a stress concentration factor of 3, the ratio of stress at the crack tip to maximum allowable stress is:

$$R_{\sigma_crack} = 3 \bullet R_{\sigma_nom} = \frac{3 \bullet \sigma_{nom}}{\sigma_{max_allow}} = \frac{84}{115} \cong 75\% \quad (3)$$

The R_{σ_crack} value of 75 % in this situation is greater than the typical fatigue threshold stress of 30 % specified minimum yield strength, hence fatigue crack propagation was possible.

4. Calculation of fracture measures in finite Elements

Finite element analysis finds extensive application in simulating fracture mechanics problems across diverse fields of engineering.

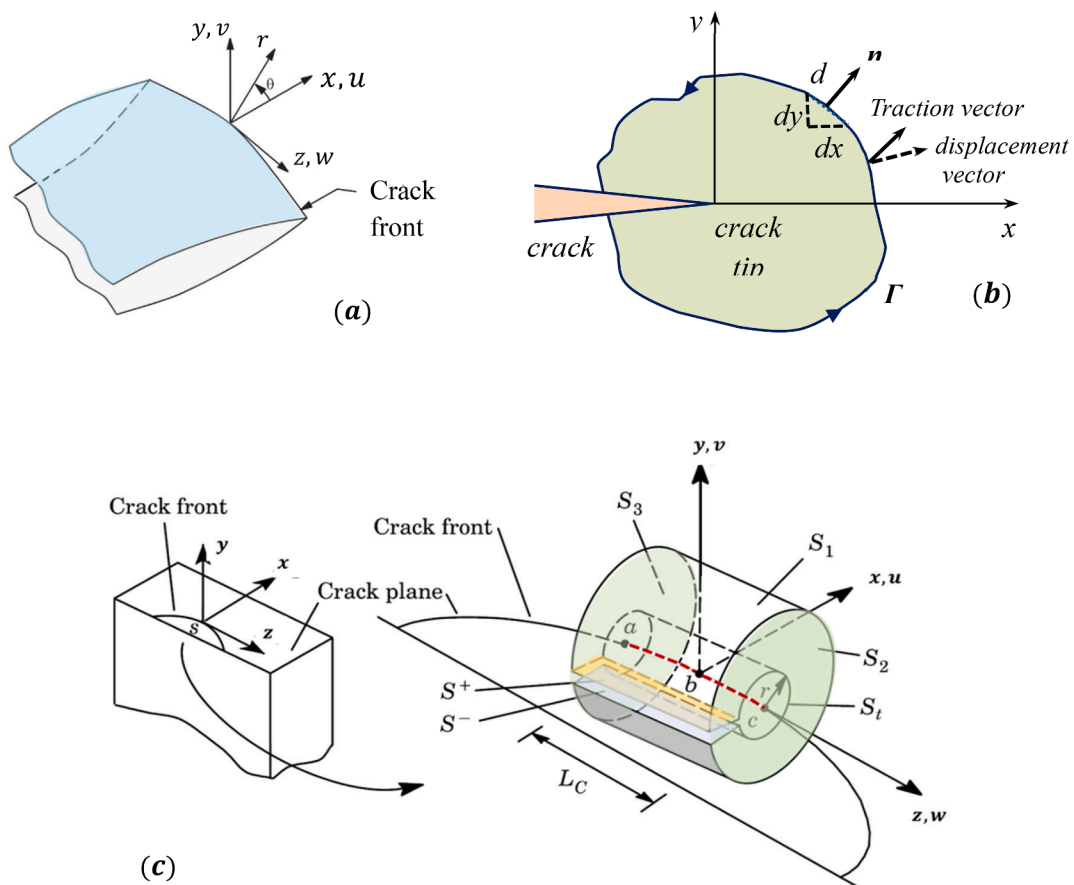


Fig. 7. (a) Local coordinate system at the crack tip for 3D crack, (b) Contour around a crack tip and the nomenclature for the J-integral, and (c) Domain used to compute the volume integrals at crack-front for 3D cracks [36,37].

Both two-dimensional and three-dimensional structures featuring various crack types can be effectively modelled using FEM techniques. Several fracture mechanics measures are employed to assess the criticality and the of cracks on the overall integrity and performance of the structure. Among these measures, the stress intensity factors serve as key indicators, quantifying stress concentration severity at crack tips and facilitating crack growth and failure prediction. Additionally, the J-integral and energy release rates are instrumental in evaluating failure and crack propagation in structures subjected to complex loading conditions. The current study utilizes the above measures to investigate the failure conditions of the seam weld.

In finite elements, calculating the stress intensity factors usually require the use of singular crack-tip elements, whereas parameters that are based on the energy of the system such as the *J*-integral may be calculated with or without the use of singular elements. There are many different ways to calculate the stress intensity factor in FEA. Each is preceded by analysis of the cracked body. The stress intensity factor can, then, be calculated using the stiffness-derivative method, the displacement extrapolation method, the *J*-integral method, the direct displacement substitution method, etc. Ansys uses the displacement extrapolation method for calculating the stress intensity factors [34]. One important advantage of this method is that it can be used in calculating the stress intensity factor for cases of mixed mode fracture. The method is based on calculating the stress intensity factor at different nodes that share the same angle θ (refer to the coordinate system in Fig. 7a) but different radii r . The results are then extrapolated to get the stress intensity factor value at the crack tip. The displacements used for extrapolation near the crack tip for elastic isotropic materials are given by:

$$\begin{aligned} u_x &= \frac{K_I}{4G} \sqrt{\frac{r}{2\pi}} \left[(2\kappa - 1) \cos \frac{\theta}{2} - \cos \frac{3\theta}{2} \right] - \frac{K_{II}}{4G} \sqrt{\frac{r}{2\pi}} \left[(2\kappa + 3) \sin \frac{\theta}{2} + \sin \frac{3\theta}{2} \right] \\ u_y &= \frac{K_I}{4G} \sqrt{\frac{r}{2\pi}} \left[(2\kappa + 1) \sin \frac{\theta}{2} - \sin \frac{3\theta}{2} \right] - \frac{K_{II}}{4G} \sqrt{\frac{r}{2\pi}} \left[(2\kappa + 3) \cos \frac{\theta}{2} + \cos \frac{3\theta}{2} \right] \\ u_z &= \frac{2K_{III}}{4G} \sqrt{\frac{r}{2\pi}} \sqrt{r} \sin \frac{\theta}{2} \end{aligned} \quad (4a)$$

The factor κ is given by:

$$\kappa = \begin{cases} (3 - 4\nu), & \text{for plane strain} \\ \left(\frac{3 - \nu}{1 + \nu} \right), & \text{for plane stress} \end{cases} \quad (4b)$$

Where u, v, w are the displacements in the local Cartesian coordinate system (refer to Fig. 7a), r, θ are coordinates in the local cylindrical coordinate system measured from a 3D crack front as shown in Fig. 7a, G is the shear modulus, K_I, K_{II}, K_{III} are the stress intensity factors for mode I, II, and III, and ν is Poisson's ratio.

As indicated above, the J-integral is another fundamental measure in fracture mechanics and it represents the total energy required to propagate a crack in a material under complex loading conditions. The measure was initially introduced by Rice [35] by applying the minimum potential energy concept to crack problems. Referring to Fig. 7b, the J-integral takes the following form for two-dimensional crack problems:

$$J = \int_{\Gamma} \left(W dy - \mathbf{T} \cdot \frac{\partial \mathbf{u}}{\partial x} ds \right) = \int_{\Gamma} \left(\sigma_{ij} d\epsilon_{ij} dy - T_i \frac{\partial u_i}{\partial x} ds \right) \quad (5)$$

Where the coordinate system is shown in Fig. 7b, $\mathbf{T}$ is the traction vector (independent of crack size and assuming crack faces to be free of traction), the contour Γ is an arbitrary contour enclosing the crack tip, S is the arc length along the contour, $\mathbf{u}$ is the displacement vector, $\sigma_{ij}, \epsilon_{ij}$ are the stress and strain tensors, respectively, and W is strain energy density.

For 3D cracks, Shih et al. [36] generalized Eqn. (5) into volume integrals suitable for numerical evaluation in the form:

$$\bar{J}(s) = \int_V \left(\sigma_{ij} \frac{\partial u_j}{\partial x} - W \delta_{ii} \right) \frac{\partial q}{\partial x_i} dV + \int_V \frac{\partial}{\partial x_i} \left(\sigma_{ij} \frac{\partial u_j}{\partial x} - W \delta_{ii} \right) q dV \quad (6)$$

In Eqn. (6) the $\bar{J}(s)$ is the energy released per unit advance of crack front segment L_c (refer to Fig. 7c [37]), surfaces S^+, S^-, S_1, S_t, S_2 and S_3 shown in Fig. 7c, enclose the simply-connected volume of integration V , and surface S_t shrinks to the crack front (i.e., $r \rightarrow 0$). The above development is carried for elastic, homogeneous materials under quasi-static, isothermal loading with no body forces and assuming that the crack-faces are traction free. In Eqn. (6) the factor q is a scalar weight-function that may be assigned a smooth variation from zero on surfaces S_1, S_2 , and S_3 to a value of 1.0 at location s on S_t . Shih et al. [35] assumed that the energy release rate varies slowly along crack segment L_c , and thus obtain an expression for the J-integral value at point b on the 3D crack front as:

$$J(s) = \frac{\bar{J}(s)}{\int_{L_c} q(s) ds} \quad (7)$$

In conclusion, the use of the J-integral method has proved to a powerful measure in the analysis of linear, nonlinear, plasticity, and large strain problems. In particular, the conjunction with adaptive meshing and Arbitrary Lagrangian-Eulerian (ALE) formulations,

represents a crucial advancement in computational fracture mechanics [38].

4.1. Finite element modeling and Results

In order to assess the conditions leading to the failure of the pipeline and the conclusions of the experimental study, a finite element model was developed using the Ansys software package [38]. Based on the experimental findings discussed above, the model focusses on two main issues: investigating the effect of small initial cracks, and examining the impact of the residual stress level on the life prediction of the pipeline. First, the model considers a very small thumbnail crack on the inside surface of the pipe (ID), and examines various factors affecting the failure and fatigue crack propagation. Second, the model considers the actual final crack that led to the failure of the pipe and the impact of residual stresses on the stress intensity factor and the final fracture. Prior to carrying the simulation, a mesh sensitivity analysis was performed by solving an initial textbook problem of a thumb-nail crack using various mesh intensities.

4.2. Mesh sensitivity Analysis

In this study the actual pipeline dimensions and load were used to assess the accuracy of various mesh intensities on the calculation of the stress intensity factor at the tip of a small thumb-nail crack. Fig. 8 shows the modelled portion of the pipeline with details of the assumed crack for mesh sensitivity analysis.

The actual dimensions and material properties of the pipe (for stainless steel 304L) is used in this analysis. The data used along with the dimensions of the assumed crack are given below:

Internal diameter: $D_i = 83 \text{ mm}$ External diameter: $D_o = 89 \text{ mm}$

Length of pipe portion: $L = 280 \text{ mm}$ Young's modulus: $E = 193 \text{ GPa}$

Poisson's ratio: $\nu = 0.31$ Crack length: $2c = 6 \text{ mm}$

Crack depth: $a = 0.6 \text{ mm}$

The dimensions of the assumed crack have been selected to approximate the characteristics of the actual crack observed in the pipe. This alignment ensures that the modeled crack is representative of the practical defect, allowing for more accurate analysis and comparison. Conventional finite elements can be used to model cracks, but they do not accurately represent the singular stress field near the crack tip unless an extremely dense mesh is employed. This results in inaccuracies in modeling deformation and the stress fields. To address this, special singular finite elements have been developed to more effectively simulate the stress concentration and the singularity at the crack tip, making it easier to evaluate fracture parameters. These specialized elements eliminate the need for an excessively dense mesh around the crack tip. One such element, the quarter-point tetrahedral isoparametric element, is straightforward to use [39]. It is created by adjusting the mid-point of a regular higher-order isoparametric element by moving it a quarter of the element's side length toward the crack tip.

For the Semi-elliptical crack, the tool provided by Ansys [38] was used to generate the crack by providing major radius, minor radius, and largest contour radius. The loading is assumed to be internal pressure. Results are shown for two different conditions; one with no pressure on the crack faces and the other with pressure on the crack faces. Fig. 9 illustrates the mesh configuration and generation at the crack tip. Fig. 9a provides a schematic of the mesh generation using the Ansys program. At the crack edge, three circumferential divisions are specified, and six mesh contours are defined. Additionally, divisions along the crack front must be set. Fig. 9b and 9c offer close-up views of one of the crack edges. The innermost layer, shown in Fig. 9d, uses quarter-point tetrahedral elements oriented towards the crack tip. The remaining layers and the surrounding mesh consist of standard second-order tetrahedral isoparametric elements. The objective here is to study the impact of mesh intensity on the accuracy of calculating the stress intensity factor at the crack tip and arrive at an intensity that optimizes the accuracy and the CPU time. For this analysis, three different meshes were used with element counts of: 105,000; 176,831; and 556,494. As indicated above, tetrahedron second order elements were used in all meshes. The selection of the optimal mesh configuration was based on the model that provided the most reliable predictions when compared to the analytical solution. Table 4 shows a comparison between the FE results and the analytical results [40,41]. The analytical results are obtained by using the following relation [41]:

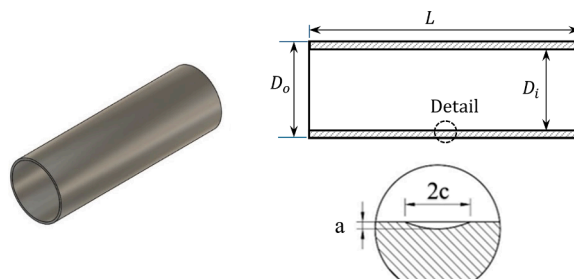


Fig. 8. Portion of the pipeline and assumed crack for mesh sensitivity analysis.

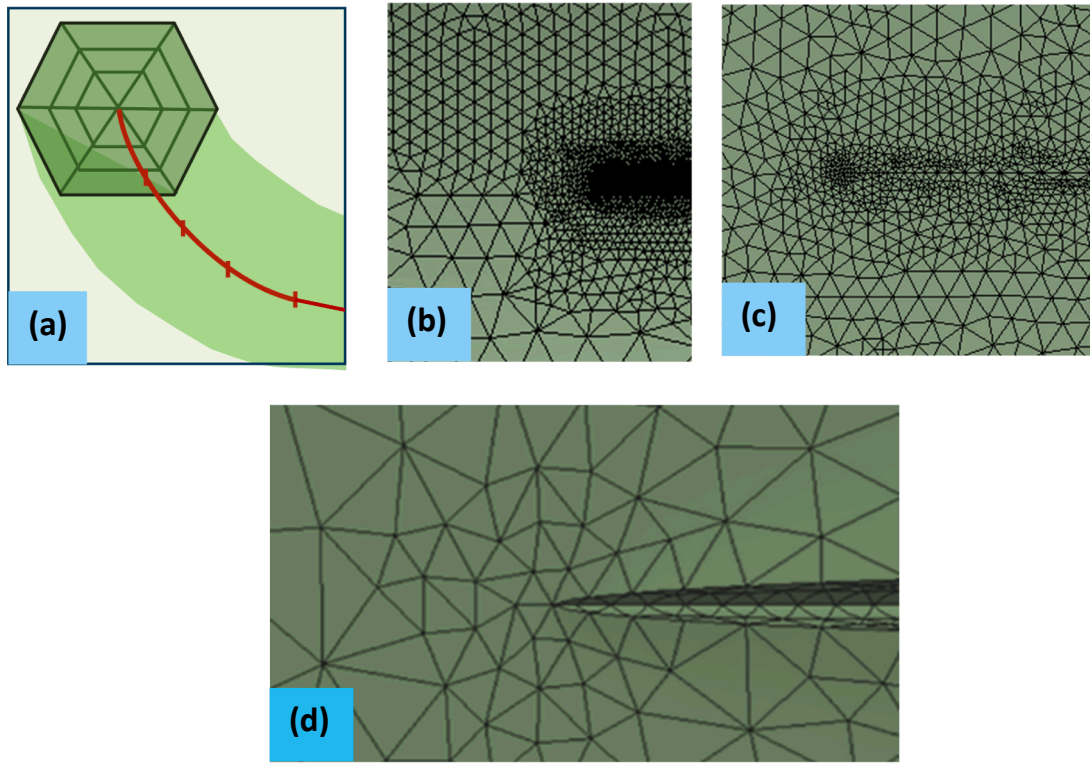


Fig. 9. Mesh details at the crack tip, (a) Schematic of mesh generation, (b, c) Zoom at the crack tip, (d) More zoom showing circumferential divisions and mesh contours.

$$K_I = MM_k \sigma \sqrt{(\pi a/Q)} \left[\sin^2 \theta + (a/c)^2 \cos^2 \theta \right]^{(1/4)} \quad (8)$$

Where: M is the magnification correction factor, M_k is the front face correction factor, σ is the applied hoop stress, Q is the shape factor for surface flaw, and θ is the angle measured from the major axis of the semi elliptical flaw with $\theta = (\pi/2)$ giving the peak value. Various factors have been obtained from charts and tables in reference [41] for the specific case. The maximum value of K_I obtained analytically is $165,792 \text{ Pa}\sqrt{\text{m}}$.

Results indicated that grid 2, with an element count of 176,831, demonstrated an acceptable percentage error of 2.71 %, with reasonable cost. The difference between the FE results and the analytical results is considered acceptable since the analytical results are based on approximate solutions with inherent assumptions and correction factors such as the flaw shape parameter and free surface correction factors. Increasing the mesh density by 215 % (from 176,831 to 556,494 elements) only decreased the error from 2.71 % to 1.55 %. Accordingly, grid 2 was adopted for the remaining analysis in this work. The results are summarized in Table 4, and Fig. 10 shows the variation of the stress intensity factor along the crack edge. An additional verification is carried out by calculating the J-integral along the crack front, then calculating the stress intensity factor utilizing the following relation for linear elastic fracture mechanics:

$$K_I = \begin{cases} \sqrt{EJ/(1-\nu^2)} & \text{for plane strain} \\ \sqrt{EJ} & \text{for plane stress} \end{cases} \quad (9)$$

The results are shown in Fig. 10 and Table 4, and further indicates the reliability of the analysis.

Table 4

Mesh sensitivity analysis.

CASE	Elements	$K_I(\text{kPa}\sqrt{\text{m}})$		Error % (Reference to analytical solution)	$J\text{N/m}$	$K_I(\text{From } J)\text{kPa}\sqrt{\text{m}}$
		No pressure on crack faces	With pressure on faces			
Case 1	105,000	121,960	137,442	17.1	0.079	129,877
Case 2	176,831	141,520	161,290	2.71	0.112	154,642
Case 3	556,494	142,450	163,220	1.55	0.114	156,017

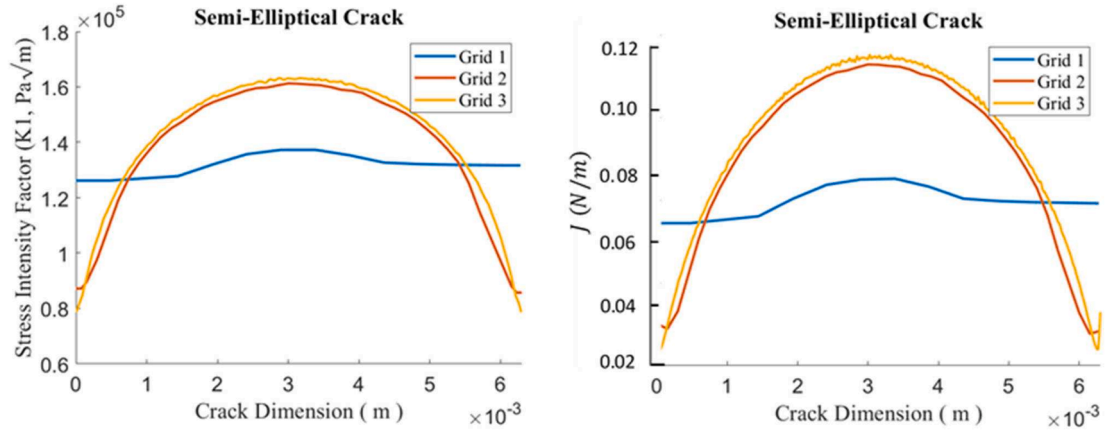


Fig. 10. Variation off stress intensity factor and J-Integral along crack edge.

4.3. Effect of initial imperfection on the fatigue Life

The effect of initial imperfections in welded joints on the fatigue life is a critical aspect in structural integrity assessment. Welding introduces inherent imperfections such as weld toe cracks and irregularities, weld misalignment, and weld profile variations, which can act as stress concentrators and significantly influence the fatigue behaviour of the structure. In the example in hand, the experimental study indicated the existence of a groove along the seam weld, secondary hairline cracks, as well as reduction in the wall thickness across the weld for both fractured and intact samples. These initial imperfections serve as nucleation sites for fatigue cracks, accelerating the initiation and propagation of cracks during cyclic loading. This section investigates the effect of the existence of a small hairline thumbnail crack on the inside surface of the pipe on the fatigue life.

An initial hairline thumbnail crack is assumed on the inside surface of the pipe as shown in Fig. 7. The dimensions of the crack are: $2c = 3 \text{ mm}$, $a = 0.2 \text{ mm}$. The loading is assumed to be the operating internal pressure, $p = 1,013 \text{ kPa}$, with $\pm 15\%$ pulsation. This pulsation was based on the manufacture specification for the operating pump which specifies the maximum pressure and pulsation that may be supplied by the pump. A simple Paris law for crack propagation is assumed in the form [41]:

$$\frac{da}{dN} = c(\Delta K)^m \quad (10)$$

Where: (da/dN) is the crack growth rate per cycle, N is the number of cycles, ΔK is the variation in the stress intensity factor due to fatigue loading, c and m are material constants. The values of the Paris law constants are taken as: $m = 2.25$ and $c = 2.5e - 19$ [41,42]. The chosen values are for stainless steel 304L tested in air and will be different in another environment. However, the intent is to get a qualitative assessment rather than exact values.

Fig. 11 shows the progression of the thumbnail crack through the thickness under the assumed pressure pulsation. It is noted that the crack quickly propagated to cover the whole thickness while the crack opening in the circumferential direction at the inner surface is very small (approximately 1.2 mm when the crack tip reached the OD). The crack length in the axial direction at the ID at this stage is approximately $2c = 6.5 \text{ mm}$. Fig. 12 a, c shows the crack depth progression versus the number of cycles for no residual stresses and for a residual stress value equal to 40 % of the yield strength, respectively; whereas Fig. 12 b, d shows the variation of the stress intensity factor with the crack depth for the same two cases. Fig. 11 a, c shows the high impact of residual stresses on the crack propagation. The number of cycles reduced from 3.75×10^5 cycles to 0.11×10^5 cycles for a residual stress level of 40 % of the yield strength. On the other hand, the maximum stress intensity factor increased from $0.7 \text{ MPa}\sqrt{\text{m}}$ to $18 \text{ MPa}\sqrt{\text{m}}$. This clearly indicates that the stress pulsations alone cannot cause the fracture since the value of $0.7 \text{ MPa}\sqrt{\text{m}}$ is far below K_{IC} of $65 \text{ MPa}\sqrt{\text{m}}$ which is the critical value of stress intensity for stainless 304L [41]. The existence of residual stresses significantly increases the stress intensity value to $18 \text{ MPa}\sqrt{\text{m}}$. This is still much lower than K_{IC} which may be explained by two reasons. First, the level of the residual stress in the pipeline may be higher than 40 % of the yield. Second, the final crack in this analysis is much smaller than the one found experimentally that caused the failure (experimentally $2c = 170 \text{ mm}$ on the ID, as opposed to $2c = 6.5 \text{ mm}$ in this analysis). These two factors are investigated in the following section.

4.4. Effect of residual stresses on the fatigue Life

Applying residual stresses in the present finite element model for the seam-welded pipe presents several challenges. One major difficulty lies in accurately replicating the complex distribution and magnitude of residual stresses that develop due to the welding process. These stresses are influenced by various factors such as weld procedure and type, weld geometry, thermal cycles, and material properties. Additionally, the detailed picture of the residual stresses can vary significantly along the length and thickness of the pipe. Accurately incorporating these stresses requires computationally advanced and intensive modeling techniques, such as thermo-

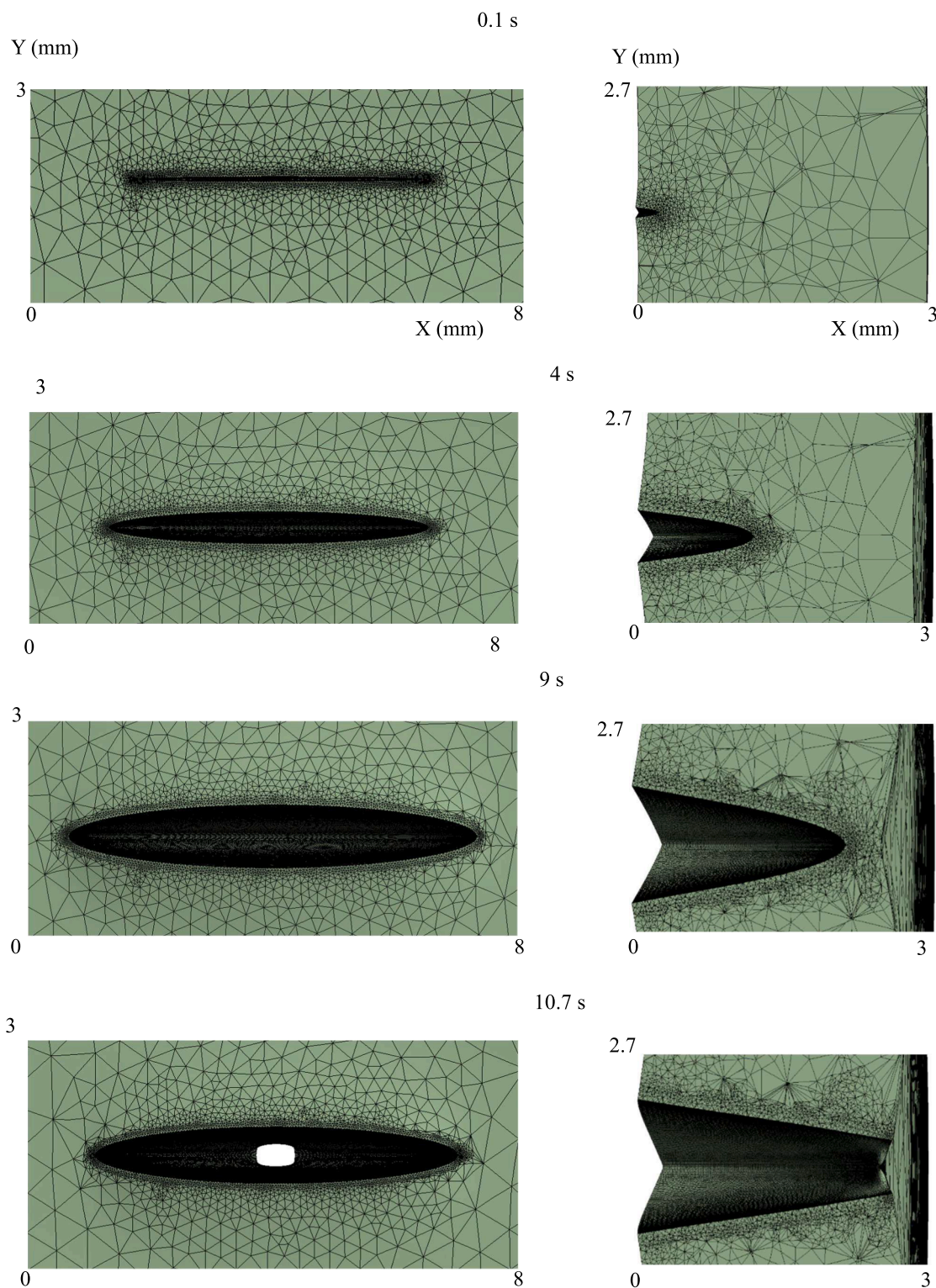


Fig. 11. Thumbnail crack propagation through the thickness under fatigue loading.

mechanical simulations coupled in many cases with experimental or empirical data integration. In finite element modeling, several approaches can be employed to incorporate known residual stresses into a simulation. One common method involves directly inputting residual stress data as initial stress or initial strain or eigen strain conditions in the model. Another approach is to apply a thermal

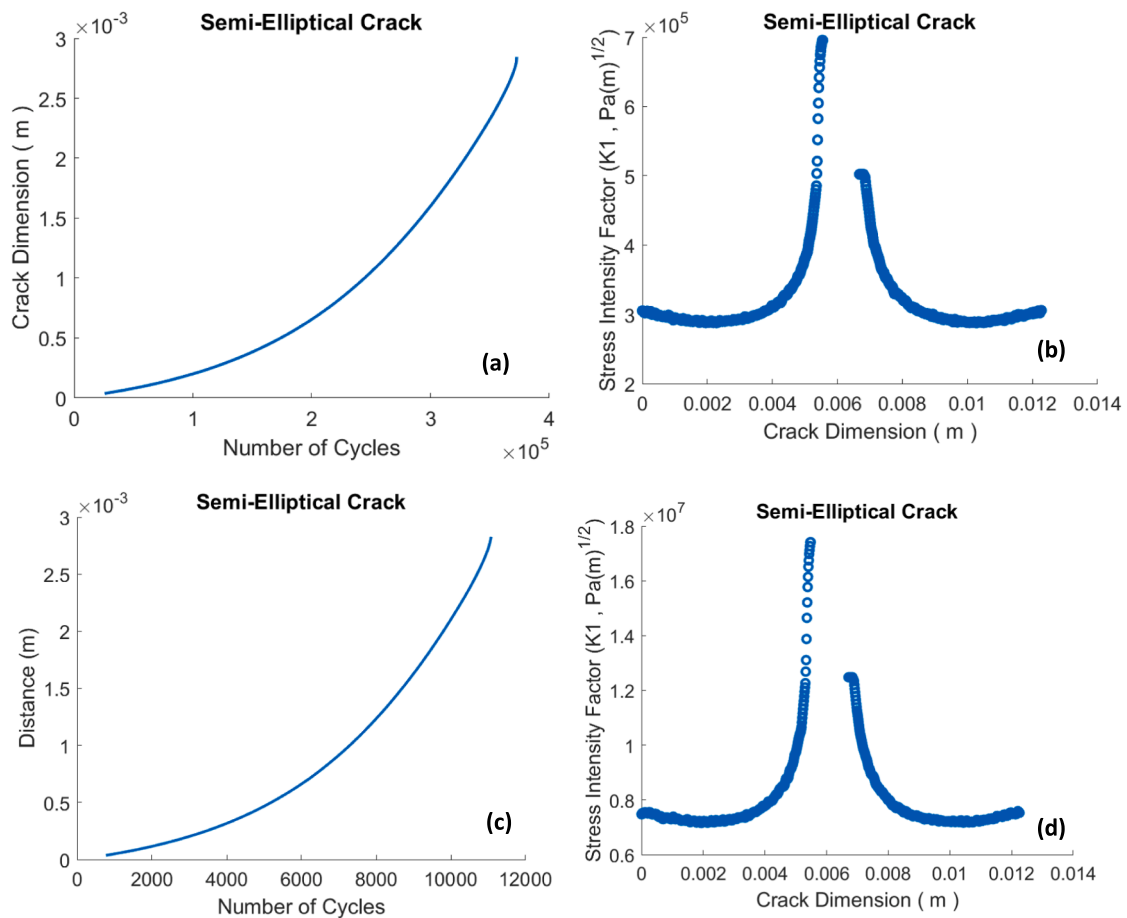


Fig. 12. (a) Crack depth progression vs. number of cycles for an assumed initial thumbnail with no residual stresses, and (c) with 40% of S_y residual stresses. (b) Variation of maximum stress intensity factor at the crack tip vs. crack progression with no residual stresses and (d) with 40% of S_y residual stresses.

gradient that replicates the thermal history of the welded component, then residual stresses can be simulated indirectly. For welded structures, residual stresses can also be incorporated through a thermo-mechanical weld simulation process that involves sequentially applying weld passes and allowing for thermal contraction to generate stresses similar to those produced during actual welding.

Another practical approach for calculating the stress intensity factor (SIF) in seam-welded pipes with residual stresses is to treat the residual stress (RS) as crack face traction (CFT) on the crack face and apply the principle of superposition [33]. This method effectively captures the impact of pre-existing stress fields, which is particularly important in welded structures. By modelling RS as CFT, the approach simplifies the analysis by breaking down the problem into more manageable components, allowing for a clearer isolation of the RS contribution to the overall SIF. The accuracy of the CFT method can be further enhanced by employing a specific crack-block mesh density around the crack. Studies have shown that using the CFT method with an optimized crack-block mesh density yields results within 1 % of those obtained through analytical methods [33].

In the current analysis, the primary objective is to determine the stress intensity factor (SIF) for a crack located in the Heat-Affected Zone (HAZ) of the pipe, and detailed stress distribution in the entire pipe is not a concern. Accordingly, a practical and simple approach is to apply a uniform tensile hoop stress that represents the residual stress in the HAZ. By doing so, the model can focus on the local effects around the crack and the analysis of the SIF without the need for a detailed representation of residual stresses across the entire pipe. This approach reduces computational complexity and resource requirements. Although this method does not capture the intricate stress variations within the HAZ or the rest of the pipe, it is often sufficient for evaluating the crack's influence on structural integrity.

To assess the stress intensity factor at the final stage before the failure and the effect of residual stresses, a final crack is modelled with the following dimensions: $2c = 170 \text{ mm}$, $a = 2.75 \text{ mm}$. These dimensions are taken from the experimental investigation of the final fracture surface (Fig. 3). The analysis is performed for various levels of residual stresses; 10, 20, 30, 40, 50, 60, 70, and 80 % of S_y (Fig. 13). The figure shows that even with these crack dimensions (taken at fracture), the maximum stress intensity factor without residual stresses is only about $1.79 \text{ MPa}\sqrt{\text{m}}$ which is again far below the critical value indicating that the operating stress level alone could not be attributed as the cause of fracture. The maximum values of the stress intensity factor escalated to $51.8 \text{ MPa}\sqrt{\text{m}}$ and $59 \text{ MPa}\sqrt{\text{m}}$ for residual stress levels of 70 % and 80 % of the yield, respectively. These are much closer values to K_{IC} for the material

and supports the conclusions arrived at from the experimental investigation.

Accordingly, both the experimental and finite element investigation support the proposition that the factors leading to the failure are the existence of high-level residual stress values along with the pressure load pulsations. The residual stress values certainly contributed much higher influence to the fracture than the pressure pulsation. This indicates that there was no post-weld heat treatment to eliminate or reduce the level of residual stresses which is a violation of the code requirements [32].

5. Conclusions

This study examined the impact of residual stresses and weld imperfections on the reliability and lifespan of SS304L seam-welded pipeline. Through experimental analysis and numerical simulations using the finite element method (FEM), the study examined various factors contributing to pipeline failure, such as weld defects, stress corrosion cracking, corrosion fatigue, internal overpressure, and fatigue failure from cyclic loads. The experimental failure analysis identified the existence of linear groove near the weld line and reductions in wall thickness which likely resulted from weld procedures. It was shown through ASME B31.3 calculations that this cause stress concentration and present a potential source for crack initiation. The absence of a stress-relieving post weld heat treatment and dampening of centrifugal pump pulsations upstream were main factors in fatigue crack propagation until ductile overload.

Numerical simulations highlighted the significant effect of residual stresses on the pipeline's lifecycle, emphasizing the importance of accounting for these stresses in design and analysis. The study also found non-compliance with ASTM A312 Clause 6.1.3 due to the use of filler material in welds, which led to procedural issues. Both experimental and numerical findings concluded that the main factors leading to failure were high residual stress levels and pressure load pulsations. The lack of post-weld heat treatment to mitigate residual stresses significantly contributed to the failure. This research emphasizes the need for proper welding procedures and post-weld treatments to improve pipeline reliability and lifespan.

The paper systematically outlines the utilization of experimental and FE analyses in examining practical engineering failure instances and strengthens the ability to avert failures and navigate risks with precision. Furthermore, the results highlight the critical significance of adhering to code and standard guidelines concerning pipe seam welds and post-weld treatment practices.

CRedit authorship contribution statement

Mohamed Gadala: Writing – original draft, Supervision, Methodology, Formal analysis, Conceptualization. **Ibrahim Gadala:** Resources, Methodology, Investigation, Data curation. **Abdulrahman Gomaa:** Software, Formal analysis, Data curation.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Data availability

Data will be made available on request.

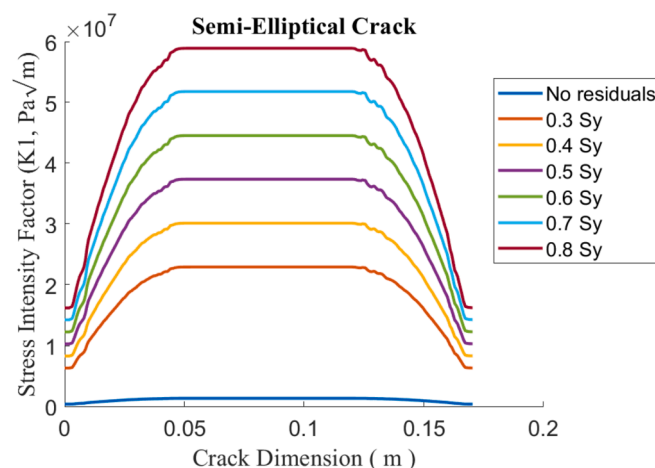


Fig. 13. Stress intensity factor along the crack edge for various levels of residual stresses.

Acknowledgment

This research was partially supported by ASPIRE Award for Research Excellence (AARE19-244), Abu Dhabi, UAE.

Appendix A

EDS and XRD complete spectrum graph

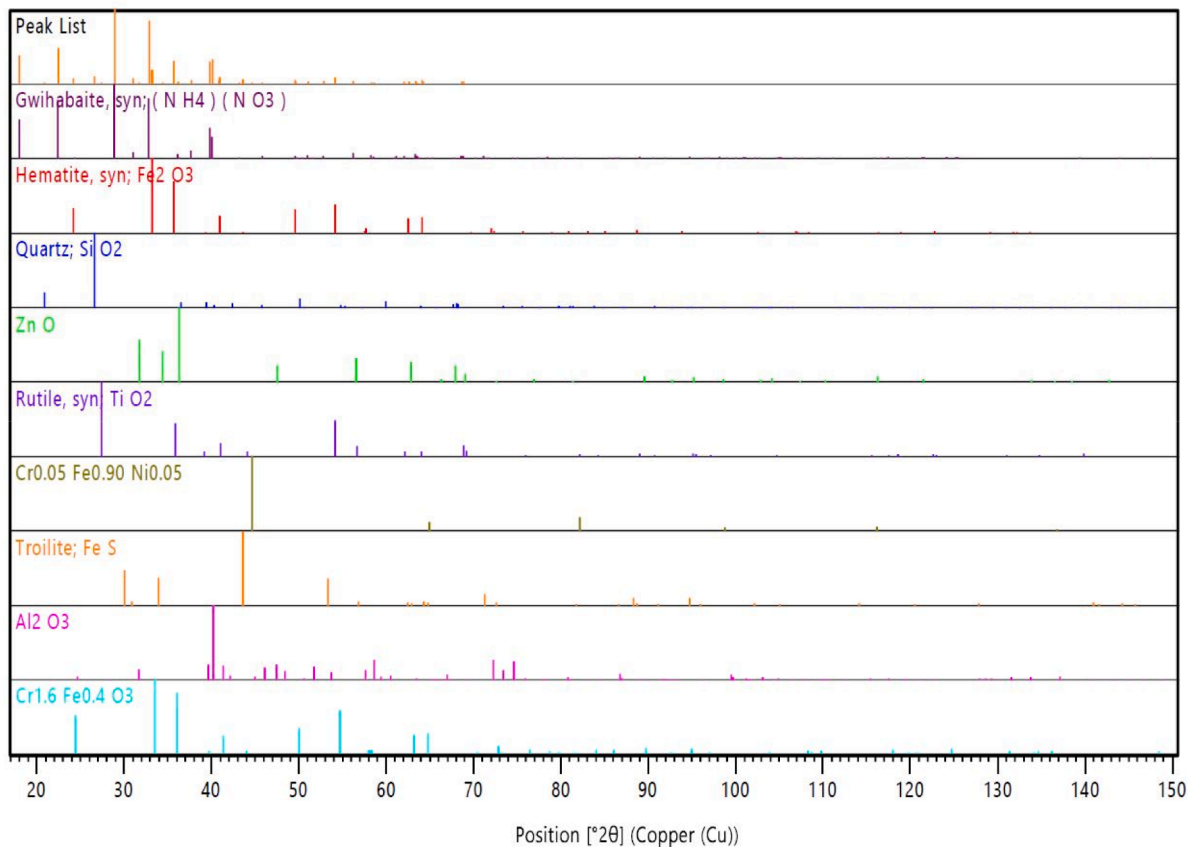
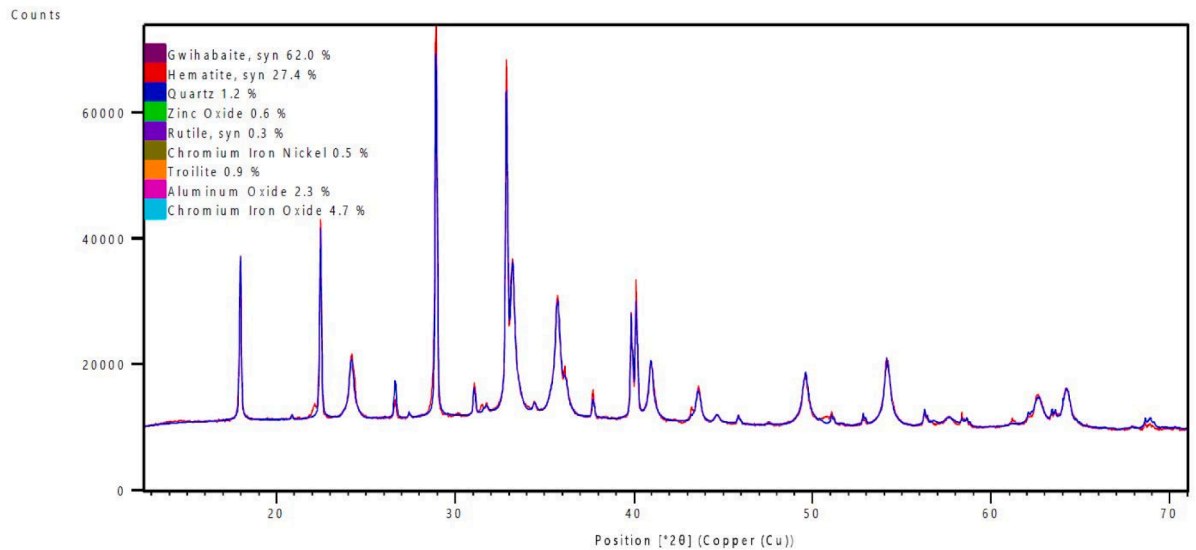
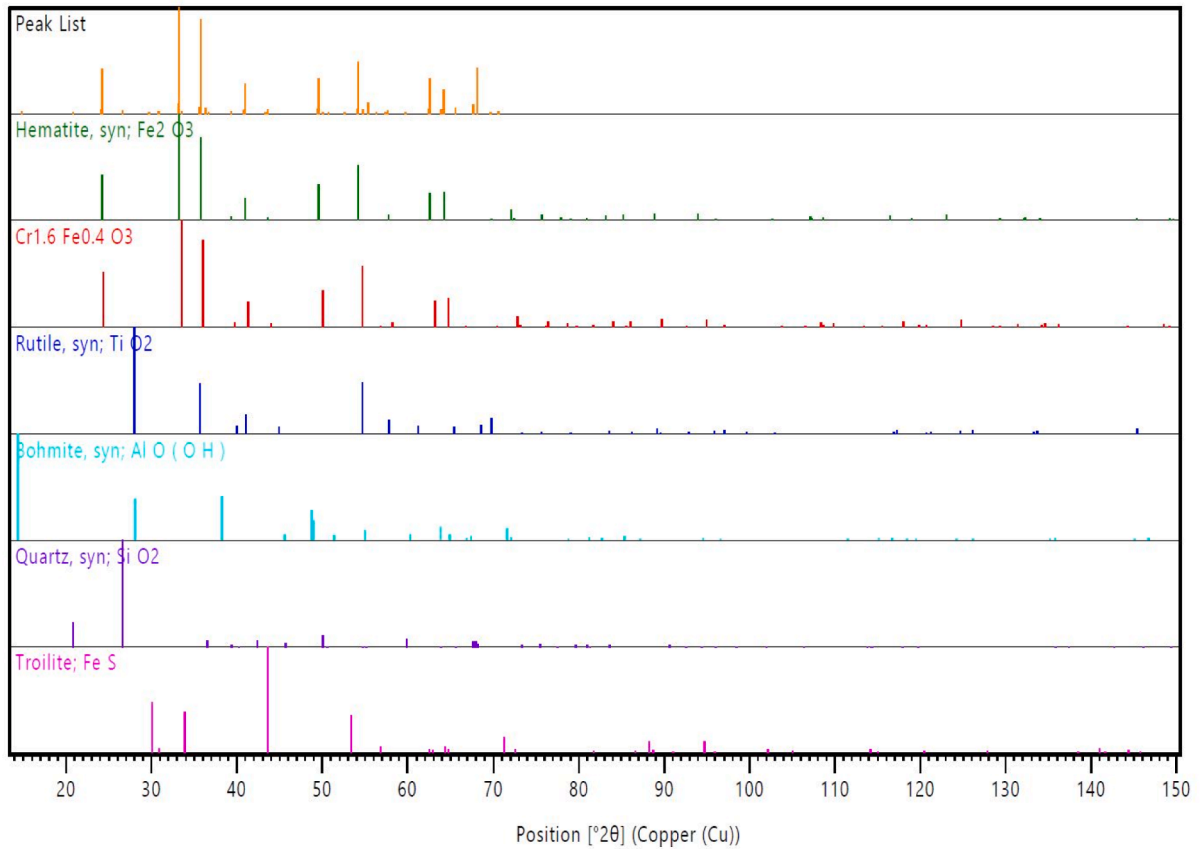
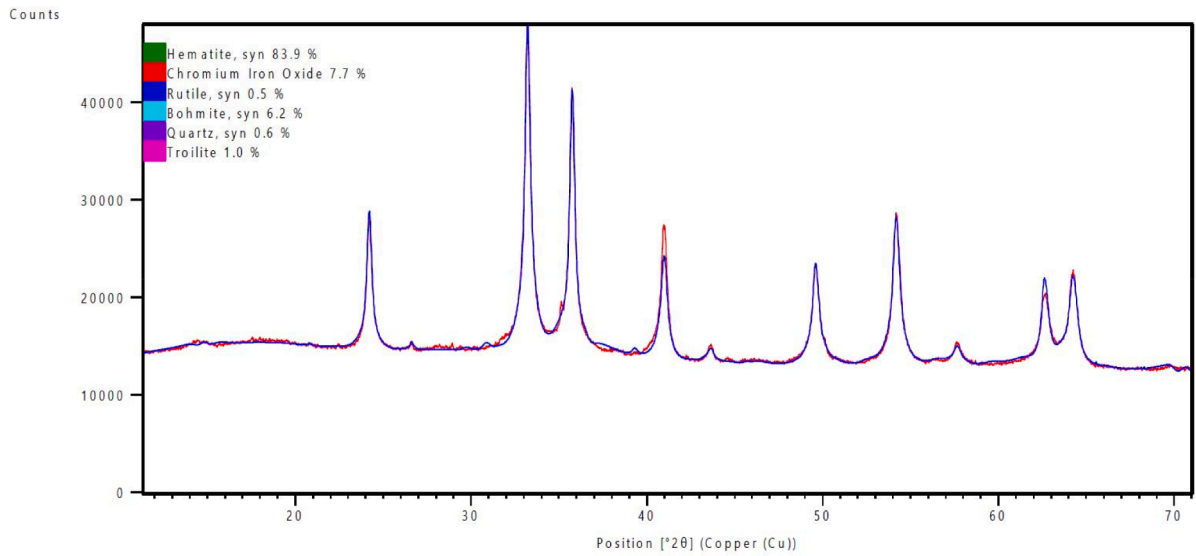


Fig. A1. The XRD spectra for the “Intact ”Good“ Sample” in Table 1.**Fig. A2.** The XRD spectra for the “Failed ”Bad“ Sample” in Table 1.

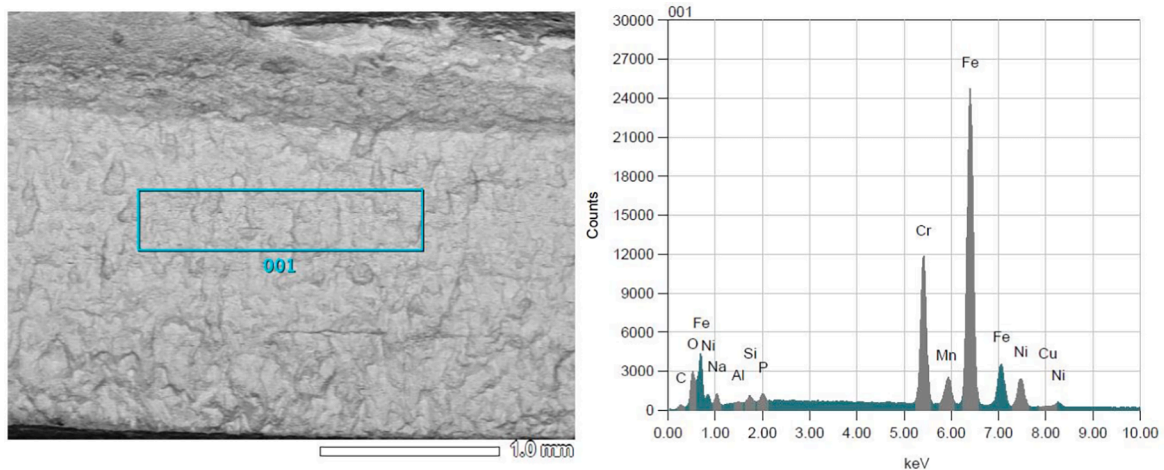


Fig. A3. The EDS location and spectrum for the “Cleavage Feature Area” in Table 2.

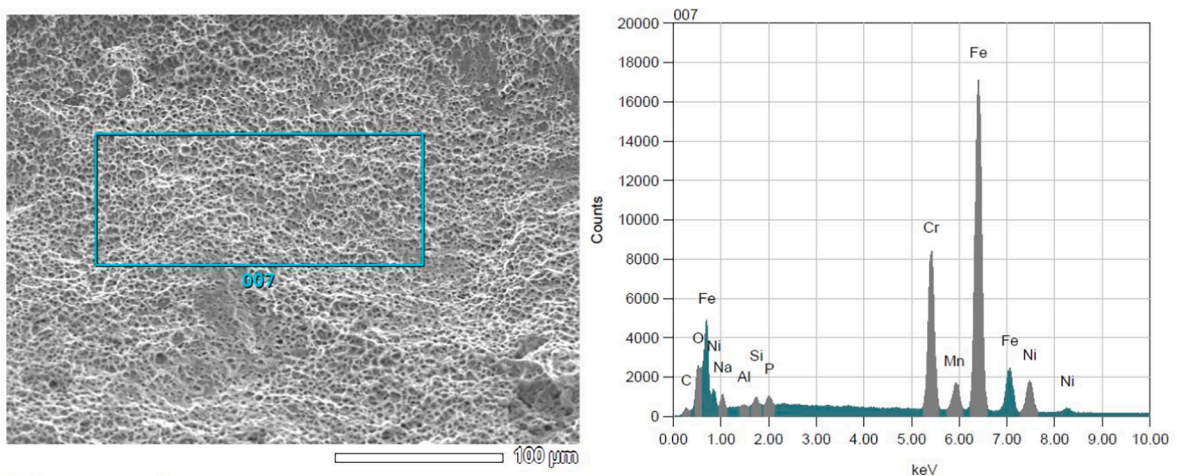


Fig. A4. The EDS location and spectrum for the “Microvoid Feature Area” in Table 2.

References

- [1] M. Singh, D. Shukla, R. Kumar, K. Arora, The structural integrity of high-strength welded pipeline steels: a review, *International Journal of Structural Integrity* 12 (2021) 470–496.
- [2] S. Sharma, S. Maheshwari, A review on welding of high strength oil and gas pipeline steels, *J. Nat. Gas Sci. Eng.* 38 (2017) 203–217.
- [3] Y. Yang, L. Shi, Z. Xu, H. Lu, X. Chen, X. Wang, Fracture toughness of the materials in welded joint of X80 pipeline steel, *Eng. Fract. Mech.* 148 (2015) (2015) 337–349.
- [4] S. Xu, and W.R. Tyson, “Toughness Properties and ECA of Current Electric Welded Pipe Seam Welds”, Report No. CMAT-2020-WF 51907294, *Canmet MATERIALS*, (2021).
- [5] B. James, A. Hudgins, in: A. Makhlof, M. Aliofkhaezrai (Eds.), *Handbook of Materials Failure Analysis with Case Studies from the Oil and Gas Industry*, Elsevier Ltd, 2016, pp. 1–38.
- [6] A. Makhlof, A.Z. Farahat, in: A. Makhlof, M. Aliofkhaezrai (Eds.), *Handbook of Materials Failure Analysis with Case Studies from the Oil and Gas Industry*, Elsevier Ltd, 2016, pp. 257–266.
- [7] K. Mankari, S. Acharyya, “Failure analysis of AISI 321 stainless steel welded pipes in solar thermal power plants”, *Eng. Fail. Anal.* 86 (2018) 33–43.
- [8] AWS D1.1: *Structural Welding Code*, The American Welding Institute AWS, 24th Edition (2020).
- [9] BS EN ISO 15614: Specification and Classification of Welding Procedures for Metallic materials, The British Standards Institution (BSI), (2024).
- [10] ANSI-NACE MR0175/ISO 15156, “Materials for Use in H₂S-Containing Environments in Oil and Gas Production”, Identical to ISO 15156-1 2020, NACE International Standards, (2021).
- [11] G. Qin, Y. Cheng, A review on defect assessment of pipelines: Principles, numerical solutions, and applications, *Int. J. Press. Vessel. Pip.* 191 (2021) 1–15.
- [12] N. Sreevidya, S. Abhijith, S.K. Alberta, V. Vinod, I. Banerjee, Failure analysis of service exposed austenitic stainless steel pipelines, *Eng. Fail. Anal.* 108 (2020) 104337.
- [13] N. Choupani, V. Asghari, H. Kurtaran, Fracture characterization of base metal, seam weld, and girth weld of welded line pipe steel at room and low temperatures, *J. Mater. Eng. Perform.* 30 (2021) 1046–1053.
- [14] K. Mankari, S.G. Acharyya, Failure analysis of AISI 321 stainless steel welded pipes in solar thermal power plants, *Eng. Fail. Anal.* 86 (2018) 33–43.
- [15] ASTM A312/A312M-01, “Standard Specification for Seamless and Welded Austenitic Stainless-Steel Pipes”, ASTM International, (2022).

- [16] Y. Liu, P. Wang, H. Fang, N. Ma, Characteristics of welding distortion and residual stresses in thin-walled pipes by solid-shell hybrid modelling and experimental verification, *J. Manuf. Process.* 69 (2021) 532–544.
- [17] S. Song, P.A. Dong, Framework for estimating residual stress profile in seam-welded pipe and vessel components part I: weld region, *Int J Pressure Vessels Piping* 146 (2016) 74–86.
- [18] M.S. Yi, J.K. Seo, J.M. Sohn, B.S. Jang, Residual stresses distribution in long seam-welded offshore catenary riser of high-manganese steel, *Ships and Offshore Structures* 15 (2020) 325–339.
- [19] Y. Li, H. Zhang, W. Zhu, Y. Cheng, W. Ma, Numerical evaluation of welding residual stress of X80 pipeline steel under the influence of various welding process parameters, *Marine Georesources & Geotechnol* 41 (2023) 1092–1103.
- [20] P. Prevey, and D. Hornbach, “Residual stress in pipelines”, in: *Oil and Gas Pipelines: Integrity and Safety Handbook*, Edited by: R.W. Revie, (2015) John Wiley & Sons.
- [21] G. Pantazopoulos, A. Sampani, Analysis of a weld failure of a rolled Zn-alloy strip – A case study, *Eng. Fail. Anal.* 14 (2007) 642–651.
- [22] K. Fan, D. Liu, J. Wu, H. Shi, X. Zhang, K. Zhou, J. Xiang, M Abdel Wahab, “Competitive effect of residual stress and surface roughness on the fatigue life of shot peened S42200 steel at room and elevated temperature”, *Tribol. Int.* 183 (2023) 108422.
- [23] V. Krivy, “Special issue: corrosion properties and mechanism of steels”, *Materials* 15 (2022) 6796.
- [24] W. Guo, N. Ding, L. Liu, N. Xu, N. Li, F. Zhang, L. Chen, Stress corrosion cracking of a 304 stainless steel elbow, *J. Failure Analysis and Prevention* 20 (2020) 483–493.
- [25] M.A. Mohtadi-Bonab, Effects of different parameters on initiation and propagation of stress corrosion cracks in pipeline steels: a review, *Metals* 9 (2019) 590.
- [26] C. Subramanian, Sulfide stress cracking of column overhead pipe to flange fitting joints in a petroleum industry, *Mater. Today Commun.* 37 (2023) 106995.
- [27] P.R. Roberge, *Handbook of corrosion engineering*, McGraw-Hill Companies, 2000.
- [28] Z. Ahmad, *Principles of corrosion engineering and corrosion control*, Elsevier Ltd., 2006.
- [29] McCafferty, “*Introduction to Corrosion Science*”, Springer Verlag, 2010.
- [30] Specification Sheet: Alloy 304/304L, Sandmeyer Steel Co., PA-USA (URL: www.SandmeyerSteel.com).
- [31] ASTM A370 Standard Test Methods and Definitions for Mechanical Testing of Steel Products, ASTM International, (2023), URL <https://www.astm.org/Standards/A370.htm>.
- [32] ASME B31.2 Process Piping Code, The American Society of Mechanical Engineers ASME, (2012).
- [33] R.S. Gadallah, Evaluation of SIFs for fatigue cracks in welding residual stress fields using the crack face traction force analysis, Osaka University, 2017. Ph.D. Thesis.
- [34] “*Theory reference for the mechanical APDL and mechanical application*”, Ansys Inc. Canonsburg, PA – USA, (2009).
- [35] J.R. Rice, A path independent integral and the approximate analysis of strain concentration by notches and cracks, *J. Appl. Mech.* 35 (1968) 379–386.
- [36] C.F. Shih, B. Moran, T. Nakamura, Energy release rate along a three-dimensional crack front in a thermally stressed body, *Int. J. Fract.* 30 (1986) 79–102.
- [37] M.C. Walters, G.H. Paulino, R.H. Dodds Jr, Interaction integral procedures for 3-D curved cracks including surface tractions, *Eng. Fract. Mech.* 72 (2005) 1635–1663.
- [38] M.S. Gadala, “Recent trends in ALE formulation and its applications in solid mechanics”, *Comput. Methods Appl. Mech. Engrg.*, 193 (2004) 4247–4275. *Ansys Structural Analysis and Simulation R-19*, ANSYS, Inc., Canonsburg, PA-15317, USA, (2022).
- [39] M. Nejati, A. Paluszny, R.W. Zimmerman, On the use of quarter-point tetrahedral finite elements in linear elastic fracture mechanics, *Eng. Fract. Mech.* 144 (2015) 194–221.
- [40] H. Tada, P.C. Paris, G.R. Irwin, *Stress analysis of cracks handbook*, 3rd Edition., American Society of Mechanical Engineers-ASME, 2000.
- [41] N. Perez, *Fracture mechanics*, Kluwer Academic Publishers, 2004.
- [42] S. Kalnaus, F. Fan, Y. Jiang, A.K. Vasudevan, An experimental investigation of fatigue crack growth of stainless steel 304L, *Int. J. Fatigue* 31 (2009) 840–849.