Fatigue of Welded Structures

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Abstract

Fatigue of metals is yet an area of material science that is not completely well understood, and there are many topics that are still being investigated. However, fatigue of welded components is an even more complex phenomenon. Apart from thermal cycles during welding, which strongly affect the base material and induce residual stresses and distortion, the fusion process and the existence of a filler (in some processes) lead to very heterogeneous microstructures, varying in mechanical properties and chemical composition throughout the area of the joint. Additionally, weld defects like porosity, slag inclusions, undercuts and overlap among others, can significantly reduce the fatigue strength. All these features create suitable conditions for weld toe cracking that is apparently the most problematic region. Based on this, several improvement techniques have been developed and they have proven to enhance fatigue resistance by either inducing compressive residual stresses in the weld toe or modifying its shape into smoother geometries.

In the present work, fatigue strength of transverse non-carrying load fillet joint was analysed. High frequency axial fatigue tests were performed in as-welded and improved joints. TIG-dressing and shot peening techniques were employed to modify the weld toe into more convenient configurations. Microstructures obtained in both cases are discussed and related to crack initiation phase. Crack path and fracture surfaces were also investigated. It was found that as-welded (AW) samples welded according specifications presents enhanced fatigue strength above FAT 90. TIG dressing fulfilled to create smoother toes and increased fatigue life by at least 50%. Testing of shot peened samples is still ongoing. An average increase of surface hardness around 10% was measured in the base metal (BM) with this technique. Compressive stresses can delay nucleation and growing of microcracks from the toe, which is translated into longer fatigue lives.

Keywords: Fatigue of weldments, TIG-dressing, shot peening, nominal stress approach.
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Introduction

The present study is triggered by a problem found in industry. Particularly, cracks were found at certain components of a mining company, subjected to vibrations and environmental actions. Before describing the damage encountered, a thorough literature survey is provided. It starts with important fatigue features, such as description of the phenomenon and laws that control it, and follows with a revision of arc welding techniques, mainly focused on microstructural changes generated by the process. A special section is considered for residual stresses, since this topic can play an important role in fatigue of weldments. Having exposed fatigue and welding processes, fatigue on weldments is dealt with. Fatigue of welded components is a special track in material science and deserves therefore, a particular treatment. The reader that is familiar with basics of fatigue and welding is steered to Section 2.4 or Chapter 3 directly. The importance of this chapter lays in the fact that it set the background for the analysis that is carried out afterwards.

Once theoretical concepts are presented, Chapter 4 discusses the most relevant aspects of the field work. Structures and components are introduced, and the state of the problem is explained.

Chapter 5 deals with the experimental procedure and covers all techniques employed along this work. Testing equipments, welding procedures and sample preparation are described. Throughout Chapter 6, all results obtained from techniques mentioned in previous chapter are treated. Interpretation of important findings is carried out, by contrasting different aspects of fatigue of weldments. However, a major interpretation of results is the subject of Chapter 7. In this part, careful examination of values, figures and charts is performed. Relationship of experimental findings with theoretical aspect described in former chapters is important for a better understanding of the problem.

Conclusions are exposed in Chapter 8, differentiating some particular findings in the case of fatigue tests.

The last but not the least, repair recommendations are given in Annex A, with the purpose of providing the company a brief guidance, in accordance with results obtained in this study. More Annexes are included for the interested reader, covering preheat temperature calculation, material sheets and electrodes specifications.
Chapter 1: Fatigue

1.1 History

Fatigue failure in metals can be defined as the formations of crack or cracks caused by continuous damage of materials subjected to cyclic loads each of which is insufficient, by itself, to generate normal “static” fracture. It is the most frequent mode of failure in industry [1] and is a problem that has caused many catastrophic disasters, with big human, economic and environmental lost. This is due to the fact that fatigue is developed in two steps: crack initiation and crack propagation, which cannot be noticed macroscopically and the final failure is caused then by sudden fracture of the remaining section in the component or part.

With the coming of train industry in XIX century, a number of failures started to be observed in components subjected to cyclic loads. As ruptures occurred under loads with values fairly lower than those to produce rupture, it was accepted the cyclic nature of the problem [2]. Wöhler proposed in 1860 a method to avoid these collapses, and create the S-N curves. By that time, designers applied high safety factors to “avoid” this kind of failures, but they did not go deeper into the reasons of the problem. The existence of cracks in the components was not considered until then.

There are consistent evidences of many catastrophes caused by fatigue, most of them in aerospace industry. Structures in this area are typically light weight and highly stressed and exposed to oscillating and vibratory loads. Perhaps the most widely known case is that of the “De Havilland Comets” between 1953 and 1954, in which a number of aircrafts broke up in mid-air under different circumstances. First accidents were ascribed to the unfamiliarity of the pilots with the new aircraft or even considered to be originated by excessive stresses in the airframe due to a tropical storm [3]. However, two more accidents occurred after that, which could not be easily explained. One of them came about 30 minutes after departure and the plane crashed into the Mediterranean causing 35 deaths. Reasons of the rapid failure were discovered later, and were attributed to fatigue crack growth from defects which were probably present from the construction of the aircraft due to continuous cycles of compression and decompression related with take-off and landing, respectively. The main sites for the growing process were around cut-outs, such as windows in the fuselage skin. They were square shaped, what made stresses to be higher than expected in the corners. Summed to that, rivets and bolts holes acted as stress raisers. Under this situation, the probability of crack nucleation was huge and finally, a fatigue crack initiated in specific sites near 10 mm holes.

It is important to note that by those days, although fatigue started to be understood as a progressive and localized damage since the beginning of XX century, fracture mechanics methods were not available and therefore no correct explanation could be done to the problem. It was not until 1956, when Irwin’s concept of stress intensity factor was introduced [4] and fatigue crack growth was better discerned, that Comet accidents and other catastrophes were able to be analyzed by means of fracture mechanics. Moreover, after chaotic events concerning failures of materials, inspections of elements became an extremely priority in almost all fields and not only in metal components.

Nowadays, lot of work has been done in this field and many theories have been developed to better understand structural components failures.
1.2 S-N curves

The basic method of compiling engineering fatigue data is by means of Wöhler curve, where the stress $S$ is plot against the number of cycles to failure, $N$. Data for the later are usually written in a logarithmic scale. The value of the nominal stress employed can be $\sigma_a = \frac{\sigma_{\text{max}}-\sigma_{\min}}{2}$, $\sigma_{\text{max}}$ or $\sigma_{\text{min}}$.

Examples of Wöhler curves are shown schematically in Figure 1a. It is important to note, that each curve is determined for a specified value of $\sigma_m = \frac{\sigma_{\text{max}}+\sigma_{\text{min}}}{2}$, $R = \frac{\sigma_{\text{min}}}{\sigma_{\text{max}}}$ or $A = \frac{\sigma_a}{\sigma_m}$.

Some of these parameters are represented in Figure 1b. Usually, $R = -1$ is used in fatigue testing and can be easily obtained from rotating beam test. In Figure 1a appear two kinds of curves, one (A) with a clear endurance limit, and another (B) that presents failure even at very low values of $S$. Nevertheless, the behaviour is similar in both cases: At higher stresses the fatigue life is continuously decreased. Special care has to be taken when dealing with high loads, where plastic deformation develops in a big scale. This kind of fatigue is called oligofatigue or low cycle fatigue ($N < 10^4$ or $10^6$ cycles).

Most non-ferrous materials, like aluminium, magnesium and copper alloys have an S-N curve similar to B, with no evident limit. For these cases, it is usual to set fatigue strength at an arbitrary number of cycles, such as $10^8$. Ferrous materials and titanium presents a fatigue limit, below which the material can withstand infinite number of cycles without failure [5].

![Figure 1: S-N curve for (A) ferrous and (B) non-ferrous materials (a) and generic stress cycle profile (b) [6].](image)

As was said in the beginning, nature of S-N curves do not consider the existence of cracks in the material. This can be stated as the classical point of view of fatigue. Later, with fracture mechanics in scene, both theories were related. Due to that, in the following section, fatigue is presented considering its microscopic nature.

1.3 Fatigue phenomena

On the macroscopic scale, fatigue fracture surface is usually normal to the direction of the principal tensile stress. It is generally characterized by a crack initiation point, which usually occurs in a site of stress concentration, a smooth region, due to the effect of crack growth progressively, and a rough area related with a failure in a ductile manner, when the resistance section is not able to stand the load any longer. Figure 2 shows those regions schematically.
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Additionally, it points out beach-marks that are indicators of the progressive nature of fatigue failure. They start in the site of crack initiation and spread inward to the fracture area forming almost concentric rings. These lines show up, when the part is affected by a corrosive environment. Continuous start-ups and stops of machines for certain time periods, allow air to flow between crack surfaces during the later, and oxidize a particular amount of surface. Then, when the machine is put again into operation, crack continues propagating, and a new surface, free of oxide is created. This region will be corroded during next stop, and the process goes on until fracture occurs.

![Diagram of fatigue fracture surface and its parts](image)

**Figure 2:** Schematic fatigue fracture surface and its parts. Adapted from [6].

It is important to underline that facts in reality may differ from the generalized case presented in Figure 2. Sometimes regions are not completely distinguishable or recognizable, and in other occasions, there might be more than one site for crack initiation. Furthermore, it can be found in the smooth region, some other lines, different from “beach-marks”. These lines cannot always be seen by a simple look at the sample, but probably need an optical instrument instead. They are known as “fatigue striations” and give useful information about the plastic blunting process for crack growth [5]. Figure 3 indicates how cycles are related with striations for an aluminium alloy. Note that magnification is really high.

![Fatigue striations on crack surface of an aluminium alloy](image)

**Figure 3:** Fatigue striations on crack surface of an aluminium alloy, and cycle profile. 12000X [7].
Fatigue failure consists of three steps, considering basic structural changes that occur in materials subjected to cyclic loads:

1. Crack initiation
2. Crack propagation or crack growth
3. Ultimate fracture

From the experimental point of view, many tests can be carried out for measuring both crack nucleation, and propagation. However, in reality things are a slightly different and probably the most convenient method is testing crack growth, making use of the stress intensity factor concept. Manufacturing techniques generate sundry defects in structures, especially when welding is used as the joining technique. Then, a descriptive trial should involve pre-existing cracks. Nevertheless, both tests give valuable information to characterize materials behaviour.

Before going deeper into the stages of fatigue damage, it is worth considering that three cases can be taken into account when dealing with cyclic loads. They are related with the amount of cycles needed to achieve rupture, giving rise to low-cycle, high-cycle and ultra-high-cycle fatigue [8]. In the former, larger proportions of the total cycles to failure are attributed to crack propagation, whereas in the last two cases where stresses are low, the majority of time (cycles) is spent on nucleating cracks.

1.3.1 Crack nucleation or initiation

Crack initiation usually involves a free surface. Even in the case that cracks form in the interior of materials, it is always related to an interface, such as a layer, carbides and incoherent particles, among others (see Figure 4). Nonetheless, fatigue cracks always nucleate in zones with high plastic deformation concentration. Surface roughness and finishing is therefore, extremely important in this first stage.

![Figure 4: Nucleation of cracks in persistent slip bands (a), inclusions (b) and grain boundaries (c) [11].](image)

During the first thousand cycles, dislocation movement assist slip bands formation in some grains [5]. There will generally be some slip bands that are more persistent than the rest and may still be visible, even after polishing the surface. Those persistent slip bands might then form extrusions and intrusions, by plastic deformation accumulation, as shown in Figure 4a and 5. It can be seen in this figure that dislocations slip in certain crystallographic planes, governed by shear stresses (Figure 5 a). Persistent slip bands will then form a free surface (Figure 5 b) that is followed by an extrusion or intrusion (Figure 5 c). Figure 5 d and e represent states with different amount of discontinuities in the surface. Fatigue cracks tend to nucleate in intrusions, where stresses are high, and a big amount of dislocations piles up.
Fatigue of welded structures

Figure 5: Stages of extrusions and intrusions formation [9].

Experimental evidence of extrusion and intrusions can be found in literature. Figure 6 is a SEM image of discontinuities formation on a metal surface. Note how the crack arises between an extrusion and material less deformed.

A characteristic point in this stage of fatigue failure, is that the crack initially grows along slip planes, and therefore oblique to the direction of the maximum stresses. However, once formed, it tends to propagate normal to the maximum applied tensile load in a transgranular manner.

The rate of crack propagation is generally very low, and the fracture surface is practically featureless. Nevertheless, there is a great influence of microstructure in this stage. Every effort to decrease the roughness of surfaces will succeed in increasing the fatigue nucleation resistance. For instance, shot peening or surface heat treatments like case hardening that may introduce compression stresses in the surfaces are common methods used in parts subjected to cyclic loads.

1.3.2 Crack growth or propagation

This second stage concerns growth along slip bands previously explained, and propagation perpendicular to the maximum tensile stress (Mode I).

As was mentioned earlier, crack progresses in discrete steps, and forms striations. Plastic blunting process is represented in Figure 7. It starts with a sharp tip. Since maximum share
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stresses are 45° to the axial load, slip bands may form in this direction (Figure 7 b). When it finally reaches its maximum width (Figure 7 c) the tip becomes blunted and permanent plastic deformation is created in tension. If the load is discharged, then elastic recovery will act in the opposite direction and the crack surfaces will close to an extend that depends on mean stress and cycle amplitude (Figure 7 d and e). Under these conditions, elastic deformed zone cannot recover the initial shape because of the opposing remaining plastic deformation. The later is therefore forced to compress and a new plastic zone is created near the crack tip by compression yielding. During the next cycle, a new yielding occurs in the opposite direction and the process is repeated (Figure 7 f).

Figure 7: Plastic blunting process for stage II propagation [5].

The model proposed before leaded Paris to formulate an expression for the plastic radius, which he considered to be highly valuable in fatigue damage. Namely:

$$r_p = \frac{1}{3\pi} \left( \frac{K_I}{\sigma_{ys}} \right)^2$$  \hspace{1cm} (1)

Equation (1) combines plastic deformation occurring during plastic blunting process, and the concept of stress intensity factor from fracture mechanics. Additionally, Paris carried out some experiments on compact specimens, and measured crack length as a function of cycles, for given values of maximum and minimum load. For each value of crack length, he found the slope of the curve, and at the same time, calculated values of $\Delta K = K_{max} - K_{min}$. Compiling results in a double logarithmic chart, he got a relationship similar to the one shown in Figure 8. Sometimes is useful to define a parameter $R = \frac{\sigma_{min}}{\sigma_{max}}$. It can be demonstrated that increasing its value cause a shift of the curve to the left [9].

In region I, for $\Delta K < \Delta K_{th}$ there is no crack propagation. This states a threshold for the stress intensity factor amplitude, in order to have crack growth, but do not mean that cracks are not present in the material. However, there is evidence of short cracks or non-propagating cracks that behaves in an unusual manner and may alter the shape of curve in Figure 8, for $\Delta K < \Delta K_{th}$. For those cracks, there might be crack propagation in stage I, but cease to growth afterwards, and the laws that controls their functioning are far from being completely understood [10].
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After $\Delta K_{th}$, a sudden increase in crack propagation velocity occurs, and the curve acquires a constant slope. This linear relationship was first proposed by Paris, to have an expression like:

$$\frac{da}{dN} = C\Delta K^m$$  \hspace{1cm} (2)

where $C$ and $m$ are constants that can be obtained from experimental data as presented in Figure 8, like antilogarithm of $y$-interception and slope, respectively.

![Crack-growth curve showing characteristic regions](image)

In zone III, several propagation mechanisms are present, and plasticity cannot be considered in the small scale any longer. In this stage, $K_{i_{max}}$ approaches to $K_{IC}$.

Zone I and III are particularly susceptible to microstructure and mean stress. Environment plays an important role in stage I, but is not relevant in the other regions. Certain combinations of environment, mean stress and frequency can alter significantly behaviour in the linear part.

Both values of $\Delta K_{th}$ and constants in Equation (2) are useful to design against fatigue and minimize damages that may be caused by fluctuating loads.

Many others formulas than Equation (2) can be found in literature, some of which are more complex in formulation but use essentially the same parameters.

1.3.3 Ultimate fracture

In structural components subjected to fatigue, a crack or several cracks can initiate in specific sites suitable for their nucleation, like stress raisers. Those cracks eventually coalescence and then propagate by plastic deformation blunting, forming striations if the amount of plastic deformation is sufficiently high.

Now, all parts are designed to withstand a specific load in static conditions considering a safety factor depending on working circumstances and function of the component. This oversizing can be clearly seen in a fractured part subjected to fatigue. The region where final rupture occurs is the cross section area that can no longer resist the applied load and it can be either crystalline...
or fibrous depending on whether the fracture is brittle or ductile. Depending load conditions, fracture surface appearance can differ. For instance, fractures caused by low stresses usually have a very smooth fatigue area, and the residual area at final fracture is small, which is consistent with what was said earlier. On the other hand, high stresses commonly produce small fatigue areas and tend to cause multiple crack initiation.
Chapter 2: Welding of metals

2.1 Electric Arc Welding Processes

Arc welding systems are composed of a power source, an electrode and the workpiece. The lack of protection from the air can cause a continuous deterioration of the hot electrode and harmful effects upon the highly reactive molten weld metal, due to oxygen, hydrogen and nitrogen present in the atmosphere. Hence, a shielding system is used for protecting the weld pool and electrode from the environment and, also, helps to stabilise the arc [11].

Direct current (DC) power sources can be designed to keep a convenient relationship between voltage and current, both before and after the establishment of the arc. Linnert [12] classifies them as drooping, constant and rising arc voltage machines (DAV, CAV and RAV, respectively). However, updated literature [13], simply distinguishes among constant-current and constant-voltage sources. For the sake of completeness, it is only pointed out that the former is commonly used in SMAW and GTAW, whereas constant-voltage sources are used in GMAW, FACW y SAW.

The power source operates generally in the range 10-2000A and at 10-50V and is responsible for setting the electric arc in the gap between the electrode and the workpiece, when the breakdown voltage of the gas is exceeded. This is highly desirable since the extremely high temperature of the arc permits it to supply a large amount of energy to a small area. Temperatures around 10000°C in the core of the arc dissociate wholly or partially the molecules present in the gas, into their constituent atoms, and the atoms themselves are ionised, forming a plasma state. The arc column is electrically neutral, but since the mass of the electrons is much lower than that of the positive ions, their mobility is much higher. Consequently, most of the current is carried by electrons.

Both electrons and positive ions are accelerated by the potential field between the cathode and anode, and collide with the oppositely charged element. Almost all their kinetic energy is converted into heat. The electrode is then heated up due to these collisions and the Joule effect of the current passing through it. Likewise, energy is conducted through the plasma to the workpiece, causing materials to melt. Forces established in the ionized gas can even support the welding process in many positions, such as overhead and vertical. It must be noted, that polarity of the electrode can influence heat distribution, efficiency, weld quality and geometry of the pool and this is particularly important when employing non-consumable electrodes (GTAW).

After the melting of the base material, a weld pool is created. Depending on the welding process, additional material can be added to the weld metal, forming a mixture. That dilution results in a gradient of composition that can affect the mechanical properties of the joint. The way the welds are made affects dilution. For example, a joint can be completed by a single run, or multirun technique, depending on the materials and joint geometry, among other factors. The welds analysed in the present work were made by means of FCAW process. For this reason, the following section particularly discusses this technique. Similarly, GTAW process is employed to improve fatigue behaviour of welded joints (by TIG-dressing) and is also described in later sections.
2.1.1 FCAW

In this process, the electric arc is established between the workpiece and a continuous flux-cored electrode. The latter can generate a protection gas for the weld metal when burning the flux, in which case, the process is called “self-shielding” (FCAW-S). Alternatively, it is possible to use an external shielding gas (FCAW-G). In both cases, the material contained in the core of the roll-formed and/or draw tubular wire cause the formation of slag on the weld pool. Figure 9a shows FCAW-G process and Figure 9b presents FCAW-S technique.

![Figure 9: FCAW-G process (a) [13] and FCAW-S technique (b) [14].](image)

While some self-shielded electrodes provide their own shielding gas through the decomposition of core ingredients, others rely on slag shielding. Metal drops are transferred across the arc and are after that protected from the atmosphere by the slag forming on the top of the weld pool. Flux ingredients for FCAW-S process can also contain substantial amounts of deoxidizing and denitifying ingredients to help achieve sound weld metal and in some cases arc stabilizers and alloying elements [13]. The self-shielding gas generated from flux in the core through the arc is more effective than when the gas is produced from an external coating, as in SMAW process [15]. For this reason, it is an excellent choice for welding in the field. American Welding Society (AWS) includes in its structural welding code, prequalified joints welded with FCAW process. A wide range of application areas and materials likely to be welded with flux-cored wires can be found in literature.

Although high deposition rates can be obtained (2-15 kg/h), the need for slag removal can make the process slower. Care must be taken to brush away all slag particles, especially when manufacturing multirun welds.

Compared to GMAW and SAW, more smoke and fume are produced, claiming usually for a fume extraction system.

An interesting characteristic of self-shielding electrodes is the fact that they can withstand long extensions of wire (19-95 mm are commonly used) during welding. The extension is defined as the length of the non-fused wire from the tip of the contact tube (see Figure 9). The longer the extension, the more the electrode is “pre-heated” and the higher the drop of voltage and
consequently the current. The heat input is therefore decreased, resulting in a narrower and shallower pool, which is specially desired when welding thin pieces. On the other hand, if gas-shielding is used, short extensions of the electrode are employed together with high currents. In the case of fillet welds, compared to SMAW process, FCAW produce a narrower bead with a large throat.

The equipment necessary for performing this process is shown in Figure 10. FCAW is a semi-automatic process, since the operator only adjusts the welding speed. Voltage and current are set automatically. However, it can be automated with proper equipment. The recommended power source is a constant-voltage DC machine, operated either with positive or negative electrode. The basic equipment includes a power supply, wire feed system, and welding gun. Auxiliary equipment may be required, such as shielding gas and fume removal system.

The wire feed system controls the wire feed speed. When using constant-voltage power sources, the wire feed speed is directly related to the welding current and is automatically adjusted to keep the arc voltage at a steady value.

The electrodes are manufactured by a particular process in which a low carbon or low alloy strip is progressively formed into a “U” shape, filled with the core ingredients, closed by rolls to form a round tube and finally drawn/rolled into a desired diameter. The wire is wound on a proper package, usually coils. The procedure is described in Figure 11. Modern FCAW-S wires are of “butted tube” design, replacing previous attempts to shield the weld metal by creating intricate metal shapes and filler compartments (see Figure 12) [16].
Regarding electrodes usage, a variety of them can be found in the market. A designation system is employed by AWS A5.20M [17] and can be generally written as “EXXT-XX-JXHX”. Letter “E” designs an electrode; the first “X” indicates the minimum tensile strength (when multiplied by 10 MPa) of the weld metal; the second “X” is the position designator (“0” is set for flat position only and “1” for all positions); “T” means that the electrode is flux-cored. The other term meaning can be found in the referenced standard and refer to usability of the electrode, shielding gas, impact resistance requirements or other mechanical properties, and diffusible hydrogen information. Impact resistance requirements should be considered previous to the selection of the method and the electrode for the particular application.

As an example, comments on EXXT-1C y EXXT-1M are done. Both electrodes have similar type slag and are proper for single and multiple pass welding using direct current electrode positive (DCEP). Lower diameters are used for all position, whereas diameters larger than 2mm are...
Fatigue of welded structures

employed for welding in the flat position and for fillet welds in the horizontal position [17]. The rutile-based slag covers completely the weld bead. Rutile is also used for controlling viscosity. Electrodes in the group T-1C are designed to be used with CO\textsubscript{2} as the shielding gas, but other mixtures of Ar-CO\textsubscript{2} may also be operated. The larger the amount of argon in the mixture, the higher the manganese and silicon contents, among others, in the weld metal. Yield and tensile strength can therefore be enhanced. Regarding electrodes in the group T-1M, the shielding gas is a mixture of 75-80% Ar-CO\textsubscript{2}.

From the shielding gas point of view, CO\textsubscript{2} is the most extensive used protection gas due to its low costs and penetration depth achieved. Transference mode is mainly globular, although spray mode can also be produced under certain flux compositions, as for the electrodes described previously. It is well-known the oxidizing effect of CO\textsubscript{2} at elevated temperatures, but this fact is compensated with the addition of deoxidizing elements in the flux. The carbon content of the base material and the electrode is of primary importance, since the CO\textsubscript{2} atmosphere can act as a carburizing or decarburizing media. Porosity or other defects can be found, if the composition of the flux is not the correct one. Mixtures of gases are utilized to take advantage of each gas benefits. Argon, for example, protects the molten metal at high temperatures and improves the efficiency of the deoxidizing element in the flux.

The weld quality produced with FCAW depends on the type of electrode employed, the method (FCAW-S or FCAW-G), base metal conditions, weld joint design and welding conditions. A careful examination of each of these factors should be carried out to obtain sound welds with the best mechanical properties [18].

A last comment is worthy of mentioning. An electrode designed for simple pass usually contains more alloying elements than that used for multiple pass, such as manganese, silicon and/or titanium for FCAW-G and aluminium for FCAW-S. In the case of the latter, the reason for that can be summarised as follows. Nitrogen is known to enter into the weld pool of an FCAW-S wire in an amount that exceeds the solubility limit of the solidified weld metal. That difference may cause a nitrogen boil during solidification and consequently form a porous weld. Aluminium is commonly used to combine with nitrogen dissolved in the liquid weld pool and prevent this adverse effect [16]. Due to the fact that purchased steels seldom contain aluminium, a higher dilution is produced in the single bead or root pass of a multiple pass welding. The aluminium available to react with nitrogen and oxygen is at a minimum. In these cases, special electrodes with higher contents of aluminium are needed. However, if this “single pass electrode” is used for multiple deposits, after the first bead, the alloying elements increasingly stay in the weld metal due to less dilution and can therefore embrittle the final weld. The exposed reasons lead to a common practice in industry for special applications. When requirements of toughness and solidness are mandatory, the first bead or root pass is performed with a special electrode. Following beads are done with conventional multiple pass fillers.

2.1.2 Gas Tungsten Arc Welding (GTAW)

The aim of this section is to give the most important characteristics of the process, having in mind its employment for improving the fatigue resistance of weldments, by means of the TIG-
Ceferino Steimbreger

dressing technique. No attempt is done to fully describe the process or details related with its operation. This information can be found elsewhere in literature [12, 13, 15, and 18].
The process is also known as HeliArc, Tungsten inert gas (TIG) and Tungsten arc welding. GTAW corresponds to AWS nomenclature, and considers the usage of not only inert gas but also a mixture of gases, which is more convenient for certain applications.
The arc in GTAW is established between a virtually non-consumable tungsten electrode and the weld pool. Shielding gas is necessary since there is no self protection of the electrode. Filler material can be added or not while welding. The operation is described in Figure 13, showing the power source, torch components, materials and important characteristics. As can be seen, the non-consumable tungsten (or tungsten alloy) electrode is held in a torch, making direct contact with the contact tube. The latter is connected to the power source and is usually water-cooled. An external system feeds the shielding gas through the torch, and the gas nozzle directs it to the workpiece, in order to protect the electrode, weld pool and solidifying metal from contamination by the atmosphere. Voltage set between the electrode and the workpiece, ionise the shielding gas and close the electric circuit. Heat generated is sufficient to melt the base material. If necessary, filler is added to the leading edge of the weld pool. Mechanised GTAW systems may include arc voltage controls, arc oscillators and wire feeders.

![Figure 13: Schematic operation of Gas Tungsten Arc Welding. Adapted from [19].](image)

GTAW produces superior quality welds, generally free of defects and spatter. It allows excellent control of root pass weld penetration and precise control of heat input and other welding parameters. Almost all metals can be welded with this technique. Disadvantages like low deposition rates, coordination (external filler should be added) and experience of the welder and relatively high cost for thicknesses greater than 10mm, do not affect the application required in the present work. TIG dressing makes use of all advantages of GMAW process without being much affected by described disadvantages. Therefore, it is a promising technique to include in welding codes to improve fatigue resistance of weldments. However, there is difficulty in shielding the weld zone properly in draughty environments [18]. Other problems
like tungsten inclusions can occur when the electrode’s tip get in contact with the weld pool. Nevertheless, the latter issue may be solved by maintaining a constant stick out.

Most important process variables are voltage, current, travel speed and shielding gas, all of which interact to achieve desired weld qualities. The process can be used with either direct or alternating current, depending on the metal to be welded. In direct current (DC), a negative electrode generates a flow of electrons to the workpiece and a reverse flow of ions. The workpiece will then receive ca. 70% of the energy, whereas the electrode withstands the rest. Penetration is deep and the pool is narrow with the additional advantage of fast welding speeds, especially with helium as the shielding gas. In contrast, a positive electrode produces a wide and shallow pool, with most of the heat focused in the electrode tip. This configuration has a strong oxide cleaning action, probably due to the breaking up of the oxide film by bombarding cations. Alternating current’s (AC) effects lay in between both previous cases, and also provides a cathodic cleaning (sputtering) which removes high temperature oxides form the joint surfaces of aluminium and magnesium. This cannot be achieved without argon as the shielding gas. For manual welding, argon is usually used both in direct or alternating current. Polarity of the electrode and its characteristics are displayed in Figure 14. Note the change in the electrode tip for each polarity.

In addition to the three described types of GTAW configurations, pulsed direct current with negative electrode is especially useful for out-of-position welding, reducing distortion and reducing difficulty of bridging gaps [19].

![Figure 14: Electrode polarity for GTAW and its characteristics [18].](image)

Arc voltage is dependent on not only the arc current but also the shape of the electrode tip, the stick out (distance between the electrode and the workpiece surface) and the shielding gas. Since shield gas, electrode and current are usually predetermined, arc voltage becomes a parameter to control arc length. Arc length is a critical variable in GMAW welded joints because it affects proportionally the width of the weld pool. However, it is hard to measure directly. In general, the desired arc is as short as short circuiting of the tip with the weld pool allows to.
The electrode in this process deserves a special description. Tungsten refers to either the pure element or various alloys. The term “non-consumable” is true if the process is properly used. As have been said, electrode tip can melt or be transferred to the weld pool and contaminate the weldment. When approaching its melting temperature (3410°C), tungsten becomes thermionic, i.e. it is a ready source of electrons [18], which significantly cool down the tip. This effect prevents the electrode tip from being melted by resistance heating. Current levels higher than those recommended for a particular diameter (see Table 1 for tungsten and thoriated tungsten electrodes) and tip configuration will cause erosion or melting of tungsten. On the contrary, low currents can produce arc instabilities.

### Table 1: Recommended diameters and gas cups for various welding currents, using argon as the shielding gas [18].

<table>
<thead>
<tr>
<th>Electrode diameter [in]</th>
<th>Use Gas Cup I.D. [mm]</th>
<th>Direct current [A]*</th>
<th>Alternating current [A]**</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Straight polarity</td>
<td>Reverse polarity</td>
</tr>
<tr>
<td>0.01</td>
<td>0.25</td>
<td>up to 15</td>
<td>up to 15</td>
</tr>
<tr>
<td>0.02</td>
<td>0.5</td>
<td>5-20</td>
<td>5-15</td>
</tr>
<tr>
<td>0.04</td>
<td>1</td>
<td>15-80</td>
<td>10-60</td>
</tr>
<tr>
<td>1/16</td>
<td>1.6</td>
<td>70-150</td>
<td>10-20</td>
</tr>
<tr>
<td>3/32</td>
<td>2.4</td>
<td>150-250</td>
<td>15-30</td>
</tr>
<tr>
<td>1/8</td>
<td>3.2</td>
<td>250-400</td>
<td>25-40</td>
</tr>
<tr>
<td>5/32</td>
<td>4</td>
<td>400-500</td>
<td>40-55</td>
</tr>
<tr>
<td>3/16</td>
<td>4.8</td>
<td>500-750</td>
<td>55-80</td>
</tr>
<tr>
<td>1/4</td>
<td>6.4</td>
<td>750-1100</td>
<td>80-125</td>
</tr>
</tbody>
</table>

*a. Use EWTh-2 electrodes (2% thoriated tungsten)*  
*b. Use EWP electrode (minimum 99.5% W)*

It can be noted from Table 1 that if direct current is used, a positive tip requires a much larger diameter to withstand a given level of current than a negative one. This is because electrons impact on the electrode and heat it up, instead of cooling it by boiling. On the other hand, when employing alternating current, the tip is cooled during the electrode negative cycle by the evaporation of electrons and heated when positive by their impact. This can explain the current capacity of the electrode according its polarity, shown at the bottom of Figure 14.

Gas selection for welding protection depends on the application. However, argon, helium or mixtures are the most common types. Argon is easier to ionise than helium (lower ionisation potential), making it easier arc initiation and lowering the voltage drop across the arc [19]. Moreover, due to the fact of being heavier, argon offers more effective shielding and greater resistance to draughty environments. In contrast, helium has the advantages of higher power inputs and greater sensitivity to arc length variations, allowing welding of thicker sections, employment of faster welding speeds and a better control of the arc length.

In summary, GTAW is a very clean process that can weld many metals with some bias towards thin sections. Very reactive metals, such as titanium and zirconium can be joined, and oxide-former metals as aluminium and magnesium are commonly welded with this technique. Low deposition rates can be overcome by pre-heating the wire in mechanised GTAW. Other factors can affect the process, such as tip shape, pre-heat of the wire in mechanised GTAW, cycle shape, and joint design. They must all be taken into account in order to accomplish desired weld qualities.
2.2 The weld thermal cycle

During arc welding, an intense heat source is used to increase the temperature of the workpiece until the melting temperature of the material forming the joint. As a result of this movable source, residual stresses develop as cooling occurs, microstructural changes take place and atoms redistribute in a suitable way. The severity of these changes depends on many parameters such as cooling rate, heat input, peak temperature, attachment conditions, geometry of the joint, preheat temperature and the material thermal properties. It is therefore extremely useful to understand such phenomena, and its effects on the welding quality. The transfer of heat in weldments is described by the time-dependent conduction of heat, expressed by Eq. (1) [20].

\[ \frac{\partial}{\partial x} \left[ k(T) \frac{\partial T}{\partial x} \right] + \frac{\partial}{\partial y} \left[ k(T) \frac{\partial T}{\partial y} \right] + \frac{\partial}{\partial z} \left[ k(T) \frac{\partial T}{\partial z} \right] = \rho C(T) \frac{\partial T}{\partial t} - Q \]  \hspace{1cm} (1)

Where x, y and z are the coordinates in the welding direction, transverse to the weld and normal to the weld surface. \( k, \rho \) and \( C \) are the thermal conductivity, density and specific heat of the metal to be welded. \( Q \) is the rate of internal heat generation, valid for inputs of energy below the surface of the workpiece. With low power densities, the heat is directed onto the weld surface, and its distribution is governed by Eq. (2).

\[ k(T) \frac{\partial T}{\partial z} = q(x, y) \]  \hspace{1cm} (2)

Whether this two equations need to be solved for one, two or three dimensions depends on the weld design and shape of the weld pool. Rosenthal [21] solved the heat-flow equations of a heat source, assuming a constant speed \( v \) of the moving point. For arc welding, it is useful to define the heat flux as a function of the arc voltage and current. The heat flow can be calculated as indicated in Equations (3) and (4).

\[ q = \frac{\dot{q}}{v} \left[ \frac{kJ}{mm \text{ of weld}} \right] \]  \hspace{1cm} (3)

\[ q = \eta V I \]  \hspace{1cm} (4)

Where \( \eta, V \) and \( I \) are the efficiency of the arc, the voltage and current, respectively. The former can be defined as the fraction of total welding power reaching the weldment [13]. It is determined by the welding process and its values are presented in Figure 15, for conventional techniques. Other techniques are shown in Table 2. From Figure 15, it can be seen a low efficiency related to GTAW process. This fact is associated to the heat losses through the electrode holder (non-consumable electrode), as is schematically illustrated in Figure 16. However, higher efficiencies were reported by many researchers [19, 20, 23]. Moreover, it was found that operating with alternating current results in lower efficiencies than with direct current electrode negative, as indicated in Table 2. On the other hand, in consumable electrode welding processes, almost all the energy consumed in melting the electrode is transferred to the joint with the molten metal droplets [20]. Consequently, higher efficiencies are achieved in these cases. Other causes of energy loss in GTAW process are also schematised in Figure 16.
Figure 15: Measured arc efficiency for conventional arc welding techniques [22].

Table 2: Efficiency of some welding techniques [19].

<table>
<thead>
<tr>
<th>Process</th>
<th>( \eta )</th>
</tr>
</thead>
<tbody>
<tr>
<td>GTAW (DCEN)</td>
<td>0.5-0.8</td>
</tr>
<tr>
<td>GTAW (AC)</td>
<td>0.2-0.5</td>
</tr>
<tr>
<td>Electroslag</td>
<td>0.55-0.82</td>
</tr>
<tr>
<td>Gas</td>
<td>0.25-0.8</td>
</tr>
<tr>
<td>Electron beam</td>
<td>0.8-0.95</td>
</tr>
<tr>
<td>Laser beam</td>
<td>0.005-0.7</td>
</tr>
</tbody>
</table>

Welding thermal cycles define a characteristic temperature distribution with an extremely steep temperature profile, resulting from the intense heat source. The rate of increase of temperature at a point in the workpiece varies inversely with the distance from the weld centreline. After the peak value, temperature decreases at a rate also inversely proportional to the distance to the centre of the weld. The cooling rate experienced is a function of the rate of energy dissipation in the weldment. Figure 17 presents a numerical solution for temperature on top and bottom surfaces at various distances from the weld centreline, for a particular case. Not the existence of a displacement in the time of occurrence of the maximum temperatures. Furthermore, curves passing through the phase transformation range are shown with a change in the slope, produced by the heat of fusion liberated or absorbed.
The movement of the source results in a bow-wave effect in which the isotherms effectively pile up at the leading edge. Therefore, increasing welding speed, concentrates the heat closer to the source, affecting the solidification structure and properties of the weld. Speed effect is illustrated in Figure 18.

*In situ* measurements of weld thermal can be done by properly located thermocouples. It has been found that, for a given welding process, weld geometry and material, the cooling time from 800 to 500°C is constant within the heat affected zone. Thickness together with the weld process and the type of material strongly influence heat distribution, and therefore analytical solutions of Eq. (1). Solutions for thin and thick plates can be equated in order to find a critical thickness $d'$ that defines a cross-over condition above which the heat flow due to welding is essentially three-dimensional, and thus independent of thickness [24]. Figure 19 shows results of cooling time between 800 and 500°C against thickness, for various heat inputs in a bead-on-
plate weld. The major practical use of cooling rate calculation is in the determination of preheat requirements of, for example, hardenable steels.

In the case of multiple pass welds, two effects should be considered. First, the base metal is preheated by the initial beads, resulting in lower cooling rates for subsequent weld passes, and less hard microconstituents. The effect is greatest in the second bead. Second, latest weld
deposits temper the material solidified in previous beads and also their heat affected base metal. Only last weld pass remains not-tempered.

To conclude this section, it must be noted the importance of describing and predicting the actual thermal cycles experienced by the heat affected zone. Many parameters described herein affect microstructures obtained during the welding operation. The width of the heat affected zone is proportional to the heat input, but it can also be altered by modifying, for example, the initial temperature of the material to be welded (preheat temperature) or the weld joint design. Metallurgical aspects of welded joints are discussed in the next section. Thermally induced residual stresses are also dealt with in following sections.

2.3 Welding metallurgy

In fusion welding, three metallurgically different zones can be distinguished: the fusion zone that solidifies from the pool temperature, the heat affected zone (HAZ) next to the fusion zone, and the unaffected parent plate, at a distance from the melted metal. These regions have particular features that contrast them between each other. The kind of materials forming the joint, the welding parameters, welding process and welding conditions can also affect the shape, size, microstructure and mechanical properties of the zones. A schematic example is presented in Figure 20, where two joints are shown; Figure 20a exhibit a cross section of a butt joint contrasting the differences between the HAZ in a pure metal and an alloy, whereas Figure 20b illustrates the case of a non-fusion weld for comparison.

![Figure 20: Zones in a fusion welded butt joint (a) and a non-fusion weld (b) [11].](image)

In the present thesis, steel welds are analysed. Therefore, the alloy side in Figure 20a best represents the transition between the fusion zone and the unaffected material for this case. However, it is worth discerning a steel joint from other kind of alloys. Figure 21 illustrates a butt
low-carbon steel joint and its regions, according the phase diagram Fe-Fe₃C. Note that the peak temperature profile throughout the section is also shown.

As was explained before, thermal cycles are inherently associated to fusion welding and, depending geometry restraints among other factors, the original microstructure and properties of the metal can be severely affected. This volume of material, referred as HAZ is usually the weak link in the joint, and a careful examination of the welding procedure is needed in order to improve the HAZ behaviour.

Easterling [24] divided the HAZ into a number of sub-zones as is presented in Figure 21. Apart from the welding conditions and thermal cycle, characteristics of the sub-zones depend also upon the thermo-mechanical state of the base material, i.e. thermal and mechanical history prior to welding. For instance, grain growth and recrystallization behaviour are affected by the presence of precipitates that can inhibit grain expansion at high temperatures (Nb, Ti, V) and the amount of cold working in the material (dislocation density), respectively. HAZ in steel will be discussed in following chapters.

![Figure 21: Sub-zones of the HAZ for a particular low carbon steel [24].](image)

### 2.3.1 Base material

The parent metal in a weld can have a wide range of mechanical properties as a result of the thermo-mechanical history. Heat treatments and cold or hot working of the steel are designed
in order to achieve the desired combination of mechanical properties (fracture toughness, yield strength and fatigue resistance). For example, hot rolling produces steel with lower strength than quenching and tempering process, but with higher ductility.

Similarly, the addition of alloying elements has a great influence on microstructure. It is well known that the grain size strongly affect mechanical properties. Hall-Petch equation relates the grain size to the yield strength and ultimate tensile strength for a number of materials, according Eq. (5) and (6), respectively [26].

\[
\begin{align*}
\sigma_{YS} &= \sigma_0 + k_{YS}d^{-1/2} \\
\sigma_{UTS} &= \sigma_0' + k_{UTS}d^{-1/2}
\end{align*}
\]

Where \(\sigma_{YS}\) and \(\sigma_{UTS}\) are the yield strength and tensile strength, \(\sigma_0\) and \(\sigma_0'\) refers to a material constant related to the frictional resistance of the lattice to the moving dislocations, \(k_i\) is another material constant and \(d\) is the average grain size or the mean width of the bainite or martensite lath in bainitic and Q&T steels.

It must also be noted, that a decrease in grain size improves fracture toughness and fatigue nucleation of cracks. It is the only unique parameter that can enhance both strength and toughness. However, at nanometric levels, several evidences exist [27] proposing an inverse Hall-Petch relationship, but this is out of the scope of the present work.

Typical grain sizes for structural steel range from 15-20 \(\mu m\) for plain C-Mn steels, around 10 \(\mu m\) for C-Mn-Al normalized steels, 5 \(\mu m\) for C-Mn-Nb-Al normalized steel and around 2-3 \(\mu m\) for Q&T steels.

### 2.3.2 Weldability and carbon equivalent

Weldability is defined as "the capacity of a material to be welded under the imposed fabrication conditions into a specific, suitably designed structure and to perform satisfactorily in the intended service" [38]. Although weldability depends on the process, operating parameters, procedures, restraint and attachment conditions, environment and joint design, the most important factor influencing the easiness of welding a material is the base metal chemical composition. Many tests can be found in the literature to assess weldability [13, 15, 20]. Direct weldability tests reproduce the conditions held during manufacturing as close as possible, reason for which they are also termed “welding tests”. In contrast, indirect weldability tests or simulated tests make use of metallurgical principles to replicate the heat effect from welding on a base material and create a simulated weld zone in terms of microstructure. Examples of the former are Tekken test, Lehigh restraint test and Sigmajig test among others.

Since chemical composition has proven to have the mayor effect on weldability of materials, numerical equations have been developed in order to grade or quantify the influence of this factor. Consequently, the concept of carbon equivalent emerged for ferrous materials and many formulas can be found in literature, each of which has an application range and consider the influence of each chemical element on the transformation characteristics (Ms temperature, CCT curves, etc.) by a proper factor. Some examples are summarized in Table 3, showing also three main groups according Yurioka et. al. [28]:

A. Based on the original proposal of Deardan and O’Neil (CEIIW).
B. In this group, carbon is more important than the first group, since low carbon concentrations the kinetic of transformation become so rapid that changes in alloy content don not affect hardenability markedly [24]. Pcm is the common parameter used.

C. CEN is the parameter used. Carbon’s effect on microstructure depends on the other element contents.

<table>
<thead>
<tr>
<th>Group</th>
<th>Formula</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>( CE (IIW) = C + \frac{Mn}{6} + \frac{Cu + Ni}{15} + \frac{Cr + Mo + V}{5} )</td>
</tr>
<tr>
<td></td>
<td>( CE (WES) = C + \frac{Mn}{24} + \frac{Cu}{6} + \frac{Ni}{40} + \frac{Cr}{5} + \frac{Mo + V}{4 + 14} )</td>
</tr>
<tr>
<td></td>
<td>( CE (Stout III) = C + \frac{Mn}{6} + \frac{Cu}{40} + \frac{Ni}{20} + \frac{Cr + Mo}{10 + 10} )</td>
</tr>
<tr>
<td>B</td>
<td>( P_{cm} = C + \frac{Si}{30} + \frac{Mn}{20} + \frac{Cu}{20} + \frac{Ni}{60} + \frac{Cr}{20} + \frac{Mo + V}{15 + 10 + 5B} )</td>
</tr>
<tr>
<td></td>
<td>( CE (Gravelle) = C + \frac{Mn}{16} + \frac{Ni}{50} + \frac{Cr + Mo}{23} + \frac{Nb + V}{7} + \frac{8 + V}{9} )</td>
</tr>
<tr>
<td></td>
<td>( CE (Duren) = C + \frac{Si}{25} + \frac{Mn}{16} + \frac{Cu}{16} + \frac{Ni}{60} + \frac{Cr + Mo + V}{20 + 40 + 15} )</td>
</tr>
<tr>
<td>C</td>
<td>( CE (Stout II) = 1000C \times \left( \frac{Mn}{6} + \frac{Cr + Mo}{10} + \frac{Ni + Cu}{20} \right) )</td>
</tr>
<tr>
<td></td>
<td>( CEN = C + A(C) \times \left( \frac{Si}{24} + \frac{Mn}{5} + \frac{Cu}{15} + \frac{Ni}{20} + \frac{Cr + Mo + Nb + V + 5B}{5} \right) )</td>
</tr>
<tr>
<td></td>
<td>Where ( A(C) = 0.75 + 0.25 \tanh \left( 20 \left( C - 0.12 \right) \right) )</td>
</tr>
</tbody>
</table>

Table 3: Formulas of Carbon Equivalency for steel welding [28].

Carbon equivalent measures the tendency of a welded joint to form brittle and hard microstructures during cooling in the HAZ. When CE is below 0.4, steel is considered weldable [29]. Both high hardness values and the presence of martensite assist the occurrence of the well-known phenomena hydrogen induced cracking or cold cracking. CE concept is mainly used in calculations of preheating temperature in order to prevent hydrogen cracking, together with other factors such as heat input, time of cooling between 800 and 500°C \( (t_{8/5}) \) and residual stresses [30].

As a final remark, CE has been related by numerical equation to hardness values. However, data compiled present considerable scatter and conclusions made regarding this relations should be used with caution [24].

### 2.3.3 HAZ

The weld heat affected zone is the region adjacent to the weld metal that has not been melted, but whose mechanical properties and/or microstructure have been modified by thermal cycles. In alloys, HAZ lies outside the partially melted zone (PMZ), which corresponds to sites that reach temperatures between liquidus and solidus during welding. Strictly speaking, HAZ should include all zones heated above room temperature; though only those regions actually influenced by the heat are part of it. This will depend on the chemical composition and thermo-
mechanical history of the metal to be welded. Materials strengthened by cold working, precipitation hardening and transformation hardening can significantly alter the HAZ. However, it remains almost unaffected when the material is hardened by solid solution or dispersion. Following sections deal briefly with each case.

2.3.3.1 Solid solution strengthened alloys

Solid solution strengthened materials, such as aluminium alloys and copper alloys, are not markedly affected by welding. High temperatures close to the fusion line will cause grain growth, but this region is usually narrow and does not significantly affect mechanical properties. However, width of the grain-grown region depends on the heat input. Hot-rolled low carbon steels and ferritic and austenitic stainless steels also belong to this category. Last two materials can present problems in the HAZ during welding related to embrittlement and phase formation at 475°C, and grain-boundary carbide precipitation (sensitization or weld decay), respectively. In ferritic stainless steels, risk of α-phase precipitation can be reduced by low heat input, no or low preheat temperature and a low interpass temperature [13]. Like austenitic stainless steel, they can suffer from sensitization, but at temperatures higher than 925°C, which means that the weld decay occurs at the area immediately adjacent to the weld metal, rather than a small distance away. Figure 22 schematically depicts sensitization range related to various thermal cycles in stainless steels. Curve “b” can lead to carbide precipitation in grain boundaries, provided time at sensitization range temperatures is long enough. Curve “a” results in high cooling rates that prevent carbides nucleation. Finally, at position “c”, peak temperature is not sufficient to allow precipitation to occur.

Austenitic stainless steels can overcome sensitization problems, by suitable post-weld heat treatments, or by affecting directly the chemical composition of the base material. Reduction of carbon content and addition of strong carbide formers like titanium or niobium can effectively reduce the risk of weld decay.

2.3.3.2 Cold worked materials

If sufficient deformation has taken place in the material, strain hardened base metals can recrystallise above a proper temperature. The recovery step associated with recrystallisation
that affects principally electrical properties, produce almost no microstructural change and no significant alteration of mechanical properties. Then it is of little concern in welding [19]. Recrystallised zone becomes softer compared to the deformed base metal, in consistence with a fine equiaxial dislocation-free grain structure. Grain growth also occurs close to the fusion line. More complex microstructures can be obtained when the base material undergoes an allotropic transformation (for example, steel and titanium), and two recrystallised regions may be formed from each allotropic transformation [20].

2.3.3.3 Precipitation hardened alloys

Some materials are strengthened by a heat treatment that form precipitates. Aluminium-copper alloys present this strengthening mechanism for particular compositions. When welding these materials, the region closest to the fusion line will suffer softening due to fully reversion (dissolution) of its precipitates\(^1\) and will generate a relatively soft single phase solid solution with some coarse grains [24]. It should be noted that the cooling rate, especially at short distances from the fusion line (higher peak temperatures), is high enough to avoid metastable phases precipitation (after reversion). Therefore, dissolved precipitates can no longer hinder dislocation movement, softening the material in that region. This is displayed in Figure 23a and b for an age-hardenable aluminium alloy. Moreover, lower peak temperatures will result in less degree of “reversal” and therefore in a less reduction of hardness (see curves 2 and 3 in Figure 23). Sites that do not reach the reversion or solution treatment temperature (curve 4) cannot dissolve precipitates. If temperature is above the original aging temperature, then that region of the HAZ can be overaged, and hence softened due to loss of coherency of the precipitate phase [15]; if not, its hardness remains almost unchanged (unaffected base metal).

\(^1\) Sometimes, precipitates that actually strengthen the material do not correspond to a stable phase. More than one metastable precipitates can take place at temperatures lower than the solidus transformation, for a particular composition, making the overall process quite complex.
Both post-welding natural aging and artificial aging allow metastable precipitates to form, which can enhance the hardness close to the fusion zone. A lower hardness can be obtained at locations 2 and 3, due to the formation of coarse precipitate particles. Overaged microstructure cannot be markedly reharden by post-weld heat treating, resulting in low hardness values.

### 2.3.3.4 Transformation hardening materials

The last case corresponds to transformation hardening alloys. The sub-zones that may form can be explained using Figure 24. It corresponds to 0.3% carbon steel, but results are similar for other compositions.

#### Figure 24: Sub-zones of the HAZ for a 0.3% carbon steel [20].

- **Zone 1**: Coarse-grained fully austenitized zone
  Disregarding the thin zone that corresponds to Fe δ phase formation at high temperatures, region 1 suffers a rapid austenitic grain growth, due to temperatures around melting temperature (generally considered above 1100°C). The extent of grain coarsening depends mainly on the peak temperature, the dwell time at this temperature, and the characteristics of the steel [31]. In lower CE steels, proeutectoid ferrite networks commonly form after welding, covering grain boundaries. In contrast, higher CE values results in lower transformation products, like Widmanstätten side plates [24].
- **Zone 2**: Fine-grained fully austenitized zone
  Farther from the fusion line, peak temperature decreases below 1100°C and little, if any, grain growth occurs in the austenite. Grain refining elements as Ti, Nb and V remain effective. During cooling, ferrite nucleates in austenite grain boundaries, leaving high carbon austenite at grain centres. The latter transforms to pearlite, bainite or martensite, depending on the cooling rate and the carbon content.
- **Zone 3**: Partially austenitized zone
When temperatures are in the biphasic region of Fe $\alpha$+Fe $\gamma$, the pearlite in the base metal austenises, enriched with C and Mn. Again, the reverse transformation of the partially transformed austenite during cooling can lead to pearlite, bainite or martensite formation, depending on the cooling rate.

- **Zone 4: Sub-critical zone**

Sites in the HAZ that withstand peak temperatures lower than the eutectoid one, experience a tempering of ferrite grains. Recrystallisation of ferrite or spheroidization of pearlite may occur in regions close to the A1 isotherm, but other types of structural change do not appear to happen. In the range up to ca. 650°C, however, the combined effect of heating and residual stresses can trigger dynamic strain ageing. In this phenomenon, dislocations interact with interstitial atoms like C or N, and become strongly locked in position during cooling. This results in a brittle structure [24].

Figure 21 schematically depicts these regions.

Particularly important are steels with sufficient carbon and alloy content to form martensite during cooling from welding or that are already heat treated to tempered martensite prior to welding. Since large grain size increase hardenability, zone 1 can readily transform to martensite. Hardenability of subsequent regions decreases as moving farther from the fusion line. Martensite formation in this case depends on the cooling rate and alloying content. It must also be noted that the onset and extent of the grain growth zone is affected by the presence of high temperature precipitates.

High heating rates and brief high temperature retention time can result in the formation of heterogeneous austenite during welding. Such a microstructure, can aid nucleation of high-carbon martensite colonies, after welding. Hence, microhardness of the HAZ tends to scatter widely [19].

The four cases presented here, are simplified situation and reality might have more complex behaviour. It is important to remember when analysing HAZ of welded components that many factors can influence them, such as alloying elements, high temperature precipitates, metastable phases, allotropic transformation, heat input, cooling and heating rates, and thermo-mechanical history of the base metal. All of them should be considered for a complete understanding of HAZ microstructures.

### 2.3.3.5 HAZ in multiple pass weldments

Multipass weldments present alteration of the microstructure obtained in single pass welds, by subsequent passes thermal cycles. Figure 25 shows different zones that can be formed in coarse-grained region of a multipass steel weldment, depending on the reheating temperature. Then, it is possible to find the following subzones:

- **Subcritically reheated grain-coarsened (SCGC) zone**; reheated below $A_{c1}$.
- **Intercritically reheated grain-coarsened (ICGC) zone**; reheated between $A_{c1}$ and $A_{c3}$.
- **Supercritically reheated grain-refined (SCGR) zone**; reheated above $A_{c3}$ and below ca. 1200 °C.
- **Unaltered grain-coarsened (UAGC) zone**; not reheated above ca. 200 °C or again reheated above ca. 1200 °C.
It was found [32] that coarsened regions present low CTOD values. These places of the HAZ are commonly known as local brittle zones (LBZs). In spite of having ICGC, SCGC and UAGC the same austenite grain size, the former usually happens to be less tough, owing to a higher amount of high carbon martensite [13]. Controlling carbon and alloying element contents, or in situations where this cannot be done, controlling properly the welding procedure, size of the LBZs can be limited and therefore, higher toughness can be achieved in brittle zones.

2.3.4 Weld metal

The weld metal is the region of the weldment that has experienced temperatures above melting temperature, and therefore has solidified. Solidification concepts allow to explain microstructures obtained in the weld metal. It is not the aim of this work to explain solidification theory, and only a brief description of its main concepts is performed.

To start with, solidification is divided into two main processes: nucleation and growth. Nucleation behaviour needs an undercooling to occur, in order to provide sufficient activation energy for the atoms to form an embryo. Eventually, this germ transforms into a solid nucleus that has enough energy to grow. Whether this situation happens aided by a foreign body or not, solidification is called heterogeneous or homogeneous, respectively. Figure 26a displays many close-packed crystal-like clusters (shaded) or embryos, formed statistically in the liquid. This is the first step for homogeneous nucleation of metals. In contrast, Figure 26b presents the case for heterogeneous nucleation in the mould surface.
In fusion welding, solidification is likely to occur in alloys instead of a pure metal, and heterogeneously, due to the not melted parent metal and impurities present in the weld pool that provide free surfaces to initiate nucleation. In pure metals, the solid/liquid interface is likely to be planar (provided undercooling is not severe). On the other hand, alloys can present planar, cellular, or dendritic interface, depending on the solidification condition and the material system involved.

Supercooling theory can explain different solidification modes, by considering the concept of constitutional supercooling. This is schematically presented in Figure 27. It can be seen an interface that is solidifying at a rate $R$, together with the binary phase diagram of the alloy. At a temperature $T^*$, in the two-phase zone, the solid phase withstands less solute atoms in solution than the liquid phase. During solidification, these atoms are rejected into the latter and consequently, a rise in solute concentration takes place near the solid/liquid interface (see the concentration vs. Position diagram in Figure 27b). Since the liquidus temperature in the phase diagram is a function of the solute content, the equilibrium liquidus-temperature profile in the liquid $T^L(x)$ is altered, as shown in Figure 27c. If the actual or real temperature of the liquid near the interface is such that it’s gradient is less than that set by $T^L(x)$, then there is a portion of liquid at a temperature lower than the corresponding equilibrium temperature. It is said then, that this region is constitutionally supercooled.

Interaction between gradient, interface shape and solute segregation is seen in Figure 28, for a solidifying weld. Note that the amount of undercooling determines whether the interface is planar, columnar or dendritic.
Figure 27: Constitutional supercooling for alloys, showing relationship between phase diagram (a), solute concentration in the liquid (b) and actual and equilibrium temperature in the liquid (c) [19].

Figure 28: Schematic representation of interface morphology obtained for different relation of equilibrium-liquidus temperature ($T_L$), and actual melting temperature ($T_A$) [24].
Factors that affect temperature of the liquid, also affect the cell spacing. For example, higher resistivity and/or welding speed lead to finer cell spacing [24].

Apart from described undercooling, other phenomena take place in weld metal. Dilution, turbulence, severe thermal gradients and diffusion among others can strongly affect solidification behaviour in weldments. Figure 29 illustrates some contributions to weld metal final composition, morphology and size.

The influence of each factor varies from one weld to another. Welding conditions and parameters can also modify to a certain extent the severity of their consequences. Nevertheless, general effects for fusion welding processes can be described as follows.

- **Impurities can be found in the weld pool.**
Weld metal solidification of most commercial metals involves microsegregation of alloying and residual elements or impurities. This mechanism promotes dendrites formation during solidification. It is well known how undesired impurities affect mechanical properties and favour cracking phenomena during solidification (hot cracking) by the formation of low melting interdendritic liquid [20]. Moreover, impurities can strongly affect cohesive strength and therefore grain boundary fracture [13].

- **Dilution of the base metal.**
Dilution implies that the base material reaches the melting temperature, and consequently, not-melted material next to it suffers grain growth due to high temperatures. On the other hand, epitaxial growth is known to happen in fusion welding. Since the initial crystal size of the weld metal is inherited directly from the parent metal, the coarser the HAZ grain, the larger the weld metal crystal structure [24].

Dilution ratio varies with geometry of the joint, as well as the welding procedure and welding technique. In situation where excessive dilution can cause cracking, it is common practice to use a “buttering” layer with a particular composition. This means that the surfaces of the joint are partially or totally clad prior to welding, as shown in Figure 30.
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- Turbulence due to convective forces and droplets of molten electrode. Stirring action of the arc and the action of Marongoni surface tension gradient induced convection forces, produce good mixing of alloying elements, and also modify temperature gradients. Both effects alter concentration profiles ahead of the solid/liquid interface and hence affect the solidification mode (planar, columnar or dendritic). Turbulence also changes the shape of the weld pool, resulting in unsymmetrical welds as shown in Figure 31b. This kind of shape leads to non-uniform grain growth in the HAZ [24].

- Ratio of molten metal volume to base metal volume is small. It was shown that heat flow is influenced by the thickness and geometry of the welded joint. The easiness with which heat escapes from the molten pool determines the cooling rate. In comparison to casting, larger volume of a casting and the poorer heat transfer make the cooling and solidification rates much lower. It must be highlighted that high cooling rates associated with welding lead to relatively fine cellular-dendritic structures, compared to castings.

- Matching compositions of materials forming the joint. Chemical composition of the molten metal obviously influences solidification. Microstructures obtained after cooling depends, apart from cooling rate, on initial composition of the weld...
Ceferino Steimbreger

pool, and the degree of dilution and mixture. For example, low impact resistance values are usually found in root beads, where dilution ratio is the highest.

- Temperature gradients in the pool.

Maximum temperature gradient set a path for grain growth. A very large temperature gradient develops in the weld pool due to the fact that heat is continuously being added. The centre of the pool can reach around 2500°C, whereas at the fusion line, the temperature is the melting temperature of the base material. As was shown earlier, liquid real temperature determines the amount of constitutional supercooling, for a given solute concentration profile. This high positive gradient helps to limit non-planar solidification [13]. It also generates a higher solidification rate at the centre of the pool and a correspondingly finer subgrain structure, than that close to the fusion line [20].

Furthermore, temperatures profiles across the weld metal section are constantly modified by convection, and this effect should also be taken into account when analysing solidification.

- Solidification behaviour is dependant of welding speed.

Higher welding speeds tend to elongate the weld pool. Different solidification behaviours are shown in Figure 32. It can be demonstrated that crystals growing at the weld centre line behind the moving heat source grow faster than crystals at the edge of the weld. Low welding speeds allow grain growth perpendicular to isotherms at a rate imposed by the heat source (Figure 32 b). In contrast, increasing welding speeds elongate the shape of isotherms and growing grains have to follow complicated path to keep direction at the weld centreline (Figure 32 a). Such behaviour can badly affect mechanical properties, since there is a high risk of segregation at the centreline.

It is plausible for some alloys, that axial grains initiate in the original weld bead and continues along the length of the weld, halting growing of lateral grains. Less pronounced isotherms in low speed weldments, lead to wide axial grains (Figure 32 d). In contrast, narrow grains grow in the centreline of high speed deposits (Figure 32 c).

It is worth mentioning that some crystallographic directions ease grain growth, and together with maximum temperature gradient and other factors determine the macrostructure of solidified weld metal. Grains with favourable orientations growth at the expense of those with other orientations.

After solidification, steel weld metal presents cellular-dendritic structure, consisting of coarse columnar austenitic grains, developed as presented in Figure 32, and a fine cellular network within the grains\(^2\). Depending on the cooling rate, different microstructures are expected. In the case of diffusive transformation, ferrite is likely to nucleate at large grain boundaries between columnar austenitic grains. This is because cell boundaries are small angle low-energy regions, formed by an arrangement of dislocations. Low cooling rates usually results in a mixture of carbon-enriched pearlite and blocky allotromorphs ferrite.

Medium cooling rates allow higher undercooling below A3 temperature. Therefore other high activation-energy microstructures can be obtained, such as Widmanstätten and acicular ferrite, depending on the alloying content. The latter is particularly desired due to its high toughness. It

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\(^2\) This structure does not always remain in the fusion zone, because of small microsegregation or quick homogenization [19].
is believed that propagating cleavage cracks are constantly deflected as they cross a chaotic acicular ferrite microstructure [34].

A mixture of previous microstructures, together with upper bainite can be found for faster cooling rates (transformations occur around 690°C). If cooling is fast enough, diffusionless transformations can take place, giving rise to lath martensite as product. It must be noted that austenite grain size and alloying elements can modify expected microstructures. For example, refined austenite grains provide a number of intergranular sites for nucleation of ferrite. Since acicular ferrite nucleates heterogeneously on non-metallic inclusion within the grain, this transformation is less favoured by a small austenite size, and therefore, bainitic transformation at grain boundaries is likely to occur instead. This is schematically illustrated in Figure 33. On the other hand, abundance of impurities and elements tend to promote lower temperature transformation products, which are characterised by high hardness and strength. Furthermore, the pattern of dendrite arms in a solute rich network impedes plastic flow during tensile testing, resulting in higher yield-to-tensile strength ratios than base material [20]. Toughness and impact resistance, however, are generally not high because of inhomogeneity of weld microstructures and segregation problems.
A variety of other kind of microconstituents can also be found in literature and a careful identification must be done, if a better understanding of the weld metal properties is needed. For instance, the International Institute of Welding published a document [35] that classifies microstructures in ferritic steel weld metal using optical microscope.

In multipass weldments, after the first bead, each pass generates a HAZ in the weld metal previously solidified. Even though complex microstructures can be found in these “re austenitized” regions, the general effect is a refinement of grains and therefore, an improvement of mechanical properties of the underlying metal [36].

Solidification of the weld metal is complex process, affected by many factors that are not independent. An overview of how the weld pool behaves during cooling has been discussed. Problems encountered in the weld metal were briefly commented, but this is the aim of the next section.

2.3.5 Discontinuities in fusion welded unions

AWS classifies discontinuities under three main groups, related to: procedure and process, design and metallurgical behaviour. All of them alter stress distribution in the weld or HAZ, but the latter may also influence mechanical properties and corrosion resistance. Discontinuities reduce the resistant cross section area and act as stress concentrators, both effects being detrimental for integrity of the structure.

Severity of the effects of discontinuities depends on their shape, size, location in the joint and orientation relative to the maximum tensile stress. Table 4 presents different kinds of defect and their classification according AWS. It must be noted that some of the flaws listed in Table 4 and groped under a particular heading, can be also related to other groups.

Some of the discontinuities listed below have been previously described. Because of the wide scope of this field, the present review is not intended to be exhaustive, but describes essential characteristics of the most important defects, particularly focusing in cracks.

<table>
<thead>
<tr>
<th>Welding process or procedure related</th>
<th>Metallurgical</th>
<th>Design related</th>
</tr>
</thead>
<tbody>
<tr>
<td>Geometric</td>
<td>Cracks</td>
<td>Microstructure alteration</td>
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<tr>
<td>Misalignment</td>
<td>Hot</td>
<td>Spherical</td>
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<tr>
<td>Undercut</td>
<td>Cold or delayed</td>
<td>Elongated</td>
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<tr>
<td>Concavity/convexity</td>
<td>Reheat, stress-relief, or strain-age</td>
<td>Worm-hole</td>
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<tr>
<td>Excessive reinforcement</td>
<td>Lamellar tearing</td>
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<tr>
<td>Improper reinforcement</td>
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<td>Overlap</td>
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<tr>
<td>Burn-through</td>
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<td>Backing left on</td>
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<tr>
<td>Incomplete penetration</td>
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<tr>
<td>Lack of fusion</td>
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<tr>
<td>Shrinkage</td>
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<tr>
<td>Surface irregularity</td>
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<tr>
<td>Arc strikes</td>
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<td>Slag inclusions</td>
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<td>Tungsten inclusions</td>
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<td>Oxide films</td>
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<td>Spatter</td>
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<tr>
<td>Arc craters</td>
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</table>

Table 4: Classification and types of discontinuities in fusion welded joints. Adapted from [20].
2.3.5.1 Porosity

Gas can be trapped in the weld pool during welding due to too much moisture on the base or filler material, or by inadequate cleaning of the surfaces forming the joint. Environment can also contaminate the weld pool, if improper shielding is used. If dissolved gas content in molten metal is higher than the solubility limit in solid metal at the solidification temperature, then a local increase in the dissolved gas content will occur in the liquid by its rejection to the solid-liquid surface [33]. When this value exceeds the solubility limit in the molten metal, bubbles will form. Because of their buoyancy, these bubbles tend to rise in the weld pool, but they are also subjected to convection. Depending on the particular force that dominates convection, bubbles will be swept in one direction or another. Usually, gas is kept into the pool, and after solidification, they form pores. Figure 34 a and b depict solubility of hydrogen and nitrogen in iron, as a function of temperature. Note sharp changes in solubility when phase transformation takes place. Oxygen does not generally cause porosity because it reacts with the majority of metals to form oxides. Carbon dioxide can be the origin of porosity when welding nonkilled steels, though the majority of consumables contain enough silicon to avoid this problem. While in ferritic steels and nickel alloys not containing nitride forming elements, nitrogen is generally the cause of pore formation, in aluminium alloys and austenitic stainless steel, hydrogen is responsible for porosity [33].

It is worthwhile mentioning, that apart from being the origin of porosity, dissolved gases like nitrogen, hydrogen and oxygen, they can chemically react with molten metal to form nonmetallic brittle compounds. Hydrogen may also diffuse to the metal surrounding the pore and produce embrittlement, due to reduction of cohesive energy and strength of the lattice [15].

![Figure 34: Solubility of nitrogen (a) and hydrogen (b) in iron. Adapted from [19].](image)

Porosity is intimately related to the welding process and procedure. It can be uniformly distributed or isolated in cluster of pores, for example, in the crater or at the beginning of the
bead, due to improper initiation or termination of the welding arc. These types of porosity are illustrated in Figure 35 a and b, respectively. In contrast, linear porosity may form along a weld interface, the root bead, or a boundary between weld passes (Figure 35 c). It is the product of gas evolution from contaminants present in boundaries.

Pores are generally spherical, but they can also present sharp points [13]. It is also possible to encounter elongated porosity inclined to the direction of welding (Figure 35 d), or under a wormlike shape and texture. The latter is the result of pore formation between growing dendrites during solidification [37].

When pores are formed in the face of a weld bead due to inadequate shielding, excessive current or wrong polarity, they should be eliminated. Otherwise, they can act as sites for slag entrapment during subsequent passes.

It has been found that amount less than 3% by volume of porosity almost no remarkable effect on static tensile or yield strength. Although there are some cases where pores can arrest a propagating crack due to blunting of its tip, this is not a justification for accepting porosity [15]. On the other hand, pores behave as stress raisers and therefore can promote crack nucleation during cyclic loading. From this point of view, surface porosity is more dangerous than bulk or internal porosity. Effect of the latter on fillet and butt welds with reinforcement is overshadowed by stress concentration on the weld toes. However, studies have shown that removing the weld reinforcement of butt joints causes exposed porosity to affect negatively their fatigue resistance [20].

![Figure 35: Common types of porosity in the weld metal. Uniform (a), isolated (b), linear (c) and elongated (d) [13].](image)

### 2.3.5.2 Geometric and other procedure related defects

Several defects resulting from deficiencies in the welding process can be seen in Figure 36. These discontinuities are dangerous since they can all be potential sites for crack nucleation. Slag inclusions are non-metallic solid materials trapped beneath the surfaces of the weld metal, in the root or between beads of multipass welds. Erratic arc or improper usage of electrode can spill the slag ahead of the arc and thereafter cover it by the weld pool [13]. In multipass
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weldments, it is very important to clean carefully the beads after each pass, removing all the slag that has formed.

In the case of lack of fusion (LOF) and lack of penetration (LOP), origins of the problem are usually incorrect selection of welding parameters and joint design, inappropriate electrode manipulation and insufficient cleaning prior to welding. LOP refers to the amount of base material that has been fused and resolidified to lead a deeper throat than that originally set before welding. In contrast, LOF consider how efficiently was the base metal melted and mixed with the filler. Then, the former are generally found in roots and LOF in the surfaces of the joint, as shown in Figure 36b.

Some geometrical discontinuities are shown in Figure 36a. Undesired mass flow can occur due to excessive convective forces, unsuitable welding parameters and improper electrode manipulation. Weld metal can distribute heterogeneously throughout the joint and lead to overlaps, undercuts, underfill and concavity or convexity, among other problems. Undercuts and overlaps can form sever mechanical notches that aid fatigue crack nucleation. Careful design of the welding procedure should be carried out, in order to avoid such defects formation. For example, backgouging of the root of the first weld can be used when welding double groove welds, ensuring that there are no areas of inadequate penetration.

![Figure 36: Geometric procedure related defects in butt and fillet joints.](image)

Although severity of presented defects varies from weld to weld, they role as stress raisers must be controlled after welding by proper non-destructive techniques. Removal of dangerous discontinuities should be accomplished before the component is place in service. Postweld grinding and TIG remelting, for instance, can remove undercuts and overlaps, reducing risk of crack formation. Nevertheless, other kind of cracks can be found in welded joints and are described in the following section.

### 2.3.5.3 Cracks

It is well known that a variety of shapes and types of cracks can be found in weldments. Examples of locations according AWS A3.0 [38] are shown in Figure 37 a and b, for butt and fillet joint, respectively.
Cracks that develop at elevated temperatures are named hot cracks or solidification cracks and occur mainly at the weld centreline or between columnar grains. In contrast, cold cracks occur after cooling and growth between and through the grains. Other common cracks are liquation cracks, lamellar tearing and reheat cracks. The former are associated with grain boundary segregation and melting of boundaries near the fusion line [24]. Lamellar tearing is illustrated as number 4) in Figure 37, and is related principally to tee and corner joints, where the fusion boundary of the welds runs parallel to the plate surface and tensile residual stresses may act across the plate thickness [39]. Low through-thickness or short transverse ductility caused by elongated FeMnS inclusions, give their characteristic step-like appearance. Susceptibility to this kind of cracks increases with the thickness. A careful design can reduce the risk or likelihood of their occurrence.

Finally, reheat cracks are intergranular in nature and usually develop in the coarse grain of the HAZ. They nucleate during post-welding heat treatment in the range of 500-650°C, or in service at high temperatures. Particularly susceptible to this type of cracking are steels containing carbide-forming elements (Cr, Mo, V) and certain austenitic stainless steels [33].

Disregarding the aforementioned characteristic cracking phenomena, cracks can nucleate in stressed sites of a joint, under fluctuating loads. Stress concentrators are potential locations for fatigue cracks to start, particularly in the root and toe of the weld. Pre-existing discontinuities such as overlaps or undercuts also enhance the risk of this problem, aggravated by welding residual stresses. The latter is the aim of the next section.

Cracks are the most severe stress concentrator of all discontinuities. Any form of cracking is unacceptable in weldments regulated by most fabrication codes. It is extremely important to repair these discontinuities regardless of the location and amount of material removed for this purpose [20].

### 2.4 Residual stresses in weldments

Localised application of heat during fusion arc welding causes the formation of a complex time-dependant state of stresses in the whole piece. Non-uniform temperature distribution and different rates of expansion and contraction between different sections during welding
generate zones under compression stresses and other under essentially tensile stresses. This can be explained with Figure 38, where a bead on plate weld is shown, and temperature and stresses profiles along the x axis are schematically presented for selected cross sections. It should be pointed out that initially, the plate has no internal stresses caused by manufacturing processes.

To start with, section A-A is sufficiently far from the thermal gradient and does not suffer notable heating, and therefore, no stresses are developed. Section B-B crosses the weld pool region and hence withstands the highest peak temperature. The liquid metal does not transmit stresses at all. However, the material surrounding experiences thermal expansion due to the high temperatures, but is restrained by metal farther away from the pool that is not sufficiently hot to expand. It is known that the yield strength decreases with increasing temperature. Due to that, above some critical temperature, it occurs that flow stress of the section is exceeded, and further heating only softens the metal. Compressive stresses are generated then in a region surrounding the heat source with a peak in their magnitude at a distance from the centreline. Consequently, tensile stresses develop farther away to maintain equilibrium throughout the section (no external force is applied).

Along section C-C, somewhat behind the arc, temperature gradient becomes less steep. During solidification, the metal contracts, limited by the material next to it that was previously under compressive stresses. This results in tensile stresses around the centreline. Similarly, a section that has cooled down like D-D presents the final stress profile that is shown in the bottom right corner of Figure 38. Finally, the shaded area M-M' represents the zone that undergoes plastic deformation during welding.

Figure 38: Schematic representation of a bead on plate weld (a) and its changes of temperature (b) and stresses (c), for different cross sections. Adapted from [13]
In three-dimensional stress field, five more components of the stress tensor exist. Figure 39 represents $\sigma_x$ and $\sigma_y$ according the coordinate system in Figure 38, for a butt joint. It is worth mentioning that the weld length can enhance longitudinal residual stresses until a constant value at 457mm, but it has little effect on the maximum transverse tensile stresses. Similarly, the effect of the width is negligible when it is several times greater than the width of the weld and the HAZ; and thickness effect should only be considered when welding plates over 25mm [20]. Furthermore, cutting the piece generates a redistribution of residual stresses, since the equilibrium condition must be satisfied.

Figure 39: Typical residual stress distribution in a butt joint. (a) $\sigma_x$ in the longitudinal direction and (b) $\sigma_y$ in the transverse direction [40].

Depending not only on the degree of restraint, rigidity of the structure or component and bevel design, but also the tensile strength of the weld and parent metal [40], residual stresses can equal yield stress of the metal. Correspondingly, residual strains develop and may lead to distortion after welding, as illustrated in Figure 40 a and b, for butt and fillet welds. Transient deformation during welding is most evident when joining asymmetric configurations. This metal movement is opposite to the distortion that may remain after cooling of the welds [13].

Apart from possible changes in shapes of components, residual stresses can cause premature failures of structures under certain conditions. Although fatigue cracks can develop in welded structures under compressive stresses, they will only growth in regions under tensile stresses. Crack propagation redistributes residual stresses and may relief those with a positive sign, eventually leading to crack arrest.

Tensile residual stresses are added to those tensile stresses externally applied, producing a higher stress than that expected and reducing therefore the fatigue strength of the component. On the other hand, compressive residual stresses contribute to enhance fatigue strength.
However, due to the fact that their existence is accompanied by the presence of their counterpart, this benefit is seldom used in design.

As the worst case, welded joints can contain yield magnitude tensile residual stresses, with geometries promoting severe stress raisers combined with crack-like discontinuities, from which fatigue cracks can readily grow under either tensile or compressive applied cyclic stresses. This, results in a relatively low fatigue strength compared to unwelded details, with a steeper Wöhler curve and a reduced endurance limit, as shown in Figure 41a.

Regardless of the initial size of defect, it is commonly assumed that fatigue crack initiation period is insignificant compared to unwelded details as is schematically illustrated in Figure 41b. Then, mechanisms governing fatigue in welded structures are generally those ruling fatigue crack growth, or Stage 2, according Paris law. However, nowadays welding techniques and quality control allow lower sizes of defects resulting from the process. This has led to increasing attempts to predict fatigue life of welded components by combining not only long crack growth, but also short crack fatigue behaviour and fatigue crack initiation in high-quality welds [42, 43].
It is clear that high tensile residual stresses produced after welding are undesired from the fatigue point of view. Nonetheless, their removal by heat treatment is sometimes inconvenient. This is because tensile residual stresses become important when the stress ratio $R$ has a negative value and applied loads are low, and therefore in high cycle regime. Moreover, the effect of residual stresses tends to diminish after repeated loading [13]. The advantages of having compressive residual stresses in the structure can be used, by inducing deformation externally, in favourable positions by means of suitable techniques. These methods are discussed deeply in the next chapter.
Chapter 3: Fatigue of welded structures

3.1 Introduction

Fatigue nature is not completely well understood, and there are many areas that are still being investigated. But fatigue in welds is an even more complex phenomenon. Apart from thermal cycles during welding, which strongly affect the base material, the fusion process and the existence of a filler (in some processes) lead to a very inhomogeneous structure, varying in mechanical properties and chemical composition.

It have been said that in most cases fatigue cracks originate at the surface, although when dealing with welded structures some of them can nucleate away from it, in welding defects. Porosity, slugs, inclusions or lack of melting are potential sites for crack initiation and reduce the fatigue strength of the part. What is more, residual stresses and distortions also have a great influence in fatigue behaviour. Due to these, endurance limits reported for metals in engineering handbooks that have usually been obtained with polished round specimens, tested in air, have little relevance in design of weldments. Nonetheless, this does not mean that defects in welds subjected to cyclic loading cannot be tolerated but demands a proper analysis of each situation in order to determine the severity of discontinuities. The effect of discontinuities in the stress profile is discussed in the next section, focusing its attention mainly on welded components.

3.2 Stress concentration factor

Stress raisers theory states that for tension or bending, the peak stress, $\sigma_p$, close to a discontinuity can be related to the average stress on the net section, $\sigma_{net}$. The same can be defined in torsion using share stresses instead. Both cases can be expressed as:

$$\sigma_p = K_t\sigma_{net} \quad (3a)$$
$$\tau_p = K_ts\tau_{net} \quad (3b)$$

In which $K_t$ and $K_ts$ are the theoretical stress concentration factor for tension and torsion, respectively. Figure 42 a and b manifests two ways of presenting stress around a hole in a sheet, subjected to uniaxial load. In Figure 42 c, a real case is exposed with a FEM solution [44]. Both the mesh and stress distribution are shown.

Under cyclic loads, local high stresses generated close to a discontinuity can eventually cause damage in the form of cracks. Once these defects appear, the sharp notch and acuity of their tips intensify the stress field ahead of them, and contribute to their propagation. Because of the lack of deformation in this mechanism, fatigue cracks are hard to see, and the damage can easily progress to a considerable extent before it is discovered. Therefore, it is important to consider the possibility of this type of failure in the early stage of design.
Theoretical stress concentration factors apply mainly to ideal elastic materials and vary with geometry and loading. In some cases, plastic deformation can occur in front of the notch, modifying the stress distribution. Other concepts were therefore introduced, such as the effective stress concentration factor $K_e$, which is obtained experimentally as the ratio between the rupture load in a set of round bars of diameter $d$ and the rupture load of another set of round bar with a circumferential groove, with $d$ as the diameter at the root of the groove [45]. It can be noted that $1 \leq K_e \leq K_t$, and it is dependent on geometry of the body (particularly the acuity of the notch), the nature of the load and material properties. The relationship between $K_e$ and $K_t$ is defined by the concept of notch sensitivity, $q$, presented in Eq. (4).

$$q = \frac{K_e - 1}{K_t - 1}$$ (4)

In fatigue design, both $q$ and $K_e$ concepts are used, but the latter is replaced by $K_f$ or $K_{fs}$ in Eq. (4), defined as:

$$K_f = \frac{\text{Fatigue limite of unnotched specimen (axial or bending)}}{\text{Fatigue limite of notched specimen (axial or bending)}} = \frac{\sigma_f}{\sigma_{nf}}$$ (5)

$$K_{fs} = \frac{\text{Fatigue limite of unnotched specimen (shear stress)}}{\text{Fatigue limite of notched specimen (shear stress)}} = \frac{\tau_f}{\tau_{nf}}$$ (6)

Where $K_f$ and $K_{fs}$ are the fatigue notch factor for normal stress and shear stress, respectively, for $R=-1$. Although it was originally proposed for high cycle regime, it can be transferred to medium and low cycle fatigue. It is worth mentioning that if notch sensitivity is not taken into account in design and only stress concentration factor is employed, predictions will be conservative, and therefore on the safe side.

Welded structures are not far from being a stress concentrator: differences in sections, shapes of the bevels and non-welded root gaps create high stress concentrations with widely varying...
Fatigue of welded structures

geometry parameters. However, definition presented cannot be directly applied to welded joints, since the material conditions at the weld notch differ from the parent metal characteristics. Furthermore, residual stresses are not the same in each case. Nevertheless, these inconveniences can be overcome by properly selection of constant when calculating $K_f$.

Radaj [46] deeply describes another convenient parameter. Fatigue strength reduction factor compares the endurance limit of typically uniaxially loaded weld joints with the endurance limit of unwelded parent metal. It is particularly useful for design purposes and for predicting tendencies. In fact, fatigue strength reduction factor results from the reciprocal value of the fatigue notch factor. However, it must be considered that the latter is defined in relation to a plain polished specimen, whereas the former refers to a plain specimen in the mill-finished condition [47]. Hence, a correction factor has to be introduced in their relationship. Neumann A. proposed a value of 0.89 for St37 steel and pulsating fatigue strength.

Due to the fact that local stress distribution in the notch tip is more dependent on the notch radius $r$ than on other geometrical parameters, it was found that plotting $q$ vs. $r$ decreases the scatter resulting from specimen geometry influence. Examples of these curves for steel and aluminium are shown in Figure 43. It can be seen that $q$ is larger for stronger materials and for larger specimens.

In order to determine the relationship between $K_t$ and $K_f$, Peterson [49] made the hypothesis that fatigue failure occurs when the stress, over a certain distance below the surface, where the stress gradient is very steep, is equal to or greater than the fatigue limit of a smooth specimen. Assuming a linear decrease of the notch tip stress field, an empirical relation as presented in Eq. (7) was formulated. Kuhn-Hardrath formula can also be used for design purposes, and is displayed in Eq. (8).
Where $\alpha$ and $\rho'$ are material constants that should be determined by test data. The relevant local material constant at the potential site for crack initiation should be derived from hardness measurements when dealing with welded components. Residual stresses generated after cooling of the weldment, can be considered by adding them to the mean stress [47]. As was said in previous chapter, residual stresses may relax during cyclic loading, and this effect is considered in other approaches like the strain approach.

Relationship between $K_f$ and $K_t$ for welded joints is shown in Figure 44. It is noted that the latter tends to infinity when notch radii tends to zero, whereas the fatigue notch factor is usually restricted to a narrow range, depending on the type of joint ($K_f = 1.4 - 1.9$ for butt joints and $K_f = 1.8 - 2.3$ for fillet joints). Other equations relating both parameters can be found elsewhere in literature both for unwelded notched specimens [48-50] and for welded joints [47].

\[
q = \frac{1}{1 + \alpha/r} \quad (7)
\]

\[
q = \frac{1}{1 + \sqrt[2]{\rho'/{\alpha}}} \quad (8)
\]

It was mentioned earlier that $K_e$, and therefore $K_f$ values, can vary with the loading mode. Although it is generally found for simple notch configurations that they are reduced in bending than in tension and even lower in torsion, the statement cannot be generalised for some welded joints. This is illustrated in Figure 45, where fatigue notch factor in the toe (first number) and in the root (second number) are shown for some typical weld designs.

Real structures seldom meet above simple conditions of loading, but instead, they are subjected to more complex loading modes. In addition, stress profile can also be altered by changing the toe angle, by increasing the thickness of the plate and by the presence of undercuts, among other factors. When functional analysis solution (derived from theory of elasticity applied to notches) of the state of stresses in a “hot-spot” for a particular case are not available in a mathematical form, the analysis should be carried out by numerical methods such as finite element method or boundary element method, or experimental techniques, like...
Fatigue of welded structures

photoelastic, thermoelastic, or strain gauge method. If electrical resistance strain gauges or mechanical extensometers are employed, it should be taken into account the rapid variation of the strains with distance in the vicinity of the stress concentrator. Since the strain gauge will measure the average strain over its length, it must therefore be as short as possible to increase the accuracy of the method.

![Figure 45: Butt joints with root face (a, b, c), cruciform joint (d, e, f) and T joint (g, h, i) and their fatigue notch factors at the weld toe and root, under tension, bending or torsion [47]](image)

Important conclusions of what have been exposed are first, that not only the peak stress at the notch root influences the fatigue strength, but also the stress gradient ahead of the tip; secondly, the notch radius affects fatigue strength and not just \( K_f \), what leads to the corollaries that two similar discontinuities with the same theoretical stress concentration factor can have different effect on fatigue. Furthermore, Frost et. al. [51] analysed a large amount of experimental data, and came up with the following observations:

- For low \( K_f \) values, \( K_f \) is generally somewhat lower.
- For high \( K_f \) values, \( K_f \) is often quite lower than \( K_f \).
- For a given material and certain geometry, there is apparently a value of \( K_f \) at which \( K_f \) reaches a maximum.

It has been demonstrated the complex nature of stress raisers, enhanced when dealing with welded components. Some researches made good analyses of stress field in critical sites particularly focused on welded joints [52-54], and can be used as guidelines apart from the references already presented in this Chapter. The importance of the previous conclusions and all the concepts defined in the present part will be clarified in later sections when dealing with different approaches to assess fatigue of welded components.
3.3 Stresses in fatigue

Having described the effect of stress raisers in the stress profile close to a discontinuity, the aim of this section is to overview their different configurations in welded structures. Stresses may arise from a variety of sources such as wind, waves, vibrations, snow, steep temperature changes, pressure fluctuations and dead weights, apart from those directly related to the functioning of component and those inherited from the manufacturing process, such as residual stresses.

In fatigue analysis three categories of stresses are employed [55]:

1. **Nominal stress**
   
   It is defined as the stress in a component resolved usually using elementary theories of structural mechanics, such as beam theory. It is generally calculated adding the effect of stresses produced by external forces and momentums in the region containing the weld detail, but sufficiently far away from the attachment to disregard any effect of it in the stress distribution. Macrogemotrical effects generated by holes, edges, misalignments or other types of discontinuities without considering the weld detail itself should also be taken into account, but in this case, it is called *local or modified nominal stress* [56]. In some cases, like statically indeterminate structures or where analytical solutions are not available, FEM modelling might be employed to resolve nominal stresses.

2. **Structural stress**
   
   Definition of structural or geometric stress considers both nominal stress and the effect of structural discontinuities, not included in the aforementioned definition. Examples of structural discontinuities are shown in Figure 46b together with macrogemotrical discontinuities, in order to picture the differences between them. Note that the presence of a weld on only one side of an axially loaded plate some amount of shell bending, apart from the nonlinear peak stress.

   Structural stress consists of membrane stress and shell bending stress. The former is the average stress across the plate or shell thickness, and the latter is one half of the difference between the magnitude of structural stress at the top and bottom surfaces (see Eq. 8 and 9).

   It is worth mentioning that the value of structural stress at a point in the structure, where fatigue cracks are expected to nucleate, is called *hot spot stress*, and the critical site is named *hot spot*. Weld toes and other notches are hot spots. However, the nonlinear stress peak caused by such a discontinuity is not included in calculations of hot spot stress.
3. Notch stress
It is the total stress at the root of a notch and it does include the nonlinear stress peak caused by the local notch. This is illustrated schematically for a butt joint with a thickness \( T \), in Figure 47.

\[
\text{Local notch stress} \quad \sigma_{\text{ln}} = \sigma_m + \sigma_b + \sigma_{\text{nlp}}
\]

Figure 47: Notch stress in the weld toe, as the addition of membrane \( \sigma_m \), shell bending \( \sigma_b \) and nonlinear peak stress \( \sigma_{\text{nlp}} \) [55].

Considering the top surfaces as \( x=0 \) with the positive direction through the thickness, equations for local notch stress determination are written as follows:

\[
\sigma_m = \frac{1}{T} \int_{x=0}^{x=T} \sigma(x) \, dx \tag{8}
\]

\[
\sigma_b = \frac{6}{T^2} \int_{x=0}^{x=T} (\sigma(x) - \sigma_m) \left( \frac{T}{2} - x \right) \, dx \tag{9}
\]

\[
\sigma_{\text{nlp}}(x) = \sigma(x) - \sigma_m - \left( 1 - \frac{2x}{T} \right) \sigma_b \tag{10}
\]

Chattopadhyay et. al. [54] made an exhaustive stress and fatigue analysis of welded components is presented. A shell finite element analysis is used to calculate bending and membrane structural stresses, data which is later combined with relevant stress concentration
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factors, and used to determine the peak stress and the non-linear through thickness stress distribution.
Welded structures are commonly subjected to variable amplitude loading resulting from variations in the production conditions, vibrations, transient of temperatures and many other factors that are far from giving constant amplitude in the spectra. Dynamic stress fluctuations must also be taken into account, since they can severely affect fatigue crack growth [1, 6].

Several methods have been developed to reduce cyclic time histories to a simpler form of cycle count for fatigue analysis and testing. Since two independent parameters are needed to define a cycle, two-parameters counting methods are more adequate. Of them, rainflow counting has become widely accepted for identification of critical events [57]. Most important techniques are compiled in ASTM E1049 [58], but this standard does not recommend any particular method and it is the task of the analyst to select the most suitable method.

Finally, in real structures, it is common to find complex loading conditions, such as biaxial or combined loading (e.g. bending, torsion and tension). In those cases, unless realistic testing set-ups were carried out, it is necessary to find an equivalent stress or interaction formula. If the degree of multiaxiality is low, as in the case where a principal stress prevails over the others, and stress direction is constant (proportional loading), maximum principal stress should be considered, provided it acts predominantly perpendicular to the weld toe. The other principal stress can be analyses, if necessary, using the fatigue class in the nominal stress approach for stresses parallel to the longitudinal axis of the weld [56]. If this is not the case, the smaller principal stress can become critical because of the notch effect. Some design codes evaluate limit angles for considering a principal stress either perpendicular or parallel. Assessment can be even more difficult when dealing with non-proportional loads but this is not discussed here.

Different kind of stresses defined in this section, are considered in the description of the fatigue assessment approaches that follow.

3.4 Fatigue analysis approaches

Many aspects of welds have been discussed, and also their behaviour as a weak link in welded structures, particularly under cyclic loading. Fatigue cracks are likely to initiate in the stress concentrators created by the weld geometry, and this is influenced by many other factors, such as residual stresses due to weld thermal cycles, flaws inherently associated to fusion welding processes and heterogeneity of the microstructure in the HAZ among others. In view of their complex nature and their importance in many industrial fields, different approaches were developed for analysing fatigue in welded structures.

Strength assessments make use of loads and stress and strain parameters including their limit values, which can cause a permanent damage or deformation, crack nucleation or final fracture. When strength assessments employ external acting forces and moments or the nominal stresses derived from them, in a critical section, they are designated as global approaches. In this case, linear stress distribution is assumed and therefore macrogeometrical effects are not taken into account (i.e. local or modified nominal stress is not considered). On the other hand, if strength assessments aim at the design, dimensioning and optimization of structural components by means of local stress or strain parameters, they are grouped under the term local approaches. Figure 48 displays both approaches used for describing fatigue strength and
fatigue life in welded components. Different parameters together with characteristic diagrams are also presented in this image. Local conditions are increasingly taken into account from left to right in the figure.

Information in Figure 48 can be summarized in the following approaches:

1. Nominal stress: Nominal stresses are used \( \Delta \sigma_n = \sigma_{\text{max}} - \sigma_{\text{min}} \), and should be defined as explained earlier.

2. Structural or hot-spot stress: structural stress range \( \Delta \sigma_s \) is employed in order to consider the effect of the structural discontinuity apart from the “macro-geometry”.

3. Notch stress and notch intensity: Elastic notch stress range \( \Delta \sigma_k \) or an equivalent parameter such as the stress intensity is utilized. This approach takes the notch effect of the weld toe or root into account.

4. Notch strain: Local plastic deformation amplitude \( \Delta \varepsilon_k \) or another parameter that describes plasticity in the crack tip can be used.

5. Crack propagation: Fracture mechanic concepts, such as J-integral or the amplitude of the stress intensity factor \( \Delta K \) must be used to describe crack velocity \( \frac{da}{dN} \).

In the case of the last approach, it is particularly useful for the fatigue assessment of welded joints, where the growth stage is typically longer than the crack nucleation stage, if failure is defined, for instance, by a through-thickness crack. In this sense, it is worthwhile mentioning that description of microphenomenon in the local approaches to fatigue assessment is approximated by a macroscopic elastic or elastic-plastic stress and strain analysis. This is done according continuum mechanics applied to initiation and propagation of a “technical crack” and not a physical crack. The former is defined due to the impossibility by common technical means to detect crack length lower than ca. 0.5 mm. The latter definition includes short crack growth in the propagation step, as illustrated in Figure 49. Advances in inspection techniques and also improvements in welding techniques and quality control, though, have allowed lower sizes of defects to be detected and also smaller flaws to be formed during welding. This has encouraged many researchers to assess fatigue in welds and to predict fatigue lives considering physical crack initiation and propagation stages [42, 43].
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Figure 49: Fatigue stages considering technical and physical crack definition [59].

It is important to highlight the decrease in scatter when increasing local considerations (going from left to right in Figure 48). Figure 50a depicts different type of stresses considered in the first three approaches for a particular weld detail, and Figure 50b presents respective S-N curves for the different approaches, plotted considering a low probability of failure. Note how the Gaussian distribution becomes narrower, when geometric parameters and structural details of the weld are taken into account. In this Figure, $K_s$ refers to the structural stress concentration factor and $K_w$ is the weld shape factor or notch factor.

Figure 50: Schematic illustration of nominal, structural and notch stresses at a welded joint (a) and design S-N curves according each approach (b) [60]

Since the approach utilised in the present study was the nominal stress approach, it is discussed exhaustively in the next section. Brief comments are also made in relation to the hot spot stress approach.

### 3.4.1 Nominal stress approach

Nominal stress amplitude in a base metal critical cross section of small specimens or near full scale beams is plotted against cycles to failure (S-N curves). Service life results from comparison between these S-N curves and the nominal stress spectrum calculated by theory of damage accumulation. It can be estimated in respect to final fracture or a maximum crack length. Welding defects and typical flaws resulting from fusion welding processes are considered by the concept of quality classes for particular weld details. The IIW recommendations [56] identify

3 There is one exception in the case of partial penetration welds, where the nominal stress should be calculated in the throat section of the weld [47].
the S-N curves by the characteristic fatigue strength of the detail in MPa at 2 million cycles, at a high stress ratio, R ≥ 0.5, with a survival probability P_s=97.7%. Its value is called the fatigue design class or FAT and is valid for any R ratio [61]. Statistical evaluation of fatigue data is commonly done by fitting S-N or crack propagation curves by regression analysis, considering the number of cycles N as the dependant variable. Characteristic values are established thereafter by drawing curves k standard deviations below the mean stress curve. Two standard deviations are adopted by IIW recommendations, giving the 97.7% survival probability mentioned before. Likewise, Eurocode 3 [62] for design of steel structures adopted the value 97.7 %, although it assumed the number 95 % when calculating the fatigue test factor. However, greater values can be assumed for greater safety, as in the case of the new European Pressure Vessel Rules that employ 3 standard deviations, corresponding to 99.9% survival probability [63], or the recently published BS 7608-14 [64] that considers a 2.3 failure probability for the standard design curve, but values even lower of 0.14 in specific cases are also addressed.

Uniform design S-N curves for each FAT can be found in Figure 51 in the case of steel structures, showing usually a slope with m=3 for normal stresses and the constant amplitude knee point at 10 million cycles. For weld details in which fatigue crack initiation phase is significant, shallower S–N curves with m >3 may be adopted [65].

![Figure 51: Wöhler curves for steel and for particular FAT values [56].](image)

Common weld details are shown as examples in Table 5, as they are presented in IIW recommendations. A number of designs are compiled in this document, but in the case of more complex structural details not considered there, only local concepts are applicable. It is worthwhile mentioning that typical misalignment effects are already included in these fatigue classes.
Table 5: FAT values according IIW recommendations for structural details in steel, on the basis of nominal stresses [56].

<table>
<thead>
<tr>
<th>N°</th>
<th>Structure detail</th>
<th>FAT (steel)</th>
<th>Description</th>
<th>Requirements and remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>216</td>
<td>Root checked by appropriate NDT.</td>
<td>71</td>
<td>Transverse butt welds, welded from one side without backing bar, full penetration.</td>
<td>Misalignment less than 10% of plate thickness.</td>
</tr>
<tr>
<td></td>
<td>No NDT.</td>
<td>36</td>
<td></td>
<td></td>
</tr>
<tr>
<td>511</td>
<td>K-butt weld, toe ground</td>
<td>100</td>
<td>Transverse non-load-bearing attachment, not thicker than main plate.</td>
<td>Grinding marks normal to weld toe. An angular misalignment corresponding to km = 1.2 is already covered.</td>
</tr>
<tr>
<td></td>
<td>Two sided fillets, toe ground</td>
<td>100</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Fillet weld(s), as welded</td>
<td>80</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>thicker than main plate</td>
<td>71</td>
<td></td>
<td></td>
</tr>
<tr>
<td>521</td>
<td>I &lt; 50 mm</td>
<td>80</td>
<td>Longitudinal fillet welded gusset of length I. Fillet weld around end.</td>
<td>Particularly suitable for assessment on the basis of structural hot spot stress approach.</td>
</tr>
<tr>
<td></td>
<td>I &lt; 150 mm</td>
<td>71</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>I &lt; 300 mm</td>
<td>63</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>I &gt; 300 mm</td>
<td>50</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

In cases where high tensile residual stresses exist and they have the same direction of the tensile applied load, S-N curves present a step in the range of medium cycle fatigue strength, due to local yielding caused by strong relief of residual stresses. This leads to a reduced mean stress, which means an increase in fatigue strength. Low stress-high cycle fatigue may not cause notable stress relief because yielding is not reached by the governing state of stress [46]. Barsoum and Gustafsson [66] found relaxation of residual stresses for spectrum loading. They propose that peak loads may reduce either compressive or tensile residual stresses, reducing eventual benefits of the former, or prolonging fatigue life, in the latter case. Figure 52 schematically illustrates an S-N curve with the transition region from a state influenced by residual stresses to another where they are relieved. In spite of this, complete stress relief can rarely be achieved in real structures and long-range residual stresses are unlikely to be relieved. Thus, most Code writers have ignored the potential benefit of stress relief [65].

![Figure 52: Effect of residual stress relief in the position of the S-N curve [46].](image)
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All in all, the nominal stress approach is a statistically robust method widely spread in industry and still preferred in many areas that require only a simplified stress determination. Furthermore, latest investigations [67] have supported the revision of ISO 5817 by relating the weld imperfection acceptance criteria and quality groups of this standard with fatigue resistance classes according to IIW recommendations. The new updated standard [68] includes additional guidance on fatigue loads, which means a great advance in the fatigue assessment of welded structures. Many other construction and design codes make use of nominal stress approach as the basis for their guidelines. Examples for steel structures are BS 5400 [69], BS 5500 [70], BS 7608 [64], BS 7910 [71], ASME: Boiler and pressure vessel code [72], Doc. IIS/IWW-693-81 [73], ECCS/CECM/EKS recommendations for the fatigue design of steel structures [74], JSSC Fatigue design recommendations for steel structures [75] and DET NORSKE VERITAS AS, Recommended Practice - Fatigue Design of Offshore Steel Structures [76]. Nominal stress approaches provide good predictions on fatigue lives and strength only in the case of components with well defined loading conditions and simple geometries of the detail, contemplated by the standard. For other situations where complex loads and geometries prevail, local approaches should be considered. Likewise, if lightweight and damage tolerances limitations become important, as in the case of aerospace industry, then local approaches should also be addressed. The same is true when explicit consideration of certain factors influencing fatigue and the scatter related to them need to be treated individually. As this is not always possible, it is still sometimes necessary to ensure structure integrity by employing large safety factors, leading to the design of unnecessary heavy structures [77]. As in the case of global approaches, many researches in the subject motivate the application of local approaches in many other fields and their consideration in design codes or recommendations [55, 56, 62-65, 67, 68, 74, 76-81] and fabrication standards [82]. A final comment deserves to be done in relation to the design S-N curves. Hitherto, most of the data was presented following IIW recommendations, but there are some other designations for design Wöhler curves. For example, AASHTO LRFD Bridge Design Specifications employs totally 8 categories divided by fatigue resistance (category A, B, B’, C, C’, E and E’) and related to a particular formula [83]; JSSC [75] uses the letters A to H for joints subjected to normal stresses; and similarly, BS 7608 [64] make use of arbitrary letters to define each class or category. The employment of one or another depends on the application field and current regulations in the area.

3.4.2 Hot spot stress approach

As was mentioned in previous sections, nominal stress approach does not consider variations of a particular structural detail, such as macrogeometrical or structural discontinuities. In contrast, structural hot-spot stress approach includes their effects into the value of the stress calculated at the anticipated crack initiation site (“hot spot”), particularly the weld toe. The local effect of a notch in the stress cause a non linear stress distribution with a peak value adjacent to the toe. This peak stress is not taken into account in the hot spot stress approach, but it is inherently included during experimental determination of the S-N curves with this methodology in the form of scatter, in the same way as Wöhler curves determined with nominal stress approach includes structural discontinuities and notch effects.
The structural hot spot stress approach is typically used where there is no clearly defined nominal stress due to complicated geometric effects, or where the structural discontinuity is not comparable to a classified structural detail. The hot spot stress is generally measured by strain gauges arrangements or calculated by FEM and the approach can be applied to welded unions where there is a high risk of cracking at the weld toe or end, and when cyclic principal stress acts mainly transverse to the longitudinal axis of the weld [84].

3.5 Fatigue improvement techniques

In view of the widespread employment of welded unions in which toe cracks are prone to nucleate, several local treatments have been developed along the years to extend their lives. Besides good design practice [85] and high quality fabrications (good workmanship) that can readily be applied with “good imagination, divination and arbitration” [86], some other methods have been developed to improve fatigue strength of welded structures. Many researchers have deal with different techniques and found remarkable improvements in both parameters, but lack of standardization caused large variation among results [87]. Many methods have been developed so far, and they are summarised in Table 6, together with relevant references. They are divided into two major groups, according their main effect [88]:

- Modification of the weld toe profile.
- Modification of the residual stress distribution.

A third technique is proposed by Booth [88] that involves protection from environment with coatings.

<table>
<thead>
<tr>
<th>Residual stress methods</th>
<th>Mechanical methods</th>
<th>Peening methods</th>
<th>Overloading methods</th>
</tr>
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<tbody>
<tr>
<td></td>
<td>Thermal methods</td>
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<td>Initial overloading</td>
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<tr>
<td></td>
<td>Thermal stress relief</td>
<td></td>
<td>Local compression</td>
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<td></td>
<td>Spot heating</td>
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<tr>
<td></td>
<td>Gunnert’s method</td>
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<td></td>
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<tr>
<td></td>
<td>Low temperature transformation electrode [99, 100]</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Nickel based electrodes</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Weld geometry improvement methods</td>
<td>Machining methods</td>
<td>Burr grinding [87, 89, 91, 92, 101]</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Disk grinding</td>
<td></td>
<td></td>
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<tr>
<td></td>
<td>Water jet gouging</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Remelting methods</td>
<td>TIG dressing [1, 40, 87, 88, 91, 92, 104]</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Plasma dressing</td>
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<tr>
<td></td>
<td>Laser dressing</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Special welding techniques</td>
<td>Weld profile control [92]</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Special electrodes [92]</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Nowadays, there is only one document with guidance to some of these methods, namely, burr grinding, needle peening, hammer peening and TIG dressing [87]. This report is based on two
Fatigue of welded structures

IIW round robin testing program during 1994-1999 and 2003-2007, and put special emphasis on reproducibility, stringent specifications and high strength steels ($\sigma_{ys} = 700 - 1100MPa$).

In the present study, TIG dressing and shot peening were selected to be tested. Although the latter is not included in reference [87], many specifications have been developed to ensure process quality, both in commercial and military industry. Examples of these are SAE specifications and military specifications like MIL-S-13165C [105].

3.5.1 TIG-Dressing procedure

As far as the TIG-dressing procedure is concerned, the following considerations were taken into account.

1. The welded joint together with the adjacent plate should be carefully de-slagged and wire brushed. It is also possible to use light grinding to obtain a clean surface. Gas pores can form due to lack of cleaning, which may have strongly detrimental effect on fatigue resistance.

2. When employing FCAW process, if TIG-dressing is carried out just after welding, a preheat of approximately 150°C for minimum 20 minutes must be chosen to avoid weld metal cracking. If the improvement technique is done some time after welding, the $T_p$ can be reduced to that calculated for GMAW welding (less hydrogen content). Though, in Annex B, there is a preheat temperature calculation for this particular case, resulting in no preheat temperature according WPS.

3. Dressing parameters, according IIW recommendations are shown in Table 7 [87]. Having this in mind together with further literature in the field, welding conditions for this particular case are presented in Table 12, on page 99.

<table>
<thead>
<tr>
<th>Shielding gas</th>
<th>Argon or argon+helium</th>
</tr>
</thead>
<tbody>
<tr>
<td>Gas flow rate</td>
<td>7 - 12 liter/min</td>
</tr>
<tr>
<td>Nozzle diameter</td>
<td>10 - 14 mm</td>
</tr>
<tr>
<td>Preheat</td>
<td>50 - 200 °C</td>
</tr>
<tr>
<td>Electrode diameter</td>
<td>3 to 4 mm</td>
</tr>
<tr>
<td>Voltage, V</td>
<td>12 - 17 volts</td>
</tr>
<tr>
<td>Current, A</td>
<td>160 - 250 amperes</td>
</tr>
<tr>
<td>Dressing speed, S</td>
<td>80 - 160 mm/min</td>
</tr>
<tr>
<td>Heat input, Ht$^2$</td>
<td>1.0 - 2.5 kJ/mm</td>
</tr>
</tbody>
</table>

Heat input increase generates a smoother bead with a higher fatigue strength. If this is combined with the fact that higher heat input allows higher dressing speeds, which is essential for higher efficiency and productivity, current should be kept as high as possible without causing undercuts or substandard bead profiles [87, 102]. Furthermore, a high heat input is usually related with a low hardness in the heat affected zone (HAZ). Hanzawa [92] recommended a minimum heat input of 1.0 kJ/mm and also highlighted the importance of keeping the electrode tip clean and sharp to avoid pores.
4. Best results of the TIG-Dressing process are obtained when remelted zone is positioned carefully with respect to the original weld toe. Figure 53a indicates a suitable location of the arc centre in relation with the weld toe and it shows a slight sideways tilt of the torch from the perpendicular position, which may result in a favourable bead profile. Likewise, a small backward tilt helps to maintain an adequate gas flow, improving therefore the shielding function.

![Figure 53: Recommended torch position in relation to the toe (a) and push angle (b) [87].](image)

The distance between the arc centre and the weld toe depends on the steepness of the weld profile and the torch should be directed closer to the weld toe for flatter beads. Figure 54 illustrate different beads profiles resulting different distances of the arc. Optimum shape is presented in Figure 54a. If bead shapes similar to those shown in Figures 54 b and c are obtained, remedial treatment should be considered, such as a second TIG run. Sometimes it is necessary to employ a weaving technique or similarly, filler material may be used. It is an advantage of this improvement technique the possibility to run more than one bead with relatively easiness [87]. A second TIG pass should also be considered on the weld metal for steels with carbon content in excess of 0.12 weight %, due to the possible formation of hard zones in the heat affected zone (see Figure 55). This second tempering TIG run also contributes to soften the weld profile by a smoother transition between the weld and the base plate. Although its benefits, a second TIG bead adversely reduce the overall economy of the [103].
5. If the process must be interrupted before completing the length of the sample, it should be done carefully to avoid undesired craters or unfavourable weld profiles. Figure 56 illustrates different options to achieve this [87]:

- Restart the arc around 6mm behind the stop position (Figure 56a).
- Restart on the bead and move to the toe (Figure 56b).
- Stop on the bead (Figure 56c)
- Combination of previous methods (Figure 56d).
- Sequence with different directions (Figure 56e).
The dressed weld should have a smooth transition from the plate to the weld face, with a minimum toe radius of 3 mm, in accordance with Figures 53 and 54. The weld should be checked for complete treatment along the entire length of the part treated. The condition of the untreated weld affects the amount of increase that is possible to obtain by TIG dressing; for a favourable weld profile (FAT 90) only a small increase is possible [Ref. 92].

### 3.5.2 Shot peening

Shot peening is well-known in industry for enhancing fatigue lives of components subjected to cyclic loads. It is used routinely in fabrication in automotive and aerospace industries. The improvement is achieved by shooting steel balls at a speed sufficiently high to induce compressive stresses at the surface. The process is schematised in Figure 57a. As was thoroughly explained before, compressive stresses at the surface have a positive effect during crack nucleation.

Residual compressive stresses form due to cold working generated after impact and Hertzian contact pressure [105]. Due to the equilibrium of actions, compression close to the surface must be balanced with tensile residual stresses a distance away from the surface, as can be seen in Figure 57b. Note that the thinner the plate, the more the importance of the positive stress.
Parameters governing the process vary from the target material characteristics to the ball microstructure, shape, velocity and frequency. Typical ball diameters are around 0.2-1.0 mm and velocities lay in the range of 40 to 60 m/s. Disadvantages can take place if some of these parameters fail to produce the desire amount of plastic deformation. On the one hand, underpeening may not be effective due to poor residuals compressive stress development, and on the other hand, overpeening can cause damage to the surface, like microcracks, excessive roughness and severe microstructural changes. Interaction between balls and surface is also important, such as impact angle and friction between surfaces.

Control of all variables that affects the quality of shot peening is difficult to carry out, as in the case of ball shape and velocity. Then, two parameters are defined to fully describe the technique: Almen intensity and Coverage [106]. The former are measured with Almen strips (spring steel, SAE 1070) positioned on the surface and subjected to the same treatment as the material. After peening, the magnitude of residual deflection is measured in certain locations using Almen gage, and for a given material and thickness, the Almen intensity is defined. The other variable is the coverage that compares the area of the peened surface with the total area. 100% coverage is said to be achieved when visual examination at 10 magnifications exposes that all dimples overlaps [105].

Since in the present work testing of SP samples could not be included, examination of these parameters is left for future publications.
Chapter 4: State of the problem

4.1 Introduction

The present thesis deals with a particular problem presented in Boliden, which is a mining company located at the town with the same name in the northeast of Sweden. Boliden is a metal producing company with core competence within the fields of exploration, mining, smelting and metals recycling. The primary focus of Boliden’s exploration is on zinc, copper and precious metal-bearing ores. The goal of the present work is to analyze damaged components in machinery for the process in the Boliden Area.

Boliden Area is located in the mineral-rich Skellefteå field, operated by Boliden since the 1920s. Today, the area consists of a concentrator at Boliden, and five nearby mines. In 2012 opened the fifth mine in Kankberg, dedicated to gold extraction. Approximately 6700 ktonnes of ore are mined and concentrated in the Boliden area every year.

In addition to mine-site exploration, extensive field exploration is also conducted in the Boliden Area. Its products are Zinc, copper, lead, gold and silver. In 2013 Tellurium was added to the list. After mining operation, ores are sent by trucks to the plant. The plant contains big containers called “Tippficka” or pockets (both terms are used during this report) that receive the material and conduct it to the shakers and crushers. The product obtained is then transported by conveyor belts out of the building.

The interest herein is put on three components, all of which work under different conditions. To begin with, the pockets were checked, and presented cracks in particular unions. These structures are situated on the shakers, the latter being connected horizontally to the cracker, manufactured by Metso. Figure 58 displays the setup where the pocket and shaker can be seen in light blue and orange, respectively. The pockets were produced by a contractor (NIAB). Figure 59 shows two different views of the cracker.

The structure was placed inside a building, not suffering directly the weather conditions, such as low temperatures and wind.

Moreover, the loading of the Tippfickas could be recorded. This action is carried out by trucks outside the building (see Figure 60a). The ores are unloaded from the truck’s containers, and they hit the pocket, that is the inner part of the filler (Figure 60b). There is a difference between the two Tippfickas. While one of them processes crushed ores namely “Tippficka 1”, the other deals with big rocks (“Tippficka 2”). According to personal working in the sector, around 50 tons are dumped in the hole per truck. It is worthy, noting that the equipment receiving the uncrushed ore will withstand the highest stress peak during the load. Apart from that, the crusher produces high cyclic loads due to its vibration.

In order to simplify the problem, both Tippficka are dealt with indistinctly of their differences when analysing results. However, in the following sections, description of each case is done separately.
Figure 58: Displays of the shaker and filler.

Figure 59: Cracker from Metso. (a) Side view and (b) Upper view.
Fatigue of welded structures

4.2 Cracks in components

4.2.1 Tippficka 1

Two different components were damaged. Figure 61a, shows a picture of the component and a red circle showing the cracks location. Figure 61b, reveals the cracks. It can be seen in the latter, that three cracks were formed in the welds in different positions, and that there is also a fourth broken sheet fixed to the structure by bolts. Oxidation in crack surface gives an idea of the relative time since the crack formation.

Analogously, Figure 62 presents pictures from the other side of the machine, showing the component and the cracks nucleated. Note here, that the crack to the left seems to follow the HAZ during its growth, whereas the other appears to develop in the WM or in between two weld beads. It is also important to see that the quality of welds is not so good: notches and incorrect weld toe angles can be detected. This may have been caused by a repair carried out after the first crack was detected. There is also some leakage of the molten metal that can act as stress raiser. According to the drawings provided by the company, weldments were made following SS-ISO 5817 standard, and were designed to be “class C” welds [107]. This group demands good weld finishing, and has special specification and restrictions about geometry and defect tolerances.

Figure 60: (a) Unloading process. (b) Inner part of the filler (outward-facing).
Figure 61: (a) Component location. (b) Cracks in the component.

Figure 62: (a) Component location. (b) Cracks in the component.
4.2.2 Tippficka 2

Likewise, in Tippficka 2, cracks were encountered in many unions. Figure 63 and 64 illustrate this for particular components. In Figure 63b, two cracks can be seen in different locations. Crack 5 apparently grew through the weld metal. Then, nucleation might possibly have occurred in the weld root and propagated through the throat until it became visible on the surface. Another possibility is that it nucleated in a stress concentrator such as an inclusion or geometry notch. However, this cannot be interpreted a priori, and a more careful examination should be done.

Crack 6 is a severe well developed crack that spread through many fillet welds, HAZ and base metal (BM). It is likely that the crack started in the bottom right corner, and evolved until it reached the position in Figure 63b. Similarly, Figure 64 presents a relevant crack that grew following the weld toe in a column attached to the main structure. This is an important finding, since it can be interpreted the severity of the vibration phenomenon affecting close-by components.

In order to highlight the damage evolution, a comparison can be made between the set up in the first visit and the second one. Figure 65a and b indicate that a screw has become loose within the three month and that the crack sizes have increased. A second inspection was carried out, and it was found out that in fact two bolts were loose probably as a consequence of vibration. Then this must be taken into account in calculations and design.
As Tippficka 2 is not attached to a crusher, vibrations in this part are mainly due to the shaker. It is expected then that Tippficka 1 will be the most compromising component. Nevertheless, severe cracks were detected in the former. Moreover, cracks found on the beam in Figure 64 were not encountered in Tippficka 1. There is another relevant difference between this two constructions and is presented in Figure 66, that may explain to a certain extent the crack in the column. Although both structures present flanges that connected them with the beams, Tippficka 1 contains rubber dampers in the flanges that will reduce vibration severity transmitted to the beams. This might have conducted to the cracks in beams from Tippficka 2 and not in Tippficka 1 (see Figure 64 and 66).

Figure 64: Crack in a fillet weld on a beam.
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4.2.3 Stacker (Conveyor belt)

As was mentioned earlier, three components are analyzed in the present work: the two Tippficka, and a third component that is the Stacker shown in Figure 67. Lower supports are the components subjected to cracks. In this case, the provider and assistant is Sandvik.
It is important to mention that this structure works under different condition than the fillers. Although it is neither subjected directly to severe vibrations from the shaker or crasher nor to stress peaks due to loading, it may be exposed to quite low temperatures and high static loads. Besides, it may present vibration but not with the intensity of its counterparts. This region of Sweden can present temperatures as low as -40°C in winter. Therefore, the ductile to brittle behaviour of the steel used is of prior importance. The weather forecast from Skellefteå’s airport (around 50km from the plant) registered an historical profile of maximum and minimum temperatures. Figure 68 a and b reveal these profiles respectively, from the date on which the fillers and the belt were mounted (October, 2011). It is found that temperatures as low as -40°C are possible. Presented information is conservative, since the plant is located inwards in the land and may experience even lower temperatures than those in the airport region.

In the structure, several repairs were detected and can be seen in Figure 69. Quality of this additional material might be checked. Cracks were also found and are presented in Figure 70.
Note that the crack propagated along the weld toe. Severity of the damaged components should be analyzed.
Although some welds were not cracked when inspecting visually, their quality was not as expected, showing paint stripping and sharp notched at the weld toe. Those defects are potential sites for fatigue cracks nucleation and must be repaired according an established procedure (for example, AWS D1.1 [81]). Figure 71 shows two welds and their aspect during the two visits to the plant. It is evident that damage has evolved negatively. Furthermore, these defects were also detected earlier in August 27\textsuperscript{th}, 2013, by an inspection company called Inspecta.
Figure 69: Repairs in the lower part of the Stacker.

Figure 70: Crack in a beam from the Stacker support.
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4.3 Materials and joints

For a trustful analysis of the welds, it is necessary to identify the materials forming the joint and the welds. This section deals with the investigation of the joints and materials involved.

4.3.1 Tippficka

As was said earlier, the Tippficka receives the ore from outside, and transports it to the shaker and later to the crusher. A better insight of its function needs to take into account the main parts of the structure. The lower part of the Tippficka can be divided into two main pieces, as

Figure 71: Weld qualities for different parts. (a) and (c) correspond to the first visit and (b) and (d) to the second one.
can be seen in Figure 72. The chute, at the bottom left corner, leads the ore from the pocket down to the feeder table (shaker). The second element is the shutdown system consisting in a big gate that can be switched on if the feeding to the crusher must be stopped. Both elements are lined inside with wear plates (Hardox 500) and bolted on a steel armour, but not welded. Furthermore, the components are joined together by bolts and screws. This union is particularly one that cracked and is magnified and pointed out with a red arrow in Figure 72.

The steel armour is made of steel S355J2+N (equivalences: A656 or AISI 1024), that is a non-alloy quality structural normalized steel with $\sigma_{0.2} > 355 \text{ MPa}$ \(^4\). Figure 73 presents a back view of the shutdown system, where the two sheets of metal (Hardox 500 and S355J2+N) can clearly be distinguished.

The two bolted ears are welded to the steel armour as was shown in Figure 61-63. Then, it can be stated that the cracks developed in the welds made in S355J2+N. Hardox 500 is not affected by the welds, since it is only bolted to the armour.

\(^4\) S355 is a low carbon steel grade often used for applications which require better mechanical properties than that of S275 grade. Commonly supplied in the as rolled condition it is used widely in the construction, manufacturing and offshore industries.
4.3.2 Stacker

The cracked components in the stacker are made of thin plates welded in both a butt and fillet design. Figure 74 presents the stacker from two different views and points out cracked component. It is the support of the whole structure and transmits its weight and that of the ore to the wheels and rail. Particularly, Figure 75 isolates the part and indicates regions with defects. Circles emphasize the joints in Figure 71. Section X-X is shown in Figure 76, in which the red arrow points out the crack in the weld between beam A and B (see Figure 70).
Figure 74: Stacker and conveyor belt showing the damaged component.
Figure 75: Back (a) and top (b) view of the support structure

Figure 76: Section X-X in Figure 19a.
Chapter 5: Experimental methodology

5.1 Analysed material

In this section description of materials used in the experimental part are described.

5.1.1 Base material

Two steels were analysed in the present project, one corresponding to the Tippficka and the other to the Stacker. Spark chemical analysis was employed in order to corroborate the chemical composition.

A S355J2+N steel was used in the Tippficka armour. This is a high yield non-alloy steel first specified in EN 10025 standard and later included by the British Standards Institute (BSI) in BS EN 10025 [110]. The letter S specifies a “structural steel” and the number following it indicates a minimum specified yield strength at ambient temperature of 355MPa for thickness ≤ 16mm. According the aforementioned standard, J2 implies an impact energy requirement of 27J at -20°C, and N inform about a normalising heat treatment or normalising rolling process that is a “rolling process in which the final deformation is carried out in a certain temperature range leading to a material condition equivalent to that obtained after normalizing so that the specified values of the mechanical properties are retained even after normalizing” [111]. Tensile strength is in the range of 470-630 MPa, and minimum elongation is 22%. With respect to the steel making process, steel S355J2+N is a fully killed steel containing sufficient nitrogen binding elements, such as aluminium. Equivalences in other standards are DIN 17100-St52-3N, ASTM A572-50, BS4360 Grade50D and JIS G3106 SM490YB. This steel is usually employed in applications that need better mechanical properties than the well-known S275 structural steel, such as construction, manufacturing and offshore industries. Particularly, common applications are freight cars, transmission tower, cranes, trailers, excavators, railway wagons, pipes, highway bridges, building structures, offshore platforms, shipbuilding, power plants, fans and pumps among others [112].

Since no information about the steel selected for the stacker armour was obtained from the manufacturer, no certain designation can be assigned. However, judging from the chemical composition and its low carbon content, that are presented in the results, it can be regarded as an ASTM A575GrD or SAE-AISI 1513.

5.1.2 Tippficka material and electrodes

Only S355J2+N was welded and tested. The filler employed in this work is the same determined by the WPS for the welds in the structure. As mentioned previously, no data about the welding process could be obtained in the case of the Stacker, and therefore, no experimental analysis could be done. This is left for future investigations.

WPS states that for material S355J2+N (group 1.2 in ISO/TR 15608-2013 [113]) and thicknesses between 3-24mm, 3 beads must be done. The root must be carried out with electrode E 70C-6M/-6C, according AWS A5.18 [114]. This is a metal cored wire used with CO2 or Ar/CO2 as the shielding gas, specially designed for single-sided welding of thin section materials or root
passes, to give significantly increased productivity. It has good impact behaviour down to -30°C and is recommended for general fabrication and structural steel work. Although the welding procedure specifications mention it as Elgacore MX 100T, the wire employed in the present project was from ESAB.

Likewise, the filler passes should be performed with a rutile flux cored wire included in AWS A5.20 and which designation is E71T-1M. It must be used with Ar-CO2 as the shielding gas and it is commonly used to weld mild and medium strength carbon-manganese structural steels. Both wires are designed to weld in all positions and runs with a very stable arc, producing negligible spatter. Hydrogen content is limited to 5ml/100g weld metal in both cases. The weld metal has a minimum yield strength of 420MPa and a minimum elongation (A5) of 22%. Impact energy values are above 47J at -20°C.

5.2 Development of the work

In order to present clearly the steps and different tasks of the project, a flow diagram is shown in Figure 77 for the case of steel S355J2+N. First of all, the problem in the components had to be detected and possible reasons for its occurrence were analysed. Then, the state of art was investigated for a better understanding of the failure. Two branches were developed simultaneously: one related to the current codes, standards, and existing literature for repairing cracks in welded structures, and the other associated with experimental analysis. The latter includes base metal characterisation, welding and improvement method, joint characterisation, fatigue tests, and discussion of results. Material characterisation was mainly carried out with optical and scanning electron microscope, together with microhardness and chemical composition. Finally, both theoretical and experimental parts were considered to explain results and to summarise important conclusions.
5.3 Sample preparation

Sample preparation was carried out according ASTM E3-11 [115]. Boliden provided two pieces of material, one corresponding to the Tippicka, taken directly from the structure, and the other belonging to the Stacker. Samples were cut into small specimens in order to analyse transverse sections, as well as longitudinal microstructures. Figure 78 illustrate the criteria considered for designating the samples. The normal to the surface related to the axis T, L and S give rise to the label of each sample, i.e. if the plane has a normal perpendicular to axes T and L, then its designation is TL (surface parallel to the blue one in Figure 78). Under this methodology, transverse section is named TS (Parallel to the red section). Having this in mind, two samples were obtained for S355J2+N (TS and TL) and three for the Stacker material (TS, TL and SL). After that, specimens were mounted into phenolic powder to avoid edges blunting during grinding and polishing. The equipment employed was a Buehler SimpliMet 1000 automatic mounting press and is presented in Figure 79a. The cycle was set to 1 minute heating at 150°C and 290 bar, and 3 minutes cooling. Once samples were mounted with the desired surface on a side, grinding and polishing were carried out in semi-automatic machines, displayed in Figure 79b. Abrasive paper was used in
the first step, and numbers 240, 500, 1200 and 2500 were selected. Thereafter, samples were cleaned in an ultrasonic machine submerged in methanol or ethanol. Polishing was done later using 3μm, 1μm and 0.25μm diamond particles. Finally, surfaces were polished with slurry, which is a colloidal silica suspension. The same procedure was employed for the transverse section of welded joints in the three conditions considered in this work (AW, TIG-D, SP), but without mounting them in phenolic powder.

Figure 78: Nomenclature used as a basis for sample designation.

Figure 79: (a) Buehler SimpliMet 1000 automatic mounting press employed for mounting samples into phenolic powder. (b) Semi-automatic machines used for grinding and polishing.

5.4 Hardness testing

Vickers hardness measurements were done on both base materials and welded joints following ASTM E384-11 [116]. The equipment used was a Matsuzawa MXT-CX microhardness apparatus (see Figure 80). The load was sufficiently low to affect a particular microconstituent but high enough to guarantee a big print, according to standards. 100 g indentations for 15 seconds were performed to observe hardness variation throughout the thickness and to account microstructural changes like segregation. Likewise, hardness variation throughout the HAZ was
evaluated and effect of each improvement technique was compared. Distance between indentations was 0.3, 0.5 and 1mm depending on the region being analysed.

5.5 Optical analysis

Macrographs were taken in order to record sample orientation and shape in the base metal, and microstructural features in the welded joint. Optical analysis was carried out in a Nikon Eclipse MA200, laid out in Figure 81. Microstructure and hardness prints were observed and pictures were taken. Quantitative analyses of ferrite and pearlite was done in the base material, by measuring the amount of coloured areas in a specific region of the surface. Special aspects of the microstructure were pointed out, like segregation, rolling direction, manganese inclusions, HAZ and weld metal.

Etching was accomplished according ASTM E407 - 07e1 [117], submerging the samples in Nital 3% for 10-20 seconds. After that, water was poured on the surface and then methanol was used to clean the etchant from the specimen. Immediately, samples were dried with a hairdryer. Before microstructure observation, all pieces were ultrasonic cleaned in ethanol to erase traces of contaminants from the preparation procedure.

Images from optical microscope were combined with data from Scanning Electron Microscope. The equipment used was a Joel JSM-646OLV SEM, with an EDS detector from Oxford Instruments. Figure 82 presents the apparatus.
5.6 Welding and sample cutting

Weldments were performed with GMAW equipment, particularly, FastMig™ KM 300 FastCool 10 with a welding range up to 300A/32V, from Kemppi. Figure 83a illustrates a picture of the apparatus together with the shielding gas tube. In the present case, EN ISO 14175 M21 (MISON® 18) was used, which is a mixture of Ar + 18% \( CO_2 \) + 0.03% NO. The addition of carefully weighted nitrogen monoxide keeps the ozone content generated by the welding process at a low level, due to their reaction to form oxygen and nitrogen dioxide [118].
Fatigue of welded structures

For TIG dressing technique, Kemppi Mastertig MLS 3000 ACDC, a conventional GTAW equipment, was employed (see Figure 83b).

Prior to welding, surfaces to be joined were slightly grinded to remove oxides and other contaminants. Steel plates were positioned as is schematically presented in Figure 84a. Relevant dimensions and a weld bead are also depicted. Similarly, Figure 84b displays a picture previous to welding, where grinding traces can be visualised. Both main plate and gusset have a thickness of 20mm. This assembly was latter cut transversally to the welding direction in order to obtain samples of 49mm width for the fatigue test, as it is represented in Figure 85a and b. A metallographic saw was considered for this purpose, making ca. 3mm cuts. After welding, the length of the gussets is no longer relevant and it must be shortened to avoid possible vibrations during cyclic loading. It is important to have a clear HAZ in all the section and therefore, the gussets were cut some millimetres far away from the toe. Since weld initiation and arc extinction lead to different local properties in the toe that are not representative of the whole weld, edges were discarded and are not used for the fatigue test. Instead, metallographic analyses of the weld and hardness measurements were carried out in the inner surface of piece “X” in Figure 85a. All important dimensions are displayed in Figure 85a. Figure 85b shows the samples ready for the fatigue tests, once they were cut, machined, labelled and polished on the weld sides. At this step, it is worthwhile clarifying nomenclature used along this work. Five plates were welded and numbers from 1 to 5 were used to distinguish between them. As was mentioned, ten samples were obtained from each plate, and labelled with two numbers; one referring to the plate, and the other to its position in the plate. Then, a sample named 3.8 means the 8th cut in Plate 3. Furthermore, some plates were later shot peened (SP), TIG dressed
Ceferino Steimbreger

(TD) or both (SP+TD). Table 8 summarise plate’s conditions, giving a total of 15 samples AW, 15 samples SP, 15 samples TD and 5 samples TD+SP.

Table 8: Summary of conditions for welded plates.

<table>
<thead>
<tr>
<th>Plate</th>
<th>Condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>TD (1.1-1.5) and TD+SP (1.6-1.10)</td>
</tr>
<tr>
<td>2</td>
<td>As-welded (AW)</td>
</tr>
<tr>
<td>3</td>
<td>SP</td>
</tr>
<tr>
<td>4</td>
<td>AW (4.1-4.5) and SP (4.6-4.10)</td>
</tr>
<tr>
<td>5</td>
<td>TD</td>
</tr>
</tbody>
</table>

As far as the TIG-dressing procedure is concerned, the following considerations were taken into account.

- Weld metal was de-slagged, and both its surface and the parent metal were wire brushed prior to welding.
- Since the TIG beads were performed quite after FCAW weldments, and the latter did not prove to need pre-heating (see Annex B), no pre-heat temperature was used.
- Torch position was set as explained in Chapter 3.
- TIG-Dressing parameters were selected according literature.
- Although carbon content in the base metal was higher than 0.12 wt%, no second run was performed.

TIG-Dressed samples and their designation can be checked in Figure 85b.
Figure 85: Division of the main T-joint into fatigue samples and metallographic samples.
5.7 Shot peening

Shot peening was carried out in Scania Ferruform and the parameters used were the same that they employ in the production line. Particularly, the velocity was ca 90 m/s and 39A is mated with ca 420 kg/min. Characteristics of the steel shot can be seen in Table 9.

<table>
<thead>
<tr>
<th>Shape when new</th>
<th>Steel Shot</th>
</tr>
</thead>
<tbody>
<tr>
<td>Typical average hardness:</td>
<td></td>
</tr>
<tr>
<td>Tukon durometer Knoop</td>
<td>Round</td>
</tr>
<tr>
<td>diamond point 1000g load</td>
<td>Normal 46 – 51 HRC</td>
</tr>
<tr>
<td></td>
<td>Special 52 – 56 HRC</td>
</tr>
<tr>
<td>Vickers pyramid 1.0 kg</td>
<td></td>
</tr>
<tr>
<td>load</td>
<td>Normal 450 – 535 HV</td>
</tr>
<tr>
<td></td>
<td>Special 550 – 630 HV</td>
</tr>
<tr>
<td>Mean hardness deviation*</td>
<td>± 2 HRC or ±60 HV</td>
</tr>
<tr>
<td>Microstructure</td>
<td>Martensite completely fine and uniform</td>
</tr>
<tr>
<td>Minimum density measured</td>
<td>≥7.4 g/cm³</td>
</tr>
<tr>
<td>by alcohol displacement</td>
<td></td>
</tr>
<tr>
<td><em>(On 10 measurements taken</em></td>
<td></td>
</tr>
<tr>
<td>halfway across the grain</td>
<td></td>
</tr>
<tr>
<td>radius. Arithmetic mean of</td>
<td></td>
</tr>
<tr>
<td>absolute values of deviation</td>
<td>±3 HRC</td>
</tr>
</tbody>
</table>

5.8 Toe angle measurement

It has been exposed the importance of the toe angle in fatigue strength of welded joints. In order to characterise the samples, an extra test was carried out with the aim of registering toe angle variation throughout the length of the bead. This can be related then with fatigue crack nucleation points to measure toe angle influence.

The procedure consists in mixing two elastomers that hardens by an exothermic reaction, and place the homogeneous material on the fillet weld by applying some pressure. The polymer will copy the toe shape, and after hardening, it can be removed and cut transversally to measure the toe angle by an optical method. Figure 86 a illustrate this technique, once the polymer is placed on the joint and Figure 86 b shows polymer specimens once they are cut. At least 10 cuts were done in all samples and sides, but in those sides that cracked during tests, additional measurements were considered.

Figure 86: Replica technique to measure toe angle. (a) Position of the polymer on the weld and (b) cuts.
5.9 Fatigue test

Literature related to fatigue testing of welded components [119] was considered for technical issues. Fatigue tests were carried out in a fatigue resonant testing machine VIBRO-Forte Rumul 500 (2009), which is shown in Figure 87. In addition it counts with a servo motor in the lower part and a resonant testing system that can reach frequencies from 60-150 Hz and loads up to 500kN. The operating frequency depends mainly on the spring-mass system and sample stiffness. Particularly in this case, due to samples dimensions, frequency was kept between 110 and 115Hz.

Samples were tested in laboratory environment under a stress range varying from 100 MPa to 300MPa and a stress ratio R=0.

Figure 87: Rumul 500 resonance fatigue testing equipment.
Fatigue of welded structures

Chapter 6: Results

6.1 Analysed material

To start with, it is primordial to present chemical compositions of materials and wires that are used in the present project. Two steels were analysed, and their composition can be checked in Table 1, together with the equivalent carbon calculated according IIW formula. Spark chemical analysis was employed. Electrode compositions can also be found in Table 1 and were extracted from the manufacturer data sheet (see Annex C).

As can be seen, the steel used in the Stacker has the lowest carbon content and therefore a lower equivalent carbon. Mayor alloying elements are Si and Mn for both cases, but S355J2+N (material utilised for the Tippficka armour) contains an extra amount of copper.

Analogously, the electrode assigned for the root bead has slightly higher carbon content than the one used for the rest of the passes, due to a higher amount of mainly C, Si and Mn.

Table 10: Chemical compositions and equivalent carbon of analysed steels.

<table>
<thead>
<tr>
<th>Material</th>
<th>Chemical composition (%wt)</th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Ni</th>
<th>Cu</th>
<th>Nb</th>
<th>V</th>
<th>Al</th>
<th>Mo</th>
<th>Ti</th>
<th>B</th>
<th>Sn</th>
<th>EC</th>
<th>CP</th>
</tr>
</thead>
<tbody>
<tr>
<td>S355J2+N</td>
<td></td>
<td>0.2</td>
<td>0.5</td>
<td>1.6</td>
<td>0.03</td>
<td>0.03</td>
<td>0.55</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stacker steel</td>
<td></td>
<td>0.11</td>
<td>0.2</td>
<td>1.3</td>
<td>0.01</td>
<td>0.01</td>
<td>0.04</td>
<td>0.02</td>
<td>0.02</td>
<td>0.03</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0.3</td>
<td>0.3</td>
<td></td>
</tr>
<tr>
<td>AWS A5.18 E 70C-6M/-6C</td>
<td></td>
<td>0.07</td>
<td>0.5</td>
<td>1.5</td>
<td>0.01</td>
<td>0.01</td>
<td>0.24</td>
<td>0.3</td>
<td>0.25</td>
<td>0.05</td>
<td>0.08</td>
<td>0.02</td>
<td>0.2</td>
<td>0.4</td>
<td>0.4</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>AWS A5.20 E 71T-1M</td>
<td></td>
<td>0.06</td>
<td>0.4</td>
<td>1.2</td>
<td>0.01</td>
<td>0.00</td>
<td>0.27</td>
<td>0.5</td>
<td>0.3</td>
<td>0.05</td>
<td>0.08</td>
<td>0.2</td>
<td>0.4</td>
<td>1</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

* According BS EN 10025-1-2004 [110]
2 Spark chemical analysis
3 Manufacturer data sheet
4 Typical values
5 Maximum values

It is also significant to present mechanical properties of each material. However, no data from the Stacker steel could be obtained from the manufacturer. Information was obtained from corresponding standards and is shown in Table 11. Note the higher impact energy requirements for the root wire and the matching properties of S355J2+N and both electrodes.

Table 11: Mechanical properties and other mechanical parameters of materials analysed in the project.

<table>
<thead>
<tr>
<th>Material</th>
<th>Yield strength [MPa]</th>
<th>Tensile Strength [MPa]</th>
<th>Elongation, A5 [%]</th>
<th>Impact energy (-20°C) [J]</th>
</tr>
</thead>
<tbody>
<tr>
<td>S355J2+N</td>
<td>345*</td>
<td>470-630</td>
<td>20</td>
<td>27</td>
</tr>
<tr>
<td>Stacker steel</td>
<td>No data</td>
<td>No data</td>
<td>No data</td>
<td>No data</td>
</tr>
<tr>
<td>AWS A5.18 E 70C-6M/-6C</td>
<td>450</td>
<td>570</td>
<td>29</td>
<td>100</td>
</tr>
<tr>
<td>AWS A5.20 E 71T-1M</td>
<td>520</td>
<td>590</td>
<td>28</td>
<td>75</td>
</tr>
</tbody>
</table>

*For thickness between 16 and 40 mm
6.2 Welding

All plates were welded according to the WPS provided by the company, although some beads presented less heat input due to a lower welding speed (minimum values around 0.7kJ/mm were calculated in some cases). This may lead to incomplete fusion or incomplete penetration but should not influence the fatigue test for the as-welded condition, since the toe remains almost unaltered. However, employing improvement techniques such as TIG dressing can move the problem of fatigue crack nucleation to the other weak point in the union: the weld root. Therefore, lack of penetration should be taken into account in design of improved joints.

Three beads were carried out as specified in the WPS. It is worth mentioning that cracks were found in a component where only one bead was performed, in contrast with the aforementioned document.

Previously, it was mention that TIG-dressing parameters were selected considering literature data. Particularly, parameters chosen in this work for the single TIG-bead, without filler material, are displayed in Table 12.

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>DCEN</td>
<td>3.2</td>
<td>160-180</td>
<td>15-17</td>
<td>100-150</td>
<td>10-14*</td>
<td>Argon</td>
<td>10</td>
<td>1.2-1.5</td>
<td>ca. 50</td>
</tr>
</tbody>
</table>

* Appropriate flow rates, for a 12.7mm gas cup, 7 l/min and for a 15.8mm cup, 10 l/min are also recommended [102].

Since the TD sample is the most complete one to show different regions in the weld metal and HAZ, it is presented in Figure 88 as an example. Numbers on the passes indicate the weld sequence and the dotted arcs roughly delimitate each weld metal. The fourth bead in Figure 12 corresponds to the TIG bead, and it is only present in samples improved with this technique. Superposition of HAZ is also visible, and a larger width in the case of the TIG bead can be detected, since the heat input was higher than those performed with FCAW.
6.3 Hardness measurements

6.3.1 Base material

Hardness measurements were performed both in base material plates and welded joints. In the case of the former, different surfaces of the specimens were tested. Particular analyses were carried out in the TS section of both materials. Results are summarised in Table 13.

In the case of steel S355J2+N, disregarding segregation microstructures, an average hardness of 168 HV$_{0.1}$ was measured in section TS. This value is around 10% lower than the average value found in section TL. In the cut TL, the grain structure is observed parallel to the rolled direction and therefore, more pearlitic grains can be indented, increasing therefore the average hardness. As was mentioned in previous chapter, segregation line is visible in the centre of the plate. Some indentations were randomly done on these microstructures leading, as expected, higher hardness values than the overall microstructure (246HV$_{0.1}$). Furthermore, hardness as high as 317HV$_{0.1}$ were found. However, standard deviation for this case was the highest of all arrays. For practical purposes and considering that a crack may develop in the through-thickness direction, the average value of 168HV$_{0.1}$ is considered for later discussions.

Analogously, the steel corresponding to the Stacker armour presents a higher average hardness compared with its counterpart. In this case, different sections resulted in only slightly different values. An average hardness of 180HV$_{0.1}$ will be considered in this case. It is worthwhile mentioning that the piece analysed showed an average hardness 10% higher in the thinnest flange of the angle compared to the thickest one. Although hardness values in this flange were not taken into account for results shown in Table 13, they must be considered when analysing the welded joint, if it is performed in this region.
Table 13: Hardness results for both steels.

<table>
<thead>
<tr>
<th>Material</th>
<th>Section</th>
<th>Number of measurements</th>
<th>Average</th>
<th>Standard deviation</th>
<th>Standard deviation (%)</th>
<th>Maximum hardness</th>
<th>Minimum hardness</th>
</tr>
</thead>
<tbody>
<tr>
<td>S355J2+N (Tipficka)</td>
<td>TS</td>
<td>59</td>
<td>168</td>
<td>20.34</td>
<td>12.1</td>
<td>215</td>
<td>93</td>
</tr>
<tr>
<td></td>
<td>TL</td>
<td>34</td>
<td>186</td>
<td>21.22</td>
<td>11.4</td>
<td>231</td>
<td>136</td>
</tr>
<tr>
<td>Segregation</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>10</td>
<td>246</td>
<td>41.63</td>
<td>16.92</td>
<td></td>
<td>317</td>
<td>188</td>
</tr>
<tr>
<td>Stacker</td>
<td>TS</td>
<td>14</td>
<td>180</td>
<td>9.68</td>
<td>5.58</td>
<td>204</td>
<td>169</td>
</tr>
<tr>
<td></td>
<td>TL</td>
<td>40</td>
<td>176</td>
<td>14.8</td>
<td>8.38</td>
<td>201</td>
<td>131</td>
</tr>
<tr>
<td></td>
<td>SL</td>
<td>23</td>
<td>174</td>
<td>21.66</td>
<td>12.44</td>
<td>209</td>
<td>103</td>
</tr>
</tbody>
</table>

Following with the analysis, sections TS and TL of S355J2+N are presented in Figure 89 a and b respectively, which illustrate a cross section together with hardness results for both cases. Red lines indicate where the arrays of indentations are located. Note the peak in hardness in the segregation line.

The same is done in Figure 90 a and b for the Stacker’s steel, where hardness values are indicated for each array.
6.3.2 Joint

Hardness profiles were carried out in samples AW, TD and SP. An array was taken horizontally from the base metal to some distance away from the root. Figure 91 presents as an example Sample 5.6 (TD) showing the procedure to measure hardness in the HAZ. The red line indicates location of the indentation array. Note the increase of hardness in the TIG bead and its HAZ.

![Figure 91: Hardness profile in Sample 5.6 (TD).](image)

In order to compare results between conditions, all results were put together in a single chart. To do this and highlight differences in the process, it is important to consider a reference point to align data. In the present case, the weld toe was selected and its position is pointed out with a red vertical line. These results are illustrated in Figure 92. As expected, SP and AW samples presented approximately the same profile with a higher hardness in the HAZ of the 2\textsuperscript{nd} pass than that of the first one. This is so because the first pass suffers the tempering effect of subsequent beads.

Another remark should be done in relation to the shot peened samples. Shot peening induces deformation in the surface of the joint, and it introduces compressive stresses. This is translated in an increase of hardness close to the surface, as it was found in the BM of the SP samples (see red circle in Figure 92). Additional measurements were made close to the surface in many SP specimens. Results are displayed in Figure 93 as light blue dots. The hard layer was present in some samples up to a depth of 1mm. Fewer tests were done in normal base metal since no remarkable change in hardness was expected close to the surface, and this is represented by red dots in Figure 93. Note that far away from the surface, there are values with still high hardness, but these arise due to local harder microconstituent, like pearlite, and not because of the peening process.
Another set of indentations was carried out at 0.1mm from the surface in both SP and not SP samples. Results revealed an increase of the average hardness of 8%, as can be noted in Figure 94.
Residual compressive stresses induced by shot peening are limited by the material ultimate tensile strength. This means that the higher the tensile strength, the higher the residual compressive stresses that can be obtained from the process.

Weld metal showed an average hardness of ca. 280HV\(_{0.1}\), that is 2/3 higher than the average hardness of the base metal (168HV\(_{0.1}\)). Hardness measurements in the weld metal were in accordance with the effect mentioned in last paragraph and an increase of hardness was also found in the weld metal. Figure 95 a and b presents two sets of indentations carried out in the weld metal of a peened sample. It is evident from this figures that the increase of hardness close to the surface compared to the average value far away from it, is higher than that obtained in the base metal. Increments up to 20% were observed in several samples.
6.4 Microstructure

6.4.1 Base metal

Characterisation of the base materials were carried out with an optical microscope after grinding and polishing. Results are shown in Table 14.

Table 14: General microstructure found in analysed steels.

<table>
<thead>
<tr>
<th>Material</th>
<th>General Microstructure</th>
</tr>
</thead>
<tbody>
<tr>
<td>S355J2+N (Tippficka)</td>
<td>Ferrite (ca. 80%) and pearlite. Hardness around 168HV₀.₁. Equiaxed grains with an average size of 15um, measured in the through-thickness direction.</td>
</tr>
<tr>
<td>Segregation</td>
<td>Mostly pearlitic with traces of acicular ferrite or Widmanstätten plates. Manganese inclusions are mainly located in this region with elongated shape and occasionally rounded. Hardness varying from 190 to 320 HV₀.₁.</td>
</tr>
<tr>
<td>Steel Stacker</td>
<td>Ferrite (ca. 84%) and pearlite. Hardness around 180HV₀.₁. Grain size 11um, measured in the through-thickness direction. Less amount of Mn inclusions</td>
</tr>
</tbody>
</table>

As steel S355J2+N is in the normalised condition, it is expected to find a fine grain structure of pearlite and ferrite. Pictures from optical and scanning electron microscopes are presented in Figure 96 a to d for different magnifications. Red arrows in Figure 96 c point out MnS inclusions with elongated and rounded shapes. The presence of manganese sulphide inclusions in the steel interrupts ferrite matrix continuity, allowing a faster mechanizing, with less power consumption and better surface finishing [120]. Machinability increases when the volume fraction of inclusions is enhanced.

Sulphur is soluble in liquid steel but it is very insoluble in solid steel. It can combine either with manganese, forming MnS around 1600°C or iron, leading to FeS at 1000°C. However, a higher affinity with Mn causes manganese sulphide to form preferably provided Mn content is sufficiently high. The presence of oxygen reduces sulphur solubility in liquid steel, encouraging the formation of liquid oxygen-rich manganese sulphide in the molten metal. After solidification, large spherical oxy-sulphide inclusions form. Some of the tiny and relatively soft particles deform in the rolling direction but others keep their rounded shape. Size of the inclusions is related also to the solidification rate of the steel, being smaller the faster the cooling rate.
The plate is manufactured by a hot rolling operation, and therefore it presents a segregation region in the centre of the piece. This zone is characterised by a harder pearlite grain and acicular ferrite, but its extension is around 400μm. Segregation can be seen in Figure 97.
A comment should be done regarding the painted surface. The structure is painted to avoid corrosion. Figure 98 depicts a cross section of the surfaces where deformation of the grains is visible.
Similarly, the Stacker is made of low carbon steel, which microstructure is displayed in Figure 99. Cold forming operation was followed by a normalising, resulting in fine equiaxed grains of pearlite and ferrite. In some places close to the inner curve, there are linear features preserving the grain distribution characteristic from the forming process (see Figure 99b). EDS analyses showed good fitting with chemical spark data, except in the case of carbon, where the former overestimated its content.
It was observed a lower amount of MnS inclusions in this steel when comparing it with S355J2-N. This may be caused by either lower manganese content or lower sulphur content, as it can be seen in Table 10. Inclusions were also longer and finer as it is illustrated in Figure 100, though it still could be found some rounded inclusions. Figure 100 can be compared with Figure 96c, since they were both taken with magnification 100x.
In order to quantify the amount of each phase that is present in the both steels, micrographs were taken in different sections and many magnifications. Software for image analysis allowed calculating the relative proportions of coloured microstructures. Figure 101 a and b present an example of this method for each steel. Note that the micrographs were taken with the same magnification, highlighting the finer grain of the Stacker’s steel in Figure 101b. It is worthwhile mentioning that three colours were used to quantify two microconstituents. It was found that the software painted grey all grain boundaries and some ferrite spots that were darker than the rest. Then, calculations considered this fact, and ferrite relative amount was determined by white and grey coloured areas. Green painted regions correspond to pearlite.

To conclude with this section, a final comment in relation to microstructures presented in this section deserves to be done. Micrographies shown before correspond to pieces provided by the company. This means that they are the real microstructures of the component materials. However, in the case of S355J2+N, tests and all analysis and procedures carried out in the experimental part, were done on commercial steel under the same designation. Figure 102 depicts both microstructures for the same magnification and no remarkable disagreement can be detected. From the segregation point of view, purchased steel presented less localised segregation in the centreline, but it was rather distributed throughout the thickness.
6.4.2 Joint

Electrode AWS A5.18 E70C-6M/6C was used for the root bead, whereas AWS A5.20 E71T1-1M was employed in the filler beads (2\textsuperscript{nd} and 3\textsuperscript{rd}). These two electrodes developed similar microstructures that can be seen in Figure 103 a to d. However, differences in the amount of microconstituents could be detected and some of them are also pointed out in Figure 103. Since the aim of this work is not an exhaustive characterisation of the weld metal, only major microconstituents were distinguished. For a deeper analysis and quantification, document in reference [35] should be followed. Results are displayed in Table 15 together with observation in the HAZ. Probably, the presence of larger amounts of acicular ferrite in electrode AWS A5.18 E70C-6M/6C compared to its counterpart, give rise to its higher energy absorbed values (100J rather than 75J in the filler electrode at -20°C-see Annex C).

<table>
<thead>
<tr>
<th>Material</th>
<th>General microstructure</th>
</tr>
</thead>
<tbody>
<tr>
<td>AWS A5.18 E70C-6M/6C</td>
<td>WM Ferritic microstructure with grain boundary primary ferrite (PF(G)), acicular ferrite (AF) and non-aligned secondary ferrite (FS(NA)) as the major microconstituents.</td>
</tr>
<tr>
<td></td>
<td>HAZ Martensite formed in the 1\textsuperscript{st} bead HAZ is tempered by subsequent welds, leading to hardness around 300 HV\textsubscript{0.1}.</td>
</tr>
<tr>
<td>AWS A5.20 E71T1-1M</td>
<td>WM Ferritic microstructure with acicular ferrite (AF) and aligned secondary ferrite (FS(A)) as the major microconstituents.</td>
</tr>
<tr>
<td></td>
<td>HAZ Transition from the WM to BM consists in a grain growth region with hardness around 420 HV\textsubscript{0.1}, followed by a refined grain zone and a partially transformed zone that decrease this value steeply to that of the BM (see Figures 92 and 104). 2\textsuperscript{nd} pass HAZ present an average hardness close to 400 HV0.1 due to the untempered martensite content.</td>
</tr>
<tr>
<td>TIG bead</td>
<td>WM Mostly martensite with an average hardness of 370 HV\textsubscript{0.1}.</td>
</tr>
<tr>
<td></td>
<td>HAZ Transition from the WM to BM consists in a grain growth region with hardness around 400 HV\textsubscript{0.1}, followed by a refined grain zone and a partially transformed zone that decrease this value steeply to that of the BM (see Figure 91, 105 and 106). Tempering effect of TIG bead decrease hardness in 2\textsuperscript{nd} pass HAZ (ca. 300 HV\textsubscript{0.1}) slightly above the 1\textsuperscript{st} pass HAZ (ca. 280 HV\textsubscript{0.1}).</td>
</tr>
</tbody>
</table>
Fatigue of welded structures

Figure 103: Microstructure electrode AWS A5.18 E70C-6M/6C (a and c) and AWS A5.20 E71T1-1M (b and d).

Figure 104: 2nd pass HAZ, Sample 2.2 (AW), showing transition between BM and WM.
Microstructure generated by weld passes developed hard microconstituents like martensite in the HAZ. This martensite can eventually tempers if a bead is deposited after it, as is the case of the 1\textsuperscript{st} pass HAZ in all samples, or both the 1\textsuperscript{st} and 2\textsuperscript{nd} pass HAZ in the TIG dressed specimens. In the latter case, TIG bead reduced hardness of previous HAZ but resulted in hard martensite in its own HAZ and WM. Sometimes, it is recommended a second TIG run closer to the WM to reduce this hardness profile [103].

Transition from BM to WM through the fusion line (FL) showed columnar grains growing from austenite grains as can be noted in Figure 107 a and b. Grain boundary primary ferrite developed following this austenite grain boundaries and got into the WM.
In order to compare the weld profile of TIG improved samples with that of AW samples, a cross section of the latter is shown in Figure 108. As can be seen from it, a sharp transition from WM to BM is generated when no dressing is employed, leading to higher stress concentrators and hence, a lower fatigue resistance. Since toe angle is a very important aspect in fatigue of welded components, special treatment is given to this topic in the following section.

6.5 Toe angle measurements

First of all it is important to clarify that this procedure was carried out in all samples, but only results for the AW condition are presented in this first report. TIG dressed samples were characterised by a smooth transition from the WM to the BM and high angles were measured in most of them as the one illustrated in Figure 105. SP specimens are still being tested and toe angle analysis is left for future publications.

AW samples presented sharp toes with varying angles from 120° to 154°. Results in the case of Plate 2, excluding run outs of the fatigue tests, are listed in Table 16.
Table 16: Angle measurement results for cracked samples in the AW condition. Plate 2.

<table>
<thead>
<tr>
<th>Sample</th>
<th>Side</th>
<th>Average angle</th>
<th>Stand. Dev.</th>
<th>Stand. Dev. %</th>
<th>Max. Angle</th>
<th>Min. Angle</th>
<th>Crack</th>
</tr>
</thead>
<tbody>
<tr>
<td>2.2</td>
<td>A</td>
<td>139.79</td>
<td>6.49</td>
<td>4.64</td>
<td>149.65</td>
<td>131.2</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>140.83</td>
<td>4.81</td>
<td>3.41</td>
<td>150.75</td>
<td>136.28</td>
<td>Yes</td>
</tr>
<tr>
<td>2.4</td>
<td>A</td>
<td>141.75</td>
<td>3.1</td>
<td>2.18</td>
<td>145.16</td>
<td>137.08</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>133.97</td>
<td>4.65</td>
<td>3.47</td>
<td>141.36</td>
<td>118.8</td>
<td>Yes</td>
</tr>
<tr>
<td>2.5</td>
<td>A</td>
<td>139.06</td>
<td>5.72</td>
<td>4.11</td>
<td>149.5</td>
<td>131.53</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>132.41</td>
<td>5.81</td>
<td>4.38</td>
<td>144.8</td>
<td>126.59</td>
<td>Yes</td>
</tr>
<tr>
<td>2.6</td>
<td>A</td>
<td>141.8</td>
<td>2.27</td>
<td>1.6</td>
<td>146.07</td>
<td>139.25</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>133.98</td>
<td>5.13</td>
<td>3.82</td>
<td>143.09</td>
<td>126.51</td>
<td>Yes</td>
</tr>
<tr>
<td>2.7</td>
<td>A</td>
<td>139.21</td>
<td>6.7</td>
<td>4.8</td>
<td>154.09</td>
<td>132.91</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>139.39</td>
<td>5.97</td>
<td>4.28</td>
<td>147.08</td>
<td>131.41</td>
<td>Yes</td>
</tr>
<tr>
<td>2.8</td>
<td>A</td>
<td>136.38</td>
<td>4.95</td>
<td>3.63</td>
<td>141.67</td>
<td>123.3</td>
<td>Yes</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>139.33</td>
<td>3.67</td>
<td>2.63</td>
<td>143.49</td>
<td>134.03</td>
<td>No</td>
</tr>
<tr>
<td>2.9</td>
<td>A</td>
<td>136.86</td>
<td>7.38</td>
<td>5.39</td>
<td>151.3</td>
<td>128.11</td>
<td>No</td>
</tr>
<tr>
<td></td>
<td>B</td>
<td>135.57</td>
<td>7.16</td>
<td>5.28</td>
<td>141.63</td>
<td>122.23</td>
<td>Yes</td>
</tr>
</tbody>
</table>

Angles were measured in both sides of each sample. Cuts were made in position where it was observed a crack initiation point after fatigue tests. On the sides that did not nucleate cracks, cuts were made separated by 5mm, indistinctly of its position along the toe. Figure 109 illustrate an example of sample 2.5, indicating with red lines the sites chosen for angle determination.

![Sample 2.5, side B](image)

Figure 109: Weld toe, samples 2.5, side B and position considered for angle measurements.

Furthermore, results presented in Table 16 can be complemented with Figure 110, where toe angles are plotted in a bar chart, together with their standard deviation. It is worthwhile mentioning that samples 2.4, 2.5, 2.6 and 2.8, presented a marked difference between values corresponding to side A or B. The side showing the lowest average toe angle was the side that cracked during fatigue tests. In samples 2.2, 2.7 and 2.9 this contrast was not so evident and direct matching of cracked side with lowest average toe angle cannot be stated directly. For example, although the average toe angle of side A in samples 2.2 and 2.7 was slightly lower than in side B, fatigue cracks were found in the latter. However, note that side A presented larger standard deviation, in both cases.
For a better understanding of the role of the toe in fatigue behaviour of welded components, it is important to expound fatigue test results. Next section deals with most important aspects of this issue.

### 6.6 Fatigue tests

Fatigue test were carried out at SSAB, in Borlänge. Results so far are shown in Figure 111. Together with data for AW samples (blue dots), tendency line and its equation were drawn. This line indicates a 50% probability of survival considering design and welding procedure specification set for the experiments. Similarly, parallel lines were plotted 2 standard deviations to both sides, corresponding to 97.7% and 2.3% survival probability. In other words, using the lowest curve for designing will determine a failure probability of 2.3%. Furthermore, Figure 111 presents an estimation of the endurance limit, which was calculated as the average of the run-out values ($\Delta \sigma \approx 113 \text{ MPa}$). Again in this case, 97.7% and 2.3% survival lines lay two standard deviation to below and above, respectively.

It is important to mention the value of the exponent, in the equation for AW results. It is 0.326 which is really close to the value proposed in IIW recommendations [56, 87] of 1/3.

On the other hand, data obtained for TIG dressed samples does not seem to follow a line with slope 1/3, but it is rather close to 1/6, as can be better understood from its equation in Figure 111. To explain this mismatch, it is worthwhile analysing individual results in this case.

TIG dressed samples presented an improved toe, with a smooth transition from the BM to the WM. This modified profile decrease the risk of fatigue crack nucleation in this site, but exposes the weld root and any defect that may be present there. Additionally, if the improvement is sufficiently good, base metal can crack in “weak regions”. Both cases were observed in the results described herein. Due to the fact that samples were not mechanised into a bone-shaped final geometry, the stress was not only increased close to the toe because of the notch effect,
but also close to the clamps. Figure 112 displays pictures of samples 5.4 and 5.9 that cracked near the clamps.

To take advantage of all samples, tests were performed until the frequency of the resonance fatigue machine decreased 1Hz (this means that the crack has already nucleated and sample stiffness is becoming smaller). After that, they were stopped and clamped again closer to each other and covering a possible growing crack. This was done 1 or two times until a crack nucleates in the weld joint. If this was not the case, the sample was not considered. Validity of this test is only qualitative, and no predictions can be done from them. However three important aspects can be pointed out.

- First of all, the necessity of a smaller width in the joint region, i.e. a bone shaped sample. This was taken into account for later tests that are still ongoing.
Secondly, an evident improvement of the fatigue resistance was achieved by TIG dressing. Results carried out showed an improvement in fatigue life of 55% at 300 MPa, which is quite below the factor 3.4 claimed by IIW recommendations [87]. In contrast, a stress range 54% higher can be withstood by the weld joint at $1.10^6$, in accordance with guidance given in the same document. Nevertheless a trustful quantitative analysis has to be done when testing bone-shaped samples.

There was a sample that prematurely cracked in the toe at the first trial, and it is shown in Figure 113. This particular case may have been caused by an irregularity in the TIG bead that left a notch and failed to improve greatly fatigue performance. Additionally, this sample was taken close to a weld end, meaning that some distortion of weld bead is possible. Far from being a discouraging result, it highlights the importance of a high quality TIG procedure.

Figure 113: Sample 2.5. As-welded condition. 763500 cycles at 180 MPa.

Continuing with the latter issue, Figure 111 can be re-plotted to isolate TIG dressed results, and this is shown in Figure 114 a. A red arrow points out the particular low life of sample 5.1, showed in Figure 113. If this value is disregarded and a new tendency line is drawn, the new set of tests follow a slope closer to 1/5. IIW recommendations [87] assume a FAT 160 for the parent material that may vary with the ultimate tensile strength and has a slope of 1/5. This curve is shown for comparison in Figure 114 b. Having this in mind, note that results are likely to be measuring fatigue strength of the base metal. Nonetheless, as was said earlier, these values should not be used for design and calculations until they are checked or compared with new results.
A similar analysis can be performed for AW samples by contrasting these results with design curves. As was exposed, in this case data is more solid and consistent with literature. Figure 115 illustrate AW data and fatigue design curves or classes, according IIW recommendations [87] and BS 7608 [64]. For the design proposed, the former recommends a FAT 80 for design with 97.7 % survival probability (green line), which is in coincidence with a BS 7608 class E. The British standard proposes, however, a much lower value (class F to G) for closer designs, shown in Table 17. Results presented in the present report can adjust to a fatigue class of D that is coincident with FAT 90, with a 2.3 failure probability lowest black line in Figure 115. Since a FAT 90 characterise the joint proposed, IIW states that a maximum improvement to FAT 100 can be claimed. This fact has to be corroborated by future data, but preliminary results gave promising fatigue improvements. As a final remark about design curves, it must be pointed out that the use of FAT 80 (or Class F2, according BS 7608) in design of transverse stiffeners, under the procedure proposed in the WPS implies a conservative estimation of life cycles.
**Fatigue of welded structures**

Table 17: BS 7608 fatigue classes for details close to the one chosen for fatigue tests. Adapted from [64]

<table>
<thead>
<tr>
<th>Product form</th>
<th>Location of potential crack initiation</th>
<th>Dimensional requirements</th>
<th>Manufacturing requirements</th>
<th>Design stress area</th>
<th>Class</th>
<th>Sketch</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rolled steel structural plates, sections, tubes, and built-up members</td>
<td>Weld toe or end of a short attachment (Sr direction)</td>
<td>Weld length (parallel to Sr) ( l \leq 150\text{mm} )</td>
<td></td>
<td></td>
<td>F</td>
<td><img src="image" alt="Sketch F" /></td>
</tr>
<tr>
<td></td>
<td>At weld toe or end of a long, narrow attachment</td>
<td>( l \leq 150\text{mm} ) ( w \leq 50\text{mm} ) Weld not within 10mm of member edge.</td>
<td></td>
<td>Minimum transverse cross section of member at location of potential crack initiation</td>
<td>F2</td>
<td><img src="image" alt="Sketch F2" /></td>
</tr>
<tr>
<td></td>
<td>At weld toe or end of any attachment close to edge of stressed member</td>
<td>Weld within 10 mm of member edge</td>
<td>Grind out undercut</td>
<td></td>
<td>G</td>
<td><img src="image" alt="Sketch G" /></td>
</tr>
</tbody>
</table>

Having described Wöhler curves for obtained results, fracture surfaces deserve a special analysis. A common observation in all samples that cracked in the toe was the development of multiple crack initiation sites at the surface. These tiny growing cracks combine afterwards to form a single fracture front or in some cases two fronts. Examples are presented in Figures 116 a and b. Figure 117 a also exhibit a fracture surface where two main cracks developed and is complemented by magnification of nucleation sites in Figure 117 b. Likewise, Figure 118 a and b illustrate SEM images showing initiation points.

If figures 116 and 117 are examined carefully, it can be recognised that samples where more than one main crack developed, presented a sharp step close to the surface. This is in correspondence with the fact that in early stages of propagation, the two shallow elliptical cracks coalesced in different fracture planes. When the width of the cracks is enough to cover the width of the sample, initial mismatching of fracture planes will generate a step in between.
Figure 116: Microcracks coalesce into a single crack (a), or form more than one fracture front (b). Sample 2.6 and 2.5, side B.
Both Figure 116 and 117a reveal also a faceted fracture surface close to the edge of the sample. This is consistent with the fact that cracks nucleate at toes with high angles, which may not be aligned. Cracks grow until it is more favourable to coalesce with another crack from a shifted plane, than continue its path along initial direction. The result then, is a stepped surface close to the edge but less severe than the in the case of macrocracks. Cracks that did not fulfil to combine may arrest or keep on growing at a lower rate, since a bigger crack formed. Evidence
of this is presented in Figure 119, where a secondary crack was found to form closer to the weld toe than the main crack. Moreover, it was observed in other sections of the same sample, that the crack progressed through the WM until it reached the HAZ and latter the BM. This observation also supports the misalignment of crack nucleation spots, and it is presented in Figure 120.

An important consequence of previous analysis is that cracks are supposed to nucleate at sharp notches close to the toe, and then, follow the path transversally to maximum principal tensile stress. By examining toe surface before and after fatigue test in Figure 121, together with toe angle values, it can be noticed that low toe angles are related to a narrower bead such as points 1 and 7, whereas high angles and therefore lower stress concentrators are linked to wider sections of the weld bead (see points 4, 5, 6 and 9).

Combining results in Figure 121 with micrograph in Figure 120, it can be assumed that external toe points did not nucleate fatigue cracks, at least in this case. These findings disagree with results from [121], where it was detected that crack initiation sites occurred only at external toe points. However, in this document a constant controlled waviness was introduced by the welding process. In the present case, the existence of one or more remarkable crest or external points is rather random and varies from sample to sample. Then, stress raisers at sharp toe angles (internal toe points) may still be governing not only nucleation at the toe, but also later coalescence and crack propagation.

Misalignment of crack nucleation sites may explain the higher service lives of manual welds than automatic ones [1]. The latter present less wavy beads and therefore, cracks can come together easily.

![Figure 119: Main crack (a) and secondary crack (b). Sample 2.7, Side B.](image-url)
A final observation must be pointed out before moving on to repair of damaged structure. Fracture surfaces and transverse cross section of cracked samples showed different behaviours during crack propagation. To explain this, Figure 122 displays a SEM image of a fracture surface superposed to a micrograph. Crack path has been divided into four main zones with characteristic features.
As can be seen in Figure 122, Zone 1 corresponds to the WM with average hardness around 300 HV$_{0.1}$ in the 2$^{nd}$ pass, and therefore a smooth fracture surface. At this region, close to the weld toe, stress concentration effect may have a strong influence. Zone 2 is related to the coarse grain HAZ, and due to its high heterogeneity in grain size and microconstituents it develops an overall rough aspect. However, a better insight of the surface as shown in Figure 123, revealed the presence of feather patterns that can possibly be related to fractured martensite grains. Moving on to Zone 3, the fracture surface turns smoother again and is in accordance with more homogeneous refined grain HAZ. Finally, partially transformed zone and BM develops a rough fracture surface.

Different features can be explained by the hardness of the main microstructure in each zone. Harder structures generate smoother surfaces. In the case of Zone 2, perhaps heterogeneity led to the different microscopic aspect observed. Results presented in previous sections showed that BM was the softest of all, and this was reflected in striation detected at high magnifications. Figure 124 illustrate some examples. On the other hand, striations were not found in the other zones.
It is important to note that during early stages, the crack did not propagate transversally to the remote stress, and followed rather inclined paths. Once in the BM, the crack redirected its course perpendicular to the applied stress.

To conclude with this section, it is worthwhile mentioning that behaviour described in later paragraphs implies an influence of microstructure in fatigue crack growth. Although it can be found in literature that this influence is small in second stage [1, 2, 6], features described here can be delved in future researches to obtain better explanations of a possible relationship.
Chapter 7: Discussion

7.1 Base material

Base materials of both structures (Stacker and Tippficka) were characterised by a ferrite-pearlite microstructure. Steel S355J2+N presented higher carbon content than the Stacker’s steel, in correspondence with lower ferrite content. However, its grain size was bigger resulting in a slightly lower hardness.

Although no information was obtained from Stacker’s manufacturer, a first approximation can be done concerning its mechanical behaviour. The impact resistance of the stacker material is probably good because of the fine grained structure. Particularly important is in the Stacker armour the ductile to brittle transition, due to the fact that the component is located in open air and in a region of Sweden that registers minimum temperatures in winter as low as -40°C. Based on its chemical composition, which is quite similar to that of a normal structural steel, it can be assumed that the energy absorbed after impact is around 27-30 J at -20°C. Additionally, it is a well-known fact that arc welding processes generate hard microconstituents in the HAZ with a consequent decrease of impact resistance. As no information about the WPS was acquired it cannot be stated the impact requirement of the joint. Furthermore, fluctuating loads can be present in the structure due to the cyclic nature of its task.

On the other hand, material specifications were gathered in the case of the Tippficka. As was said earlier, the steel corresponds to a S355J2 in normalised state. WPS specifies a three run fillet weld with differentiated electrode for the root. This is done to obtain higher toughness and solidness in that region that can be highly diluted and is more susceptible to cracking. The Tippficka is not subjected to extreme low temperatures, but it withstands severe vibrations. Loose nuts and progressive cracks monitored during visits to the company, are evidences of fatigue phenomena. Some of them were unsuccessfully repaired, and cracked following a particular path, initiated at a weld toe. All these motivated the study of these components and their materials, with the aim to describe the problem and give a reasonable explanation. Fatigue tests are the main objectives of the present work.

7.2 Welded joint

Welds were carried out following the WPS, but lack of fusion was evident in the root when examining the samples. This kind of defect showed to be the weak point when weld toe was improved by TIG dressing. Therefore, special care has to be taken when welding or repairing the components. Heat input has to be kept close to the maximum stated in the WPS, to decrease the risk of lack of fusion. However, excessive heat input can generate rather convex beads with steeper angles at the toe. AW samples did not appear to be influenced by lack of fusion.

Microstructure of WM was mainly acicular ferrite with a hardness varying from bead to bead due to the tempering effect of subsequent passes. Likewise, HAZ presented a maximum of hardness in the coarse grain HAZ of the 2nd pass, which is reduced when moving on to the HAZ of the 1st pass that is tempered. TIG extra bead tempers both the WM and the HAZ of the 2nd pass, decreasing peak hardness values. In this case, the maximum hardness was found in the...
TIG WM and its HAZ, with values up to 420. If static loads are kept at a relatively low value, and weld is properly protected from environment to avoid interaction with hydrogen, oxygen or other aggressive gasses, high hardness values should not be an inconvenience. If it is engineers consider this to be risky, a second TIG run can be performed closer to the weld metal to temper previous one. Also, preheat can be applied before running the TIG bead. On the other hand, shot peening increased the hardness of base metal close to the surface by 8%. Improvement is better the higher the strength of the metal, and this was consistent with 20% enhancement of the hardness at the region in the weld metal close to the surface. Finally, it must be clarified that only TIG dressing technique effectively increased the toe angle, leading to smoother transition between BM and WM.

7.3 Fatigue testing results

Fatigue tests were performed in laboratory air, under normal conditions of pressure and temperature. Stress ratio was set at R=0, and the frequency varied from 110 to 115 Hz. Results were in the range expected from literature, confirming the validity of high frequency testing for measuring fatigue life of welded components. It was clearly found that AW samples presented a shorter life than TD samples. However, intrinsic resistance of the former, following the WPS provided by the manufacturer was higher than many of the fatigue design classes proposed in standards, documents and codes, for this particular geometry. Values of FAT 90 (or BS 7608 Class D) can be claimed with a 97.7% survival probability. Moreover, scatter in results was not remarkable and experimental slope achieved was only 2.3% lower than the theoretical value that can be found in literature. These correspondences of results with theoretical values allow the use of a FAT 90 in design, provided that WPS is followed correctly. As was mentioned in previous chapter, TD samples showed an improvement in fatigue strength, but it could not be trustfully quantified. The reason for that is that the proposed geometry of the specimens did not have a narrower gauge length than the region of the grips. Hence, a better design was carried out in another set of samples by mechanizing a bone shaped specimen. In this case, it a higher stress will be assured in the joint and not in the clamping spots. Results of this new trial will be reported in following publications, together with shot peened samples fatigue lives. Focusing the attention on AW specimens, it is of primary importance to relate results with cracks in the structure. Most data presented in literature is based on constant amplitude loading, and the same was followed in the present work. However, loads in real components are far from being constant and they rather fluctuate in an irregular manner, determining spectrum load profiles. Moreover, stresses can vary not only in magnitude, but also in period from cycle to cycle. It is not the purpose here to describe thoroughly spectrum fatigue analysis; although a brief explanation is made. The interested reader is forward to some thoroughgoing documents and books. Generally, this analysis starts if possible with strain-gauge measurements on a similar structure, considering a representative period of time and working conditions. Sometimes, however, this is not a viable option and the designer must reckon the load spectrum with a high safety factor.
Fatigue of welded structures

that, a proper cycle counting method should be applied, preferably “rainflow” or “reservoir” counting. Having determined the spectrum, service life under mixed loading conditions must be estimated by cumulative damage theory, such as Palmgren-Miner’s Rule, which relates the life under variable loading conditions to the normal constant amplitude Wöhler curves [122]. If a register of load spectrum can be made in the Tippficka, it is recommended to apply this methodology and make use of experimental fatigue data obtained in the present work to predict fatigue lives.

Variable amplitude loading has another important effect on fatigue, namely residual stress relaxation. The effect is more evident at the low stress range and sometimes a transition region from a state influenced by residual stresses to another where they are relieved can be observed [46]. Berge and co-workers [125] analysed the effect of short-range residual stresses on longitudinal stiffeners and they found that there were no change in fatigue capacity for AW specimens at R=0 and R=-1 and stress-relieved specimens at R=0, under constant amplitude loading. However, the latter exhibited an enhanced strength of 20-30 %. Additionally, they encounter that variable amplitude loading relaxed short-range residual stresses due to local yielding at peak loads. Besides, interaction effect (retardation) showed to be higher at R=0 than R=-1. Likewise, Round robins carried out in the 90s [126, 127] showed more than 50 % stress relaxation at early stages in the fatigue life (8% of the total specimen life) in the low stress range. Until now, further evidences can be found of relaxation and interaction effects. Residual stresses generated by arc welding processes depend on the preceding load cycles, which determine a strong load sequence effect on fatigue strength [89, 128].

It is worthwhile pointing out that real structure may develop higher residual stresses than small-scale specimens used for designing S-N curves [40]. However, complete stress relief can rarely be achieved in real structures and long-range residual stresses are unlikely to be relieved. Thus, most Code writers have ignored the potential benefit of stress relief [65]. In cases where there is clear evidence of stress relaxation, IIW [56] recommends to reduce the fatigue strength at 2 million cycles (FAT) by 20% to make the S-N curve steeper.

Having described possible effects of spectrum loading and residual stresses in real structures, it is time to study the effect of a single pass instead of three, in the fatigue strength. Recalling Figure 63b, where the cracked component was shown, it can be seen that the fillet presented a sharp toe angle (low angle) and visible undercuts. This is shown again in Figure 125. Without any doubts a great difference is put in evidence when comparing it with welds performed in this work. Although it can be argued that welding conditions will not be the same in the real structure than in the workshop, workmanship can surely overcome the vertical position to obtain enhanced quality joints. It can also be noted in Figure 125 excessive convexity, which can be probably caused by an excess of heat input. This is consistent with the fact that less than 3 beads where done: in order to have a good fillet dimension with less passes, heat input should be increased. This was apparently combined with an oscillating torch resulting in a wavy toe. It was mentioned in previous chapters that oscillation can increase the fatigue resistance because of the mismatch of fatigue nucleation points. However, benefits can be overshadowed by extra material added that create sharp toes. If it is assumed that the fillet side is the same in both cases, the low toe angle may have generated high stress raisers that strongly influenced the fatigue effect.
Ceferino Steimbreger

To conclude with, it is important to check the correct quality of the weld. It must be remembered that drawing shown in Figure 72 and 73, claimed a C quality for the joint according SS-EN ISO 5817. Table 18 illustrates the imperfections that might not be under the limits demanded by the standard.

Table 18: SS-EN ISO 5817 limits for imperfection in quality C fillet welds [68].

<table>
<thead>
<tr>
<th>Imperfection designation</th>
<th>Remarks</th>
<th>( t ) [mm]</th>
<th>Limit for imperfections for quality “C”</th>
</tr>
</thead>
<tbody>
<tr>
<td>Continuous undercut</td>
<td>Smooth transition is required. This is not regarded as a systematic imperfection.</td>
<td>0.5 to 3</td>
<td>Short imperfections: ( h \leq 0.1t )</td>
</tr>
<tr>
<td>Intermittent undercut</td>
<td>( h \geq 3 )</td>
<td>( h \leq 0.1t ), but max. 0.5mm.</td>
<td></td>
</tr>
<tr>
<td>Excessive convexity</td>
<td>( \geq 0.5 )</td>
<td>( h \leq 1mm + 0.15b ), but max. 4mm.</td>
<td></td>
</tr>
<tr>
<td>Incorrect weld toe</td>
<td>( \geq 0.5 )</td>
<td>( \alpha \geq 100^\circ )</td>
<td></td>
</tr>
<tr>
<td>Excessive throat thickness</td>
<td>The actual throat thickness of the fillet weld is too large.</td>
<td>( \geq 0.5 )</td>
<td>( h \leq 1mm + 0.2a ), but max. 3mm.</td>
</tr>
</tbody>
</table>
Figure 125: Toe crack at the Tippicka.
Chapter 8: Conclusions

Throughout this project it was exposed the yet unclear nature of fatigue, which is even more uncertain when it comes to fatigue on welded structures. Welded components may have complex state of stresses, complicated geometries and a great variety of defects introduced during arc welding processes. Residual stresses and spectrum loading add another difficulty to the problem. Many theories have been developed along the years to account different factors and their influence in fatigue behavior. The rate of progress has been so rapid that even what people learn at university is always a bit out of date. Due to this, a literature review was performed at the beginning to introduce most important concepts of fatigue in welded structures, including not only famous authors working in the field, like Maddox and Gurney [1, 40], but also updated references. Besides, comments and comparisons between codes, standards and other technical documents have been performed with the aim to provide the reader a better understanding of regulation from the industry point of view. In this sense, most important findings were recently published IIW recommendations [87] and an updated version of ISO 5817 [68].

Keeping theoretical aspects in mind, description of the problem that motivated this study was presented, focusing the attention particularly in the Tippficka. This structure presented fatigue cracks in welded unions and unsuccessful repairs that failed apparently in the same regions as the previous crack. Nevertheless, Stacker’s material was likewise analysed from the microstructure and hardness point of view.

In order to characterize the weld in the Tippficka, field work had to be done. Information was obtained from the company and manufacturers. Drawings and materials specifications were discussed to find out the best way to design a fatigue test that describes the components, but keep the methodology and sample preparation as simple as possible. Decision was made with bias toward non-load carrying T joints. This kind of joint showed good behavior in the case of AW samples, but presented some inconveniences in improved joints. Particularly, cracks developed in the BM close to the clamps. Therefore, a modified design was proposed for future testing: bone shaped non-load carrying T joints. This geometry has the advantage that for an applied load, the stress will be higher in the gauge length than in the grip region, decreasing hence the risk of cracking far from the joint.

Fatigue test were carried out in a resonance fatigue machine that reached frequencies as high as 115Hz. It can be queried the applicability of these high frequencies to structures that withstand much lower frequencies, but in the case of vibrations, frequencies can reach this value. Moreover, it was found that over the range 3-114 Hz, there is no frequency effect, provided that there is no overheating in the specimen and no direct action of the environment [1, 40]. In fact, data obtained from tests demonstrated to be in good agreement with literature. Most important findings in relation to the fatigue tests can be summarized as follows:

- Microcracks nucleated preferably at internal toe points where toe angles were lower, i.e. the stress concentration effect was higher.
- Multiple fatigue cracks nucleate at the surface and then combine to form a single or double fracture front.
Microcracks that grew in different fracture planes due to internal toe points mismatching, coalesce with each other forming microscopic steps. The same was observed when more than one macrocrack grew from the surface, but this time in a larger scale.

Striations were more evident in the BM (Zone 4), due to its lower hardness.

AW samples showed good agreement with literature, and presented a slope of 0.326, which is only 2.3% lower than theoretical value (1/3).

FAT 90 was achieved for the AW condition for a failure probability of 2.3%. This result implies that current standards and documents dealing with the topic, underestimate the fatigue life of non-load carrying T joints, at least for the design proposed in the present study.

TIG Dressing effectively increase the cycles to failure in the proposed design. Failure was mainly at the root due to lack of penetration, or close to the clamps.

Data for TD results appear to follow a FAT 160 line with slope 1/5, which is the proposed line for parent metal, according IIW recommendations [87].

TIG dressing can be applied to welded joints according the WPS, provided that the document was strictly followed. Special care has to be taken in relation to the heat input in order to avoid incomplete penetration at the root.

Results discussed herein can be applied when repairing cracked structure, provided that there is complete penetration and the weld and the welding conditions keep close to those specified in the WPS.

Finally, overall conclusions are presented:

- Stacker steel present a smaller grain size, lower C and Mn wt% (then lower CE), and higher hardness. It can be a high strength structural steel, hardened by grain refinement. Low temperatures could have embrittled the structure and cause cracking, aided by vibration and environment.
- Single bead welds in the Tippficka may have resulted in lower fatigue strength due to sharp toes and undercuts. Heat input must be kept to the range specified in the WPS.
- Repair procedure was not satisfactory, and a better procedure should be developed. Proposals are given in Annex A.
- Updated version of SS-EN ISO 5817 should be applied from now on, when inspecting welded components. It includes additional guidance on fatigue loads in its Annexes, for each quality level.
- IIW recommendations [87] should be followed to assess fatigue of welded components and also to repair or upgrade existing structures.
- Further investigations ought to be performed in the structures, such as vibration and stress measurements.
Future works

Future works can be divided into two categories: those tasks related to the Tippficka and those corresponding to the Stacker. In the former case, the list below is proposed:

- Analyse shot peened sample results.
- Measure vibration in the structures, and determine stresses.
- Cumulative damage analysis of stress profiles and comparison with results obtained in the present thesis.
- Express data of toe angles in terms of manageable parameters for their analysis with fracture mechanics, such as toe radius.
- Apply fracture mechanics to data obtained throughout this study.
- Study the effect of microstructure on crack propagation across HAZ.

From the Stacker point of view, the primary task is to obtain information about the welding procedure employed for its weldments. Once this knowledge is available, next step is to perform representative welded samples in order to measure joint characteristics in a similar way as presented here for the Tippficka. However, in the case of the Stacker, results should be focused on ductile to brittle transition curves. To achieve this, Charpy impact tests should be carried out according codes, standards or another valid document. Location of the notch should be varied throughout the different zones of the HAZ, and besides, different temperatures must be tested. A temperature range from -50°C to 20°C must be covered in order to obtain results that reproduce working conditions.
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[72] ASME, Boiler and pressure vessel code, Section III, Rule for construction of nuclear power plant components, Division 1, Subsection NB, Class 1 components, New York, American Society of Mechanical Engineers, 2013.
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[109] [http://www.weatheronline.co.uk/weather/maps/city](http://www.weatheronline.co.uk/weather/maps/city). Last access in April 29th, 10.03.


Fatigue of welded structures


[117] ASTM E407 - 07e1, Standard Practice for Microetching Metals and Alloys, developed by Subcommittee E04.01, ASTM international, 2007


Annex A: Repair recommendations

Having analysed the cracks in the damaged components, some recommendations can be done to repair the welds, on the basis of results obtained. Items listed below are only brief indications that can be followed. The interested reader is forward to particular references in the topic, if a deeper understanding is desired [A1-A6].

It is important to highlight that a bad repair can be ineffective, and in a worst but still possible case, detrimental. The latter can be the result of incomplete removal of defects, introduction of new flaws, degradation of material properties and microstructures, increase of positive residual stresses and distortion, or simply inadequate repair and inspection procedure [A1].

Data and information of the original welded component (WPS) is important for a successful repair [A6]. In the present case, only information about the Tipficka was obtained. Therefore, this procedure is mainly focused on this structure. Since there is no access to data for the Stacker, microstructure analyses carried out in Chapter 6 can be considered for future repairs. However, it is worthwhile mentioning that no investigation of the weld deposit was considered here.

Generally, the repair welding procedure has to be carried out under the code construction specifications. Then, particular code employed in the Tipficka should be checked. Examples of these codes are American Society of Mechanical Engineers (ASME), American Welding Society (AWS), American Petroleum Institute (API), among others.

In relation to cracks that developed from or along weld toes, the following items must be considered.

1. Analyse the possibility of changing loads to more tolerable ones. Generally this is not a feasible step, but if information is available, it should be considered.
2. Whenever it is possible, complete removal of cracks by grinding must be carried out. This step is mandatory if a good quality of the repaired component is needed. NDT are useful in this sense, to check if grinding was done successfully. Incomplete grinding can led to stress raisers from the tip of the un-removed crack. Figure 1-A shows an example of a toe that was ground.

![Figure 1-A: Example of a ground toe prepared for latter repairing [A7].](image)

3. If possible, modification of detail designs by stiffeners.
4. Complete cleaning of the surface, by removing paint, rust, mill scale, grease, oil and any contaminant that may be present. Rest these undesired dirtiness can introduce hydrogen to the molten metal pool, with adversely effects on toughness.

5. Selection of material and repair welding procedure.

Repair welding procedure must consider, apart from the original WPS, new joint design and thicknesses involved, welding position, access to the equipment, availability of materials in the location where the structure is settled, etc.

Repair procedures must be approved by the engineer and the welding procedures ought to have the corresponding WPS, also validated. The use of preheat prior to welding has to be checked properly. This can be examined in Annex B. If Charpy V-notch impact test are required, they should be performed following updated standards.

Selection of electrodes can be done following specifications presented in AWS A5.1 [A8], AWS A5.18 [A9] or AWS A5.20 [A10], depending whether the process is SMAW, GMAW or FCAW.

6. Weld toe improvement techniques.

Particularly recommended in the joint investigated is TIG dressing, provided that lack of penetration was not present in the main weld. Procedure ought to be done in accordance with IIW document [A4].

7. Quality control of the repair weld.

This step is extremely important, since new imperfections introduced by the repair process are potential sites for crack nucleation.

8. Paint.

Painting the structure after completing the repair is important because it not only protects the joint form environmental conditions, but also enhances the quality of the surface.

On the other hand, repairing of the cracked column attached to the Tippficka deserves a special comment. Since these kinds of welds are difficult to repair [A11], it is preferable to use open holes at both ends of the crack. This alternative has the advantage of being costless.

References


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Annex B: Preheat temperature calculations

In this Annex, only a fast calculation of the preheat temperature is carried out for materials involved in the Tippficka welded union. Results should be corroborated with other methods available.

The preheat temperature for the FCAW welding process with the electrode Elgacore DWA 50 can be done according DIN EN 1011-2:2001-05 [B1].

- Carbon equivalent is calculated according the following formula:

\[ CE_{IIW} = C + \frac{Mn}{6} + \frac{Cu + Ni}{15} + \frac{Cr + Mo + V}{5} \]

Then for the base metal (S355J2-N) its value is 0.567 (see Table 1-B). Similarly, for the weld metal (Table 2-B), CE value is 0.41. All results were calculated for maximum values in order to be conservative.

Table 1-B: Chemical composition of the base metal.

| Chemical composition % of grade S355J2+N (1.0570 (dubl)) |
|-------------|-----|-----|-----|-----|-----|-----|-----|-----|
| C           | Si  | Mn  | Ni  | P   | S   | Cr  | Mo  | Al  |
| max 0.22    | max 0.55 | max 1.6 | max 0.3 | max 0.035 | max 0.035 | max 0.3 | max 0.08 | max 0.02 |
| Cr+Mo+Ni    | < 0.045 |

Table 2-B: Chemical composition of the weld metal according the manufacturer sheet.

<table>
<thead>
<tr>
<th>Chemical composition, wt.%</th>
</tr>
</thead>
<tbody>
<tr>
<td>Min</td>
</tr>
<tr>
<td>Typical</td>
</tr>
<tr>
<td>Max</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th></th>
<th>Mo</th>
<th>Cu</th>
<th>V</th>
<th>Nb</th>
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<tbody>
<tr>
<td>Min</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Typical</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Max</td>
<td>0.20</td>
<td>0.30</td>
<td>0.08</td>
<td>0.05</td>
</tr>
</tbody>
</table>

- Electrode selected has a maximum hydrogen content of 5ml/100g (see Annex C) weld metal. Then, according Table 3-B, its classification is a “D”.

Table 3-B: Hydrogen value according DIN EN 1011-2:2001-05.

<table>
<thead>
<tr>
<th>Cantidad de hidrógeno difusible ml/100 g de metal depositado</th>
<th>Valor de hidrógeno</th>
</tr>
</thead>
<tbody>
<tr>
<td>&gt; 15</td>
<td>A</td>
</tr>
<tr>
<td>10 ≤ 15</td>
<td>B</td>
</tr>
<tr>
<td>5 ≤ 10</td>
<td>C</td>
</tr>
<tr>
<td>3 ≤ 5</td>
<td>D</td>
</tr>
<tr>
<td>≤ 3</td>
<td>E</td>
</tr>
</tbody>
</table>
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- Figure 1-B states that for a double fillet T joint, the equivalent thickness is $\frac{1}{2}(d_1+d_2+d_3)$. This results in a value of 30mm.

![Figure 1-B: Equivalent thickness for different geometries.](image)

- Heat input is determined directly from the welding parameters. Their values are displayed in Table 4-B, extracted from the WPS. Since three different welding conditions were used for each bead, a minimum heat input of 1kJ/mm is taken into account for calculations.

<table>
<thead>
<tr>
<th>Strång</th>
<th>Strömpol</th>
<th>Ampere</th>
<th>Trådsmatning m / min</th>
<th>Spännning Volt</th>
<th>Sträcklängd mm / min</th>
<th>Energi KJ / mm</th>
<th>Verkngrad</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>DC+</td>
<td>115 - 130</td>
<td>2.4 - 3.2</td>
<td>15 - 18</td>
<td>100 - 125</td>
<td>0.9 - 1.4</td>
<td>1.0</td>
</tr>
<tr>
<td>2-N alt.1</td>
<td>DC+</td>
<td>165 - 195</td>
<td>5.4 - 6.5</td>
<td>22 - 25</td>
<td>130 - 170</td>
<td>1.4 - 2.0</td>
<td>1.0</td>
</tr>
<tr>
<td>2-N alt.2</td>
<td>DC+</td>
<td>165 - 195</td>
<td>5.4 - 6.5</td>
<td>22 - 25</td>
<td>175 - 300</td>
<td>0.8 - 1.4</td>
<td>1.0</td>
</tr>
</tbody>
</table>

- Finally, from charts in Figure 2-B and 3-B, preheat temperature can be determined. Note that only base metal demands a preheat temperature. However, it is around 20°C even in the case presented where conservative calculations were made. Then, it can be assumed, that according this procedure, no preheat temperature is needed.
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Figure 2-B: Charts to determine the preheat temperature with the heat input, hydrogen content and the equivalent thickness in the BM.

Figure 3-B: Charts to determine the preheat temperature with the heat input, hydrogen content and the equivalent thickness in the WM
References

## Annex C: WPS and electrodes data sheets

### SVETSDBABLAD - WPS

**Datum:** 2004-09-15  
**Utgiven av:** Folke Berglund

**WPS nr:** 1.1 T 136 269 d  
**Baserad på WPAR nr:** 2.1 T 136 32  
**Tillverkare:** IVAB

<table>
<thead>
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<th>Läge 2</th>
</tr>
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<td>ISO 5817 A:</td>
<td>1.1 - 1.2, S235JR - P355GH (se ritning)</td>
<td>1.1 - 1.2, S235JR - P355GH (se ritning)</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Hantering av tillsatsmaterial</th>
<th>Skärning/kapning - slipning</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rotstöt:</td>
<td></td>
</tr>
<tr>
<td>Fixtur:</td>
<td></td>
</tr>
<tr>
<td>Häftning:</td>
<td></td>
</tr>
<tr>
<td>Fixering:</td>
<td></td>
</tr>
<tr>
<td>Min temp:</td>
<td></td>
</tr>
<tr>
<td>Häftlängd:</td>
<td>5 - 15 mm</td>
</tr>
<tr>
<td>Antal strängar:</td>
<td>1</td>
</tr>
<tr>
<td>Avstånd mellan häftor:</td>
<td>&lt; 200 mm</td>
</tr>
</tbody>
</table>

### Svetsläge

<table>
<thead>
<tr>
<th>Svetsläge</th>
<th>Alla för svetsaren godkända, dock ej svetsning nedåt.</th>
</tr>
</thead>
<tbody>
<tr>
<td>svetsning på en eller två sidor:</td>
<td>se ritning</td>
</tr>
</tbody>
</table>

### Sträng | Svetsmетод | Tillsatsmaterial | dim mm | Beteckning | Gas / pulver | Gasflöde liter / minut |
<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>136</td>
<td>Elgacore MX 100T</td>
<td>1,2</td>
<td>EN 758 T 42 2 M M 1 H5</td>
<td>M21 (MISON 18)</td>
<td>20 - 24</td>
</tr>
<tr>
<td>2-N alt.1</td>
<td>136</td>
<td>Elgacore DWA 50</td>
<td>1,2</td>
<td>EN 758 T 42 2 P M 1 H5</td>
<td></td>
<td></td>
</tr>
<tr>
<td>2-N alt.2</td>
<td>136</td>
<td>Elgacore DWA 50</td>
<td>1,2</td>
<td>EN 758 T 42 2 P M 1 H5</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

### Sträng | Ström pol | Ampere | Trädfasthet m / min | Spänning | Sträcklängd mm / min | Energi KJ / mm | Verkn grad | Hot-start | Båg-längd | Anm. pulser |
<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>DC+</td>
<td>115 - 130</td>
<td>2.4 - 3.2</td>
<td>15 - 18</td>
<td>100 - 125</td>
<td>0.9 - 1.4</td>
<td>1.0</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>2-N alt.1</td>
<td>DC+</td>
<td>165 - 195</td>
<td>5.4 - 6.5</td>
<td>22 - 25</td>
<td>170 - 170</td>
<td>1.4 - 2.0</td>
<td>1.0</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>2-N alt.2</td>
<td>DC+</td>
<td>165 - 195</td>
<td>5.4 - 6.5</td>
<td>22 - 25</td>
<td>170 - 170</td>
<td>0.8 - 1.4</td>
<td>1.0</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

### Värmebehandling:

<table>
<thead>
<tr>
<th>Värmepåverkad zon:</th>
</tr>
</thead>
</table>

### Anmärkningar:

**Kvalificerad enligt:**  
**Norm:** SS-EN-ISO- 15614-1: 2004  
**Kundspec:**

**Datum:**  
**Stämpel:**

**Signatur:**
Elgacore MX 100T
FCAW - Flux cored arc welding
Un-alloyed

Description:
Elgacore MX 100T is a metal cored wire for use with a CO2 or Ar/CO2 gas shield, specially designed for single-sided welding of thinner section material. The wire is all-positional and runs with a very stable, spatter-free arc even under clip transfer conditions at welding currents as low as 50 A. Root passes normally made with the TIG or MMA process can be carried out with Elgacore MX 100T to give significantly increased productivity, making the wire particularly suitable for pipe welding. Elgacore MX 100T has good notch toughness properties down to ~30°C and is recommended for general fabrication and structural steel work.

Welding positions:
- DC +
- DC -
Deposition efficiency:
96%

Shielding gas:
M21, 80% Ar + 20% CO2, 22-25 l/min
C1, CO2 22-35 l/min

Stick-out:
15-25 mm

Hydrogen content / 100 g weld metal
≤ 5 ml

Chemical composition, wt.%

<table>
<thead>
<tr>
<th></th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Ni</th>
</tr>
</thead>
<tbody>
<tr>
<td>Min</td>
<td>0.07</td>
<td>0.5</td>
<td>1.5</td>
<td>0.05</td>
<td>0.014</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Max</td>
<td>0.12</td>
<td>0.90</td>
<td>1.75</td>
<td>0.03</td>
<td>0.03</td>
<td>0.20</td>
<td>0.50</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th></th>
<th>Mo</th>
<th>Cu</th>
<th>V</th>
<th>Nb</th>
</tr>
</thead>
<tbody>
<tr>
<td>Min</td>
<td></td>
<td>0.05</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Max</td>
<td>0.20</td>
<td>0.30</td>
<td>0.08</td>
<td>0.05</td>
</tr>
</tbody>
</table>

Mechanical properties

<table>
<thead>
<tr>
<th>Specified</th>
<th>Typical</th>
</tr>
</thead>
<tbody>
<tr>
<td>Yield strength, Re</td>
<td>≥ 420 MPa</td>
</tr>
<tr>
<td>Tensile Strength, Rm</td>
<td>540-640 MPa</td>
</tr>
<tr>
<td>Elongation, A5</td>
<td>≥ 22%</td>
</tr>
<tr>
<td>Impact energy, CV</td>
<td>-20°C • 47 J</td>
</tr>
</tbody>
</table>

Recommended parameter range:

Deposition rate per hour:

Product data:

<table>
<thead>
<tr>
<th>Diameter</th>
<th>Product code</th>
<th>Spool weight</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.2</td>
<td>95651012</td>
<td>15 kg BS300</td>
</tr>
<tr>
<td>1.2</td>
<td>95651112</td>
<td>5 kg BS200</td>
</tr>
</tbody>
</table>

Note:
- Strep:
  - S ≤ 0.015%
  - P ≤ 0.025%
  - N ≤ 0.004%
ElgaCore DWA 50
FCAW - Flux cored arc welding
Un-alloyed

Description:
ElgaCore DWA 50 is a rutile flux cored wire for use with an Ar/CO2 gas shield. The wire is all-positional and runs with a very stable, soft arc producing excellent weld bead shape and finish with negligible spatter. The slag is easily detachable and furnace emission is very low. It is suitable for welding mild and medium strength carbon manganese structural steels and produces excellent root beads on ceramic backing. Ease of use and high productivity in combination with good mechanical properties and a weld metal hydrogen content less than 0.2 ml/100g make ElgaCore DWA 50 an extremely versatile general purpose cored wire.

Welding positions:
- All positions

Welding current:
- DC +

Deposition efficiency:
- 08%

Shielding gas:
- M21, 80% Ar + 20% CO2, 22-25 l/min

Stick-out:
- 15-25 mm

Hydrogen content / 100 g weld metal
- ≤ 5 ml

Chemical composition, wt.%

<table>
<thead>
<tr>
<th></th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Cr</th>
<th>Ni</th>
</tr>
</thead>
<tbody>
<tr>
<td>Min</td>
<td>0.06</td>
<td>0.4</td>
<td>1.2</td>
<td>0.015</td>
<td>0.007</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Typical</td>
<td>0.18</td>
<td>0.90</td>
<td>1.75</td>
<td>0.03</td>
<td>0.03</td>
<td>0.20</td>
<td>0.50</td>
</tr>
<tr>
<td>Max</td>
<td></td>
<td></td>
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<td></td>
<td></td>
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</table>

<table>
<thead>
<tr>
<th></th>
<th>Mo</th>
<th>Cu</th>
<th>V</th>
<th>Nb</th>
</tr>
</thead>
<tbody>
<tr>
<td>Min</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Typical</td>
<td></td>
<td>0.30</td>
<td>0.08</td>
<td>0.05</td>
</tr>
<tr>
<td>Max</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Mechanical properties

<table>
<thead>
<tr>
<th></th>
<th>Specified</th>
<th>Typical</th>
</tr>
</thead>
<tbody>
<tr>
<td>Yield strength, Rm</td>
<td>≥ 426 MPa</td>
<td>520 MPa</td>
</tr>
<tr>
<td>Tensile strength, Rm</td>
<td>500-640 MPa</td>
<td>590 MPa</td>
</tr>
<tr>
<td>Elongation, A5</td>
<td>≥ 22%</td>
<td>28%</td>
</tr>
<tr>
<td>Impact energy, CV</td>
<td>-20°C • 47 J</td>
<td>-20°C • 75 J</td>
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</tbody>
</table>

Classification:

<table>
<thead>
<tr>
<th></th>
<th>EN 758</th>
<th>T 422 P M 1 H5</th>
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<tbody>
<tr>
<td>AWS A5.20</td>
<td>E71T-1M</td>
<td>T 422 P M 1 H5</td>
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<td>ISO 17632-A</td>
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Approvals:

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<tr>
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<tbody>
<tr>
<td>DNV</td>
<td>III YMS H5</td>
<td></td>
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<tr>
<td>LR</td>
<td>3S, 3YS H5</td>
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<td>DB</td>
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<td>MRS</td>
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<td>RINA</td>
<td>3YS</td>
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<td>BV</td>
<td>SA 3Y M HH-H</td>
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<tr>
<td>CE</td>
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</table>

Recommended parameter range:

Deposition rate per hour:

<table>
<thead>
<tr>
<th>Kg</th>
<th>Ampere</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.12</td>
<td>23 mm</td>
</tr>
<tr>
<td>0.1</td>
<td>4-25 mm</td>
</tr>
<tr>
<td>0.1</td>
<td>6-25 mm</td>
</tr>
</tbody>
</table>

Product data:

<table>
<thead>
<tr>
<th>Diameter</th>
<th>95601012</th>
<th>95602112</th>
<th>95602212</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.2</td>
<td>15 kg</td>
<td>5 kg</td>
<td>250 kg</td>
</tr>
<tr>
<td>1.2</td>
<td>BS300</td>
<td>D200</td>
<td>ProPac</td>
</tr>
<tr>
<td>1.2</td>
<td>12,5 kg</td>
<td>D300</td>
<td></td>
</tr>
</tbody>
</table>

Note

Strip:

S ≤ 0.015%