WELDING OF HIGH STRENGTH AND STAINLESS STEELS:
A STUDY ON WELD METAL STRENGTH
AND STRESS RELIEVING

by

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Submitted to the Departments of Mechanical Engineering and Ocean Engineering on May 7, 1982 in partial fulfillment of the requirements for the Degrees of Master of science in Mechanical Engineering and Master of science in Ocean System Management.

ABSTRACT

The approach of weld metal strength undermatching in the fabrication of high strength steel structures is presented and justified in the Part I of the study.

Analytical techniques for the evaluation of the tensile strength of undermatched butt welded joints are presented and a numerical verification of assumptions and results is performed using the finite element program A.D.I.N.A.

The applicability and effectiveness of various stress-relieving methods is examined in Part II. An analytical model for the study of stress relaxation during post-weld heat treatments is also developed.

Welding and stress relieving experiments performed on stainless steel specimens in order to test the validity of the analysis, are described in Part III of the study. The development of a microcomputer-based data acquisition system for these experiments is also covered.

Finally in Part IV a welding cost model is developed and the economic aspects of welding production are outlined, together with the cost savings possible through the application of weld metal strength undermatching.

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CHAPTER I
INTRODUCTION

1.1 Background and General Considerations

Welding is extensively used today in the fabrication of many structures including ships, buildings, pressure vessels and aerospace vehicles and certainly provides many advantages over other fabrication techniques. Welded structures however are by no means free from problems. The local nonuniform heating during welding and the subsequent cooling cause complex thermal strains to develop that finally lead to residual stresses, distortion and all their adverse consequences (such as brittle fracture, fatigue fracture, stress corrosion cracking or even buckling).

The extent to which these effects appear is directly related to the design and fabrication parameters and to the material properties of both the base plate and the weld metal. So for example, when the material is brittle, residual stresses can reduce the fracture strength of the weldment significantly. On the other hand, when the material is ductile the effects of the residual stresses on fracture are negligible. Most of the above problems are also seriously aggravated in the case of higher strength materials which find an increasing number of applications today.

For the case of high strength steels in particular, which are examined in this study, their inherent characteristic of decreasing fracture toughness with increasing strength should also be considered. Furthermore the fracture toughness of the
weld metal is usually less than that of the base plate. This is due to the existence of both high residual stresses and various types of defects such as cracks, porosity or slag inclusions. It should also be noted that good fracture properties in high strength steels are usually obtained through heat treatments, that are drastically different from the thermal cycles encountered in the weld metal during welding. Therefore, as the strength of the steel increases, it becomes more and more difficult to obtain weld metals that match the base plate in both strength and fracture toughness, as is traditionally required by the codes.

In addition, preheating is usually required before welding of high strength steel systems, in order to avoid hydrogen-induced delayed cracking of the weldments. It is generally believed that preheating results in slower cooling rates and therefore permits more hydrogen to diffuse and in the same time leads to lower thermal stresses. However, preheating complicates the whole welding operation and increases fabrication costs.

An alternative approach that results in lower stresses in the weld metal and requires less or no preheating is to use a filler metal having yield strength lower than the one of the base plate but ample fracture toughness. This "undermatching" philosophy is successfully applied in Japan and is under serious consideration in U.S. Justification of this approach is basically the underlying objective of Part I of this study. The cost reductions realizable through undermatching are further examined in Part IV.
It should be pointed out here, however, that despite of the precautions taken residual stresses of significant magnitude and possibly distortion will usually develop during welding of high strength steels. These can sometimes be brought under acceptable limits by some kind of stress relieving operation. Uniform or localized heat treatments are frequently specified by the fabrication codes. Such treatments, however, would again complicate the welding operations and increase the production costs. Furthermore, they might be detrimental to the microstructures and therefore to the mechanical properties of the base and / or the weld metal.

The effectiveness and applicability of stress relieving heat treatments and possible alternatives are examined in Part II and III of this study.

1.2 Objectives

The basic objectives of this study are (in the order they appear in the text):

(a) A literature survey of the past studies dealing with the applicability and justification of the undermatching philosophy in high strength steels.

(b) An analytical evaluation of the strength of undermatched butt welded joints.

(c) A numerical evaluation of the strength of undermatched joints using the Finite Element Method.

(d) An evaluation of the various stress relieving methods and their effects as well as a survey of the applicable industrial codes.
(e) The development of an analytical model for the calculation of remaining stresses after stress relieving operation.

(f) The experimental verification of the analytical model.

(g) The development of a welding cost model and the evaluation of the cost savings possible through weld metal strength undermatching.

Finally an important byproduct of the experimental part of this study that should be mentioned separately was:

(h) The development of a microcomputer-based data acquisition system for welding and or stress relieving experiments.

1.3 Organization of the study

The next several chapters of this study deal with the objectives set forth in the previous section. Specifically in Chapter II a justification of the undermatching philosophy is attempted and to this end results of various past studies (mostly by Japanese investigators) are reviewed. In Chapter III analytical techniques for the evaluation of the strength of simple undermatched butt welded joints are presented. Further in order to verify the assumptions and to confirm the results of the analysis a numerical evaluation of the strength of the same joints is performed using the finite element program ADINA.

The various stress relieving methods and the applicable codes are reviewed in Chapter IV. Special consideration is given in the assesement of the effects of thermal stress relieving treatments and the evaluation of possible alternatives. In Chapter V an one dimensional model is developed for the analysis
of stress relaxation due to heat treatments. However, since necessary creep properties were not available for high strength quenched and tempered steels further analysis was limited to 304 stainless steels.

For comparison, welding and stress relieving experiments, described in Chapter VI, were performed. Furthermore, the basic characteristics of the developed microcomputer-based data acquisition system are also presented in this chapter. Results conclusions and recommendations were summarized in Chapter VII.

In Chapter VIII a welding cost model is developed and various economic aspects of welding production are outlined. The possible cost savings through the application of weld metal strength undermatching are finally examined in Chapter IX.
PART I

WELD METAL STRENGTH UNDERMATCHING
CHAPTER II
THE APPROACH OF WELD METAL STRENGTH
UNDERMATCHING-A LITERATURE SURVEY

2.1 Overmatching Versus Undermatching

In the design and fabrication of welded structures efforts are usually made, in accordance with the codes, to ensure that the weld metal has both adequate strength and toughness. When welding ordinary low-carbon steel it is not difficult to obtain weld metals that match the base metals in both strength and fracture toughness,[5], whereas for higher yield strength steels, this match becomes increasingly difficult to maintain.

This problem is particularly evident in the case of quenched and tempered steels, such as the HY-series U.S. Navy steels, whose high yield strength and excellent toughness are obtained through heat treatments. There are basically two approaches in coping with the problem.

The first is to require a weld metal with tensile strength at least equal to the specified minimum tensile properties of the base metal and toughness that can be achieved reliably by good welding practices, which is often less than that of the base metal. The second approach is to accept weld metals that slightly undermatch the base metal in strength but have adequate fracture toughness.

The overmatching approach, that has been traditionally followed by U.S. Navy codes, evolved in order to ensure that the weldment was not a "weak link" in the structure [6]. The adequacy of the approach for ordinary low-carbon steels was
proven by explosion bulge tests, by Masubuchi [5] and Pelini [3]. However, it is questionable whether an extrapolation of this philosophy is appropriate for high strength steel systems. That is, there exists little evidence that strength overmatching would guarantee adequate performance.

In contrary to U.S. specifications, some Japanese industrial standards have accepted the undermatching approach both for repairs and for initial welding of various layers in multipass welds, following extensive research efforts. Lower strength filler metals may permit reductions in the preheating temperatures required and would result in easier welding of highly restrained heavy sections. Additionally, with weld metals of lower yield strength, local plastic deformations can reduce stress concentrations in hard spots. It should also be noted, that the overall strength and ductility of an undermatched joint is usually not adversely influenced by the existence of the lower yield strength zone. Specifically as was observed by various investigators the joint strength can often reach that of the base metal, if the reduction in strength is not large and the weld size sufficiently small.

A number of researchers in different countries have studied the effects of the mechanical properties of the weld metal and the heat affected zone on the mechanical behavior of weldments in different materials ([7] to [10]). Specifically in Japan, however, the S.J. (Soft Joint) Committee of the Japan Welding Engineering Society (J.W.E.S.) has undertaken extensive research efforts, during the past decade, to determine the
mechanical behavior of undermatched welded joints and to find reasonable strength levels for the filler metal from the standpoint of both workmanship and joint performance,[11].

The initial fundamental studies of Satoh et al.,[12],[13],[14] and [15], were followed by detailed performance analysis of static tensile strength, fatigue strength and brittle fracture strength of undermatched butt and fillet welds. The next sections of this chapter present in summary the results of these studies. A more extensive review was performed by the author in [16].

The analytical and numerical evaluation of the strength of an undermatched butt welded joint will be presented in the next chapter.

2.2 Tensile Strength of Undermatched Butt Welds

2.2.1 Fundamental Experimental Studies

The static tensile properties of welded plates, including soft interlayers and loaded either across or parallel to the weld line, were evaluated in an extensive research program completed by Satoh and Toyoda in the late 1960's and early 1970's at Osaka University,[12],[13],[14] and [15].

Round bar specimens of a medium carbon steel including a flash welded soft interlayer of low carbon steel were initially tested,[13]. Heterogeneity in mechanical properties along the specimen was estimated by the hardness distribution over a longitudinal section. Results of the tensile tests suggested that the strength of the joint approaches that of the base metal when the ratio of the thickness of the soft interlayer to
the diameter of the bar is sufficiently small. Tests of flat
bar specimens loaded across the weld line followed [14]. The
specimens, shown in figure 2.1, were prepared either by flash
welding of round bars of S15C* and S35C* structural steels or by
gas metal arc welding of high tensile strength, HT-80*, steel
plates (minimum tensile strength of 80 Kg/mm²), using electrodes
producing weld metal with minimum tensile strength of 50 Kg/mm².
Results of the tensile tests given in figure 2.2 and 2.3
indicate that the ultimate tensile strength and yield stress
of the joint depend on both the relative thickness Xₜ (the
ratio of the soft interlayer thickness, H, to the plate
thickness, t) and the plate width to thickness ratio (w/t).
Specifically, the joint strength increases as the Xₜ decreases
and reaches the strength of the base metal when Xₜ is
sufficiently small. Additionally, figure 2.3 suggests that when
the plate width to thickness ratio increases, the ultimate
tensile strength of the joint increases also up to a certain
definite value that depends on Xₜ. The plate width, Wₜ, above
which the tensile strength becomes equal to the one of an
infinite plate depends on both H and t. This influence was
also verified by Yoshinaga [17] and Hisamitsu [18]. For Xₜ < 1,
in general, Wₜ can be roughly estimated by Wₜ = 5 t and is
independent of H. When Xₜ > 1, however, plastic constraint in
the plate thickness direction - which is the cause of the
strength increase - will disappear at the mid cross-section of

* S15C, S35C and HT-80 are Japanese steel grades. Note that
HT-80 has nothing to do with HY-80 (the primary U.S. Navy hull
construction high strength steel)
Figure 2.1: Specimen sizes and configuration
Figure 2.2: Ultimate tensile strength vs relative thickness (Series A, B)

Figure 2.3: Effect of plate width on ultimate tensile strength of welded plates (Series S, T)
the soft interlayer and the \( W_\infty \) value will depend only on the H-value. Experimentation and analysis showed that in this case we can roughly estimate \( W_\infty = 5H \).

To simulate more applications, other experiments were carried out with loading parallel to the weld line, [15]. The results of these tests suggest that the strength and the ductility of the joint depend on the value of the ratio of the width of the hard zone to that of the soft zone and become almost equal to those of the hard metal when the ratio is larger than 10.

Strain distributions in the composite weldment are almost uniform along the mid cross-section of the specimen at each load level except when yielding and after the maximum load. Behavior of the axial strain around yielding seems to be influenced by the ratio of the width of the soft metal to the thickness of the plate. When this ratio is smaller than 2, the strain increases almost uniformly along a cross section until general yielding occurs. At that point, base metal strains are temporarily larger than those of the soft metal. When the ratio is larger than 2, nonuniform distribution of the strain occurs at average stress somewhat larger than the yield strength of soft material and continues until general yielding.

2.2.2 Performance Study

The initial experimental studies, indicated that, for the idealized joints examined, the ultimate tensile strength may be as high as that of the base metal if the average width of the weld metal is sufficiently small.
To assess the applicability of undermatching in actual structures, the S.J. committee of the Japan Welding Engineering Society carried out a performance study presented in [19],[20] and [21]. Wide plate specimens shown in figure 2.4 were prepared by shielded metal arc welding of 70 mm thick HT-80 plates with various under- or overmatching electrodes. Results indicated that for butt welded specimens with an average relative thickness \((X_t)_{av}\) between 0.2 and 0.3, the strength of the joint reached the strength of the base plate when the ultimate tensile strength of the weld metal, \(\sigma_u^w\), was nearly 90% of the ultimate tensile strength of the base metal, \(\sigma_u^B\). That is for \((\sigma_u^w/\sigma_u^B)\) ratio larger than 0.9, the undermatched welded joint behaved in almost the same way as the base plate in terms of both strength and ductility.

2.3 **Tensile Strength of Undermatched Fillet Welds**

The S.J. Committee of the J.W.E.S. also investigated the applicability of undermatching in fillet welds. Experiments were performed with various specimens of high strength steel (U.T.S. of 84.1 Kg/mm\(^2\)) welded with undermatched electrodes (U.T.S. of 40 to 80 Kg/mm\(^2\)). Detailed presentation and results for tensile and shear tests appear in [11]. It should however be noted that the geometry and the size of the fillet is also important now, since they can be adjusted to compensate for the lower strength of the weld metal.

2.4 **Fatigue Strength of Undermatched Welded Joints**

Gelman and Kudrayartzev showed experimentally, [9], that the fatigue strength of bars with a soft interlayer increased
Figure 2.4(a): Results of the tensile tests

Tensile strength of the weld metal $\sigma_{u}^{w}$ in kg/mm²

<table>
<thead>
<tr>
<th>Type</th>
<th>$W_{o}$</th>
<th>$L_{o}$</th>
<th>$t_{o}$</th>
<th>$W'$</th>
<th>R</th>
</tr>
</thead>
<tbody>
<tr>
<td>L</td>
<td>500</td>
<td>400</td>
<td>70</td>
<td>300</td>
<td>200</td>
</tr>
<tr>
<td>M</td>
<td>70</td>
<td>400</td>
<td>70</td>
<td>140</td>
<td>200</td>
</tr>
</tbody>
</table>

Figure 2.4(b): Specimen design and dimensions for static tensile tests
when the thickness of the interlayer decreased. In the late 1960's Satoh and Nagai,[12], investigated the fatigue strength of welded or locally work hardened round bars having hard or soft interlayers. Fatigue tests were performed with a rotating bending machine and the results indicated in general that the hard interlayer had no effect on fatigue strength, whereas for the soft interlayer the fatigue strength decreases drastically as the thickness of the interlayer increases.

To evaluate the performance of actual undermatched welded joints, the S. J. Committee of the Japan Welding Engineering Society conducted a series of fatigue tests using HT-80 specimens welded with E7016 and E11016 electrodes. The first series of specimens tested (FT, FL) is shown in Figure 2.5.

The mechanical properties of the base metal and the weld metals together with fatigue test results are shown in Table 2.1. Specimens were tested under a pulsating load between zero and 35 Kg/mm$^2$, and between zero and 55 Kg/mm$^2$. In the FT specimens, where tensile stress was applied transversely to the weld, fatigue cracks occurred at the toe of reinforcement and propagated in the direction of the thickness. No appreciable difference in the number of cycles to fracture appeared between the overmatched and the undermatched joints. In FL specimens, however, where tensile stresses were applied parallel to the weld, the fatigue life of the overmatched joint was somewhat longer because fatigue cracks were initiated on the surface of the weld metal. In both FL and FT specimens, the weld reinforcement had not been removed. Further tests for other
Figure 2.5: Type FT and FL fatigue specimens [28]

Table 2.1: Mechanical Properties of the materials used in the fatigue tests [28]

<table>
<thead>
<tr>
<th>Material</th>
<th>Tensile Strength (Kg/mm²)</th>
<th>Yield Strength (Kg/mm²)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Base metal HT 80 (a)</td>
<td>84</td>
<td>79</td>
</tr>
<tr>
<td>Weld E 11016</td>
<td>86</td>
<td>74</td>
</tr>
<tr>
<td>Metal E 7016</td>
<td>55</td>
<td>0</td>
</tr>
</tbody>
</table>

(a): Chemical composition: C 0.10%, Mn 0.79%, Si 0.26%
P 0.004%, S 0.007%, Ni 0.83%, Cr 0.52%, Mo 0.34%

<table>
<thead>
<tr>
<th>Maximum Stress applied (Kg/mm²)</th>
<th>Electrode used</th>
<th>No. of cycle at fracture Type FT (x10⁴)</th>
<th>Type FL (x10⁴)</th>
</tr>
</thead>
<tbody>
<tr>
<td>35</td>
<td>E 11016-G</td>
<td>15.2</td>
<td>20.8</td>
</tr>
<tr>
<td></td>
<td></td>
<td>13.2</td>
<td>23.1</td>
</tr>
<tr>
<td></td>
<td>E 7016</td>
<td>13.8</td>
<td>16.2</td>
</tr>
<tr>
<td></td>
<td></td>
<td>14.1</td>
<td>22.3</td>
</tr>
<tr>
<td>55</td>
<td>E 11016-G</td>
<td>3.72</td>
<td>2.62</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1.38</td>
<td>2.45</td>
</tr>
<tr>
<td></td>
<td></td>
<td>3.17</td>
<td></td>
</tr>
<tr>
<td></td>
<td>E 7016</td>
<td>8.65</td>
<td>4.63</td>
</tr>
<tr>
<td></td>
<td></td>
<td>6.63</td>
<td>5.34</td>
</tr>
</tbody>
</table>
geometries, as well, verified the above results and showed that in general removing the reinforcement led to improved fatigue behavior [11] as shown in Figure 2.6.

2.5 Brittle Fracture Behavior of Undermatched Welded Joints

Various studies were performed to investigate the brittle fracture strength of undermatched welded joints in high strength steels. ([22],[23] and [24]). It was generally indicated from test results that higher fracture toughness or lower transition temperature should be required for the undermatching weld metal than for the overmatching one.

Satoh and Toyoda, [23], have further shown that if $v T_s$ is the fracture transition temperature obtained from a V-notch Charpy test and $\Delta v T_s$ is the difference in the fracture transition temperature, between overmatching and undermatching filler metals, required to obtain the same fracture initiation temperature, $T_i$, for a welded and notched wide plate, then:

$$\Delta v T_s = 80 \ln \left( S_r \right)_y \left[ 1-65(1/T_i-1/273) \right]$$

where $(S_r)_y$ is the ratio of the yield stress of the undermatching and overmatching filler metals and $T_i$ is in degrees K.

So if for example the $(S_r)_y = 0.8$ and $T_i = -50^\circ C$ to $-150^\circ C$ then the transition temperature required for the undermatching filler metal is $15^\circ C$ to $20^\circ C$ less than that of the overmatching one.

2.6 Residual Stresses in Undermatching Welded Joints

In low carbon steel weldments maximum tensile residual stresses in the weld metal usually reach the yield stress of
Figure 2.6: Fatigue test results for various specimens tested by JWES [11]
the material, [1]. In high strength steel weldments, however, the experimentally obtained distribution of residual stresses has maximum peaks lower than the yield strength of the material. Additionally the width of the tensile stress zone is usually observed to be wider than what it should have been if the material behaved analogously to lower strength steels.

This behavior has been confirmed by various investigators, [25],[26],[27], and is depicted schematically in Figure 2.7 adapted from [1]. Direct analogy with lower strength steels would suggest curve (1). Analytical predictions, support (2) whereas experiments tend to indicate that (3) is correct. It is assumed in the bibliography, however, that the actual distribution lies between (2) and (3). The discrepancies are usually attributed to additional expansion during cooling, due to phase changes that occur in the higher strength steels.

Although no actual results have been reported it is believed that lower yield strength filler metals would result in lower residual stress peaks.

2.7 Structural Applications

The applicability of undermatching filler metals in structural fabrication, as established by the initial fundamental studies and joint performance tests was further verified by actual structural applications.

One of the early examples was the burst test of welded pipes 4100 mm long, 950 mm in diameter and made of 12 mm thick HT-80 steel plates. Weld metal had a measured ultimate strength of 77 Kg/mm². However, as reported in [28] and [11], during the
Figure 2.7: Possible distributions of longitudinal residual stresses in butt welded plates of high strength steel.
burst test, fracture started at an internal pressure of 235 Kg/mm$^2$ corresponding to a circumferential stress of 90 Kg/mm$^2$ (just above the minimum ultimate tensile strength of the base plate, 89 Kg/mm$^2$). It is apparent therefore that the undermatched filler metal had no harmful influence.

Another extensive investigation of the potential applicability of undermatching in the field welding of HT-80 heavy plates was also carried out by Satoh et al, [21],[29] and [30]. Both experiments and service experience verified that the use of undermatching electrodes effectively lowered the preheating temperature, required to prevent root cracking caused by the first pass, and weld metal cracking, caused by the subsequent passes, in multipass welding of 50 mm thick sections.

Additionally not appreciable differences in tensile strength and uniform elongation between the overmatched and the undermatched welded joints was observed. Tests were performed on wide plate tension specimens, with or without notch, tested at a temperature slightly lower than the minimum service temperature experienced in the field.
CHAPTER III

ANALYTICAL AND NUMERICAL EVALUATION OF THE
STRENGTH OF UNDERMATCHED BUTT WELDED JOINTS

3.1 Analytical Strength Evaluation

3.1.1 General Discussion

The effect of a region of lower yield strength weld material on the mechanical behavior of a joint, depends, in general, on the type of the joint, the size of the weldment, the degree of reduction in strength, the width of the lower strength zone, the types of loading encountered and the loading directions.

Thus referring to Figure 3.1(a) we note that when tensile loading is applied to a transverse butt weld, the joint is under constant stress. When the applied stress exceeds the yield strength of the weld material, strain concentrations occur in the weld metal and result in fracture. However, the extent of the effects of the L.Y.S. zone depends on the degree of reduction in strength and the width of the zone. In the case of Figure 3.1(b), where the tensile loading is applied to a longitudinal butt weld, the joint is under constant strain and the effect of the lower yield strength material is very small as long as the zone has enough ductility and the width of the weldment is reasonably larger than that of the weld metal.

Thus, in what follows, we will only restrict ourselves in the analytical evaluation of the strength of butt welded joints, loaded in a direction perpendicular to the weld line, and later in the numerical verification of assumptions and results.
Figure 3.1: Butt welds subjected to tensile loading, (a) transverse to the weld line and (b) parallel to the weld line.
(by the F.E.M. method).

3.1.2 Plane Strain Case-Infinite Width Plate

An idealized butt welded joint is shown in Figure 3.2. If the weld metal is of lower yield strength the plastic flow under tensile loading starts in the interlayer. Large transverse plastic flow near the base metal will be prevented and the triaxial stress state in the interlayer will be similar to the one of the neck of a tension specimen.

When the joint width $W_0$ is much larger than the thickness $t_0$ and $H_0$, deformations in the direction of the width will take place in planes perpendicular to the $x$ axis except for parts close to the ends. The case is eventually one of plane strain with $\varepsilon_x = 0$.

We further assume the following:

- The joints consist only of two kinds of metals each being an isotropic and homogeneous material.
- The base metals behave like rigid bodies and do not contract in the thickness direction even when plastic flow occurs sufficiently in the soft interlayer.
- During the deformation of the neck there is no permanent volume change.
- The equivalent stress $\bar{\sigma}$ versus equivalent strain $\bar{\varepsilon}$ relation of the soft interlayer is

$$\bar{\sigma} = K\bar{\varepsilon}^n$$  \hspace{1cm} (3-1)

where $K$ and $n$ are material constants.

The analysis of the triaxial stress state in the neck was performed by Satoh and Toyoda,[13], in a way similar to the one
(I) Hard base metal
(II) Soft interlayer

Figure 3.2: Idealized model of two butt welded plates with a lower strength interlayer. (Plane strain case for $W_o \gg t_o$)
(I) Hard base metal
(II) Soft interlayer

Figure 3.3: Deformation of welded joints including a soft interlayer (plane strain case)

Figure 3.4: Deformation of the neck area. Stress trajectories and equilibrium (plane strain case)
followed by Davidenkov and Spiridonova [31].

Let $s_x', s_y', s_z$ be the true stress components at the neck, $e_x', e_y', e_z$ the true strains and $\varepsilon_x', \varepsilon_y', \varepsilon_z$ the corresponding engineering strains. Refering to Figure 3.4, we denote by $\rho$ the radius of curvature of a certain stress trajectory in the $yz$ plane. For an element at the neck having a unit length in the $x$-direction, the stress equilibrium in the $y$-direction will give:

$$-s_y\rho d\theta + (s_y + ds_y)(\rho + dy)d\theta - s_z dyd\theta = 0$$

(3-2),

but since $dy/\rho \ll 1$, (2) gives:

$$ds_y = \frac{1}{\rho} (s_z - s_y) dy$$

(3-3)

Supposing that the material yields according to Von Mises yield condition we have:

$$(s_x - s_y)^2 + (s_y - s_z)^2 + (s_z - s_x)^2 = 2\bar{\sigma}^2$$

(3-4)

where $\bar{\sigma}$ the equivalent stress. But for the plane strain state

$$e_x = 0$$

and

$$s_x = \frac{1}{2} (s_z + s_y)$$

(3-5)

thus, from (4) and (5), the yield condition will be:

$$s_z - s_y = \frac{2}{\sqrt{3}} \bar{\sigma}$$

(3-6)

(6) in (3) gives

$$s_y = \frac{2}{\sqrt{3}} \int_{y}^{\alpha} \frac{\sigma}{\rho} dy$$

(3-7)
and then from (6)

\[ s_z = \frac{2}{\sqrt{3}} \left\{ \bar{\sigma} + \int_y^{\alpha} \frac{\sigma}{\rho} \frac{dy}{\rho} \right\} \]  
(3-8)

Now we use two experimental observations made by Davidenkov and Spiridonova (and also used by Bridgman [32] with no experimental basis).

(a) the \( e_y \) and \( e_z \) strains are independent of \( y \) (same across the section).

(b) the curvature \( 1/\rho \) of the stress trajectory is proportional to \( y \) that is

\[ \frac{1}{\rho} = \frac{Y}{\alpha R} \]  
(3-9)

(7), (8) and (9) then give:

\[ s_y = \frac{2}{\sqrt{3}} \bar{\sigma} \frac{\alpha^2-y^2}{2\alpha R} \]  
(3-10)

\[ s_z = \frac{2}{\sqrt{3}} \bar{\sigma} \left( 1 + \frac{\alpha^2-y^2}{2\alpha R} \right) \]  
(3-11)

Thus, the average axial stress at the neck will be:

\[ \bar{s}_z = \frac{1}{\alpha} \int_0^{\alpha} s_z dy = \frac{2}{\sqrt{3}} \bar{\sigma} \left( 1 + \frac{\alpha}{R} \right) \]  
(3-12)

But from (1)

\[ \bar{\sigma} = K \varepsilon^n \]

and
\[ \bar{\varepsilon} = \frac{2}{\sqrt{3}} \sqrt{e_x^2 + e_y^2 + e_z^2} \]  

(3-13)

However, for plane strain \( e_x = 0 \) and for volume conservation \( e_y = -e_z \), therefore the equivalent strain will be:

\[ \bar{\varepsilon} = \frac{2}{\sqrt{3}} e_z \]  

(3-14)

and

\[ \bar{\sigma} = \left( \frac{2}{\sqrt{3}} \right)^n K e_z^n \]  

(3-15)

which substituted back to (12) gives:

\[ \sim s_z = \left( \frac{2}{\sqrt{3}} \right)^n + \frac{1}{K \left( 1 + \frac{a}{3R} \right)} e_z^n \]

or rewriting it in terms of nominal stress \( \sigma_z \) and strain \( \varepsilon_z \)

\[ \sigma_z = \left( \frac{2}{\sqrt{3}} \right)^n + 1 \frac{K \{ln(1+\varepsilon_z)\}^n}{1+\varepsilon_z} \left( 1 + \frac{a}{3R} \right) \]  

(3-16)

since

\[ \sim s_z = \sigma_z (1 + \varepsilon_z) \]

and

\[ e_z = ln(1 + \varepsilon_z) \]

From geometry in Figure 5, we have for the nominal strain \( \varepsilon_y \)

\[ \varepsilon_y = \frac{\alpha - \alpha_0}{\alpha_0} = \frac{\alpha}{\alpha_0} - 1 \]
But 
\[ e_y = \ln(1 + \varepsilon_y) \]
and from volume conservation
\[ e_y = -e_z \]
then
\[ \frac{a}{a_0} = e^{-e_z} = e^{-\ln(1 + \varepsilon_z)} = \frac{1}{1 + \varepsilon_z} \]  
(3-17)

Also from the geometry
\[ \sqrt{R^2 - h^2} = R - (a_0 - a) \]
or,
\[ h^2 = (a_0 - a) [2R - a_0 + a] \]  
(3-18)

and from volume conservation:
\[ a_0 h_0 = (a + R) h - \frac{1}{2} \left( R h \sqrt{1 - \left( \frac{h}{R} \right)^2} + R^2 \sin^{-1} \frac{h}{R} \right) \]  
(3-19)

If we now introduce:
\[ x_t = \frac{h_0}{a_0}, \quad y_t = \frac{a_0}{3R}, \quad \varepsilon = \frac{1}{1 + \varepsilon_z} \]  
(3-20)

we get from eqns. (17), (18) and (19)
\[ x_t = \frac{1}{\sqrt{3}} \sqrt{(1 - \varepsilon) \left\{ \frac{2\varepsilon}{2 \varepsilon + 1} \frac{1}{3} - \frac{(1 - \varepsilon)^2 y_t}{2\varepsilon} \right\}} \]  
(3-21)

Equation (21) relates \( \varepsilon \) to \( y_t \) for every \( x_t \) and together with (16) gives the stress strain (\( \sigma_z \) vs. \( \varepsilon_z \)) relation for every \( x_t = h_0/a_0 \).

Based on equations (16) and (21) Satoh and Toyoda
Figure 3.5: Ultimate tensile strength in the plane state case as a function of relative thickness $x_t$ (Assume a stress strain law: $\sigma = K \epsilon^n$), [13]
calculated different $\sigma_z - \varepsilon_z$ curves and determined the effect of relative thickness on the ultimate and yield strength (Figure 3.5).

Taking into account the effect of plastic flow in the base metals, however, the results derived earlier are modified, as shown by the dotted curves of Figure 2.2[14]. In the same figure, are shown some experimental results also by Satoh and Toyoda.

3.1.3 Axisymmetric Case

For the case of a round tensile specimen which deforms as in Figure 3.6, we will start with similar assumptions as before.
- Both base material and the soft interlayer are uniform and isotropic materials.
- Suppose (in a first approximation) that the base material is rigid.
- Suppose that the interface between the soft and the hard layer remains perpendicular to the loading direction.
- After necking the joint configuration will be as in Figure 3.6.
- Volume remains constant.
- The true stress-true strain law will be
  \[ S = K e^n \]  
  (3-22)

Again based on Davidenkov's analysis and assuming that the stress trajectories will be as in Figure 3.7, we further assume that
- the tangential and radial true strains are equal, and since volume is constant we get:
Figure 3.6: Welded joint including soft interlayer - Axisymmetric case

Figure 3.7: Sketch of the stress trajectories in the neck of a round tensile specimen
$$e_r = e_\theta = -\frac{1}{2} e_z \quad (3-23)$$

and

-the curvature is linearly related to the radius or

$$\frac{1}{\rho} = \frac{r}{\alpha R} \quad (3-24)$$

We further assume that the strain does not change along the neck cross section and, thus, the differences in principal stresses are also the same, according to hypotheses of both maximum shearing stress and the octahedral shearing stress. So starting with the equilibrium equation in the radial direction and integrating along a section we get:

$$s_r = s_o \int_r^\alpha \frac{dr}{\rho} \quad (3-25)$$

where $s_o$ denotes the difference in principal stresses. Now using (24) we get

$$s_r = \frac{s_o (\alpha^2 - r^2)}{2R} \quad (3-26)$$

whereas the axial stress will then be:

$$s_z = s_o + s_r = s_o \left\{1 + \frac{\alpha^2 - x^2}{2R\alpha}\right\} \quad (3-27)$$

and the average axial stress will be

$$\bar{s}_z = s_o \left\{1 + \frac{\alpha}{4R}\right\} \quad (3-28)$$
If $\varepsilon_r$, $\varepsilon_\theta$, $\varepsilon_z$ are the true strains and $\varepsilon_r$, $\varepsilon_\theta$ and $\varepsilon_z$ are the engineering ones then from geometry:

$$
\varepsilon_r = \frac{\alpha - \alpha_o}{\alpha_o} = \frac{\alpha}{\alpha_o} - 1
$$

or

$$
\frac{\alpha}{\alpha_o} = 1 - \frac{\varepsilon_z}{2}
$$

and also from geometry

$$
h_o^2 = (\alpha_o - \alpha)(2R - \alpha_o + \alpha)
$$

whereas from volume conservation

$$
(2R^2 + 2\alpha R + \alpha^2)h - \frac{h^3}{3} = (R + \alpha)
$$

which after expanding in series in $h/R$ and keeping the first terms only gives:

$$
\alpha h^3 + 3\alpha^2 Rh - 3\alpha_o^2 R h_o = 0
$$

Introducing now

$$
x = \frac{h}{R} \quad \text{and} \quad y = \frac{\alpha_o}{R}, \quad x = \frac{h_o}{\alpha_o}
$$

we rewrite eqns. (30) and (32) as

$$
x^2 = y\varepsilon_z\left(1 - \frac{\varepsilon_z}{4}\right)
$$
and
\[
\gamma x \left( 1 - \frac{\varepsilon z}{2} \right)^2 + \frac{1}{3} \gamma x^3 \left( 1 - \frac{\varepsilon z}{2} \right) = y^3 x \tag{3-34}
\]

and also
\[
X = \left( 1 - \frac{\varepsilon z}{2} \right)^2 \sqrt{\frac{\varepsilon z}{y} - \frac{\varepsilon z^2}{4}} + \frac{1}{3} \left( 1 - \frac{\varepsilon z}{2} \right) \cdot y \cdot \left( \sqrt{\frac{\varepsilon z}{y} - \frac{\varepsilon z^2}{4}} \right)^3 \tag{3-35}
\]

Introducing
\[
y \equiv \frac{\alpha}{4R} = \frac{y}{4} \left( 1 - \frac{\varepsilon z}{2} \right)
\]

(35) gives
\[
X = \frac{1}{2} \left( 1 - \frac{\varepsilon z}{2} \right)^2 \sqrt{\frac{\varepsilon z}{y} \left( 1 - \frac{\varepsilon z}{2} \right)} - \varepsilon z^2 + \frac{1}{6} y \left( \sqrt{\frac{\varepsilon z}{y} \left( 1 - \frac{\varepsilon z}{2} \right)} - \varepsilon z \right)^3 \tag{3-36}
\]

Now rewriting (28) using (22)
\[
s_{z} = K e^n \left( 1 + \frac{\alpha}{4R} \right) \tag{3-37}
\]

where plugging the nominal stress and engineering strain we finally get:
\[ \sigma_z = K \{ \ln (1 + \varepsilon_z) \}^n \left( 1 + \frac{\alpha}{4R} \right) \left( 1 - \frac{\varepsilon_z}{2} \right)^2 \]

or

\[ \sigma_z = K \{ \ln (1 + \varepsilon_z) \}^n (1 + Y) \left( 1 - \frac{\varepsilon_z}{2} \right)^2 \quad (3-38) \]

(38) together with (36) relate \( \sigma_z \) and \( \varepsilon_z \) for every value of \( X = (h_o/\alpha_o) \).

Satoh and Toyoda used the above relations in predicting the stress-strain curves of Figure 3.8 and the relation of ultimate tensile strength and relative thickness of Figure 3.9[14]. More results can be found in other papers reviewed in [16] and [11].

Although the assumption of rigid base plate is justifiable in the plane strain case, it is not realistic in the axisymmetric case. Analysis taking into account the yielding of the base material was also performed by Satoh and Doi and results appear in [11].
Figure 3.3: Families of axial nominal stress vs engineering strain curves.
(\(K\) and \(n\) are the stress-strain law constants)

Figure 3.9: Ultimate tensile strength as a function of relative thickness \(X\)
3.2 Numerical Strength Evaluation by the Finite Element Method

3.2.1 General Approach

Although experimental results seem to verify the analytical predictions an attempt was made in this study to confirm the assumptions and results of theoretical analysis using the finite element program ADINA, [4].

Both two dimensional plane strain and axisymmetric analysis was performed corresponding to the wide plate and round bar idealized geometries treated by other researchers.

To investigate the stress strain state at large deformations a nonlinear incremental analysis, using the Updated Lagrangian formulation, [33], was employed.

An elastic-plastic material model was used, assuming linear strain hardening and Von-Mises yield condition. The bilinear stress strain law for the model is shown in Figure 8.10, and the mechanical properties of base metal and interlayer are given in Table 8.1.

With ADINA the material model #8 was mainly employed. However, to test the improvement of convergence, material model #10 (thermo-elastic-plastic and creep) was occasionally used at reference temperature. The latter model incorporates an option of optimizing the time step subdivision, taking into account the convergence characteristics of the iterative calculations [4].

The ultimate strength value was not incorporated into the model but was implied in a way discussed later.
Table 3.1: Mechanical properties of the base and filler metal

<table>
<thead>
<tr>
<th></th>
<th>( E ) Kg/mm(^2)</th>
<th>( E_T ) Kg/mm(^2)</th>
<th>( \nu )</th>
<th>( \sigma_Y ) Kg/mm(^2)</th>
<th>( \sigma_U ) Kg/mm(^2)</th>
</tr>
</thead>
<tbody>
<tr>
<td>HT-80 (base metal)</td>
<td>( 2.1 \times 10^4 )</td>
<td>( 1/12 \times 10^3 )</td>
<td>0.3</td>
<td>78.0</td>
<td>84.1</td>
</tr>
<tr>
<td>HT-50 (soft interlayer)</td>
<td>&quot;</td>
<td>&quot;</td>
<td>&quot;</td>
<td>48.8</td>
<td>58.3</td>
</tr>
</tbody>
</table>

Figure 3.10: Bilinear stress-strain law used in the model
3.2.2 Simulation of the Tensile Tests

For the simulation of the tensile tests, a prescribed loading formulation was preferred, because it was found better than a prescribed displacement one in equilibrium iterating in the plane strain model.

The time dependence of the loading had to be such as to ensure that for each time increment the respective strain increments were small enough. Thus an initial estimate of the loading that causes yielding was required in order to adjust the time increments.

So it was initially assumed that:

$$\sigma_2 = 0 \quad \text{in the plane strain state}$$

and

$$\sigma_r = \sigma_\theta = 0 \quad \text{in the axisymmetric case}$$

Thus for the plane strain case where

$$\varepsilon_3 = \frac{1}{E} (\sigma_3 - \nu (\sigma_2 + \sigma_1)) = 0$$

we get:

$$\sigma_3 = \nu(\sigma_2 + \sigma_1)$$

And assuming

$$\sigma_2 = 0$$

we get

$$\sigma_3 = \nu \sigma_1$$

Substituting in the Von Mises yield condition

$$(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2 = 2\sigma_{yp}^2$$

we get:

$$\sigma_1^2 [1 + \nu^2 - \nu] = \sigma_{yp}^2$$

Hence the loading at yield must now be
\[ \sigma_1^2 = \sigma_{yp}^2 / [1 + \nu^2 - \nu] \]

which for steel (\(\nu = 0.3\)) becomes

\[ \sigma_2^1 = \sigma_{yp}^2 / 0.79 \]

or

\[ \sigma_1 = 1.125 \sigma_{yp} \]

where \(\sigma_{yp}\) the yield stress in simple tension.

For the axisymmetric case the estimate of the load at yield simply is equal to the yield stress in simple tension.

This for the base metal and soft interlayer materials in Table 3.1, the estimates of load at yield were:

\[
\begin{align*}
P(\text{I}) &\approx 87.75 \text{ Kg/mm}^2 \quad \text{and} \\
P(\text{II}) &\approx 54.9 \text{ Kg/mm}^2 \quad \text{in plane strain and} \\
P(\text{I}) &\approx 78 \text{ Kg/mm}^2 \quad \text{and} \\
P(\text{II}) &\approx 48.8 \text{ Kg/mm}^2 \quad \text{in the axisymmetric case.}
\end{align*}
\]

The respective loading histories are given in Figure 3.11 and 3.12.

To investigate the effect of the different parameters [Length (L), plate thickness (t_o), layer thickness (H_o)] various configurations, shown in Table 3.2, were examined. Several element meshes were used and some of them are shown in Figure 3.13, 3.14, and 3.15.

To minimize the bandwidth of the resulting stiffness matrices, the numbering scheme shown in the above figures was used.
Figure 3.11 and 3.12: Applied load history for the plane strain and axisymmetric case.
Table 3.2: Dimensions of specimens modeled.

<table>
<thead>
<tr>
<th></th>
<th>Half length L/2 (mm)</th>
<th>Half plate Thickness $a_0 = t_0/2$</th>
<th>Half interlayer Thickness $h_0 = H_0/2$</th>
<th>Relative Thickness $X_t = h_0 / a_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>A1</td>
<td>200.</td>
<td>12.0</td>
<td>0.0</td>
<td>0.000</td>
</tr>
<tr>
<td>A2</td>
<td>200.</td>
<td>12.0</td>
<td>1.0</td>
<td>0.083</td>
</tr>
<tr>
<td>A3</td>
<td>200.</td>
<td>12.0</td>
<td>2.0</td>
<td>0.167</td>
</tr>
<tr>
<td>A4</td>
<td>200.</td>
<td>12.0</td>
<td>3.0</td>
<td>0.250</td>
</tr>
<tr>
<td>A5</td>
<td>200.</td>
<td>12.0</td>
<td>4.0</td>
<td>0.333</td>
</tr>
<tr>
<td>B1</td>
<td>200.</td>
<td>6.0</td>
<td>0.0</td>
<td>0.000</td>
</tr>
<tr>
<td>B2</td>
<td>200.</td>
<td>6.0</td>
<td>2.0</td>
<td>0.333</td>
</tr>
<tr>
<td>B3</td>
<td>200.</td>
<td>6.0</td>
<td>3.0</td>
<td>0.500</td>
</tr>
<tr>
<td>C1</td>
<td>200.</td>
<td>3.0</td>
<td>1.0</td>
<td>0.333</td>
</tr>
<tr>
<td>C2</td>
<td>200.</td>
<td>3.0</td>
<td>3.0</td>
<td>1.000</td>
</tr>
<tr>
<td>D1</td>
<td>100.</td>
<td>5.0</td>
<td>0.75</td>
<td>0.150</td>
</tr>
<tr>
<td>D2</td>
<td>100.</td>
<td>5.0</td>
<td>1.0</td>
<td>0.200</td>
</tr>
<tr>
<td>D3</td>
<td>100.</td>
<td>5.0</td>
<td>2.0</td>
<td>0.400</td>
</tr>
<tr>
<td>D4</td>
<td>100.</td>
<td>5.0</td>
<td>4.0</td>
<td>0.800</td>
</tr>
</tbody>
</table>
Figure 3.13: Element mesh for undermatching joint. Long specimen, plane strain case. (Only a quarter of the specimen was modeled)
Figure 3.14: Dense element mesh for undermatching joint. Short specimen. (Only a quarter of a specimen was modeled)
Figure 3.15: Other element meshes used.
In all cases, 8 node quadratic isoparametric elements were employed. Using symmetry only a quarter of the plate's cross section had to be considered. Rigid body motion was prevented by fixing the center node.

Another important characteristic of the analysis was that, in order to induce necking, we had to incorporate a geometric imperfection. This was actually realized by slightly misplacing (e.g. 0.01 mm for a specimen half thickness of 12 mm) the end node of the middle cross section, as shown in Figures 3.13 and 3.14.

The material model used does not incorporate an ultimate tensile strength value. Therefore, during loading the finite element solution will give stress states not actually possible. Therefore, the load at fracture can be approximated as the one where the maximum observed equivalent stress, in either the base plate or the interlayer, is larger or equal to the ultimate tensile strength of the material in hand.

3.2.3 Results and Conclusions

The previously outlined procedure for the calculation of ultimate tensile strength of the joint is highlighted in Figure 3.16, where the maximum observed equivalent stress is plotted versus the applied tensile load for two different values of relative thickness. From the plot, we can easily estimate that the values of the applied load are

at yield of the interlayer: $55 \text{ Kg/mm}^2$ always

at yield of the base metal: $68 \text{ Kg/mm}^2$ for $X_t = 0.2$

$74 \text{ Kg/mm}^2$ for $X_t = 0.4$
at fracture (of interlayer): \[78 \text{ Kg/mm}^2 \text{ for } X_t = 0.2\]
\[85 \text{ Kg/mm}^2 \text{ for } X_t = 0.4\]

Obviously fracture occurs first (and thus only) in the interlayer. The base metal yields at substantially higher load than the interlayer and thus confirms the assumption of the theoretical analysis that the base plate is rigid. Similar results were obtained also for the other investigated cases and some are given in Figure 3.17.

The applied load at fracture for different relative thickness \(X_t = h_o/\alpha_o\) is shown in Figure 3.18. Similarly the load at yield of both the base plate and the interlayer is shown in Figure 3.19. The results of Figure 3.18, show a very good correlation with the theoretical ones for an infinite plate obtained by Satoh and Toyoda and confirm the fact that for decreasing \(X_t (X_t < 0.5)\) the ultimate tensile strength of the joint is substantially higher than the U.T.S. of the interlayer (close to the U.T.S. of the base metal).

To estimate the yield strength of the overall joint, the applied load versus the elongation of a gauge length was plotted. The load at yield of the joint can then be approximated as the one that causes a sudden increase in the elongation. However, due to the arbitrariness of this gauge length (this is not an ordinary tensile specimen) no quantitative results are shown. The general trend, for all gauge lengths, was again increasing yield strength for decreasing \(X_t (X_t < 1)\). The relation between the applied load and the observed end displacements (gauge length equal to the specimen length) is shown in Figures 3.20 and 3.21.
Figure 3.16: Maximum observed equivalent stress versus applied tensile load. Short specimen, plane strain case.
Figure 3.17: Maximum observed equivalent stress vs applied load. Long specimen, plane strain.
Figures 3.18 and 3.19: Applied tensile load at fracture and yield.
Figure 3.20: Applied tensile load versus end displacement of the joint. Short specimen plane strain case.
Figure 3.21: Applied load versus end displacement. Long specimen.
for some of the analyzed cases. It should be noted here, however, that those curves correspond to the idealized model used and thus have no physical meaning for loads over the load at fracture.

Further analysis of the obtained results showed that indeed the assumptions or rigid base metal are more or less justifiable. This is because, for most of the specimens the transverse deformation of the skin nodes of the base metal was two orders of magnitude less than the respective of the interlayer, before the yield of the base metal, and one order of magnitude less, well after yield (close to fracture).

Also, with good approximation, the interface between the layers remained perpendicular to the center line (loading direction) (at least before the yielding of base metal). The linear dependence between the curvature and the thickness was not checked since the element mesh was not very fine.

Since most of the assumptions and results of the theoretical analysis of the idealized joints were verified by the finite element modeling it appears that the simulation of more realistic joint geometries would be easily accomplished. However, due to the lack of sufficient funds such a study was not undertaken here.
PART II

STRESS RELIEVING
CHAPTER IV

STRESS RELIEVING TREATMENTS

4.1 Residual Stresses due to Welding

The local non uniform heating and subsequent cooling, which takes place during any welding process, causes complex thermal strains and stresses to develop that finally lead to residual stresses, distortion and all their adverse consequences.

Residual stresses and distortion must be a major cause of concern to the designer since they usually are detrimental--directly or indirectly-- to the integrity and the service behavior of a welded structure. High tensile residual stresses in the region near the weld might promote brittle fracture, change the fatigue strength or aid, under suitable environmental conditions, stress corrosion cracking. Compressive residual stresses, combined with initial distortion may reduce the buckling strength of the structure whereas excessive distortion might directly prevent the structure from performing its intended task.

Three sources of welding residual stresses are usually identified in the literature, [1], [34]. One is the difference in shrinkage of differently heated and cooled areas of a welded joint. The weld metal, originally subjected to the highest temperatures, tends upon cooling to contract more than all other areas. This is hindered by the other parts of the joint, thus resulting in the formation of high longitudinal stresses in the weld metal, and equilibrating compressive stresses in the rest of the base material. The residual stress peaks often are as
high as the weld metal yield stress.

A schematic representation of the temperature and longitudinal stress changes during welding is given in Figure 4.1 adapted from [1]. Similarly residual stresses develop in the weld in the transverse direction, but are quite smaller in magnitude (Figure 4.2).

A second source of residual stresses is the uneven cooling in the thickness direction of the weld. The surface layers cool more rapidly than the interior ones, especially in thick plates. This gives rise to thermal stresses which can lead to nonuniform plastic deformations and thus to residual stresses, compressive at the surface and tensile ones in the interior.

Finally, residual stresses can arise from the phase transformations that might occur during cooling. Such transformations are accompanied by an increase in specific volume of the material being transformed. This expansion is hindered by the cooler material and thus causes residual stresses.

In analysing residual stresses various investigators have followed a number of different approaches. A brief presentation of these methodologies is given by Masubuchi and the author in [35], together with results of recent studies at M.I.T. A more extensive discussion on the subject can be found in [1], a recent book by Masubuchi. Specifically for high strength HY steels, results of analytical and experimental studies at M.I.T. are presented in [36] and [37] by Papazoglou.

The adverse effects of residual stresses and distortion can,
Figure 4.1: Schematic representation of changes of temperature and longitudinal stresses during welding.

Figure 4.2: Typical distribution of transverse residual stresses in butt welded plates.
sometimes, be kept under acceptable limits by selecting proper
design and fabrication parameters and suitable material
properties. Such design parameters include the geometry of the
structure, the plate thickness and the joint types that are used.
Fabrication parameters include the type of the welding processes
employed, the actual procedure parameters, welding sequence, etc.
As for the effect of the material properties, the designer must
be concerned with both the base and the filler metal selection,
as was already pointed out in the previous chapters.

Nevertheless, despite the precautions taken, residual
stresses and distortion do usually develop during the fabrication
of a welded structure. These can often be brought under
acceptable limits by some kind of stress relieving process. Post
weld heat treatment is frequently specified by the codes since
it can reduce the level of residual stresses and also change the
microstructure. However, the latter effect is sometimes a
disadvantage and this is why mechanical and vibrational stress
relieving methods are often also used.

The various stress relieving treatments will be presented
in the next few sections of this chapter. The underlining
mechanisms will be examined and the problems associated with
their application will be highlighted.

4.2 Thermal Methods for Stress Relieving
4.2.1 Post-Weld heat treatments in general

Heat treatment can be defined as any process whereby metals
are better adapted to desired conditions or properties in
predictably varying degrees by means of controlled heating and
cooling in their solid state without alteration of their chemical composition, [38]. A vast variety of such treatments exists each applicable to specific materials and for specific purposes. An in depth presentation of these processes is given by A.S.M. in [39]. Some necessary definitions, however, follow.

**Lower critical Temperature (for steels):** the temperature at which perlite begins to transform into austenite. Shown in the iron equilibrium diagram by the line $A_\perp$ ($A_{C1}$ for heating, $A_{R1}$ for cooling)

**Upper critical Temperature:** the temperature at which the steel becomes composed entirely of austenite. Shown as $A_3$ in the equilibrium diagram. ($A_{C3}$ for heating and $A_{R3}$ for cooling) it defines together with $A_\perp$ temperature the critical range or the transformation range for the particular alloy.

**Annealing** is the process of applying alternate heating and cooling cycles to induce softening of the metal, to alter physical or mechanical properties and / or to produce a specific microstructure. For ferrous alloys **full annealing** involves heating to just above the upper critical temperature for hypoeutectoid steels and just above the lower critical temperature for hypereutectoid ones, followed by slow furnace cooling to under $1000^\circ F$ ($537^\circ C$). This results in the softest pearlitic structure and thus to a steel with reduced hardness and tensile properties but improved ductility.

During **recrystallization annealing** the sites of high residual stress concentrations begin to rearrange themselves into new stress-free grains, at a temperature which is
determined by the purity of the metal, the grain size and the amount of cold work. During recovery annealing, which is performed at a temperature between ambient and recrystallization temperature, the residual stresses are partially relieved but the tensile strength does not decrease, as with recrystallization.

Solution annealing is the heating of a multi-phase alloy into a temperature range where only one homogeneous phase exists at equilibrium, holding at this temperature until the desired degree of homogeneity is achieved and then rapidly cooling to retain the elements in solution until they can be precipitated in the required manner.

Age or Precipitation Hardening refers to the processing of an alloy wherein precipitation of the hardening phase occurs over a period of time, at room or higher temperature, after solution annealing.

Normalizing involves heating of the steel well above the upper critical temperature $A_c^3$, followed by still-air cooling to room temperature to obtain the "normal" pearlitic structure in that steel. This treatment refines the grain size and leads to increased yield strength and better fracture resistance.

Quenching involves heating the material to a certain temperature and then subjecting it to a controlled cooling rate by immersing it in a fluid or by air blast. It is rarely applied after welding. In steels, quenching from above the upper critical temperature gives rise to microstructures of higher strength than those obtained by normalizing. However, to improve fracture toughness it is always followed by tempering.
Tempering is a treatment that involves heating of the material to a temperature below that of transformation but high enough to cause some metallurgical changes. The higher the tempering temperature, or the longer the time at that temperature, the softer and more ductile the steel gets.

4.2.2 Stress Relieving Heat Treatments

Stress relieving basically involves heating of the part to a subcritical temperature, below \( A_{c1} \), holding it at that temperature to ensure uniformity, and slow cooling to room temperature, usually in air, to prevent the reintroduction of stresses. The stress relieving temperatures usually are of the order of 1100°F to 1300°F (590°C to 700°C), where the yield strength has drastically decreased and creep occurs. The welding residual stresses can no longer be supported. Thus the stress distribution will be uniform and at a very low level. Up to \( A_{c1} \), the higher the stress-relieving temperature, the more completely the stress is removed, as shown in Figure 4.3 (adapted from [40]).

Full relief of the residual stresses can only be ensured through an annealing treatment. This, however, would be more costly and time consuming and would cause more problems due to the higher temperatures.

Stress relieving is always followed by some dimensional changes. Even warpage and distortion can result when the residual stresses are high enough. It may therefore be necessary to straighten the part and some times to stress relieve again to reduce the straigthening stresses.
Figure 4.3: Effect of stress relieving temperature in mild steel weldments.

Figure 4.4: Bandwidth of heated zone necessary for stress relief in: (A) Flat plate, (B) Cylinder, and (C) Sphere.
4.2.3 Heat Treating Ovens and Localized Heating Equipment and Procedures

The size and shape of the fabrication and the type of the material determine, in most cases, the best method for applying a stress relieving heat treatment. However, there are three main requirements that must be in general fulfilled by the heat treating method:

(a) It should be able to produce the required temperature.

(b) The temperature should be controllable within specified limits (e.g. ± 20° to 40° F for steels), (10° to 20° C).

(c) It should be possible to achieve a uniform and even heating and cooling rate throughout the thicker section to be treated. This requirement is especially important for the case of joints of complex geometry and variable thickness.

Post weld heat treatments at high temperatures can be ideally performed by placing the structure in a fixed furnace where temperature uniformity and controllability are excellent in most cases. Furnaces for stress relieving are usually of the batch type and can be heated by various methods utilizing either gas or oil flames, or electrical energy. The most recent types are of low thermal mass with insulation of ceramic fiber and mineral wool, instead of brick, which was conventionally used. Such construction reduces the erection and operation costs. High velocity gas burners give excellent temperature distribution and improved heat transfer. However, the final selection of heating mode, largely depends on fuel costs and availability.

More details on furnaces can be found in [38], [39] and [43].
However, in many instances postweld heat treatment of a complete fabrication is not possible due to the size of the structure or because heat treatment has to be applied in the field. Such cases arise when stress relieving closing welds in pressure vessels, or joints between prefabricated (and stress relieved) sections of pipework. Also repair maintenance welding might necessitate localized stress relieving.

Such a treatment can be performed in a temporary furnace erected around the structure, or by localized heating of an area around the weld zone. Appropriate insulation should always be applied. An extensive presentation of the localized heat treating methods is given in [39] by A.S.M. and in [41] by A.W.S. Some discussion on their applicability and relative advantages follows.

**Electric resistance heaters**, (Figure 4.5), direct the Joule heat, which is produced in the resistance elements, to the part by proper placement and insulation. The four commonly used heater types are: finger element heaters, braided heaters, flexible ceramic pads, and wrap-around heaters. The achieved temperature can be adjusted quickly and easily and can be maintained even through a welding operation. Uneven heat input can be obtained if required. However, the elements have a relatively short life and may short circuit with the part.

In **induction heating**, (Figure 4.5), alternating current is applied to coils wrapped around the parts to be heated and thus induces magnetic fields and currents inside the part. The low mains frequency (50 or 60 Hz) is used for heaters of power
(A) RESISTANCE HEATING

(B) INDUCTION HEATING

Figure 4.5: Localized heat treating equipment.
up to about 25 KVA whereas medium frequency (1000 to 10,000 Hz) is used for powers between 20 and 400 KVA. The coils have a long life and the achieved temperature is very uniform and can be controlled within a very accurate range. However, the initial cost is high, the portability of equipment low and uneven heat input is difficult to achieve. Furthermore the heater has to be turned off during welding. More details about the process are given in [42] by Müller.

Manual flame heating by gas torches is convenient low cost method particularly suited for field work. However, minimal precision and repeatability can be achieved and if not performed by a very experienced operator it is likely to damage the weldment.

Exothermic heating employs a consumable heat source. Such a process is the thermite reaction between Fe$_2$O$_3$ and Al. Exothermic packages that can produce the required holding temperatures are marketed. No capital investment cost or operator during heating is required and the equipment is very portable. However, there is no possibility for adjustments after ignition and limited flexibility in meeting code requirements regarding heating and cooling rates and holding time.

Finally gas flame generated infrared heating and radiant heating by quartz lamps utilize radiation as the principle mechanism for heat transfer. The former method uses relatively economical fuel and can be readily controlled. The latter has an extremely fast response time ($4000^\circ$ F in one second) and fast cool down due to minimal thermal mass and large efficiency.
No combustion takes place and no heat is wasted. However, the cost is high and a separate "furnace" has to be fabricated for each different part configuration.

4.2.4 Requirements and Specifications for Localized Heat Treatments

For local stress relieving heat treatments it is absolutely necessary to ensure that the temperature distribution during the heat treatment does not induce new thermal stresses which exceed the material yield stress and can lead to the development of new residual stresses on cooling. This imposes strict requirements on the level and uniformity of temperature, on the heating and cooling rates, and width of the heating zone. The latter largely determines the existing temperature gradient through the thickness (between the heated and unheated surfaces). Most of these requirements are usually specified by the applicable codes (e.g. ASME Pressure Vessel Codes, Section VIII).

For butt welded plates it was experimentally proven by Cotterell in [44], that satisfactory relief of residual stresses can be expected if uniform heat input is applied over a band-width of twice the length of the weld, as shown in Figure 4.4(A).

For circumferential welds in cylinders and pipes of diameter $R$ and thickness $t$, it was shown by Burdekin in [45], that relief of residual stresses can be achieved if uniform heat input is applied over a circumferential band width of $5\sqrt{Rt}$ (Figure 4.4(B)). This is also what BS 1515 and 5500 (British Standards) suggest. However Shifrin and Rich, in [46], had clearly shown that satisfactory through thickness gradients
can be achieved with a minimum heated band with of five times the wall thickness, \(5t\), irrespective of the technique used for heating (resistance or induction). ASME codes on the other hand require that "the width of the heated band on each side of the greatest width of the finished weld shall be not less than two times the weld metal thickness". This again results in a bandwidth of approximately \(5t\) which is generally much smaller than what specified by the British standards. Further both standards require that temperature gradients beyond the heated zone should be not harmful, although no clear definition of "harmful" is given. British codes also suggest that the full heat treatment temperature range be achieved for a distance of \(3t\) on each side of the weld seam and a minimum of half the soak temperature be achieved at the edges of heated zone.

For welded spherical vessels it has been shown theoretically by Cotterel, [47], that local stress relief heat treatment is possible by slowly moving a heated spot (cap) of diameter \(5\sqrt{Rt}\) or by heating a circumferential band of the same width.(Figure 4.4(C)).

For complex junctions of branches in pipes or pressure vessels it is necessary to ensure that the heating of a weld will not induce substantial thermal gradients around the junction. Therefore exact thermal stress analysis might be needed and additional background heating of the vessel as a whole might be required.
4.3 Effects of Stress Relieving Heat Treatments

4.3.1 Effect of Treatment on the Mechanical Properties

Stress relieving heat treatments are usually very effective in reducing the high residual stresses present in a weldment. However, they also have an effect on the microstructure and the properties of both the base plate and the weld metal since they are carried out at relatively high temperatures. These effects vary with the material under consideration.

During the last decade, the desirability of post weld heat treatments and their effects were extensively investigated by the Working Group on Thermal Stress Relief of the Commission X of the International Institute of Welding. Information from the series of documents that were produced, (References [48] to [51]) is presented in this section.

Specifically for non work-hardened base metal of C-Mn and microalloyed steels, it was generally concluded that tensile properties are impaired to a significant extent, especially at higher temperatures. Resistance to brittle fracture is affected but not drastically. Temperature is a more important factor than soaking time. For low alloy and creep resistant steels, which are used in a normalized and tempered or quenched and tempered condition, the effect of stress relieving treatments on the properties will depend on the temperature. The effect will be minimal if the treatment is carried out at a temperature lower than that of initial tempering. Additional tempering will result however, if higher temperature treatment is performed. This is usually beneficial for the resistance to brittle fracture
but detrimental for other properties, such as creep resistance, and usually is not recomended by the codes.

For the case of work hardened base material, on the other hand, the heat treatments usually restore the base properties and prevent strain aging and are therefore beneficial.

The effect of stress relieving heat treatments on the properties of the heat affected zone (H.A.Z.) will not only depend on the type of steel but also on the microstructural state of the H.A.Z. Therefore welding procedure and conditions, heat input, plate thickness and distance to the fusion line are important parameters. For C-Mn steels the heat treatment will in general soften the H.A.Z. structures, except if carbide precipitation occurs. The yield strength of the H.A.Z. will usually be higher than that of the base material after the same treatment. Heat treatments are in most cases also beneficial to the resistance to brittle fracture for these steels. For low alloy steels the main problem is to retain satisfactory toughness in the H.A.Z. The effect of treatment is usually strongly dependent on the type of steel and the exact metallurgical changes associated with the welding and heat treating tempering. The problem of stress relief cracking that is known to occur in the H.A.Z. of some steels will be examined in detail in the next section.

Although not much work has been done on the effect of heat treatments on the properties of weld metal, it appears that tensile properties diminish considerably on tempering. Again temperature seems to be more important than the soaking time.
As in the base metal, embrittlement can occur and some instances of stress relief cracking have been reported.

4.3.2 Stress-Relief Cracking

Stress-relief cracking is defined as "intergranular cracking in the heat affected zone or weld metal that occurs during the exposure of welded assemblies to elevated temperatures produced by post weld heat treatments or high-temperature service", [52]. It has also been referred to in the literature as "post-weld-heat cracking" or "reheat cracking" and in general is caused when some relief of stresses by creep occurs. This form of cracking became a problem with the austenitic stainless steels in the 1950's and with the low-alloy constructional steels in the 1960's. It also occurs to ferritic creep-resisting steels and nickel base alloys and is generally related to precipitation hardening. Non-precipitation hardening materials such as plain carbon steels and certain nickel alloys are not susceptible to reheat cracking.

The cracks can be positively identified by metallographic examination due to their characteristic branching intergranular morphology, along the coarse-grain region of the heat-affected zone, [53]. Cracking usually occurs at high temperature when creep ductility is insufficient to accommodate the strains required for the relief of applied or residual stresses. When residual stresses are high, as in thicker and restrained sections reheat cracking is most likely to occur.

Stress relief treatments are required for almost all pressure vessels and piping systems fabricated today, and this
is why stress-relief cracking caused considerable concern. Extensive investigations of the cracking mechanism and of possible remedies have been undertaken all over the world, and are in detail reviewed by Meitzner in [53] and Dhooge, et al., in [54]. In 1970 I.I.W. established, in Commission X, a Working Group on "Reheat Cracking" to collect and assimilate information on the subject; (References [55] to [60]).

In an effort to develop a simple, reliable specimen that includes all the pertinent variables related to cracking, such as high stresses, triaxiality, thermal history and microstructure, a large number of tests are used today. These tests are extensively reviewed by Dhooge, et al. in [54] and are either:

(a) Tests on complete weldments.
(b) Tests on specimens containing a weld.
(c) Tests on specimens containing a thermally simulated H.A.Z.

In (a) and (b) weld/H.A.Z. thermal cycles and microstructures are produced by an actual welding operation, whereas in (c) the H.A.Z. microstructure is created by subjecting base metal coupons to a simulated H.A.Z. thermal history, as in a Gleeble. In both cases the specimens are then reheated and tested at temperatures typical of stress relief treatments.

Tests have shown that there is definite influence of chemical composition on stress relief cracking susceptibility. Japanese researchers have used regression analysis to derive two predictive formulae. [61],[62].

(a) \( \Delta G = Cr \% + 3.3 \text{ Mo } \% + 8.1 \text{ V } \% - 2.0 \) (by Nakamura, et al.) and
(b) \( P_{SR} = \text{Cr} \% + \text{Cu} \% + 2.\text{Mo} \% + 10 \text{V} \% + 7 \text{Nb} \% + 5\text{Ti} \% - 2 \)

(by Ito and Nakanishi)

Positive values of the \( \Delta G \) or \( P_{SR} \) parameters would indicate susceptibility to stress relief cracking. However, other investigators have shown that such formulae were not general enough to be used conclusively, [63].

It has also been established by these studies that martensitic or lower bainitic microstructures are more prone to S.R. cracking than the upper bainitic or ferritic-perlitic ones. Thus risk of cracking can be reduced by the choice of welding heat inputs or preheat levels ensuring lower cooling rates in the H.A.Z. The same holds for proper choice of consumables and the use of a temper bead in the last welding pass.

4.3.3 Stress Relieving of HY-130 Steels

The very high residual stresses that develop during welding of HY-130* steels make stress relief treatments very attractive. Substantial reduction of stresses was shown to occur, in both the base and weld metal at temperatures between 950°F (510°C) and 1050°F (566°C). This is evidenced in Figure 4.6 and 4.7, adapted from [64]. However, a severe degradation of notch toughness may also occur at these temperatures. This embrittlement, which phenomenologically is similar to temper embrittlement, is believed to be influenced by the soaking

*Also referred to as 5 Ni-Cr-Mo-V, HY-130(\(\tau\)), HY-130/150 in different stages of its development. (Appendix A).
Figure 4.6: Estimated residual stress after stress relief.

Figure 4.7: Comparison of relaxation properties.
temperature, the time at temperature, the cooling rates, the plastic strain, the exact alloy composition and prior heat treatment.

Extensive investigation on the subject was performed by the U.S. steel Corporation and the U.S. Navy (N.S.R.D.C. Materials Laboratory). Results by Rosenstein, [65], indicated that stress relief cycles are cumulative in nature, by comparing the tensile and Charpy properties resulting from consequent heat treatments of short duration and one single treatment of long duration, both having the same soaking times.

Furthermore it was observed that the degradation of notch toughness was maximum when stress relieving at 950° F (510° C) and minimum when at 1050° F (566° C), as evidenced in Figure 4.8. However, investigations of the effects of stress relieving on the weld metal, [64], [65], have shown that the higher temperatures may substantially reduce the yield and tensile strength of the weld metal and thus may cause undermatching. Therefore the most practical temperature range for effective stress relieving of HY-130 weldments is between 1005° F (538° C) and 1025° F (552° C).

Additionally, it was established that the degradation of toughness at 950° F (510° C) occurs during both soaking and cooling. The isothermal degradation was shown to be directly dependent upon time at temperature. The degradation during cooling, on the other hand, is inversely related to the cooling rate and does not depend on the soaking time. Therefore, cooling embrittlement constitutes the major portion of
Figure 4.8: Effect of stress-relief temperature on toughness of HY-130 steels.
degradation after short times at temperature and only a minor one after long soaking periods.

It was also shown that stress relief at the tempering temperature of 1050°F (566°C) results in softening at temperature (accompanied by increased toughness) and embrittlement on cooling. Therefore, the resulting properties will depend both on the cooling rate and the soaking time.

The toughness degradation due to stress-relief can in general be recovered by retempering to a lower strength level. It should also be finally noted that the fundamental difference between stress relief embrittlement and temper embrittlement is the presence of strain due to creep at high temperature or due to previous plastic deformation, [66].
4.3.4 Stress Relieving of Austenitic Stainless Steels

Austenitic stainless steels have to be heated to about 1650° F (900° C) to attain adequate stress relief because of their good creep resistance. Only partial stress relief can be attained at temperatures lower than 1600° F (870° C). Best stress-relieving results can be achieved by slow cooling. Quenching or rapid cooling in general reintroduce high residual stresses.

Additionally, an optimal stress relieving temperature is usually difficult to select, since the heat treatments that would provide adequate stress relief can be detrimental for the corrosion resistance and the ones that are not harmful to corrosion resistance may not provide adequate stress relief.

The major metallurgical effects of a stress relieving treatment are:

(a) When heating between 900 and 1500° F (480 to 815° C), chromium carbides might precipitate in the grain boundaries of wholly austenitic unstabilized grades and can promote intergranular corrosion.

(b) When heating between 1000 and 1700° F (540 to 925° C), hard sigma phase may result decreasing both corrosion resistance and ductility.

(c) When slow cooling the above nondesirable effects have more time to take place.

(d) When heating between 1500° F and 1700° F (815 to 925° C), improvements in the corrosion resistance and mechanical properties can result due to coalescence of chromium carbide
precipitates or sigma phase.

(e) Heating above the annealing temperature at 1750 to 2050° F (955 to 1120° C) fully softens the steel and causes all the grain-boundary precipitates to redisolve.

In the final selection of a proper stress relieving treatment due consideration must be given not only to the material itself, however, but also to the fabrication parameters and to the operating environment. Table 4.1, by A.S.M., adapted from [39], summarises the suggested stress-relieving treatments for various applications and environments.
Table 4.1 Stress-relieving treatments for austenitic stainless steels

<table>
<thead>
<tr>
<th>Application or desired characteristics</th>
<th>Extra-low-carbon grades, such as 304L and 316L</th>
<th>Suggested thermal treatment (a)</th>
<th>Stabilized grades, such as 318, 321 and 347</th>
<th>Unstabilized grades, such as 304 and 316</th>
</tr>
</thead>
<tbody>
<tr>
<td>Severe stress corrosion ..................</td>
<td>A,B</td>
<td>B,A</td>
<td>(b)</td>
<td></td>
</tr>
<tr>
<td>Moderate stress corrosion ..............</td>
<td>A,B,C</td>
<td>B,A,C,F</td>
<td>C(b)</td>
<td></td>
</tr>
<tr>
<td>Mild stress corrosion ...................</td>
<td>A,B,C,E,F</td>
<td>B,A,C,E,F</td>
<td>C,F</td>
<td></td>
</tr>
<tr>
<td>Remove peak stresses only .............</td>
<td>F</td>
<td>F</td>
<td>F</td>
<td></td>
</tr>
<tr>
<td>No stress corrosion .....................</td>
<td>None required</td>
<td>None required</td>
<td>None required</td>
<td></td>
</tr>
<tr>
<td>Intergranular corrosion ...............</td>
<td>A,C(C)</td>
<td>A,C,B(C)</td>
<td>C</td>
<td></td>
</tr>
<tr>
<td>Stress relief after severe forming ......</td>
<td>A,C</td>
<td>A,C</td>
<td>C</td>
<td></td>
</tr>
<tr>
<td>Relief between forming operations ......</td>
<td>A,B,C</td>
<td>B,A,C</td>
<td>C(d)</td>
<td></td>
</tr>
<tr>
<td>Structural soundness(e) ...............</td>
<td>A,C,B</td>
<td>A,C,B</td>
<td>C</td>
<td></td>
</tr>
<tr>
<td>Dimensional stability ..................</td>
<td>G</td>
<td>G</td>
<td>G</td>
<td></td>
</tr>
</tbody>
</table>

(a) Thermal treatments are listed in order of decreasing preference.
A : anneal at 1950 to 2050°F (1065 to 1120°C), slow cool.
B : stress relieve at 1650°F (900°C), slow cool.
C : anneal at 1950 to 2050°F (1065 to 1120°C), quench(f) or cool rapidly.
D : stress relieve at 1650°F (900°C), quench or cool rapidly.
E : stress relieve at 900 to 1200°F (480 to 650°C), slow cool.
F : stress relieve at below 900°F (480°C), slow cool.
G : stress relieve at 400 to 900°F (205 to 480°C), slow cool (usual time, 4h per inch of section).

(b) To allow the optimum stress-relieving treatment, the use of stabilized or extra-low-carbon grades is recommended.

(c) In most instances, no heat treatment is required, but where fabrication procedures may have sensitized the stainless steel the heat treatments noted may be employed.

(d) Treatment A,B or D also may be used, if followed by treatment C when forming is completed.

(e) Where severe fabricating stresses coupled with high service loading may cause cracking. Also, after welding heavy sections.
4.4 Alternative Methods of Stress Relieving

4.4.1 Mechanical Overstressing

When any of the undesirable metallurgical effects of thermal treatments cannot be tolerated, it is possible to stress relieve by mechanical means. Specifically, referring to Figure 4.9, adapted from [1], if tensile loading is applied parallel to the weld line, yielding will be caused in the highly stressed weld metal. The adjacent plate material, however, will be stressed further into tension, as depicted by curve (1). Further increase in loading will even out the stress distribution across the plate, as in curve (2). If the applied load increases further, yielding will take place across the entire cross-section, as in curve (3). If the plate is then unloaded the remaining stress will be very low and more or less uniform (curve 3'). So overloading of the structure can greatly reduce the level of residual stress peaks.

In addition, if any cracks or defects exist, the first application of loading will cause localized yielding at their tips. Subsequent unloading will produce a pattern of compressive residual stresses around these defects and other points of stress concentration.

Therefore after the first successful overstressing of a structure appreciable assurance is usually provided against both brittle fracture and fatigue fracture, [1], [40]. Obviously, however, no improvement of the basic fracture toughness of the HAZ microstructures is to be expected through mechanical treatments. This should be contrasted with the metallurgical
Curve 0: Residual stresses in the as welded condition
Curve 1: Stress distribution at $\sigma = \sigma_1$
Curve 2: $\sigma = \sigma_2, (\sigma_2 > \sigma_1)$
Curve 3: $\sigma = \sigma_3, (\sigma_3 > \sigma_2)$
Curve 4: Residual stresses after $\sigma = \sigma_1$ is applied and then released
Curve 5: $\sigma = \sigma_2$
Curve 6: $\sigma = \sigma_3$

Figure 4.9: Schematic distributions of stresses in a butt weld when uniform tensile loads are applied and of residual stresses after the loads are released.
benefits often resulting from thermal stress relieving treatments.

4.4.2 Vibratory Stress Relief (V.S.R.)

It has been reported by various investigators that reductions of residual stresses occurred and dimensional stability during subsequent machining was ensured when a welded structure was vibrated. The technique, employing eccentric weight type vibrators attached to various positions on the structure, is currently in industrial use with some degree of success. However, evidence on the capability of the method to effectively and repeatedly relieve residual stresses is rather contradictory at this point. It still remains much to be understood as to how and even whether the method actually works. Numerous studies have been undertaken in this direction during the past forty years. A survey of most of the published results appears in [68] by Dawson and Moffat and in [69] by Brogden.

Early work by Mc Goldrick and Saunders, [70], postulated that occurrence of plasticity at some time during the treatment was required for successful stress relief, and that in order to achieve the necessary amplitudes, the structure should be vibrated at a frequency very close to resonance. Relief of residual stresses at this time was inferred from the reduction of warpage. Buhler and Pfalzgraf, in the early 1960's, were among the first to attempt to directly measure residual stresses after vibratory treatment, [71]. Their results were not encouraging however, because they restricted the applied cyclic stresses below the fatigue limit of the materials used. The same
concern for fatigue damage was shared by other investigators as well. Nevertheless, more recent studies concluded that for any stress relief to occur during vibration, the fatigue limit of the material has to be exceeded and fatigue damage must therefore occur, although this is likely to be small, [68].

Substantial relief of residual stresses by vibration was reported in latter investigations. Specifically Sagalevich and Meister claimed 50% reduction in welding deformations of wagon bodies [68] and Zubchencho and Gruzd reported 67% decrease in residual stress peaks in welded truck frames [72]. In 1968 Wozney and Crawmer reported in [73] a 33% reduction of residual stresses by cyclic bending of residually stressed Almen strips. Furthermore they used a derived cyclic stress strain curve of the material (similar to the one in Figure 4.10) to predict successfully the residual stress reduction. In 1972 Weiss, et al. reported in [74] a substantial reduction of residual stresses in plain carbon steel weldments vibrated on a laboratory shaker.

In an effort to analyze the mechanism of the reduction of residual stresses, Kazimirov et al. presented in [75] a rigorous derivation of the stresses and strains caused in a flat plate by a pulsating load and their interaction with existing residual stresses. Makhnenko and Pivtorak used a finite difference approach to show that the presence of residual stresses did not affect the condition of resonance in a beam. Additionally they proved that the vibration amplitude would be decreased when high residual stresses exist in the structure and concluded that in order to appreciably redistribute residual stresses, it would
Figure 4.10: Monotonic and cyclic stress-strain curves for SAE 4340 steel. Data points represent tips of stable hysteresis loops.
be necessary to apply cyclic additional strains at least of the same order of magnitude as the residual strains themselves. Experimental investigations by Mryka led to similar conclusions, [77]. In 1979, Sagalevich, et al., showed by energy methods that it is possible to completely eliminate the residual stresses in welded beams by a rational combination of static and vibrational loading. Satisfactory agreement between calculated data and experimental results was observed,[78].

Despite these encouraging studies, however, other extensive investigations, such as one completed at Battelle Memorial Institute by Cheever gave rather inconclusive final results [79]. Additionally there is no clear consensus in the literature regarding the exact mechanism of the stress reduction. Further examination of vibratory stress relief treatments was considered to be outside the scope of this study. The next chapters will only deal with thermal stress relief treatments.

4.4.3 Explosive Stress Relieving

The impact from explosive contact charges was shown to be capable to redistribute (rather than to relieve) welding residual stresses. Important parameters in such a treatment are the intensity and distribution of the explosive load. Some results on the optimal selection of these parameters are given in [80]. However, since the method is rarely used it will not be examined any further in this study.

4.5 Fabrication Techniques to Reduce Residual Stresses and to Eliminate Postweld Treatments

There are cases in welding fabrication, where even a
modification of the residual stress patterns is beneficial. For example, it is the tensile residual stresses usually developed in the inner surface of welded stainless steel pipes that promote intergranular stress corrosion cracking in boiling water reactor installations. The various fabrication methods that were developed to solve this problem are effective exactly because they limit and change these tensile stresses to compressive, [81].

Specifically in the heat sink welding technique the first two welding passes are made conventionally with an inert gas back purge. Then the inside of the pipe is cooled with flowing or stagnant water or water spray while the remaining weld passes are completed. Since the inside surface is kept relatively cool during most of the welding passes the circumferential shrinkage is less than with a conventional weld. In addition, when the outer weld layers shrink axially while cooling, they tend to induce compressive axial stresses on the already cool inside surface. Results of investigations in G.E. by Chrenko appear in Figure 4.11. The axial residual stress patterns on the inside diameter of 304 stainless steel pipes were measured by X-ray diffraction for conventional and heat sink welding, [82].

Beneficial compressive stresses can also be induced in the inner surface of pipes that have been already welded. One method developed by I.H.I. in Japan the induction heating stress improvement (I.H.S.I.). In this case the interior of the pipe is cooled with water, while heat is applied to the outside near the weld. The resulting temperature gradient (between 400°C
and 500°C causes yield in compression at the outside surface and in tension at the inside. When the outside heating is removed compressive stresses are induced on the interior. Some experimental results appear in Figure 4.12 again adapted from [82].

Another technique that is usually employed in order to avoid postweld heat treatment is buttering. The joint preparations are first buttered, inspected, heat treated and remachined before final butt welding. Detailed description of the technique is given by Lochhead in [83].
Figure 4.11: Axial residual stresses at the inner surface of a 10 in. dia. schedule 80 type 304 stainless steel pipe for both conventional and heat sink welding. (adapted from [82])

Figure 4.12: Axial residual stresses at the inner surface of a 16 in. dia. schedule 80 type 304 stainless steel pipe for conventional welding and subsequent induction heating stress improvement, (IHSI). (adapted from [82])

Note: Residual stresses were measured by X-ray diffraction.
CHAPTER V

ANALYSIS OF RESIDUAL STRESS RELAXATION DUE TO HEAT TREATMENTS

5.1 General Considerations

Stress relieving heat treatments are usually applied in order to reduce residual stresses and to induce metallurgical benefits. The metallurgical changes, positive or negative, have been briefly dealt with in the previous chapter, and are not the main concern of this study. The reduction of residual stresses, however, will be further examined now.

Residual stress changes can arise during all three stages of a heat treatment. Specifically, referring to Figure 5.1, during the heating part of the process residual stresses decrease due to the temperature dependance of the mechanical properties, mainly through a reduction of the yield strength with temperature. During the holding (or soaking) period the temperature is kept constant and residual stresses are reduced due to creep. Experimental evidence suggests that the major portion of this stress reduction occurs in the first part of this stage. This is clearly shown in Figure 5.2 depicting load versus time for constant-strain relaxation tests performed on HY-130 steel, and matching weld metals, by N.S.R.D.C., [64]. Finally, during the cool-down period, residual stresses increase due to the temperature dependence of the mechanical properties, but hopefully (when the treatment is successful) not to their initial levels.

To judge the effectiveness of a stress relief treatment, with regards to the accomplished reduction of residual stresses,
Figure 5.1: Stress relieving temperature history

Figure 5.2: Load vs. time from constant-strain relaxation tests on HY-130 steels and matching weld metals
it should be necessary to measure the maximum residual stresses before and after the treatment. The difference would be a realistic measure of performance. An acceptable alternative, however, to the time-consuming, costly and usually destructive residual stress measurements, would be a proper analytical model. Furthermore, such a model would be very helpful in determining an optimal lower temperature heat treatment, where a properly selected heating pattern would most effectively reduce stresses, while keeping the metallurgical changes minimal.

In that direction, various approaches have been followed in the literature by several investigators. Very simple uniform residual stress distributions are usually assumed for the weld metal, so that the unidimensional stress-strain curves can be directly employed. Such analytical results are obtained and experimentally verified by Tanaka in [84] and [85]. For more complex cases and two-or three-dimensional stress states numerical models have been proposed to handle the thermal-elastic-plastic and creep analysis required. Ueda and Fukuda present in [86],[87] a finite element model capable of calculating welding residual stresses and stress relief due to creep. Fujita, et al., develop in [88] a thermo-visco-elastic-plastic model to study the mechanism of stress relief annealing. In [89] finally, Cameron and Pembretton present a numerical model of the thermal stress relief in thin shells of revolution.

For the purposes of this study, it was decided that the analysis of the thermal stress relieving operation be accomplished using an one-dimensional model, similar to that
successfully employed in the past at M.I.T. for the prediction of residual stresses in long, thin, butt or edge welded plates, [90],[1]. The program was modified so as to calculate residual stresses, not only after welding, but also after any specified heat treatment. These modifications will be presented in the next few sections of this chapter.

5.2 The One-Dimensional Model

5.2.1 Assumptions

The fundamental assumptions incorporated in this model are:

(a) The plate is infinite and very thin (Refering to Figure 5.3, L → ∞, h → 0)

(b) The welding arc is modeled as a line heat source and there is no temperature gradient through the thickness of the plate. (Two-dimensional temperature distribution).

(c) Furthermore the temperature distribution is stationary if viewed from a system moving with the heat source. (Quasi stationary state [1]).

(d) Stress is non-zero only in the direction parallel to the weld centerline. (One-dimensional stress distribution).

(e) These stresses are a function of the transverse distance from the weld centerline only.

Additional assumptions for the analysis of thermal stress relief treatment were made as follows:

(f) Any arbitrary temperature distribution and history would be input to the modified program. However, for the purposes of this study uniform temperature distribution was assumed over the entire plate, changing with time as in Figure 5.1.
Figure 5.3: Weldment configuration (Butt welding of plates)

Figure 5.4: Thin infinite strip with temperature distribution across the width
(g) Due to the relatively fast heating and cooling rates it was assumed that no creep occurs during these periods.

(h) During the holding or soaking period at the stress relieving temperature, residual stresses can only decrease due to creep. In other words, if creep is not included in the model, no change in stresses will take place during this period.

5.2.2 Temperature Distribution

During welding the non uniform and changing with time temperature distribution is estimated in the one dimensional program by the well-known Rosenthal solution. Specifically, as proved in [91], the exact solution for a line heat source moving along an infinite plate is:

\[
\theta - \Theta = \frac{Q}{2\pi \lambda h} \cdot e^{-\frac{V}{2k}} \xi \cdot K_0 \left( \frac{VR}{2k} \right)
\]  

(5-1)

where:

- \( \theta \) = Temperature at point \((x,y)\) at time \(t\)
- \( \Theta \) = Initial temperature
- \( h \) = Plate thickness
- \( \lambda \) = Thermal conductivity
- \( k \) = Thermal diffusivity \((k = \frac{\lambda}{\rho c_p})\)
- \( \rho \) = Density
- \( c_p \) = Specific heat
- \( Q \) = Total heat input
- \( v \) = Welding speed
- \( K_0(x) \) = Modified Bessel function of second kind and zero order

The moving coordinates \( \xi \) and \( r \) are:
\[ \xi = x - vt \]  
\[ r = (\xi^2 + y^2)^{1/2} \]  

The total heat input, \( Q \), is \[ Q = V.I.n_a \]

where: \( V \) = Arc voltage  
I = Arc current  
\( n_a \) = Arc efficiency

During heat treatment in a furnace the temperature distribution can be assumed to be uniform along the entire plate.

For the case of a localized treatment, however, by flame heating for example the temperature distribution can be calculated modifying the solution for a point heat source moving on an semi-infinite body. Specifically the point heat source (three-dimensional) solution is: ([91],[1]).

\[ \theta - \theta_o = \frac{Q}{2\pi \lambda} \cdot e^{-\frac{v}{2k} \xi} \cdot e^\frac{-\frac{V}{2k} R}{R} \]  

where: \[ R = (\xi^2 + y^2 + z^2)^{1/2} \]

and all the other variables same as in (5-1).

The boundary conditions that have to be satisfied on the surfaces of a finite plate are:

\[ \frac{\partial \theta}{\partial n} = 0 \]

where: \( n \) the normal to the surface.

Therefore the solution has to be modified including infinite
series of images of the heat source with respect to the boundaries as depicted in Figure 5.5, adapted from [128].

The Rosenthal solutions, two- or three-dimensional, assume that the material is isotropic \((\lambda_x = \lambda_y)\) and that properties are independent of temperature. The latter assumption is by no means realistic for the welding or heat treating temperatures and an iterative scheme, described in section 5.4, has to be incorporated in the model to account for that.

Finally, it should be noted that equation 5.1 was also modified to account for heat losses due to radiation and convection from the surfaces of the plate, becoming:

\[
\theta - \theta_0 = \frac{Q}{2\pi \lambda h} \cdot e^{-\frac{V}{2\kappa}} \cdot K_0 \left( r \sqrt{\left(\frac{v}{2\kappa}\right)^2 + \frac{H}{\lambda T}} \right) \tag{5-7a}
\]

where \(H = \text{Average surface heat loss coefficient}\).

Furthermore, for a plate of finite breadth \(c\), equation (5-1) has to be modified using an infinite number of images of the heat source. Thus it becomes in general:

\[
\theta - \theta_0 = \frac{Q}{2\pi \lambda h} \cdot e^{-\frac{V}{2\kappa}} \cdot \xi^+ \sum_{-\infty}^{\infty} K_0 \left( r_m \sqrt{\left(\frac{v}{2\kappa}\right)^2 + \frac{H}{\lambda T}} \right) \tag{5-7b}
\]

where \(r_m = (\xi^2 + (\gamma + 2mc)^2)^{1/2}\) for the \(m\)th image source.
Figure 5.5: Arrangement of heat source images for a finite width and thickness plate.
5.2.3 **Stress Analysis**

To calculate the transient and residual stresses during and after welding and subsequent heat treatments the method of successive elastic solutions is employed. The procedure, outlined by Mentelson in [92], was first used in the solution of welding problems by Tall, [93],[94], and later by Masubuchi, [95].

To analyse the stress state at the center cross section of the plate (Figure 5.3) due to an arbitrary, and changing with time, temperature distribution, \( \theta(y,t) \), it is assumed that at time \( t \) the section is a part of an infinitely long plate subject to the same temperature distribution over its entire length, as in Figure 5.4. This temperature profile will remain the same during the current time increment, \( \Delta t \).

The only non-zero stress and strain are assumed to be \( \sigma_x = \sigma_x(y) \) and \( \varepsilon_x = \varepsilon_x(y) \).

Compatibility equations for one dimension reduce to,

\[
\frac{d^2 \varepsilon_x}{dy^2} = 0 \tag{5-3}
\]

or

\[
\varepsilon_x = c_1 + c_2 y \tag{5-9}
\]

where : \( c_1 \) and \( c_2 \) are constants to be determined. The above equation essentially states that plane sections will always remain plane.

Considering an incremental approach, at the end of a time interval \( \Delta t \) the following will hold along the cross section.

\[
\varepsilon_x = \frac{\sigma_x}{E} + \alpha \cdot \Delta \theta + \varepsilon_x^{in} + \Delta \varepsilon_x^{in} \tag{5-10}
\]
or
\[ \sigma_x = E(\varepsilon_x - \alpha \Delta \theta - \varepsilon_{x}^{\text{in}} - \Delta \varepsilon_{x}^{\text{in}}) \]  

(5-11)

where:
- \( \sigma_x / E \) = Elastic part of strain, \( \varepsilon_x^{\text{el}} \)
- \( \alpha \Delta \theta \) = Thermal strain, \( \varepsilon_x^{\text{th}} \)
- \( \Delta \theta = \theta - \theta_0 \)
- \( \varepsilon_{x}^{\text{in}} \) = Accumulated (during the previous time increments) inelastic strain = \( \varepsilon_{x}^{\text{pl}} + \varepsilon_{x}^{c} \)
- \( \varepsilon_{x}^{\text{pl}} \) = Plastic strain
- \( \varepsilon_{x}^{c} \) = Creep strain
- \( \Delta \varepsilon_{x}^{\text{in}} \) = Change in inelastic strain during the time increment \( \Delta t \)

From global equilibrium (no external forces and moments acting on the plate).

\[ \int_{-c}^{+c} \sigma_x \, dy = 0 \]  

(5-12a)

\[ \int_{-c}^{+c} \sigma_x \, y \, dy = 0 \]  

(5-12b)

Substituting Eqns. (5.9) and (5.11) into (5.12), a set of linear equations is obtained for the determination of the unknown coefficients \( c_1 \) and \( c_2 \). Solving this system and substituting back into Eqn. (5.9) the following expression is obtained for the total strain:

\[ \varepsilon_x(y) = (A_1 - yA_2) \int_{-c}^{+c} E(\alpha \Delta \theta + \varepsilon_x^{\text{in}} + \Delta \varepsilon_x^{\text{in}}) \, dy \]

\[ - (A_2 - yA_3) \int_{-c}^{+c} E(\alpha \Delta \theta + \varepsilon_x^{\text{in}} + \Delta \varepsilon_x^{\text{in}}) y \, dy \]  

(5-13)
where:

\[ A_1 = \left[ \int_{-c}^{+c} E y^2 \, dy \right] / B \]

\[ A_2 = \left[ \int_{-c}^{+c} E y \, dy \right] / B \]

\[ A_3 = \left[ \int_{-c}^{+c} E \, dy \right] / B \]

(5-14)

and

\[ B = \left[ \int_{-c}^{+c} E \, dy \right] \cdot \left[ \int_{-c}^{+c} E y^2 \, dy \right] - \left[ \int_{-c}^{+c} E y \, dy \right]^2 \]

Equations (5.13) and (5.14) are not enough to solve the problem. What is still needed is a stress-strain law and a relation between stress and creep strain increments.

To proceed further the assumption was made that creep will only take place during the soaking stage of the temperature history. Thus during this period the accumulated plastic strain, \( \varepsilon_{pl} \), will remain constant. The heating and cooling stages where no creep occurs, are treated in exactly the same way as the welding problem.

5.2.4 The Method of Successive Elastic Solutions

(A) During welding, when creep does not occur:

\[ \varepsilon_{in}(y) = \varepsilon_{pl}(y) \]

(5-15)

At each time step the total strain is first calculated along the cross section from (5-13) assuming that no plastic strain exists.
\[ \varepsilon_{x}^{pl}(y) = 0 \]  

(5-16)

The mechanical strain, \( \varepsilon^{m} \), then is:

\[ \varepsilon^{m}(y) = \varepsilon_{x}(y) - \varepsilon^{th}(y) = \varepsilon_{x}(y) - \alpha \cdot \Delta \theta(y) \]  

(5-17)

Now assuming a bilinear stress-strain law, a first approximation of the plastic strain along the cross section can be obtained, as in Figure 5.6. This value can be used again in (5.13) to obtain a second approximation of the total strain and the process can be repeated until convergence is reached.

Further details of this iterative procedure, which can also be applied during the heating and cooling stages of a heat treatment, can be found in [90] and [92]. It should be noted, however, that during the calculation of the total strains, at each time step, the accumulated plastic strains from previous time steps should be included to account for possible elastic unloading or reverse yielding.

(B) During the holding period, when creep is taken into account, this procedure has to be slightly modified:

At the start of the first time increment the inelastic strain is:

\[ \varepsilon_{x}^{in}(y) = \varepsilon_{x}^{pl}(y) \]  

(5-18)

where \( \varepsilon_{x}^{pl} \) is the total accumulated plastic strain up to that instant (due to welding and heating).

As mentioned before \( \varepsilon_{x}^{pl} \) will remain constant during the whole soaking period.

To get a first approximation of the total strain, \( \varepsilon_{x} \),
Figure 5.6: Bilinear stress strain law used in 1-D model

Figure 5.7: Uniaxial creep curve
after $\Delta t$, from equation (5.13) it is now assumed that:

$$\Delta \varepsilon^i_x = \Delta \varepsilon^c_x = 0$$  \hspace{1cm} (5-19)

This first approximation of the total strain, $\varepsilon_x$, is then substituted in equation (5-11) to obtain a first approximation of stress $\sigma_x$. Using this stress approximation and the appropriate creep law (section 5.2.5) a second approximation for the creep strain increment, $\Delta \varepsilon^c_x = \Delta \varepsilon^i_x$, is obtained. This value is now again substituted in equation (5-13) for a new approximation of the total strain $\varepsilon_x$ and the process is repeated until convergence is reached.

At the start of the second and any subsequent time increment the total strains will be equal to the initial plastic strain plus the accumulated creep strain during the previous time increments:

$$\varepsilon^{i\text{in}}_x(y) = \varepsilon^{pl}_x(y) + \sum_{i=1}^{n-1} \Delta \varepsilon^c_x(y)$$  \hspace{1cm} (5-20)

Assuming again that $\Delta \varepsilon^c_x$ is zero during this time step the process outlined above can be repeated.
5.3 Creep Laws

5.3.1 Introduction

Creep, the time dependent deformation and fracture of materials, is probably the most general type of material behavior. A typical experimental uniaxial creep curve is shown in Figure 5.7 showing increasing with time strain for constant stress and temperature. At \( t=0 \) the instantaneous response \( \varepsilon_0 \) is either elastic or elasto-plastic depending on the magnitude of stress. The strain rate \( \dot{\varepsilon} = \frac{d\varepsilon}{dt} \) is decreasing, in the primary range, reaching a minimum constant value, in the secondary range, and steeply increasing in the final tertiary range, where creep rupture occurs.

The current state of the art requires that plasticity and creep constitutive equations be formulated largely on independent bases. However, elevated-temperature deformation is, essentially, the result of time-dependent processes where both plastic and creep behaviors are present simultaneously. Prior creep deformations influence subsequent plastic behavior and vice versa. Only limited information is available on these mutual interactions as outlined by Pugh, et al., in [96] and by Corum, et al., in [97]. Some recent studies, as [104] by Newman, et al., attempt to treat plasticity and creep with a single model. However, for the purposes of this study the two behaviors were modeled separately as already noted in the previous section.

5.3.2 Uniaxial Creep Laws for the Materials Used in this Study

Very limited information is available in the literature on the creep behavior of high-strength, quenched and tempered
steels, as HY-80 and HY-130. Only some data on the minimum creep rate and creep rupture time are reported by Domis [100], and are presented in Appendix A, as adapted from [101].

For stainless steels, on the other hand, numerous studies have been performed to investigate their elevated-temperature inelastic behavior. Creep data for 304 austenitic stainless steel appear in Appendix A. Furthermore, it is reported by Clinard, et al., in [102], and Corum, et al., in [97], that the uniaxial creep behavior of stainless steels during the primary and secondary stage can be very well modeled by an equation of the form:

\[
\varepsilon^C(\sigma, t, T) = f(\sigma, T) \left[ 1 - e^{-r(\sigma, T)t} \right] + g(\sigma, T).t
\]

initially proposed by Garofalo, et al., in [103],

where : \( \varepsilon^C \) = Uniaxial creep strain

\( \sigma \) = Applied uniaxial stress

\( T \) = Test temperature

\( t \) = Time

The functions \( f(\sigma, T), r(\sigma, T) \) and \( g(\sigma, T) \) can be deduced from creep test data by curve fitting. Clinard et al., [102], report for 304 stainless steels, the following representation at \( T = 1100^\circ F \) (594\(^\circ\) C).

\[
\begin{align*}
  f(\sigma) &= 5.436 \times 10^{-5} \sigma^{1.843} \\
  r(\sigma) &= 5.929 \times 10^{-5} \exp(0.2029\sigma) \\
  g(\sigma) &= 6.73 \times 10^{-9} \left[ \sinh (0.1479\sigma) \right]^{3.0}
\end{align*}
\]

(5-22)

where : \( \sigma \) is expressed in Ksi, \( t \) in hours and the creep strain
$\varepsilon^C$ in in./in.

Existing data are not enough to support a creep law for compressive stresses that is different from the creep law in tension. Therefore creep response to constant uniaxial compression is usually assumed identical to that in tension (actually a reflection of it with respect to the time axis).

The creep strains predicted from equation 5-21 at various stress levels are plotted versus time in Figures 5.8a and 5.8b. 5.3.3 Multiaxial Creep Models

A "flow rule" for the case of multiaxial stress can be developed based on the experimentally verified assumptions that (a) the material is isotropic and incompressible (b) the creep strains are indifferent to hydrostatic states of stress and (c) the principal directions of stress and creep strain should coincide. (As detailed in references [96] to [99]).

Such a flow rule that would also reduce to the uniaxial creep law is of the form:

$$\dot{\varepsilon}^C_{ij} = \frac{3}{2} \varepsilon \left( \sigma^s, t, T \right) \sigma^\prime_{ij} \quad (5-23)$$

where: $\dot{\varepsilon}^C_{ij}, \sigma^\prime_{ij}$ = The components of creep strain and deviatoric stress tensors respectively.

$\bar{\varepsilon}, \bar{\sigma}$ = The effective strain and stress

$\bar{\varepsilon}^2 = \frac{2}{3} \varepsilon^C_{ij} \varepsilon^C_{ij}$

$\bar{\sigma}^2 = \frac{3}{2} \sigma^\prime_{ij} \sigma^\prime_{ij}$

and $\varepsilon \left( \sigma^s, t, T \right) = \varepsilon \left( \sigma^s, t, T \right)$ = The uniaxial creep law with axial stress and strain variables replaced by their
Figure 5.8: Uniaxial creep law for type 304 stainless steel at 1100°F
effective counterparts.

However, in the simplified one-dimensional model such a flow rule is not needed since a uniaxial stress state is assumed.

5.3.4 Creep Under Variable Loading

For the complete description of the time dependent behavior of the material, a "hardening rule" is needed in order to predict the creep response when the stress levels are changing. The two most commonly used rules, time hardening and strain hardening, are schematically shown in Figure 5.9. When the applied uniaxial stress is \( \sigma = \sigma_1 \) the creep response follows the constant-stress creep curve (\( \sigma_1 \)). At time \( t_1 \), when the applied stress increases to \( \sigma = \sigma_2 \) the time hardening rule would predict that the creep response follows the (\( \sigma_2 \)) curve beginning at point T. The strain hardening formulation, on the other hand, would indicate that the response also follows the curve (\( \sigma_2 \)), but beginning at point S.

The two different hardening rules also result in different formulations for the creep strain rate in a variable stress situation. Specifically, if time hardening is assumed, the creep strain rate is a function of stress time and temperature; if strain hardening is postulated, however, the creep strain rate becomes a function of stress, strain and temperature [99].

Experimental evidence tend to support a strain hardening formulation for the case of 304 and 316 stainless steels. Additionally, if stress reversals occur, auxiliary strain hardening rules have to be introduced in order to avoid unrealistic predictions. These auxiliary rules in detail presented by Corum, in [97], would for example indicate that
Figure 5.9: Strain-hardening and time-hardening models of creep response under a stepwise varying load.
the creep response produced by the first application of compressive stress starts at zero strain hardening as in the case of a virgin specimen. It should be pointed out, however, that these rules are strictly correct only in step changes of stress that are of long duration. In our problem where small changes of stress take place at each infinitesimal time step, it was decided, for computational efficiency, to adopt time-hardening. The reason for that will become evident in section 5.4.3.

5.4 Notes on the Computer Implementation

5.4.1 Temperature Distribution

A special iterative scheme has to be used in order to account for the temperature dependence of the material properties. The procedure starts by assuming a temperature $\Theta_A$ and using it for a first estimate of $\rho$ and $\lambda$. Substituting these values back to equation (5-1) would give a first approximation of the temperatures $\Theta(y)$ along the cross section. These can now be used for a better estimation of the properties ($\rho$ and $\lambda$) which again can be substituted in (5-1) for a new approximation of $\Theta(y)$. This process, shown in Figure 5.10, can then be continued until convergence is attained.

5.4.2 Stress Analysis

A non-dimensional form of the equations is used in the program. Specifically we define the non-dimensional stress and strains:

$$ S = \frac{\sigma_x}{\sigma_0}, \quad e_x = \frac{\varepsilon_x}{\varepsilon_0}, \quad \tau = \frac{\alpha \Delta \Theta}{\varepsilon_0} $$
Figure 5.10: Iterative scheme to take into account the variation of properties with temperature.
\[ e_x^{el} = \frac{\varepsilon_x^{el}}{\varepsilon_o}, \quad e_x^{pl} = \frac{\varepsilon_x^{pl}}{\varepsilon_o}, \quad e_c = \frac{\varepsilon_x^c}{\varepsilon_o} \] (5-24)

and the non-dimensional transverse distance and Young's modulus:

\[ \eta = \frac{\varepsilon}{c} \text{ and } H = \frac{E}{E_o} \] (5-25)

where: \( \sigma_o \) = Yield stress at reference temperature

\[ \varepsilon_o \] = Yield strain at reference temperature

\[ E_o \] = Young's modulus at reference temperature

Now equations (5-8) to (5-12) can be expressed in non-dimensional form and the total strain will be given by:

\[ e_x(\eta) = (A_1 - \eta A_2) \int_{-1}^{+1} H(\tau + e_x^{in} + \Delta e_x^{in}) \, d\eta - (A_2 - \eta A_3) \int_{-1}^{+1} H(\tau + e_x^{in} + \Delta e_x^{in}) \, \eta \, d\eta \] (5-26)

where:

\[ e_x^{in} = e_x^{el} + e_x^{pl} + e_x^c \] (5-27)

\[ A_1 = \left[ \int_{-1}^{+1} H \, \eta^2 \, d\eta \right] / B \]

\[ A_2 = \left[ \int_{-1}^{+1} H \, \eta \, d\eta \right] / B \]

\[ A_3 = \left[ \int_{-1}^{+1} H \, d\eta \right] / B \]

\[ B = \left[ \int_{-1}^{+1} H \, d\eta \right] \left[ \int_{-1}^{+1} H \, \eta^2 \, d\eta \right] - \left[ \int_{-1}^{+1} H \, \eta \, d\eta \right]^2 \]

The integrals are evaluated numerically and the equations can be simplified even further in specific cases.
For bead on plate welding where the temperature distribution and the resulting strains and stresses are symmetric around the weld line, equation (5-26) yields:

\[ e_x(\eta) = \frac{\int_0^1 H(\tau + e_x^{in} + \Delta e_x^{in}) \, d\eta}{\int_0^1 H \, d\eta} \]  
\hspace{1cm} (5-29)

For edge welding along the side of a plate of breadth c, equation (5-26) still holds, but with the integration limits from 0 to 1.

During heat treating at a constant temperature equation (5-26) can be further simplified. If we assume a symmetric previous distribution of strains and stresses, we readily get from (5-29):

\[ e_x = \tau + \int_0^1 (e_x^{in} + \Delta e_x^{in}) \, d\eta \]  
\hspace{1cm} (5-30)

Whereas for any nonsymmetric distribution of stresses we can get after some algebra from (5-26) that:

\[ e_x = \tau + (12\eta - 6)\int_0^1 (e_x^{in} + \Delta e_x^{in}) \, d\eta + (4 - 6\eta) \int_0^1 (e_x^{in} + \Delta e_x^{in}) \, d\eta \]  
\hspace{1cm} (5-31)

For butt welding of plates the solution for edge welding is used ahead of the arc and the solution for bead on plate welding behind the arc (where the weld puddle is solidified).

All the above integrations are performed numerically in the program and more details on the integration scheme that was used are given in Appendix B. A listing of the FORTRAN code can be found in Appendix C.
5.4.3 Creep Analysis

Equation (5-21) is the form of creep law employed in the numerical model developed in this study for the analysis of stress relieving of 304 stainless steel. Specifically the creep strain increment \( \Delta \varepsilon_C^C(y) \) accumulated between time \( t \) and \( t+\Delta t \) (at each point along the cross section) is:

\[
\Delta \varepsilon_C^C(y) = \varepsilon_C^C \left[ \sigma_C^x(y), (t+\Delta t), T(y) \right] - \varepsilon_C^C \left[ \sigma_C^x(y), t, T(y) \right]
\]

(5-32)

where:
- \( T(y) \) = The temperature distribution and
- \( \sigma_C^x(y) \) = The current approximation of the stress distribution

In equation (5-32) the creep strain rate \( \Delta \varepsilon_C^C(y)/\Delta t \) is a function of stress, time and temperature. That is, time hardening was assumed in order to avoid the added computations of solving (5-21) for time, as a strain hardening formulation would require. Nevertheless, however, the latter would not necessarily guarantee better results due to the small size of the time steps. It should be again noted here that both temperature and stress at each point change stepwise. That is, they are assumed to remain constant at each point for the duration of each time increment, (as in the "time increment-initial strain" method described in [96]).
5.4.4 A Sample Case

In what follows a sample case is presented. Specifically butt welding of two plates and subsequent stress relieving heat treatment at 1100°F (594°C) are analysed. The welding conditions were assumed the same as in the edge welding case of the next Chapter (Table 6.6).

Predicted temperatures strains and stresses during welding are plotted in Figures 5.11, 5.12 and 5.13 respectively. The assumed temperature history during stress relieving (uniform heating) is shown in Figure 5.14. The variation of stresses throughout the treatment is followed in Figure 5.15 and a comparison of the residual stresses before and after stress relieving can be found in Figure 5.16.
Figure 5.11: Temperature history during butt welding, as predicted by the one dimensional program (304 stainless steel).
Figure 5.12: Mechanical strains during butt welding as predicted by the one-dimensional program (304 stainless steel).
Figure 5.13: Stresses during butt welding, as predicted by the one-dimensional program (304 stainless steel).
Figure 5.14: Stress relieving temperature history, uniform along the entire plate at each time step.
Figure 5.15: Variation of stresses during heating, soaking and cooling (304 stainless steel)
Figure 5.16: Remaining stresses distribution along the plate after welding, heating, soaking and cooling (304 stainless steel)
PART III

EXPERIMENTS AND COMPUTER AIDED DATA ACQUISITION
CHAPTER VI

EXPERIMENTS AND COMPUTER-AIDED DATA ACQUISITION

6.1 General Description of Experiments

To verify the analytical results, experiments were performed with 304 stainless steel plates. All plates were edge welded and all but one were subsequently stress relieved in a furnace at different holding temperatures. The plates were finally sectioned and residual stresses were measured by stress relaxation.

Temperatures and strains at various locations on the plates were monitored throughout welding and stress-relieving operations. For this purpose a microprocessor-based data acquisition system was interfaced with the minicomputer (MINC-23) that performed the data reduction, processing and plotting.

The geometry and the dimensions of the specimens are given in Table 6.1 together with a brief description of the experiments performed on each of them. Exact welding conditions and stress relieving parameters are given in the next sections of this chapter.

6.2 Specimen Instrumentation (Strain Gages and Thermocouples)

The temperature and strain changes during welding and subsequent heat treatment were measured by thermocouples and electrical-resistance strain gages attached on the plates. The thermocouple and strain gage locations are depicted in Figure 6.1. The total number of strain gages and their configuration is given in Table 6.2 for all specimens.

Thermocouples were of Chromel-Alumel type (ANSI symbol K)
Figure 6.1: Specimen geometry and instrumentation

Note:  

□  Strain gage locations
■■  Thermocouple locations
### Table 6.1: Specimen Dimensions and Experiments Description

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Dimensions inches (mm)</th>
<th>Experiments Performed</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>22 x 6 x 3/8 (555.8x152.4x9.5)</td>
<td>Instrumented, edge welded (one pass), stress relieved (oven), cut.</td>
</tr>
<tr>
<td>2</td>
<td>as above</td>
<td>Instrumented, edge welded (one pass), strain gages added, cut.</td>
</tr>
<tr>
<td>3</td>
<td>as above</td>
<td>Instrumented, edge welded (one pass), stress relieved, cut.</td>
</tr>
<tr>
<td>4</td>
<td>as above</td>
<td>Instrumented, edge welded (one pass), stress relieved, reinstrumented, cut.</td>
</tr>
<tr>
<td>5</td>
<td>22 x 4 x 3/8 (555.8x101.6x9.5)</td>
<td>Instrumented, bead on plate welded (multiple passes). Test of data acquisition system.</td>
</tr>
<tr>
<td>6</td>
<td>22 x 5 x 3/8 (555.8x127.0x9.5)</td>
<td>Instrumented (T/C only), stress relieved to test oven heating uniformity.</td>
</tr>
<tr>
<td>7 to 9</td>
<td>as above</td>
<td>Welded -edge and bead on plate -to determine welding conditions (multiple passes).</td>
</tr>
</tbody>
</table>
Table 6.2: Arrangement of Strain Gages and Thermocouples

<table>
<thead>
<tr>
<th>Specimen No</th>
<th>Strain Gages</th>
<th>Thermocouples</th>
<th>Arrangement as in</th>
</tr>
</thead>
<tbody>
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<td></td>
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<td>Configuration</td>
<td>Number</td>
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<td>A</td>
<td>5</td>
</tr>
<tr>
<td>3</td>
<td>10</td>
<td>B</td>
<td>5</td>
</tr>
<tr>
<td>4</td>
<td>10&lt;sup&gt;(2)&lt;/sup&gt;</td>
<td>B</td>
<td>5</td>
</tr>
<tr>
<td>5</td>
<td>1</td>
<td>A</td>
<td>1</td>
</tr>
<tr>
<td>6</td>
<td>0</td>
<td>-</td>
<td>3+1</td>
</tr>
</tbody>
</table>

Key: (A): Single gages in the longitudinal direction on the one side of the plate only.
(B): Single gages in the longitudinal direction on both sides of the plate.
(C): Thermocouples located at the surface of the specimen on one side only.
(D): Thermocouples buried at the mid-thickness of the plate.

Notes: (1): 5 more gages were added before cutting in the transverse direction.
(2): 10 new gages were installed before cutting in both longitudinal and transverse direction.
Table 6.3: Strain Gage Characteristics

<table>
<thead>
<tr>
<th>Type</th>
<th>WK-09-062AP-350</th>
</tr>
</thead>
<tbody>
<tr>
<td>Temperature Range</td>
<td></td>
</tr>
<tr>
<td>Continuous use:</td>
<td>$-452^\circ F$ (-269°C) to $550^\circ F$ (+290°C)</td>
</tr>
<tr>
<td>Short term exposure:</td>
<td>up to $700^\circ F$ (+370°C)</td>
</tr>
<tr>
<td>Strain limits</td>
<td></td>
</tr>
<tr>
<td>Room temperature</td>
<td>$\pm 1.5%$</td>
</tr>
<tr>
<td>$-320^\circ F$ (-195°C)</td>
<td>$\pm 1.0%$</td>
</tr>
<tr>
<td>$+400^\circ F$ (+205°C)</td>
<td>$\pm 3.0%$</td>
</tr>
<tr>
<td>Fatigue life</td>
<td>$10^5$ cycles at $\pm 2000$ $\mu$in./in. ($\mu$m/m)</td>
</tr>
<tr>
<td></td>
<td>$10^7$ cycles at $\pm 2200$ $\mu$in./in. ($\mu$m/m)</td>
</tr>
<tr>
<td>Resistance</td>
<td>$350.0 \pm 0.3%$</td>
</tr>
<tr>
<td>Gage factor</td>
<td>$2.01 \pm 1.0%$ (at $75^\circ F$)</td>
</tr>
</tbody>
</table>
recommended for use up to 2300°F (1260°C).

Strain gages used were all of the same type, WK-09-062AP-350, made by Micro-Measurements. They are fully encapsulated single-element, K-alloy gages with general characteristics summarized in Table 6.3. The temperature-induced apparent strain, \( \varepsilon_{\text{app}} \), for these gages and the variation of the gage factor, \( S_g \), is plotted versus temperature in Figure 6.2. Gage resistances at the time of installation were measured and summarized in Table 6.4.

During stress relieving at 1100°F (593°C) the strain gages of specimen #4 were destroyed and were replaced before cutting with gages of the same type, at the same positions, but oriented both in the longitudinal and transverse direction. In that way both the longitudinal and transverse strain relaxation during cutting could be measured. For the same reason five strain gages were added on the specimen #2 before cutting.

6.3 Welding and Stress Relieving Operations

6.3.1 Welding Equipment

Welding of the specimens was performed in the Ocean Engineering Welding Laboratory at M.I.T. with equipment shown in photo 6.1 and basically consisting of:

(a) Welding Power Supply: Deltaweld 650 made by the Miller Electric Manufacturing Company. This is a solid-state, direct current, constant potential welding power source suited for 100% duty cycle up to 650 Amperes.

(b) Gas Metal Arc Welding Torch: An Air-cooled, concentric Barrel model AM50-C by Airco, was mounted on a Machine Head
Figure 6.2: Temperature induced apparent strain for strain gages type WK-09-062AP-350, Lot #KL4FE01 (Tested on 304 stainless steel by Micro-Measurements).
Table 6.4: Measured gage resistance (in ohms)

<table>
<thead>
<tr>
<th>Gage Plate</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
<th>7</th>
<th>8</th>
<th>9</th>
<th>10</th>
</tr>
</thead>
<tbody>
<tr>
<td>#1</td>
<td>349.6</td>
<td>349.9</td>
<td>349.7</td>
<td>349.7</td>
<td>349.5</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>#2</td>
<td>349.5</td>
<td>349.7</td>
<td>349.5</td>
<td>349.2</td>
<td>349.8</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>#3</td>
<td>349.2</td>
<td>349.4</td>
<td>349.7</td>
<td>349.8</td>
<td>349.6</td>
<td>349.7</td>
<td>349.4</td>
<td>349.1</td>
<td>349.7</td>
<td>350.1</td>
</tr>
<tr>
<td>#4</td>
<td>349.9</td>
<td>350.1</td>
<td>349.6</td>
<td>349.6</td>
<td>350.3</td>
<td>349.5</td>
<td>349.1</td>
<td>349.2</td>
<td>349.4</td>
<td>349.5</td>
</tr>
<tr>
<td>#5</td>
<td>349.5</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Resistance to ground for all gages > 20k

Table 6.5: Stainless steel welding wire typical welding parameters

<table>
<thead>
<tr>
<th>Process</th>
<th>Wire Diameter</th>
<th>Operating Current Range (amps)</th>
<th>Operating Voltage Range</th>
<th>Shielding Gas</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pulsed power</td>
<td>.035</td>
<td>40-200</td>
<td>16-27</td>
<td>A+2%O₂</td>
</tr>
<tr>
<td></td>
<td>.045</td>
<td>50-300</td>
<td>18-32</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1/16</td>
<td>70-300</td>
<td>19-33</td>
<td></td>
</tr>
<tr>
<td>Spray transfer</td>
<td>.035</td>
<td>125-300</td>
<td>18-32</td>
<td>A+2%O₂</td>
</tr>
<tr>
<td></td>
<td>.045</td>
<td>155-450</td>
<td>20-34</td>
<td></td>
</tr>
<tr>
<td></td>
<td>1/16</td>
<td>210-500</td>
<td>26-36</td>
<td></td>
</tr>
<tr>
<td>Dip transfer</td>
<td>.035</td>
<td>55-200</td>
<td>15-23</td>
<td>90%He+7-1/2%A+2-1/2%CO₂</td>
</tr>
<tr>
<td></td>
<td>.045</td>
<td>75-200</td>
<td>16-24</td>
<td></td>
</tr>
</tbody>
</table>

NOTE: Ranges subject to change due to variances in welding conditions.
Positioner also by Airco (Stock No 2354-01-91). The positioner and torch assembly was bolted on a Jetline Travel Carriage moving on a Jetline horizontal side beam, at a controllable speed.

(c) Wire feeder: Wire was fed to the torch at controllable speed by a Miller Model S-54D, digitally controlled feeder. The wire feed wheels and guides were suited for use with 0.035 inches diameter wire.

(d) Voltage Controller: Weld arc voltage was controlled by a Miller Digital Voltage Controller Model DVC DW-1.

(e) Spot-Continuous Control Panel: A Miller Model CS-4 panel allowed selection of either spot or continuous welding and control of pre- and post- flow time and burnback time.

Exact specifications for all the equipment used can be found in the respective owners' manuals. However there was no calibration chart for the multi-turn weld-travel-speed dial on the Jetline carriage and thus time-distance checks had to be performed.

6.3.2 Welding Process and Consumables

Straight polarity (electrode negative), dip transfer, Gas Metal Arc (G.M.A.), welding was performed on all the specimens.

The wire used was type 308 stainless steel wire, 0.035 inches (0.89 mm) in diameter (AWS specifications A 5.9 and ASME SFA 5.9).

To make dip transfer possible a 90% He - $7\frac{1}{2}$ % A - $2\frac{1}{2}$ % CO$_2$ shielding gas mixture was used.
6.3.3 Welding Conditions

Some typical welding parameters for stainless steels are given by Airco in Table 6.5 [116]. Specifically for 304 stainless steels, however, experiments were performed utilizing dip transfer by Koreisha for bead on plate welding, [117], and by the author for edge welding. Figure 6.3 gives the maximum possible wire feed speed (for satisfactory welds) versus arc voltage at various tested weld travel speeds.

The finally chosen welding conditions, summarized in Table 6.6, were tested to ensure that the strain gages closest to the weld line will not encounter temperatures above 550°F during welding.

The exact variations of arc voltage and arc current are shown in Figures 6.4 (a) and (b). Current was measured across a shunt resistance (50 mV/500 A) inserted in the circuit next to the torch. Arc voltage was measured between the torch and the specimen. The short circuiting is evidenced by the voltage drops and the immediately following current peaks.

6.3.4 Stress Relieving Equipment and Conditions

A "Lucifer" furnace, model # HDL-7021H, was used for the stress relieving of specimens #1, #3, #4, and #6. The furnace was electrically heated up to 2300°F consuming 13KW at 440 Volts (3 phases). The temperature in the furnace was controlled by a "Guardsman" type on-off controller.

The stress relieving conditions are shown in Table 6.7. Exact records of the temperature during stress relieving will be given in the next chapter.
Figure 6.3: Maximum wire feed speed vs. arc voltage for satisfactory welds at different weld travel speeds.
Figure 6.4: Arc voltage and current variations during short circuiting welding of 304 stainless steels (Measured during welding of specimen #1)
Table 6.6: Selected Welding Conditions (Same for specimens #1 through #6)

Arc Voltage
set at 23 Volts

Arc Current
measured off the Amp-meter 90 Amperes (approx)

Wire feed Speed
set at 175 in/min

Weld travel Speed
set dial at 700
( equivalent to 15.96 in/min)

Table 6.7: Stress Relieving Conditions

<table>
<thead>
<tr>
<th>Specimen Number</th>
<th>Holding Temperature</th>
<th>Time in Furnace</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>500°F (260°C)</td>
<td>6</td>
</tr>
<tr>
<td>2</td>
<td>not stress relieved</td>
<td>-</td>
</tr>
<tr>
<td>3</td>
<td>approx. 370°F (188°C)</td>
<td>4</td>
</tr>
<tr>
<td>4</td>
<td>1100°F (593°C)</td>
<td>7</td>
</tr>
<tr>
<td>6</td>
<td>500°F (260°C)</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>370°F (188°C)</td>
<td></td>
</tr>
</tbody>
</table>
6.4 Computer Aided Data Acquisition System

6.4.1 General System Configuration

In the past, light-writing oscillographs —"visicorders"— were almost exclusively used as data recording devices during welding experiments at M.I.T. However, the relative simplicity of operation and low cost of such systems should be contrasted with the significant manual data reduction effort required in order to decipher the various traces from endless rolls of photo-sensitive paper. These considerations coupled with the availability of compact and powerful microcomputers led to the decision to develop a computer-aided data acquisition system. Such a system would not only be used for recording strains, temperatures or welding conditions during experiments but could possibly be the first necessary element in real-time welding process control applications.

The finally configured system, that will be in more detail described in the next few sections of this chapter, basically consisted of:

(a) A MINC-23 Laboratory Data Processing System by Digital Equipment Corporation including a 16-channel analog to digital converter module and a programmable clock module.

(b) A 9000 Data Acquisition System by Daytronic Corporation, including thermocouple and strain gage conditioners, scanner slave modules and a microprocessor based computer interface module.

The system is versatile enough permitting operation under
three different configurations:

(a) Sampling of the Daytronic signal conditioners directly from the MINC A/D converter module.

(b) Sampling of the signal conditioners from the Daytronic computer interface module and serial data transfer to the computer (or an independent terminal) via an RS-232-C (or 20-mA current loop), serial ASCII, full duplex, communications link.

(c) Sampling as in (b) but parallel data transfer via a IEEE-488 instrument interface bus.

Furthermore the modular nature of the system makes expansion or modification a straightforward procedure.

In what follows a description of the system is given and the specifics pertaining to our application are covered. An in depth treatment of all the related issues is considered outside the scope of this study and will be given by the author in [118]. Further details and background information can be found in the literature (for example in Osborne [119] or Tocci [120] for an introduction to microprocessors/microcomputers and in Artwick [121] or Lipovski [122] for a relatively more detailed treatment of interfacing issues).

6.4.2 System Elements Description

(A) MINC-23 Hardware

The microcomputer used, built by D.E.C., basically consisted of:

(a) An MNC-chasis, housing the following modules:

- KDF11-AB Central Processing Unit based on the 16-bit
LSI 11/23 microprocessor

- MSV11-DD 32K word Memory Single Road (Up to four MSV11-DD can operate with LSI 11/23 microprocessor)
- BDV11-A Bootstrap/Diagnostic ROM module
- IBV11-A IEEE-488 Instrument Bus Interface
- DLV11-J 4-Line Asynchronous Serial Interface
- RXV21 Disk Controller Interface
- MNCAD 16 channel Analog/Digital Converter module
- MNCKW Real Time programmable clock module

(b) An RXO2 1.0 M byte dual, double density, Floppy Disc Subsystem

(c) A VT-105 Alphanumeric and Graphic Display Terminal

(d) An LA-36 DECWriter II terminal and

(e) A 4662 Tektronix Interactive Digital Plotter.

The actual system configuration as described above is outlined in figure 6.5.

(B) MINC-23 Software

The system software included

(a) The RT-11 Operating System (Version 4.0)
(b) FORTRAN IV programming language and
(c) FORTRAN Enhancement Package PEP-11 containing the following groups of FORTRAN callable subroutines

- REAL-11/MNC providing real-time control of all MNC-series modules
- IBS, the IEEE Instrument Bus Subroutines Package
- SSP, the Scientific Subroutine Package
- LSP, the Laboratory Subroutine Package and
Figure 6.5: MINC-23 System Configuration and possible communication options
-FDT, the FORTRAN Debugging Technique Package

(C) **Daytronic 9000 data acquisition system**

The basic system components housed in a 9020 Daytronic Mainframe are:

(a) One 9110AK, Type k (Chromel-Alumel) Single Thermocouple Conditioner

(b) One 9610TC, Thermocouple Scanner Slave, capable of multiplexing ten thermocouple inputs into a single 9110AK conditioner

(c) Five 9170 Strain Gage Conditioners for full or half bridge transducer inputs and 5 or 10V D.C. bridge excitation

(d) Ten 9178A X-12 Strain Gage Conditioners with 6V A.C. (3.28 KHz) bridge excitation, modified to accept quarter bridge transducer inputs (single gages)

(e) Two 9610 scanner slaves each capable to "call" up to ten signal sources

(f) One 9635 Computer Interface Module, based on the Intel 8086 16 bit microprocessor and capable of:
   - Setting-up and calibrating up to 398 data channels, with calibration data stored in a battery protected memory
   - Scanning of analog, digital and logic data channels at a rate of 1500 ÷ 1800 channels per second depending on the microprocessor workload.
   - Automatic digital zeroing and scaling of the analog channels
   - Operator-to-computer communications via a front-panel keyboard
- Random access servicing of the external computer's requests for input data, via an RS-232-C, or 20mA current loop, communications link (or optionally via an IEEE instruments bus)

The system configuration is outlined in Figure 6.6. Detailed specifications, circuit schematics and operating instructions are given in the system manuals, [124].

6.5 Data Acquisition System Set-up and Operation

6.5.1 Sampling and Interfacing Considerations

As was outlined in section 6.4.1, system operation under three different configurations is possible. However, for the experiments of this study only the second configuration was used, since it would require minimal modification of both systems. Discussion for the rest of this chapter will only refer to this configuration and details on the other two will be given by the author in [118].

Specifically, refering to Figure 6.6, during operation the 9635 Computer Interface module scans continuously at high speed (1500 - 1800 channels per second) a number of operator selected channels (T/C or S/G signal conditioners in our case), and writes the measurements, properly scaled, into the DATA RAM, an internal buffer memory. Via an RS-232-C full duplex port, the external computer (MINC-23) can "read" the contents of the DATA RAM by simply prompting commands in ASCII. These writing and reading operations are completely transparent to each other.

From the computer's point of view the 9635 emulates a standard RS-232-C/ASCII data terminal (D.T.E.) responding
Figure 6.6: Daytronic 9000 system configuration and possible communication options.
"instantly" to simple interrogation and control commands. The response time, by default set to 384 milliseconds, is controllable to prevent problems when I/O buffer is not provided. In the MINC-23 computer the four SLU ports, connected to the DLV1l-J interface channels, are used for serial I/O (Figure 6.5).

Both systems are flexible enough permitting communication at various baud rates (bits per second) and different parity and stop bit schemes. The maximum (for DLV1l-J) receive/transmit speed of 9600 baud was selected and the character format was set to:
- 1 start bit
- 8 data bits
- 1 stop bit
- No parity

It should be noted here that serial data transmission and subsequent data processing take certain amount of time, orders of magnitude greater than the sampling period in the A/D of 9635. This would cause severe cross-channel time-skew problems in the measurements. Therefore in order to take full advantage of the actually very high scanning rates, the "LOC" command available in the 9635 software was used. This option permits the computer to instantly freeze the contents of the DATA RAM - that is, in effect to "take a snapshot", of the monitored channels - and then take whatever time it needs to serially transfer or process the "effectively simultaneous" measurements.

The actual required pin-to-pin connections between the 9635, SLU port #1, and DLV1l-J are shown in Figure 6.7 together
Figure 6.7: Interconnections between the two systems

Note: GND: Protective ground
SGND: Signal ground
TDATA: Transmit data
RDATA: Receive data
RTS: Request to send
CTS: Clear to send

Asynchronous Serial Interface

DLVII-J 10 pin connector
SIU Port 14 pin connector

J3 25 pin connector

GND
TCN
TDATA
TDATA-
RS
CTS
SGND
with a brief description of the various signals. Although an RTS/CTS (Request To Send/ Clear To Send) "handshake" protocol can be implemented with the 9635, this was not necessary for the MINC-23 and therefore pins 4 and 5 of 9635 output connector were simply tied to each other, [124].

6.5.2 Data Acquisition Programs

Listings of the data acquisition programs used in this study are given in Appendix D. For serial character input and output through the DLV11-J interface the D.E.C. provided subroutines CIN and COUT (written in MACRO-11) had to be slightly modified for our application. Necessary details on the LSI-11/23 instruction set and addressing modes can be found in [125].

6.5.3 Calibration Procedures

(a) 9110AK Thermocouple Conditioners:

The modules are self-zeroed requiring only span adjustment (in °C or °F) and give a linear analog output for temperatures in the range -148 to +2300°F (-100 to +1260°C).

(b) 9178 Strain Gage Conditioners:

Shunt calibration was performed on all the strain gage conditioners. However, in order to verify the accuracy of the measurements an actual "deadweight" calibration was performed with one of the specimens.

During shunt calibration one fixed resistor is shunted across one arm of the strain gage bridge as in Figure 6.8 and produces an electrical unbalance equivalent to that caused by a particular value of strain on the active arm of the bridge.

It is proven in experimental stress analysis texts, (Dally and
Riley [126]), that this value of equivalent strain input, for the one-active-arm bridge of Figure 6.8 is:

$$\varepsilon_{\text{cal}} = \frac{R_2}{S_g (R_2 + R_C)}$$  \hspace{1cm} (6-1)

where \( R_2 \) = The arm's initial resistance

\( R_C \) = The shunted calibration resistance

\( S_g \) = The gage factor

For the 350 Ohm strain gages used in this study and for the originally installed 59 K Ohm calibration resistor the equivalent strain value is:

$$\varepsilon_{\text{cal}} = 2948.6 \ \mu \text{strain}$$

In the 9635 module this value would be used as the upper limit of the linear range (0-5000 millivolts) of the strain gage conditioner output [124].

The computer simulation, however, using the one dimensional program, showed that the expected maximum strains during welding and subsequent stress relieving might be higher than this value. Therefore it was decided to replace the calibration resistor, \( R_C \). Precision metal film resistors of 34 K Ohm value were installed on all 9178A x 12 strain gage conditioners resulting in an equivalent strain value of:

$$\varepsilon_{\text{cal}} = 5094.6 \ \mu \text{strain}$$

A value of 2.0 was used for the gage factor \( S_g \) in the above calculations. The slightly different actual value and its variation with temperature would be taken into account during
Figure 6.8: Strain gage bridge configuration

\[ R_g \] : gage resistance

\[ R_2, R_3, R_4 \] : bridge completion resistors

\[ R, R_5, R_6 \] : balancing resistors

\[ R_c \] : calibration resistor

\[ V \] : excitation voltage
data reduction (Section 6.7.1) (Calibration and previous zero balancing was performed with all the gages at room temperature).

To verify the accuracy of shunt calibration a test was performed with specimen #1. The plate was simply supported on two "knife" edges along the two 6 inch sides and loaded with various loads. Strains measured in all 5 strain gages were off by 2% to 3% from the values calculated by simple beam theory.

6.5.4 **Necessary System Modifications**

A number of modifications were necessary in order to set-up and interface the two systems for our application. Specifically:

(a) In the DLV11-J card wire-wrapped jumpers were used to configure all channels for 8 data bits, no parity and one stop bit. Baud rates were set at 1200, 9600, 300 and 9600 for channels #0, #1, #2 and #3 respectively. (DLV11-J operation is covered in [123]).

(b) Extra software was prepared and stored on two 2k x 8 EPROM chips (type 2516JL) for use with the 9635 computer interface module. This would cause the line feed (LF) and carriage return (CR) characters to be transmitted only once, in the end of a DATA-RAM DUMP operation (DMP), [124].

(c) External bridge completion circuits for the 9170 strain gage conditioners were built and crammed in the housing of the 14 pin input connectors. 350 Ohm precision metal film resistors by Micro-Measurements (type S-350-01) were used.

(d) The initially installed 59 K Ohm calibration resistors in the 9178A modules were replaced with 34 K Ohm precision metal
film resistors.

(e) Through the 9020 mainframe patch wiring card, pins 12 of all 9178A strain gage conditioners were interconnected to ensure synchronization of the 3.28 KHz excitation oscillators.

6.6 System Performance Evaluation

6.6.1 System Limitations

The installation, software development and testing of a data acquisition system from scratch could very well be viewed as a challenge to Marphy's Laws. However, apart from the trivial-but nevertheless time consuming - troubleshooting problems, some limitations in the system performance - at least in its current configuration - should be noted here:

(a) Scanning speed, probably the most important characteristic of such a system, was limited due to the relatively slow transmission and processing of serial data.

(b) The reliability of the 9635 computer interface was rather low. The module, which was introduced in the market less than a year ago, undoubtedly needs further development both in hardware and software.

(c) The 9110A/9610TC thermocouple conditioner and slave combination could not give any meaningful full temperature readings during dip transfer welding. This was most probably due to the "noisy" and unstable (short-circuiting) nature of the arc during dip transfer since it also happened during the unstable arc-initiation phase of spray-transfer welding tests performed with the same set-up on HY-130 specimens [128].
6.6.2 Suggestions for Further Improvement and Expansion

Various possibilities exist for improvement and/or expansion of the data acquisition system. Specifically:

(a) Subroutines CIN and COUT could possibly be modified or "fine tuned" to reduce further the serial data processing time in the MINC-23 computer.

(b) The new version of the RT-ll software could be installed. This version now directly supports the serial input/output ports and the new thermocouple conditioner MNC module.

(c) Slow serial data transfer could be avoided by sampling the Daytronic (or other) signal conditioning modules directly from the A/D module of MINC-23 (MNCAD). This would bypass the 9635 computer interface module, but would necessitate installation of more thermocouple conditioners or of a dual multiplexer (MNCAM) module in the MINC.

(d) The IEEE instrument bus interface could be used for parallel data transfer but the 9635 would have to be factory modified.

(e) More modules could be added in both parts of the system. For example L.V.D.T. conditioners (type 9130) could be added on the 9020 Daytronic mainframe for welding distortion measurements. Or on the other end preamplifier (MNCAG), and/or multiplexer (MNCAM) modules could be plugged in the MINC-23 to make possible sampling of other analog inputs from the MNCAD A/D module.

(f) Finally in order to close a control loop, for a welding process control application, analog or digital output
modules could be installed in both parts of the system. (MNCAA D/A converter and MNCDO digital output modules on the MINC, or 9316 control logic input, 9317 control logic output and 9410 analog control modules in the 9000 Daytronic system)

6.7 Data Reduction

6.7.1 Compensation for Temperature - Induced Apparent Strain and Gage Factor Variation

Higher than room temperatures are encountered on all specimens during welding or stress relieving. Therefore compensation for temperature-induced apparent strain and gage factor variation is necessary.

Apparent strain is caused by two concurrent and algebraically additive effects: (a) Change in the gage resistance due to the temperature dependence of the electrical resistivity of the gage material and (b) Differential thermal expansion between the grid material and the test piece or the substrate material to which the gage is bonded. The metallurgical properties of certain strain gage alloys are such that these alloys can be processed to minimize the apparent strain over a wide temperature range when bonded to specific materials.

Such "self-temperature-compensated" strain gages are the ones used in this study (M-M type WK-09-062AP-350) with apparent strain versus temperature variation presented in Figure 6.2. A regression-fitted (least-squares) polynomial expression was also provided by M-M for the apparent strain:

\[ \varepsilon_{\text{app}}(T) = -81.4 + 1.39T - 4.63 \times 10^{-3}T^2 + 8.57 \times 10^{-6}T^3 - 9.33 \times 10^{-9}T^4 \] (6-2)
where: \( T \) = Temperature in degrees F and \\
\( \varepsilon_{\text{app}} \) = In microstrain (microinches/inch or micrometres/metre)

The strain gage factor, \( S_g \), defined as

\[
S_g = \frac{\Delta R}{R} \cdot \frac{1}{\varepsilon_a}
\]

(6-3)

where: \( \varepsilon_a \) = Applied strain \\
\( \Delta R \) = Change in gage resistance due to \( \varepsilon_a \) \\
\( R \) = Initial resistance

also changes slightly with temperature. For the gages used the percent variation is also presented in Figure 6.2. For use in the data reduction programs the curve was approximated by three linear segments as follows:

\[
\Delta S_g(T) = \begin{cases} 
0.17 - 0.69 \frac{T}{200} & \text{for } 0 < T < 200^\circ F \\
-0.52 - 1.18 \frac{(T-200)}{200} & \text{for } 200^\circ F < T < 400^\circ F \\
-1.70 - 1.03 \frac{(T-400)}{100} & \text{for } 400^\circ F < T < 500^\circ F 
\end{cases}
\]  

(6-4)

The actual gage factor at temperature \( T \) would then be:

\[
S_g(T) = S_g(To) \left(1 + \frac{\Delta S_g(T)}{100}\right)
\]

(6-5)

where \( S_g(To) \) = The room temperature gage factor

In order to correct simultaneously for apparent strain and gage factor errors the following procedure is proposed by Micro-Measurements in [127]:

(a) Perform balance and calibration with the gage at room temperature employing the gage factor used by Micro-
Measurements in determining the apparent strain data ($S_g^* = 2.0$)

(b) Get the strain gage reading, $\hat{\epsilon}(T)$, at temperature $T$ ($T \neq T_{room}$)

(c) Correct for apparent strain:

$$\hat{\epsilon}(T) = \hat{\epsilon}(T) - \epsilon_{app}(T)$$  \hspace{1cm} (6-5)

where:
- $\hat{\epsilon}(T)$ = The strain gage reading at temperature $T$
- $\hat{\epsilon}(T)$ = Semicorrected strain
- $\epsilon_{app}(T)$ = Apparent strain at temperature $T$

(d) Correct for gage factor variation

$$\epsilon(T) = \hat{\epsilon}(T) \frac{S_g^*}{S_g(T)}$$  \hspace{1cm} (6-6)

Listings of the data reduction programs where the above presented compensation procedure is implemented are given in Appendix D.

6.7.2 **Residual Stress Measurements**

The residual stresses after welding (specimen #2) and after stress relieving (specimens #1 and #4) were measured by sectioning the plates and removing a narrow center strip carrying the strain gages. This stress relaxation technique for residual stress measurements is based upon the principle that "strains created during unloading are elastic even if the material has previously undergone plastic deformation", [1]. This fact is illustrated, for one dimension, in Figure 5.6. If the removed center strip of the plate is small enough then it can be safely assumed that residual stresses no longer exist and the measured strain changes $\bar{\epsilon}_x$, $\bar{\epsilon}_y$ and $\bar{\gamma}_{xy}$ are
\[ \varepsilon_x = -\varepsilon^e_x \]
\[ \varepsilon_y = -\varepsilon^e_y \]
\[ \gamma_{xy} = -\gamma^e_{xy} \]

(6-7)

The residual stresses therefore are

\[ \sigma_x = -\frac{E}{1-\nu^2} (\varepsilon_x + \nu \varepsilon_y) \]

\[ \sigma_y = -\frac{E}{1-\nu^2} (\varepsilon_y + \nu \varepsilon_x) \]  

(6-8)

\[ \tau_{xy} = -G \gamma_{xy} \]

where:  
\( E \) = The Young's modulus 
\( \nu \) = The Poisson's ratio and 
\( G \) = The coefficient of rigidity

The actually calculated residual stresses at various distances from the weld line in all three sectioned specimens are presented in the next chapter.
Photo 6.1 (above): The Data Acquisition System

Photo 6.2 (right): Welding Equipment
CHAPTER VII
RESULTS AND CONCLUSIONS

7.1 Experimental Results

Results of all the experiments will be presented and discussed in this section. Edge welding was performed on all the instrumented specimens using the same welding conditions (described in Table 6.6). The resulting temperature distributions are plotted in Figures 7.1, 7.4, 7.7 and 7.12 and are - not surprisingly - very similar for all four specimens. As was already discussed in the previous chapter, the short-circuiting nature of dip transfer welding introduced a significant amount of noise in the thermocouple readings during welding. Thus, for clarity, it was decided not to plot the very first part of the temperature history.

The strain gage readings taken during the welding experiments are plotted in Figures 7.2, 7.5, 7.8, 7.9, 7.13 and 7.14 for all four specimens. Noise problems with the strain gage conditioners were not encountered. It should be noted here that the observed slightly different strain readings on the two sides of specimens #3 and #4 are due to bending caused by the initial deviation of the plate from the straight-line path of the welding arc. On the back side of specimens #3 and #4 only the four strain gages closest to the weld line were monitored due to problems with one strain gage conditioner.

The strain gage readings were further corrected for temperature-induced apparent strain and gage factor variations in the way presented in section 6.7.1. Compensated strains for
all specimens are plotted in Figures 7.3, 7.6, 7.10, 7.11, 7.15 and 7.16.

Stress relieving heat treatments were performed on specimens #1, #3 and #4 in an electric furnace at various holding temperatures. Thermocouple and strain gage readings were continuously taken in all cases. The thermocouple readings are plotted versus time in Figures 7.17, 7.20 and 7.25. The noisy readings above 1000°F are due to the fact that the glass insulation of the thermocouple wires was almost destroyed since it was subject to very high temperatures caused by radiant heating from the "red-hot" furnace elements.

Uncompensated strain gage readings for specimens #1 and #3 are plotted in Figures 7.18, 7.21 and 7.22 whereas corrected strains are given in Figures 7.19, 7.23 and 7.24. Strain gage readings for specimen #1, however, should be viewed with some reservation, since the gages were not covered and thus were subject to radiant heating from the furnace elements. In specimen #3 gages were covered with "fiberfrax" insulation and always encountered temperatures inside their permissible range. Strain gages in specimen #4 were destroyed when the plate reached temperatures between 550°F and 600°F. New gages had to be installed before cutting.

Specimens #1, #2 and #4 were cut in order to calculate the distribution of residual stresses after welding and stress relieving. The strain gage readings before and after cutting are summarized in Table 7.1. The residual stresses, calculated in the way presented in section 6.7.2, are also given in the
same Table and are plotted in Figure 7.26(a) and (b) (Longitudinal and transverse respectively).

Finally it should be mentioned that specimen #5 was heated at various temperatures in order to investigate the uniformity of the heating attainable in the furnace. It was noticed that a gradient of 5 to 45°F existed across the length of the specimen, depending on the level and the rate of increase of temperatures. More "fiberfrax" insulation around the door reduced slightly these gradients. Further more it was noticed that there existed almost no temperature gradient through the thickness of the specimen. This was evidenced by the identical measured temperatures on the surface and halfway through the thickness (where thermocouple #3 was embedded).
WELDING OF SPECIMEN #1

Figure 7.1: Thermocouple readings during welding of specimen #1.

[*]. Note: The key to the curves of Figure 7.1 also holds for Figures 7.2 to 7.25.
Figure 7.2: Uncompensated strain gage readings during welding of specimen #1.
WELDING OF SPECIMEN #1

Figure 7.3: Strains during welding of specimen #1 (corrected for temperature induced apparent strain and gage factor variations)
Figure 7.4: Thermocouple readings during welding of specimen #2.
Figure 7.5: Uncompensated strain gage readings during welding of specimen #2
Figure 7.6: Strains during welding of specimen #2 (corrected for temperature-induced apparent strain and gage factor variations)
Figure 7.7: Thermocouple readings during welding of specimen #3
Figure 7.8: Uncompensated strain gage readings during welding of specimen #3, front side
Figure 7.9: Uncompensated strain gage readings during welding of specimen #3, back side
Figure 7.10: Strains during welding of specimen #3, front side (corrected for temperature induced apparent strain and gage factor variations)
Figure 7.11: Strains during welding of specimen #3, back side (corrected for temperature induced apparent strain and gage factor variations)
WELDING OF SPECIMEN #4

Figure 7.12: Thermocouple readings during welding of specimen #4
Figure 7.13: Uncompensated strain gage readings during welding of specimen #4, front side
Figure 7.14: Uncompensated strain gage readings during welding of specimen #4, back side
WELDING OF SPECIMEN #4

Figure 7.15: Strains during welding of specimen #4, front side (corrected for temperature induced apparent strain and gage factor variations)
WELDING OF SPECIMEN #4

Figure 7.16: Strains during welding of specimen #4, back side (corrected for temperature induced apparent strain and gage factor variations)
STRESS RELIEVING OF SPECIMEN #1

Figure 7.17: Thermocouple readings during stress-relieving of specimen #1
STRESS RELIEVING OF SPECIMEN #1

Figure 7.18: Uncompensated strain gage readings during stress-relieving of specimen #1
STRESS RELIEVING OF SPECIMEN #1

Figure 7.19: Strains during stress-relieving of specimen #1 (corrected for temperature-induced apparent strain and gage factor variations)
STRESS RELIEVING OF SPECIMEN #3

Figure 7.20: Thermocouple readings during stress-relieving of specimen #3
STRESS RELIEVING OF SPECIMEN #3

Figure 7.21: Uncompensated strain gage readings during stress-relieving of specimen #3, front side
STRESS RELIEVING OF SPECIMEN #3

Figure 7.22: Uncompensated strain gage readings during stress-relieving of specimen #3, back side
Figure 7.23: Strains during stress-relieving of specimen #3, front side (corrected for temperature induced apparent strain and gage factor variations)

STRESS RELIEVING OF SPECIMEN #3
STRESS RELIEVING OF SPECIMEN #3

Figure 7.24: Strains during stress relieving of specimen #3, back side (corrected for temperature induced apparent strain and gage factor variations)
Figure 7.25: Thermocouple readings during stress-relieving of specimen #4
Figure 7.26 (a): Comparison of longitudinal residual stresses after welding and stress relieving (1100°F)
Figure 7.26 (b): Comparison of transverse residual stresses after welding and stress relieving (1100°F)
Table 7.1: Strain gage readings before and after cutting

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Key: / : Gage not installed
* : Gage destroyed during cutting
7.2 **Comparisons with Predictions of the One-Dimensional Program**

The predictions of the one-dimensional program for the case of edge welding and subsequent stress relieving are presented in this section. Welding conditions were assumed exactly the same as in the experiments and temperatures, strains and stresses were calculated across a center strip of the specimen throughout welding and stress relieving operations.

The actually used input data can be found in the end of Appendix C. A range of values was found in the literature for the arc efficiency, \( n_a \), and surface heat loss coefficient, \( H \), \([1],[37],[135]\)). Since, no experimental measurements of these parameters were made in this study, the actually selected values - more or less within that range - were such as to minimize the deviation of the predicted temperature history from the experimentally measured one.

The predicted temperatures, mechanical strains and stresses during welding are plotted in Figures 7.27, 7.28, 7.29. For ease of comparison with experimental data the same locations (0.5, 1.0, 1.5, 2.0 and 3.0 inches from the weld line) were selected.

During stress relieving at 500°F the assumed temperature history is shown in Figure 7.30 and the predicted variations in stress and mechanical strain are plotted, versus time, in Figures 7.31 and 7.32. A comparison of the predicted residual stress distribution after welding and after stress relieving are given in Figure 7.33. The assumed temperature history and the respective predictions for stress relieving at 1100°F can
be found in Figures 7.34 to 7.37. It should, however, be noted that creep was taken into account only in the latter case \((1100^\circ F)\) where creep properties were available (Appendix A).
Figure 7.27: Temperatures during edge welding, as predicted by the one-dimensional program.
Figure 7.28: Mechanical strains during edge welding as predicted by the one-dimensional program
Figure 7.29: Stresses during edge welding as predicted by the one-dimensional program
Figure 7.30: Temperatures during stress-relieving at 500°F
Figure 7.31: Stresses during stress relieving at 500°F, as predicted by the one-dimensional program
Figure 7.32: Mechanical strains during stress relieving at 500°F, as predicted by the one-dimensional program
Figure 7.33: Comparison of residual stresses before, during and after stress relieving at 500°F, as predicted by the one-dimensional program.
Figure 7.34: Temperatures during stress relieving at 1100°F
Figure 7.35: Stresses during stress relieving at 1100°F, as predicted by the one dimensional program
Figure 7.36: Mechanical strains during stress reliefing at 1100°F as predicted by the one-dimensional program.
Figure 7.37: Comparison of residual stresses before, during and after stress relieving at 1100°F as predicted by the one-dimensional program.
7.3 Conclusions and Recommendations for Future Research

As evidenced in the previous sections, the correlation between the experimental results and the predictions of the one-dimensional model for temperatures strains and stresses during welding and subsequent stress relieving is quite good. However there exist a number of possibilities for further improvements, extensions and modifications:

(a) Further sensitivity analysis should be performed to investigate the effect of parameter variation on the model performance. Specifically, for example, it was noticed that small changes of the surface heat loss coefficient drastically affect the cooling rates. Furthermore the arc efficiency which is strongly dependend on the type of welding process, directly determines the heat input to the weld and thus the maximum temperatures attained.

(b) It is rather a straight forward procedure to modify the computer code so as it can accept any temperature history (as long as the temperature distribution is uniform over the specimen at any given time instant). However, it should be slightly more difficult to analyze the stress relaxation during localized heat treatments. Heat flow analysis - similar to the welding case - should be performed in the case of flame heating for example. As soon as the temperature distribution and history is known, however, the general stress analysis presented in chapter V is directly applicable.

(c) If the assumptions, on which the development of the one-dimensional model was based, are not any longer satisfied
two- or three-dimensional heat flow and stress analyses should be performed. A finite element model should then most possibly be appropriate. This would be the case for the localized heating of a thick plate or a pipe for example.

(d) Direct application of the developed model to the case of high strength steels, and HY-130 in particular, was prevented due to the lack of comprehensive creep data for these materials. The available information is summarised in Appendix A.

Stress relieving experiments on HY-130 (both uniform and flame heating) are, however, currently performed at M.I.T. and will be described in [128].

(e) The underlying objective of this part of the study was to analyze the effectiveness of the various stress relieving heat treatments and to identify possible improvements. However, it is also hoped that it is one step towards the development of a rational procedure for the selection of optimal stress relieving treatments that would give maximum residual stress relaxation with minimal effects on the integrity of the welded structure.
PART IV

ECONOMIC ANALYSIS
CHAPTER VIII

ECONOMIC ASPECTS OF WELDING

8.1 Introduction

In a construction industry, such as shipbuilding, welding operations can account for 10 to 20% of the total fabrication time and welding departments usually employ more than 10% of the total labor force. These relatively high percentages can only dictate efforts towards cost reductions and/or productivity increases in the welding sector.

The introduction of high deposition rate processes, such as submerged arc or electroslag welding, together with special procedures, such as one sided welding or narrow gap welding were some initial steps towards increased productivity and efficiency. These developments should certainly be viewed as coupled to the major advancements in production technology that occurred during the past few decades, such as rationalization of facilities and layout, prefabrication of large units, introduction of numerical control etc..

Nevertheless, the construction industry, and especially shipbuilding was very slow to adopt, in large scale production, the recent advances in high energy processes (Electron Beam and Laser Welding), automation and robotics, that would certainly boost productivity. This reluctance should be attributed to the low technology nature of the construction industry, the large investments necessary and the bad market conditions of 1970's, as well as to the technical difficulties of implementation.

When assessing the advantages of a new welding process or
procedure, however, it is necessary to be able to estimate comparative costs savings and productivity increases. Further, the welding costs must be accurately determined since they represent a part of the total product or job costs, and as such are necessary in price setting and bidding.

The next sections of this chapter deal with the determination of welding costs and the factors that affect them.

8.2 The Elements of Welding Cost

The costing of welds and weldments should be done according to generally accepted accounting principles and must fit into the cost accounting practices of a particular company or activity. In general, however, the cost of welding, and any other industrial process as well, includes the cost of direct materials and direct labor and a fair share of the indirect production costs (overhead costs).

The direct material costs include the costs of filler metals, shielding gas, fluxes and other miscellaneous materials (e.g. guide tubes in consumable guideelectroslag welding or ferrules and studs in arc stud welding) directly consumed in the welding process. The basis for the determination of material costs is usually the amount of weld metal that must be deposited to produce the welded joint. In autogenous welding, where no filler metal is deposited the total weld length is used for the same purpose.

The direct labor costs are the ones that can be directly traced or related to welding operations. The basis for labor costing is time (time per weld, or time per unit length or time
to weld a part). When a time-rate wage system is used the time directly translates to labor costs. When another wage system is employed, as payment by results for example, again labor costs can be related to time per part or to parts welded per unit of time. In the determination of time the most relevant parameters are the rate of depositing weld metal and welding speed.

Overhead costs include all the indirect production costs such as indirect labor (supervisors, janitors, inspectors, toolroom personnel, timekeepers), indirect material costs and such services as heating, lighting, power, maintenance, depreciation, taxes and insurance related to assets used in the fabrication process. Additionally distribution costs (marketing and selling) and general and administrative costs also are included in the full cost of a welded product. The basis for allocating these overhead costs varies depending on the practices of the company and the nature of the cost. Usually these costs are prorated according to the direct labor involved in fabricating the part, using a predetermined overhead rate. Extensive discussion of this subject can be found in the various cost accounting texts, [129],[130].

At this point, it should be emphasized that the cost of a specific weld is not necessarily the only cost that must be determined to establish the cost of a weldment. The latter includes the cost of the weld and also the material required for the weldment, the preparation of the parts prior to welding, and the postweld treatment that might be required. Joint preparation varies according to the material thickness and to
joint design. Also some processes, such as electroslag and 
electrogas welding, require less accurate fit up and preparation 
than others. Postweld treatment includes final machining 
grinding and polishing, heat treating, shot blasting and possibly 
straightening. Some processes and some materials require more 
(or less) postweld treatment which influences the total cost of 
the weld and the weldment.

Although detailed analysis of the elements of welding cost 
and the factors influencing them, will be presented in the next 
few sections, some general comments are due here. Specifically 
it should be noted that field welding costs more than shop 
welding and welding in the horizontal, vertical, or overhead 
positions cost more than welding in the flat positions. Further, 
the local working conditions, availability of equipment, 
experience and skill of the welders, local power rates, special 
code requirements, weather and temperature conditions and 
industrial regulations might drastically affect the costs as 
well.

8.3 Material Costs
8.3.1 Weld Metal Requirements

For processes where filler metal is deposited, the basis 
for the calculation of the material cost is the amount of weld 
metal deposited in the joint. The latter can be estimated if 
the cross sectional area of the deposit, the length of the weld 
and the density of the weld metal are known. Specifically:

\[(W.D.) = (C.S.A.) (S.W.) (R.F.) \cdot a\]  

(8-1)
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<td>DOUBLE J</td>
<td>( \text{CSA} = \frac{1}{4}(T - 2R - RF)^2 \tan (A + R)(T - 2R - RF) + \frac{1}{2}\pi R^2 + RO \times T )</td>
</tr>
</tbody>
</table>

Figure 8.1: Cross sectional areas for various designs
where: W.D. = Weight of deposit per unit length (lb/ft)

C.S.A. = Cross-sectional area (in$^2$)

S.W. = Specific weight of weld metal (lb/in$^2$)

R.F. = Reinforcement factor

a = Constant (12 for the units used)

The cross-sectional area can be calculated using straightforward geometric formulas if the exact joint preparation is known. Some cases are shown in figure 8.1 but more detailed tables can be found in the bibliography, [131].

The reinforcement factor has to be added to account for the fact that the weld surface will not be flush. A value of reinforcement of 10% is usually added to single groove welds and of 20% to double groove ones. 10% reinforcement is also added to fillet welds.

The equation (8-1) can be readily applied in the comparison of the material costs of various weld designs. However for more accurate calculations, test welds must be performed.

8.3.2 Filler Metal

The weight of the filler metal required is greater than the weight of the weld metal deposit. This is due to a loss of filler metal through spatter and slag formation and due to the unused electrode stub. The amount of this loss is accounted for by the deposition efficiency factor which is also called filler metal yield or recovery rate, and is the ratio of the weight of the deposited weld metal divided by the gross weight of the filler metal used.

Specifically the filler metal cost per unit length of weld
seam deposited, $C_{FM}$, is

$$C_{FM} = \frac{(W\cdot D)}{(Y_{FM \%})} \quad P_{FM} \quad (\text{in } \$/\text{ft}) \quad \quad \quad (8-2)$$

where:

- $W\cdot D$ = Weight of deposit per unit seam length (lb/ft)
- $Y_{FM \%}$ = Filler metal yield (%)
- $P_{FM}$ = Price of filler metal per unit of weight ($/lb$)

Filler metal yield varies with the process as can be seen in Table 8.1. The covered electrodes have the lowest yield of 55% to 75% due to a 7% to 15% end stub loss, 10% to 50% coating or slag loss and a 5% to 10% spatter loss. End losses are minimized when using continuous electrode wire where the scrap end weight is usually negligible compared to the total weight of the coil. Further the spatter loss is eliminated in submerged arc welding resulting in a 100% yield. In flux cored electrodes the deposition efficiency decrease to 75% or 85% due to the flux which is consumed and lost as slag.

An alternative way of calculating the cost of filler metal per unit length, $C_{EL}$, using short electrodes is based on the number of electrodes needed to produce a unit of weight of weld deposit, $B$ (electrodes/lb), and the price per electrode, $P_{EL}$ ($/electrode)$.

$$C_{EL} = (W\cdot D) \cdot B \cdot P_{EL} \quad (\text{in } \$/\text{ft}) \quad \quad \quad (8-3)$$

For the case of continuous wire processes another approach can also be followed. Specifically the weight of filler metal required per hour is given by:
<table>
<thead>
<tr>
<th>Electrode Type and Process</th>
<th>Yield %</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Covered Electrode for:</strong></td>
<td></td>
</tr>
<tr>
<td>SMAW 14&quot; manual</td>
<td>55 to 65%</td>
</tr>
<tr>
<td>SMAW 18&quot; manual</td>
<td>60 to 70%</td>
</tr>
<tr>
<td>SMAW 28&quot; automatic</td>
<td>65 to 75%</td>
</tr>
<tr>
<td><strong>Solid Bare electrode for:</strong></td>
<td></td>
</tr>
<tr>
<td>Submerged arc</td>
<td>95 to 100%</td>
</tr>
<tr>
<td>Electroslag</td>
<td>95 to 100%</td>
</tr>
<tr>
<td>Gas metal arc welding</td>
<td>90 to 95%</td>
</tr>
<tr>
<td><strong>Tubular-flux cored electrode for:</strong></td>
<td></td>
</tr>
<tr>
<td>Flux cored arc welding</td>
<td>80 to 85%</td>
</tr>
</tbody>
</table>

Table 8.1  Filler metal yield-various types of electrodes.
\[ W_{FM} = \frac{V_{WF} \cdot a}{L_W} \quad \text{(in lb/hr)} \]  

where:  
\( V_{WF} \) = the wire feed speed (in/min)  
\( L_W \) = the length of wire per unit of weight (in/lb)  
\( a \) = constant (60 for the units used)  

The wire feed speed can be determined from charts, supplied by the wire feeder manufacturer, that relate the welding current to wire feed speed, depending on the size and composition of the electrode wire, the welding process and the molten metal transfer mode such a chart is shown in figure 8-2, adapted from [116]. The length per unit of wire weight is a physical property of the material and given in table 8-2 as a function of the wire diameter and type.

The weight of filler metal required per unit length of seam welded, \( W_{FM} \), can now be calculated:

\[ W_{FM} = \frac{W_{FM}}{V_{WT} \cdot a} \quad \text{(in lb/ft)} \]  

where:  
\( V_{WT} \) = the weld travel speed (in/min)  
\( a \) = constant (60 for the units used)  

8.3.3 Flux Requirements

The cost of flux in submerged arc, electroslag or oxy-fuel gas welding can be related to the weight of weld metal deposited and may be calculated as:

\[ C_{FLX} = P_{FLX} \cdot (W.D.) \cdot R_{FLX} \]  

where:  
\( C_{FLX} \) = The cost of flux per unit weld seam deposited  
\( ($/ft)$)
Figure 8-2: Wire feed speed vs current for stainless steel wires [116]
<table>
<thead>
<tr>
<th>WIRE DIAMETER</th>
<th>MATERIAL</th>
</tr>
</thead>
<tbody>
<tr>
<td>Decimal Inch</td>
<td>Fraction</td>
</tr>
<tr>
<td>0.020</td>
<td></td>
</tr>
<tr>
<td>0.025</td>
<td></td>
</tr>
<tr>
<td>0.030</td>
<td></td>
</tr>
<tr>
<td>0.035</td>
<td></td>
</tr>
<tr>
<td>0.040</td>
<td></td>
</tr>
<tr>
<td>0.045 3/64</td>
<td></td>
</tr>
<tr>
<td>0.062 1/16</td>
<td></td>
</tr>
<tr>
<td>0.078 5/64</td>
<td></td>
</tr>
<tr>
<td>0.093 3/32</td>
<td></td>
</tr>
<tr>
<td>0.125 1/8</td>
<td></td>
</tr>
<tr>
<td>0.156 5/32</td>
<td></td>
</tr>
<tr>
<td>0.187 3/16</td>
<td></td>
</tr>
<tr>
<td>0.250 1/4</td>
<td></td>
</tr>
</tbody>
</table>

Table 8-2: Length vs weight (inches per pound) of bare electrode wire of type and size shown.
\[ P_{FLX} = \text{The price of flux per unit weight ($/lb)} \]

\[ R_{FLX} = \text{The flux-to-steel weight ratio} \]

The flux ratio varies with the process and the flux used, being approximately 1.0 for submerged arc welding and 0.05 to 0.1 for electroslag or oxy-fuel gas welding processes.

8.3.4 Shielding Gas Requirements

The cost of shielding gas is directly related to the time required to make the weld and the specified flow rate, \( V_{S.G.} \). Specifically:

\[ C_{SG} = \frac{P_{SG} \cdot V_{SG}}{V_{WT} \cdot 5} \]  

(8-7)

where:

- \( C_{SG} \) = The cost of gas per unit length of weld ($/ft)
- \( P_{SG} \) = The price of gas ($/ft^3)
- \( V_{WT} \) = The weld travel speed (in/min)
- \( V_{SG} \) = The gas flow rate (ft^3/hr)

Slightly different formulas should be employed when using CO\(_2\) gas which is marketed in liquid form and sold per unit weight, or when calculating the total cost of shielding gas per weld.

8.4 Labor Costs

Welding, and particularly manual welding is a highly labor intensive manufacturing process. The cost of labor is probably the single greatest component in the total welding cost. The basis for the determination of labor costs is generally the time required to make a weld or a weldment.

Various wage systems are employed in production processes today but we can in general distinguish between the flat hourly wage and the productivity, or incentives related, wage systems.
<table>
<thead>
<tr>
<th>Sector</th>
<th>Nature of work</th>
<th>Supervision</th>
<th>Wage system</th>
</tr>
</thead>
<tbody>
<tr>
<td>Shipbuilding, steel construction</td>
<td>Long or numerous welds of the normal type</td>
<td>Foremen, inspectors</td>
<td>Piecework by length</td>
</tr>
<tr>
<td>Construction of machines and apparatus</td>
<td>Series of small workpieces always with equal welds</td>
<td>Foremen, inspectors</td>
<td>Piecework by no of pieces or bonus per piece</td>
</tr>
<tr>
<td>Container construction</td>
<td>Pressure tanks</td>
<td>Welding engineer, NDT inspection, X-ray tests</td>
<td>Flat rate with wage allowance</td>
</tr>
<tr>
<td>Car and vehicle construction</td>
<td>Series, car frames</td>
<td>Foremen, inspectors</td>
<td>Piecework by no of pieces</td>
</tr>
<tr>
<td>Construction of apparatus for the chemical industry</td>
<td>Corrosion-resistant joints</td>
<td>Welding engineer, all kinds of tests, ultrasonics, X-ray, crack and halogen tests</td>
<td>Flat rate with wage allowance</td>
</tr>
<tr>
<td>Pressure tanks, bridge building</td>
<td>Highly refractory steel, fine-grain steel, preheating of butt welds</td>
<td>Welding engineer, X-ray and crack tests</td>
<td>Productivity wage, flat rate with wage allowance</td>
</tr>
<tr>
<td>All sectors</td>
<td>Straightening</td>
<td>Foreman</td>
<td>Flat rate with wage allowance</td>
</tr>
<tr>
<td>All sectors</td>
<td>Tacking, one-off production</td>
<td>Foreman</td>
<td>As for assembly line workers, flat rate with wage allowance</td>
</tr>
<tr>
<td>All sectors</td>
<td>Tacking, series</td>
<td>Foreman</td>
<td>Piecework by no of pieces or workpiece bonus</td>
</tr>
</tbody>
</table>

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Detailed discussion on these systems can be found in [132] and [133]. Table 8-3 lists suitable wage systems for various sectors of the fabrication industry together with some information on the nature of the work.

Only the time rate systems will be examined in this section however. Specifically for a single-pass weld or for Gas Tungsten Arc or Plasma Arc welding processes where weld metal is not deposited the labor costs per unit length of seam welded arc (in $/ft).

\[ C_L = \frac{P_W}{V_{WT} \cdot (OF) \cdot a} \]  \hspace{1cm} (8-8)

where:  \( P_W = \) The welder pay rate (in $/hr)

\( V_{WT} = \) The weld travel speed (in in./min)

\( OF = \) The operator factor (%)

\( a = \) Constant (5 for the units used)

The welder pay rate may, or may not, include fringe benefits (as cost of insurance, holidays, vacations etc) and should be determined according to the accounting practices of the company or activity.

The weld travel speed is known from the welding procedure schedule. Finally the operator factor, or arcing factor, is the same as duty cycle, that is the percentage of arc time against the total allowed or paid time. Specifically

\[ OF = \frac{t_{arc}}{t_{arc} + t_{idle} + t_{electrode\ change} + t_{movement}} \]  \hspace{1cm} (8-9)

As can be seen from figure 8-3 operator factors vary
considerably depending on the nature of the process, arrangement of the work use of fixtures and positioners, and also on the location (field or shop).

When the welding procedure schedule is not available or when welding involves more than one pass the following equation should be used for the labor cost per unit length of weld deposited (in $/ft).

\[ C_L = \frac{P_W \cdot (W.D.)}{(D.R.) \cdot (O.F.)} \quad (8-10) \]

where :  \( P_W \) = The welder pay rate ($/hr)

\( W.D. \) = The weight of weld metal deposited per unit length (lb/ft)

\( D.R. \) = The deposition rate (lb/hr)

The deposition rate expresses the weight of filler metal deposited in a unit of time and can be calculated as:

\[ D.R. = \frac{V_{WF} \cdot a}{L_W \cdot Y_{FM}} \quad (lb/hr) \quad (8-11) \]

where :  \( V_{WF} \) = The wire feed rate or melt off rate (in./min)

\( L_W \) = The length of electrode per unit weight (in./lb) (table 8-2)

\( Y_{FM} \) = The filler metal yield (%)

\( a \) = Constant (60 for the units used)

Deposition rates for various processes are given in figure 8.4, plotted versus weld current. Accurate calculation however requires some test welds to be performed.

8.5 Power and Overhead Costs

Electric power cost is usually considered as a part of
<table>
<thead>
<tr>
<th>PROCESS</th>
<th>OPERATOR FACTOR (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>MANUAL</td>
<td></td>
</tr>
<tr>
<td>SEMIAUTOMATIC</td>
<td></td>
</tr>
<tr>
<td>MACHINE</td>
<td></td>
</tr>
<tr>
<td>AUTOMATIC</td>
<td></td>
</tr>
</tbody>
</table>

Figure 8-3: Operator factor for various processes

Figure 8-4: Deposition rate vs current for various processes
overhead expense. Specifically for welding, however, it is
sometimes considered a direct cost and is charged against the
particular job. In such a case the following equation should be
used for the cost of electric power per unit length of seam
welded: (in $/ft).

\[ C_{EP} = \frac{P_{EP} \cdot V \cdot I \cdot (W.D.)}{(D.R.) \cdot (O.F.) \cdot n_{PS} \cdot a} \]  

(8-12)

where:

- \( P_{EP} \) = The local power rate ($/kwh)
- \( V, I \) = The welding voltage and current
- \( (W.D.) \) = The weight of weld metal deposited per
  unit length (lb/ft)
- \( (D.R.) \) = The deposition rate (lb/hr)
- \( (O.F.) \) = The operator factor (%)
- \( n_{PS} \) = The power source efficiency (%)
- \( a \) = Constant (1000 for the units used)

The power source efficiency varies with the equipment size
and quality and is approximately as follows: [134]
d.c. welding generators 45-60%
a.c. welding generators 65-70%
welding rectifier 65-75%
welding transformers 75-85%

Overhead costs include as was already mentioned all the
costs that cannot be directly charged to the individual job or
weldment. These costs are allocated pro rata among all work
going through the plant. If the overhead rate is known (in $/hr)
then the total overhead cost per unit length of weld seam can
be calculated from equation (8-8), for single pass welding, or
equation (8-10), for multipass welding, with the overhead rate substituted for the welder pay rate.

8.6 Conclusions

The previous sections of this chapter focused on the calculation of the various elements of welding costs. The methods described should be used in order to compare different welding processes or procedures in terms of cost or efficiency. However, it should be noted that, as in all the cases of alternate choice decisions, only the costs that are actually different in the two alternatives should be taken into account. This is particularly true for the components of overhead costs, which do not always vary proportionally with direct labor costs, as the use of a standard overhead rate might superficially suggest.

Cost evaluation of the weld joint designs and the welding procedures should always be made, since weld metal is usually the most expensive metal involved in steel fabrication. The various possibilities for cost reductions are briefly highlighted in the next chapter.
CHAPTER IX
COST REDUCTIONS REALIZABLE THROUGH WELD METAL STRENGTH UNDERMATCHING

9.1 Introduction - Possibilities for Cost Reductions

As was mentioned in the previous chapter welding is a sector of the construction industry, where a small percentage of cost reduction or productivity improvement represents a significant overall cost saving. This is mainly due to the fact that welding is a highly labor intensive process.

Cost reductions can be realized in various ways. Some general guidelines that could be followed are:

(a) Eliminate welded joints whenever possible substituting them with rolled sections, formed plates, or small castings.
(b) Limit field welding by prefabricating larger units in the shop.
(c) Reduce the cross sectional area of welds, utilizing smaller root openings, smaller groove angles and double-instead of single-groove preparations.
(d) Utilize fillet welds with caution, since doubling their size and strength results in quadrupling their weight.
(e) Use positioners and fixtures to limit the extent of overhead or vertical welding that must be performed.
(f) Modify the design to permit easy accessibility to all welds.
(g) Reduce labor costs by utilizing, whenever applicable semi- or fully-automated welding processes and or welding robots.
(h) Limit the number of electrodes that should be used in the fabrication of a part.
(i) Avoid complex preparations or post welding treatments selecting proper weld and base metal combinations.

Particularly for high strength steels, however, the existing specifications require preheat and interpass temperature controls, electrode controls and post weld magnetic particle testing that unavoidably result in increased fabrication costs. Most of these requirements are results of the well established philosophy of weld metal strength overmatching. However, as was shown by various investigators, whose work was presented in chapter II, some of these requirements can be relaxed, or eliminated, when a lower yield strength filler metal is used in conjunction with a lower hydrogen process.

A more detailed analysis of the possibilities for cost reductions in the fabrication of HY-80 steel, through weld metal strength undermatching, will be presented in the next few sections of this chapter.

9.2 Preheating and Preheat Control

9.2.1 Existing Preheating Requirements for HY-80 steels

The reason for preheating in HY-steels systems is to reduce cracking. It is generally believed that preheating results in slower cooling rates and thus permits greater quantities of hydrogen to diffuse from the weld zone. Additionally, the more uniform cooling results in lower thermal stresses and thus reduces the likelihood of cracking.

The existing preheat requirements when welding HY-80 steels are summarized in table 9-1. Lower preheating temperatures are accepted for thinner sections since the diffusion path is
<table>
<thead>
<tr>
<th>THICKNESS</th>
<th>PREHEAT/INTERPASS MINIMUM</th>
<th>PREHEAT/INTERPASS MAXIMUM</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>MIL-11018</td>
<td>MIL-9018</td>
</tr>
<tr>
<td>1 1/8&quot; and over</td>
<td>200°F</td>
<td>150°F</td>
</tr>
<tr>
<td>From 1 1/8&quot; to 1/2&quot;</td>
<td>125°F</td>
<td>150°F</td>
</tr>
<tr>
<td>1/2&quot; and less</td>
<td>60°F</td>
<td>60°F</td>
</tr>
</tbody>
</table>

(1) Post weld NDT is not required when using austenitic electrodes

Table 9-1: HY-80 Preheat requirements
reduced and the level of restraint is lowered. Also lower levels of preheat are permitted when welding with lower yield strength electrodes (MIL-9018) and lower hydrogen processes (G.M.A. and submerged arc welding). As was mentioned in earlier chapters, lower yield strength electrodes would result in lower residual stresses. Therefore, since hydrogen cracking is directly related to the level of imposed or residual stresses, the use of lower yield-strength filler metal should effectively reduce cracking. Therefore a corresponding reduction in preheat should be tolerated. Further it should then be possible even to eliminate preheat for certain combinations of material thicknesses and joint designs. This is where substantial savings would result, as will be analyzed in the next section.

9.2.2 Cost Reductions Realizable Through the Elimination of Preheat

The cost of preheating is the single most significant factor contributing to the higher cost of fabricating HY-80 steel structures. Thus the elimination of preheating, even in some cases only, would substantially reduce fabrication costs. The simple reduction of the level of preheat would marginally reduce costs, however, since it would only lower the power consumption but would not affect other more significant cost elements.

Specifically the main elements of the preheating cost are the following:

(a) Capital cost of the necessary facilities, such as the central power station and switch gear.
(b) Cost of electric power for preheating, which largely exceeds the power costs of the welding operations.

(c) Capital and replacement costs of the preheating devices, which have relatively short lifespan. The cost of the necessary temperature control devices would also be included here.

(d) The direct labor costs of applying and supervising the heating operation.

Additionally the preheating operation increases the fabrication costs through:

(a) Reduced productivity due to the high temperature environment in which the welders would have to work. Temperatures between 200°F and 300°F are usually specified making it practically impossible to weld in a tight spot or an enclosed area.

(b) Scheduling problems due to the trades disruption caused by preheating. The areas being preheated are not accessible to other trades, when in high temperature.

(c) Delays in the outfitting phase caused when welding attachments to the basic HY-80 structures an operation which also requires preheating.

It should be emphasized again, at this point, that the simple reduction of the level of preheating in thick plate butt welds, possible when using lower strength electrodes, has only a minimal effect on the total cost. These welds are usually performed in open unrestricted areas, most often during prefabrication. Furthermore they only represent a small percentage of the total welding that must be performed.

There are, however, numerous attachments, brackets,
stiffeners or foundations, usually made of a lower yield strength steel, which have to be welded on the basic HY-80 structures. It is believed that use of lower yield strength electrodes would permit the total elimination of preheating in these welds. Such an improvement would have a drastic effect on the total cost.

9.3 **Electrode Selection and Moisture Controls**

The existing specifications for electrode storage and handling require that electrodes should be baked after they are received from the manufacturer, and kept in special dry conditioners in order to ensure that their initial moisture level is minimal. Furthermore once issued they can only be exposed to the atmosphere for five hours and should then be rebaked. This results in issuing electrodes at least twice during the normal eight hour shift, a practice which certainly disrupts the work and reduces productivity.

Lower yield strength metals used in electrodes with special moisture resistant coatings were shown to permit exposure periods over the eight hour shift. This would certainly improve scheduling and productivity.

Additionally lower strength filler metal can permit the use of a single electrode (e.g. E 9018) for joining to HY-80 other steels of lower yield strength (35 to 55 ksi). This would alleviate the problem of having to identify the various materials during fabrication and to use a different electrode for each combination and therefore would certainly reduce the fabrication costs drastically.
The single electrode would undermatch HY-80, but would overmatch the lower yield strength attachments. This would cause no problem, however, since these steels do not have a microstructure sensitive to hydrogen cracking.
REFERENCES


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64. N.S.R.D.C. reports on "Stress Relief Embrittlement of AX-140 and E-11018 Weld Metals", and "Stress Relief Characteristics of a 5% Ni Weld Metal", obtained after private communication October 1981.


76. V.I. Makhnenko and N.I. Pivtorak, "Redistribution of


114. Iron and Steel Institute, Committe of Stainless Steels Producers, "High-Temperature Characteristics of Stainless


133. S. Roweden, "Wages and Incentives", ibid.


A.1 HY-80 Steel

HY-80 is a low alloy Ni-Cr-Mo steel of a minimum yield strength of 80 Ksi (552 MPa) and excellent toughness. It is the primary U.S. Navy hull construction material and achieves its strength and toughness through a quenching and tempering heat treatment.

Table A.1 summarizes the compositional ranges for the steel. Mechanical properties specifications are, according to MIL-S-16216G, outlined in Table A.2.
TABLE A.1 Compositional Ranges of HY-80, HY-130 and 304 Stainless Steel  (weight, %)

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td>0.18 max</td>
<td>0.08-0.12</td>
<td>0.08 max</td>
</tr>
<tr>
<td>Mn</td>
<td>0.10-0.40</td>
<td>0.60-0.90</td>
<td>2.00 max</td>
</tr>
<tr>
<td>Si</td>
<td>0.15-0.35</td>
<td>0.20-0.35</td>
<td>1.00 max</td>
</tr>
<tr>
<td>Ni</td>
<td>2.00-3.25</td>
<td>4.75-5.25</td>
<td>8.00-10.50 (or 11.0)</td>
</tr>
<tr>
<td>Cr</td>
<td>1.00-1.80</td>
<td>0.40-0.70</td>
<td>18.00-20.00</td>
</tr>
<tr>
<td>Mo</td>
<td>0.20-0.60</td>
<td>0.30-0.65</td>
<td>0.50 max</td>
</tr>
<tr>
<td>V</td>
<td>0.03 max</td>
<td>0.05-0.10</td>
<td>-</td>
</tr>
<tr>
<td>S</td>
<td>0.025 max</td>
<td>0.010 max</td>
<td>0.030 max</td>
</tr>
<tr>
<td>P</td>
<td>0.025 max</td>
<td>0.010 max</td>
<td>0.045 max</td>
</tr>
<tr>
<td>S+P</td>
<td>0.045 max</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Ti</td>
<td>0.02 max</td>
<td>0.02 max</td>
<td>-</td>
</tr>
<tr>
<td>Cu</td>
<td>0.25 max</td>
<td>0.25 max</td>
<td>-</td>
</tr>
</tbody>
</table>
Table A.2: Specification Limits of HY-80 Mechanical Properties [2]

<table>
<thead>
<tr>
<th>PROPERTY</th>
<th>PLATE THICKNESS</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Less than 5/8 in. (16mm) 5/8 in. (16mm) and over</td>
</tr>
<tr>
<td>Ultimate Strength</td>
<td>For information</td>
</tr>
<tr>
<td>Yield Strength at 0.2% Offset</td>
<td>80 to 100 Ksi (552 to 690 MPa)</td>
</tr>
<tr>
<td></td>
<td>80 to 95 Ksi (552 to 655 MPa)</td>
</tr>
<tr>
<td>Min. Elongation in 2 in. (50mm)</td>
<td>19%</td>
</tr>
<tr>
<td></td>
<td>20%</td>
</tr>
<tr>
<td>Reduction in area</td>
<td></td>
</tr>
<tr>
<td>Longitudinal</td>
<td>--</td>
</tr>
<tr>
<td></td>
<td>55%</td>
</tr>
<tr>
<td>Transverse</td>
<td>--</td>
</tr>
<tr>
<td></td>
<td>50%</td>
</tr>
</tbody>
</table>

Charpy V-Notch Energy Requirements

<table>
<thead>
<tr>
<th>Plate Thickness</th>
<th>Specimen Size</th>
<th>Absorbed Energy Minimum</th>
<th>Test Temperature</th>
</tr>
</thead>
<tbody>
<tr>
<td>1/4 in. (6mm) to 1/2 in. (13mm) Excl.</td>
<td>10 x 5 mm (0.4x0.2 in.)</td>
<td>For information</td>
<td>-120°F (-84°C)</td>
</tr>
<tr>
<td>1/2 in. (13mm) to 2 in. (50mm) Incl.</td>
<td>10 x 10 mm (0.4x0.4 in.)</td>
<td>50 ft-lb</td>
<td>-120°F (-84°C)</td>
</tr>
<tr>
<td>Over 2 in. (50mm)</td>
<td>10 x 10 mm (0.4x0.4 in.)</td>
<td>30</td>
<td>-120°F (-84°C)</td>
</tr>
</tbody>
</table>
A.2 HY-130 Steel

HY-130 steel is a Naval hull-construction steel with a minimum yield strength between 130 and 150 Ksi (895 MPa to 1030 MPa), also referred to, in different stages of development, as 5 Ni-Cr-Mo-V, HY-150, HY-140, HY-130/150 and HY-130(T). The steel is to be used in a quenched and tempered condition, in which the microstructure is primarily tempered martensite, as in the case of HY-80 steel.

The compositional ranges of this steel are given in Table A.1. Some as-received mechanical properties are shown in Table A.3 and Temperatures for thermal treatments in Table A.4.

Mechanical and physical properties of HY-130 at room and elevated temperatures are plotted in Figures A.1 and A.2.

Finally, the minimum observed creep rate for various temperatures and various applied stresses appears in Figures A.3 (a) and (b) adapted from [100] and [101].
<table>
<thead>
<tr>
<th>TABLE A.3 : General Properties of HY-130 Type Steel [107]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Yield Strength</td>
</tr>
<tr>
<td>min 130 Ksi (895 MPa)</td>
</tr>
<tr>
<td>at center of 4in (101.6mm) plate</td>
</tr>
<tr>
<td>Elongation</td>
</tr>
<tr>
<td>15-20% in 2in. (50mm)</td>
</tr>
<tr>
<td>Reduction of Area</td>
</tr>
<tr>
<td>50-64% transversely</td>
</tr>
<tr>
<td>70% through thickness</td>
</tr>
<tr>
<td>Charpy-V-Notch Impact</td>
</tr>
<tr>
<td>Energy Absorbsion</td>
</tr>
<tr>
<td>60ft-lb (87.3J) at 0°F (-17.8°C)</td>
</tr>
<tr>
<td>(ductile fracture region)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>TABLE A.4 : Thermal Treatment Related Properties for HY-130</th>
</tr>
</thead>
</table>
| \[
| A_{cl} \quad \text{Temperature} & 1210^\circ F \ (654^\circ C) \\
| A_{c3} \quad \text{Temperature} & 1415^\circ F \ (768^\circ C) \\
| M_s \quad \text{Temperature} & 715^\circ F \ (379^\circ C) \\
| \] |
| Recommended final austenitizing temperature              |
| 1500°F (815°C)                                            |
| Recommended Quenching                                     |
| Medium                                                     |
| Water                                                      |
| Microstructure (as quenched, midthickness)                |
| 0.5in (12.2mm) plate                                      |
| 100% martensite                                           |
| 4.0in (101mm) plate                                       |
| 60-75% martensite, remainder bainite                      |
| Recommended tempering                                     |
| temperature range                                         |
| 1000-1150°F (538-621°C)                                  |
Figure A.1 (a) Variation of virgin yield stress with temperature for HY-130
(b) Variation of Young's modulus with temperature for HY-130
(c) Variation of tangent modulus with temperature for HY-130
(d) Variation of Poisson's ratio with temperature for HY-130
Figure A.2  (a) Variation of thermal conductivity with temperature for HY-130 
(b) Variation of specific heat with temperature for HY-130 
(c) Variation of density with temperature for HY-130
Figure A.3(a): Minimum creep rate at various temperatures and levels of applied stress, for HY-130(T) standard 0.25 in. dia. specimens. [101]

Figure A.3(b): Minimum creep rate at various temperatures and levels of applied stress, for HY-130(T) 1 in. thick plates. [100],[101]
Table A.5: Typical Mechanical Properties of Annealed 304 Stainless Steel at Room Temperature [109]

<table>
<thead>
<tr>
<th>Form</th>
<th>U.T.S.</th>
<th>Yield Strength</th>
<th>Elongation</th>
<th>Hardness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Ksi</td>
<td>MPa</td>
<td>Ksi</td>
<td>MPa</td>
</tr>
<tr>
<td>Bar</td>
<td>85</td>
<td>586</td>
<td>35</td>
<td>241</td>
</tr>
<tr>
<td>Plate</td>
<td>82</td>
<td>565</td>
<td>35</td>
<td>241</td>
</tr>
<tr>
<td>Sheets</td>
<td>84</td>
<td>579</td>
<td>42</td>
<td>290</td>
</tr>
<tr>
<td>Strips</td>
<td>84</td>
<td>579</td>
<td>42</td>
<td>290</td>
</tr>
<tr>
<td>Tubing</td>
<td>85</td>
<td>586</td>
<td>35</td>
<td>241</td>
</tr>
<tr>
<td>Wire</td>
<td>90</td>
<td>621</td>
<td>35</td>
<td>241</td>
</tr>
</tbody>
</table>

Table A.6: Thermal Treatment Temperatures for 304 St.Steel [108],[109].

- Initial Forging Temperature: 2100-2300°F (1149-1260°C)
- Annealing Temperature: 1850-2050°F (1010-1121°C)
- Stress Relief Ann. Temperature: 400-750°F (204-399°C)
- Melting Range: 2550-2650°F (1399-1454°C)
- Carbide Precipitation range: 800-1600°F (427-871°C)
A.3 304 - Stainless Steel

304 Stainless Steel is a low-carbon (max 0.08% C), unstabilized austenitic stainless steel specially developed for better corrosion resistance and for restriction of carbide precipitation during welding.

Chemical composition ranges are shown in Table A.1. Mechanical properties of annealed material at room temperature are given in Table A.5, and physical properties in Table A.7. Thermal treatment temperatures are shown in Table A.6. Physical and mechanical properties at both ambient and elevated temperatures are plotted in Figure A.4 to A.10 adapted from [108] through [113]. It should be noted, however, that in many instances data from different sources were not in complete agreement, reflecting the normal variations from heat-to-heat of the alloy and differences between the experimental procedures of different laboratories. In such cases either all the different data were presented, with their sources cited, or judgment was used, in order to obtain a single compromise curve for use in the computer modelling.

Creep data adapted from [100], [101], and [114] appear in Figures A.11 and A.12.

No data were found in the literature for the temperature dependence of density. Figure A.13 gives the assumed variation of density, calculated from the thermal expansion data of Figure A.9 and the known density at room temperature (Table A.7).
Table A.7: Typical Physical Properties of 304 Stainless Steel, [108],[109],[110]

Density ($\rho$) ....................... $0.29 \text{ lb/in}^3 (8000 \text{ Kg/m}^3)$
Elastic Modulus (E) ................. $28.0 \times 10^3 \text{ Ksi (1.93} \times 10^5 \text{ MPa)}$
Tangent Modulus ($E_T$) ............. $0.73 \times 10^3 \text{ Ksi (5} \times 10^3 \text{ MPa)}$
Average Thermal ..................... $[32^\circ \text{F to 212}^\circ \text{F (0}^\circ \text{C to 100}^\circ \text{C)}]$
Expansion Coefficient ($\alpha$) ...... $9.6 \mu \text{ in/in}^\circ \text{F (17.2} \mu\text{m/m}^\circ \text{C)}$
Thermal Conductivity
at $212^\circ \text{F (100}^\circ \text{C)}$ ($\lambda$) ............... $9.4 \text{ Btu/hr ft}^\circ \text{F (16.2} \text{ W/m}^\circ \text{K)}$
Specific Heat ($c_p$)
[32 to 212$^\circ \text{F (0 to 100}^\circ \text{C)}$] ........ $0.12 \text{ Btu/lb}^\circ \text{F (0.5} \text{ KJ/Kg}^\circ \text{K)}$
Figure A.4: Variation of virgin yield stress with temperature for 304 stainless steel. [110], [112], [113]
Figure A.5: Variation of ultimate tensile strength with temperature for 304 stainless steel. [110], [112]
Figure A.6: Variation of Young's modulus with temperature for 304 stainless steel. [108],[112]
Figure A.7: Variation of tangent modulus with temperature for 304 stainless steel. [111]
Figure A.8: Variation of thermal conductivity with temperature for 304 stainless steel.
Figure A.9 : Average thermal expansion coefficient for 304 SS
Figure A.10: Variation of specific heat with temperature for 304 stainless steel.
Figure A.11: Creep rate curve for 304 stainless steel. Adapted from [114].

Figure A.12: Stress vs. rupture-time and creep-rate curves for annealed type 304 stainless steel. [114]
Figure A.13: Variation of density with temperature for 304 stainless steel (based on thermal expansion)
APPENDIX B

NUMERICAL INTEGRATION

In the program the various integrations are performed numerically for twenty one integration points, unequally spaced along the breadth of the plate. The numerical integration scheme used was based on Newton-Cotes closed integration formulas and was adapted by Imakita from Isoda [115].

In general, if the integral

$$ I = \int_{x_1}^{x_N} f(x) \, dx \quad (B-1) $$

is to be evaluated given the set of data $[x_i, f(x_i)]$, $i=1,2,\ldots,N$ the following cases are distinguished, in the numerical integration scheme used: (I) If $N=2$, the trapezoidal rule is used (assuming a first order approximation to $f(x)$)

$$ I = \frac{h}{2} [f(x_1) + f(x_2)] \quad (B-2) $$

where

$$ h = x_2 - x_1 \quad (B-3) $$

(i) If $N \geq 3$ and odd then two subcases have to be considered

(ii) If all integration points are equally spaced then the integral can be calculated using Simpson's first rule for every three consecutive integration points (assuming a second order approximation to $f(x)$):

$$ I = \frac{h}{3} [f(x_1) + 4f(x_2) + f(x_3)] \quad (B-4) $$

where

$$ h = x_2 - x_1 = x_3 - x_2 \quad (B-5) $$

(iib) If the integration points are unequally spaced then a
modified version of equation (B-4) is used for every three consecutive points. Specifically, assuming again a second order approximation to \( f(x) \), the respective Lagrange interpolation polynomial will be:

\[
p(x) = \frac{(x-x_2)(x-x_3)}{(x_1-x_2)(x_1-x_3)} f(x_1) + \frac{(x-x_1)(x-x_3)}{(x_2-x_1)(x_2-x_3)} f(x_2) + \frac{(x-x_1)(x-x_2)}{(x_3-x_1)(x_3-x_2)} f(x_3)
\]

(B-6)

where 
\[ x_1 \leq x_2 \leq x_3 \]

If as in Figure B.1(a), \( \alpha \) is the midpoint between \( x_1 \) and \( x_3 \) then

\[ \alpha = \frac{(x_1+x_3)}{2} \]

(B-7)

and substituting in (B-6) we get

\[
p(\alpha) = \frac{d}{2(h+d)} f(x_1) + \frac{h^2}{(h+d)(h-d)} f(x_2) - \frac{d}{2(h-d)} f(x_3)
\]

(B-8)

where \( h \) and \( d \) such as

\[
x_3 - x_1 = 2h
\]

\[
x_2 - x_1 = h + d
\]

(B-9)

\[
x_3 - x_2 = h - d
\]

For the equally spaced points now \((x_1, \alpha, x_3)\) integral

I can be calculated as in (B-4), that is

\[ I = \frac{h}{3} \left[ f(x_1) + 4p(\alpha) + f(x_3) \right] \]
\[ = \frac{h}{3} \left[ f(x_1) + \frac{d}{2(h+d)} f(x_1) + \frac{h^2}{(h+d)(h-d)} f(x_2) - \frac{d}{2(h-d)} f(x_3) + f(x_3) \right] \]

\[ = \frac{h}{3} \left( (1 + \frac{2d}{h+d}) f(x_1) + 2\left( \frac{h}{h+d} + \frac{h}{h-d} \right) f(x_2) + (1 - \frac{2d}{h-d}) f(x_3) \right) \] (B-10)

(iii) If \( N \geq 4 \) and even the following subcases have to be considered.

(iiiia) If all integration points are equally spaced then \( I \) is calculated using (B-4) for all the points, taken three at a time, except the last four, where the Simpson's second rule is used (assuming a third order approximation to \( f(x) \)):

\[ I = \frac{3h}{8} \left[ f(x_1) + 3f(x_2) + 3f(x_3) + f(x_4) \right] \] (B-11)

(iiiib) If the integration points are unequally spaced then the same method is employed as in (iiiia), but now we use (B-10) instead of (B-4), and a modified version of (B-11) for unequal intervals. Specifically, referring to Figure B-1(b), we finally have (assuming a third order Lagrange interpolation polynomial):

\[ I = \frac{h}{3} \left( (1 + \frac{2d_1d_2}{(h+d_1)(h+d_2)}) f(x_1) + \frac{4h^2}{d_2-d_1} \left( \frac{d_2}{h^2-d_1^2} f(x_2) - \frac{d_1}{h^2-d_2^2} f(x_3) \right) \right) + (1 + \frac{2d_1d_2}{(h-d_1)(h-d_2)}) f(x_4) \] (B-12)

where

\[ h = \frac{(x_4-x_1)}{2} \]

\[ d_1 = x_2-x_1-h \] (B-13)
\[ d_2 = x_3 - x_1 - h \]

The integration subroutine QUAD, used in the one-dimensional program was adapted from Isoda, [115].
Figure B.1: Second and third order approximations to function $f(x)$ used in numerical integration.
APPENDIX C

FORTRAN LISTING OF THE MODIFIED ONE-DIMENSIONAL PROGRAM

A listing of the one-dimensional program, as modified by the author for the prediction of temperatures, strains and stresses, during welding and subsequent stress relieving, is presented in this Appendix.

Although the input and output format is somewhat different from the original version of the program the inclusion of comment statements and the existence of READ and WRITE FORMAT statements makes it unnecessary to repeat the input specifications here.

The program is general enough to handle, with minor only modifications, any type of stress relieving temperature history and profile. Only one creep model, for 304 stainless steel, was included. If for any other material, however, the uniaxial creep law is known, subroutine CREPLO has to be changed accordingly.
ONE DIMENSIONAL PROGRAM AS MODIFIED BY J A FOR STRESS RELIEVING

0001 DIMENSION EAN(21), STRSK(21), EBN(21), ENPN(21), STRESR(21), STRPL(21)
0002 0003 DIMENSION STNTH(200), ST5(200), EMT(200), TY1(200)
0004 0005 DIMENSION S19(200), EBM(200), TY9(200)
0006 0007 DIMENSION S11(200), EMI1(200), TY11(200)
0008 0009 DIMENSION S15(200), EMI5(200), TY15(200)
0010 0011 DIMENSION S18(200), EMB(200), TY18(200)
0012 0013 DIMENSION TPI(300), INE(300), S551(300), EWM(300)
0014 0015 DIMENSION SSS7(300), SSS9(300), SSS1(300), SSS15(300), SSS18(300)
0016 0017 DIMENSION EMMN(300), EMMS(300), EMMN(300), EMMIS(300), EMM18(300)
0018 0019 COMMON /TEM/ (TY1, TEMP, CAO, RMD, COND, TREF, 10, 21)
0020 0021 COMMON /E/E(I), TEP(I), ALPH(10), SLOP(10)
0022 0023 COMMON /ST/RAT(20), E(I), EX(21), EM(21), EPN(21), STRES(21), EL(21)
0024 0025 COMMON /W/W(21), TO(21)
0026 0027 COMMON /PP/P, V, Y, YEP, YDUT, THICK
0028 0029 COMMON /HC/H, HCK, HCT(10)
0030 0031 COMMON /KS/KSPS(21)
0032 0033 COMMON /TD/TDS, TMAX, TIX, TREF
0034 0035 COMMON /T/T11, T11, T2, T12, T13, T13, TST
0036 0037 COMMON /D/K1, K0
0038 COMMON /C/C2, ECREEP(21)
0039 COMMON /NP/NP, TUP, NSG, OK, TISK, NON, TION, SREV
C
C **** READ INPUT DATA ****
C
0040 *888 READ(KI, 100) KI, VOL, AMP, EFF, THICK, Y
0041 *777 READ(KI, 100) HAI(K), K+1, 20
0042 100 FORMAT(ST, 4)
0043 110 FORMAT(1X, K, K+1, 20)
0044 200 FORMAT(1X, K, K+1, 10)
0045 300 FORMAT(1X, K, K+1, 10)
0046 400 FORMAT(1X, K, K+1, 10)
0047 500 FORMAT(1X, K, K+1, 10)
0048 600 FORMAT(1X, K, K+1, 10)
0049 700 FORMAT(1X, K, K+1, 10)
0050 800 FORMAT(1X, K, K+1, 10)
0051 900 FORMAT(1X, K, K+1, 10)
0052 100 FORMAT(1X, K, K+1, 10)
0053 101 FORMAT(1X, K, K+1, 10)
0054 102 FORMAT(1X, K, K+1, 10)
0055 READ(KI, 101) (HCX(I), I, 1, 10)
0056 READ(KI, 102) NEDG
C
C ** NEDGE = 1 EDGE WELD, 0 BUTT WELD, -1 BEAD ON PLATE **
C
J0H00010
J0H00020
J0H00030
J0H00040
J0H00050
J0H00060
J0H00070
J0H00080
J0H00090
J0H01010
J0H01100
J0H01200
J0H01300
J0H01400
J0H01500
J0H01600
J0H01700
J0H01800
J0H01900
J0H02000
J0H02100
J0H02200
J0H02300
J0H02400
J0H02500
J0H02600
J0H02700
J0H02800
J0H02900
J0H03000
J0H03100
J0H03200
J0H03300
J0H03400
J0H03500
J0H03600
J0H03700
J0H02800
J0H03900
J0H04000
J0H04100
J0H04200
J0H04300
J0H04400
J0H04500
J0H04600
J0H04700
J0H04800
J0H04900
J0H05000
J0H05100
J0H05200
J0H05500
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MAIN

DATE = 02/12/67

14/42/06

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0040 104 READ(KI,104) NPAS
0041 104 FORMAT(12)
0042 READ(KI,101) (YPHI(I),I=1,11,11)
0043 READ(KI,105) ((YIINHNP,1,1=1,11),1=1,11,11)
0044 FORMAT(11F7.4)
0045 DO 301 NP=1,NPAS
0046 DO 301 1=1,11,11
0047 301 YIINHNP,1=1,11
0048 READ(KI,103) YDIS,TMAX,TK,TREF
0049 FORMAT(10F4.4)
0050 READ(KI,103) T11,12,13,13,T13,TST
0051 103 FORMAT(7F10.2)
0052 READ(KI,103) TLAST
0053 READ(KI,109) NPC,N1,N2,N3
0054 109 FORMAT(4I5)

C
C NPC * 0 IF PRINT AND PLOT, 1 PRINT ONLY, 2 PLOT ONLY
C PLOT THE FIRST N1,N2,N3 POINTS ONLY
C FOR TEMPERATURES, STRESSES, STRAINS RESPECTIVELY
C READ (KI,191) ISREV
0055 191 FORMAT (12)

C ISREV * 1 FOR STRESS RELIEVING, 0 FOR WELDING
C IF(ISREV,F0.0) GO TO 192
C READ (KI,171) ICREP,NUP,NSOK,NUM,TIEUP,TISK,TION,ISREV
0056 171 FORMAT(4I5,4F10.2)

C **** PRINT INPUT DATA ****
0057 192 WRITE(KO,223) (RAI(KI),K=1,20)

C FORMAT(8X,20A4/)
0058 223 WRITE(KO,266)

C WRITE((//4X,'WELDING CONDITIONS/')
0059 266 WRITE(KO,221) VOLT,AMP,EFF,V
0060 221 WRITE(KO,212) VOLTS,F6.2,' AMPS',F6.2,' ARC EFF.',F4.2.
0061 ' WELD SPEED '*F7.5.', ' INCHES/SEC')

C WRITE(KO,179) NEDGE
0062 179 WRITE(KO,244) (NEDGE,K=1,3X,12.3X,'(1 FOR EDGE, 0 FOR BUTT, 1 FOR BEAD)

C IN PLATE')
0063 WRITE(KO,224)

C FORMAT(4X,20A4/)
0064 WRITE(KO,250) (TEMP(K),K=1,10)
0065 250 WRITE(KO,250) (TEMP,K=1,10)

C FORMAT('CONDUCTIVITY',3X,10B3.5, '(', DEGREES F')
0066 251 WRITE(KO,251) (D3X,12HCONDUCTIVITY',3X,10B3.5, '(', DEGREES F')
0067 252 WRITE(KO,252) (D3X,12HCONDUCTIVITY',3X,10B3.5, '(', DEGREES F')

C WRITE(KO,253) (ROH(K),K=1,10)
0068 253 WRITE(KO,253) (ROH,K=1,10)

C WRITE(KO,254) (EPS1(K),K=1,10)
0069 254 WRITE(KO,254) (EPS1,K=1,10)

C FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0070 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0071 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0072 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')

0073 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0074 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0075 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0076 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0077 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0078 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
0079 254 FORMAT(1X,'ISOTROPIC S MODULUS,2X,10F8.5, '(', PSI+0**3')
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0080      WRITE(KO,255)(YPI(J),J=1,10)  
0081      FORMAT(2/2X,'INITIAL YIELD',3X,10F8.3,5X,'(KSI)'/6X,'STRESS')  
0082      WRITE(KO,256)ALPH(I),I=1,10  
0083      FORMAT(2X,'COEFFICIENT OF',2X,10F8.3,5X,'MICRO INCHES/INCH/DEG F')/4X,'EXPANSION')  
0084      WRITE(KO,257)SLOP(I),I=1,10  
0085      FORMAT(2X,'TANGENT MODULUS',6X,10F8.3,5X,'KSI**3+3')  
0086      WRITE(KO,280)T=1,10  
0087      FORMAT(1X,'SURFACE HEAT LOSS',10F8.3,5X,'(WATTS/INCH/DEG F')/4X,'COEFFICIENT')  
0088      WRITE(KO,175)TMX  
0089      FORMAT(1X,'REFERENCE TEMPERATURE (DEG F')=','F8.2)  
0090      WRITE(KO,176)TREF  
0091      FORMAT(1X,'MELTING TEMPERATURE (DEG F')=','F8.2)  
0092      WRITE(KO,177)TDEL  
0093      WRITE(KO,178)THICK,YDIS  
0094      FORMAT(1X,'PLATE GEOMETRY')/4X,'THICKNESS (INCHES')=','F6.3)  
0095      FORMAT(1X,'PLATE WIDTH (INCHES')=','F7.2)  
0096      WRITE(KO,271)NPAS  
0097      FORMAT(1X,'NUMBER OF WELDING PASSES ',12)  
0098      WRITE(KO,272)1+NPAS  
0099      FORMAT(1X,'POSITION OF WELDING ARC FROM CENTER (INCHES')//  
0100      11X,'PASS NO 1 2 3 4 5 6 7 8 9/10,10F8.3/3)  
0101      WRITE(KO,273)NPAS  
0102      FORMAT(1X,'CHANGE OF JOINT SHAPE BY EFFECT OF MULTIPASS ',')
0103      1X,'PASS=',12,11F8.3/7)  
0104      YD(1)=0.0  
0105      DO 1 J=2,21
0106      1 YD(J)=YD(J-1)  
0107      WRITE(KO,274)YD(J),J=1,11,(YD(J),J=13,21)  
0108      FORMAT(1X,'LOCATION',5X,16F7.3)  
0109      C  
0110      C  
0111      C  
0112      C  
0113      C  
0114      C  
0115      C  
0116      C  
0117      C  
0118      C  
0119      C  
0120      C  

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J001070
J001080
J001090
J001100
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J001120
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J001190
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J001570
J001580
J001590
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0121 58 WRITE(6,126) (W(I,JK),IJK=1,N)

0122 181 FORMAT(1X,'TIME STEP INFORMATION')

0123 WRITE(KO,259)T1,T1,T2,T3,T4,T5,T6

0124 259 FORMAT(8X,'T1=','T1=',',',T2=',',',T3=',',',T4=',',',T5=',',',T6=','F7.1,','F7.1,','F7.1,','F7.1,','F7.1,','F7.1,')

0125 WRITE(KO,211) T1X

0126 211 FORMAT(1X,'ARC PASSES FROM OBSERVATION SECTION AT T=',',T=2.',

0127 1' SECS')

0128 WRITE(6,126) (W(I,JK),IJK=1,N)

0129 1226 FORMAT(1H,,'T1=',',15E1X,10F10.2)

0130 WRITE(KO,182)

0131 182 FORMAT(1H,'WELDING *****/)

0132 WRITE(KO,210)100,(YD(J),J=1,11),(YD(J),J=13,21,2)

0133 210 FORMAT(27X,46HTRANSVERSE DISTANCE FROM CENTER LINE IN INCHES//

0134 1 16X,16F7.3)

0135 I=MAX-TEMP(I)

0136 C CHANGE UNITS TO PSI AND INCH/INCH

0137 DO 50 I=1,10

0138 E(I)=E(I)+10000.0

0139 VY(I)=VY(I)+1000.0

0140 50 ALPH(I)=ALPH(I)/100000.

0141 YDP=YP(I)+1000.0

0142 YDP=YP(I)+1000.0

0143 DO 668 J=1,21

0144 CONTINUE

0145 668 CONTINUE

0146 DO 51 J=1,21

0147 CONTINUE

0148 51 ECREEP(J)=0.0

0149 DO 134 J=1,10

0150 CONTINUE

0151 134 EP(I,J)=U

0152 DO 145 K=1,10

0153 CONTINUE

0154 DO 146 K=1,10

0155 CONTINUE

0156 DO 147 K=1,10

0157 CONTINUE

0158 DO 148 K=1,10

0159 CONTINUE

0150 CALL TEMPK(XT)

0151 CALL TEMPSK(K)

0154 CALL TEMPSK(K)

0157 CALL TEMPSK(K)

0158 CALL TEMPSK(K)

0161 XN(I)=XN(I)

0162 YN(I)=YN(I)
C PRINT TEMPERATURES IN DEGREES F
C STRAINS IN INCHES/INCH *10**3
C STRESSES IN KSI

IF(NPC.EQ.2) GO TO 777
IS=1/(5)**5
IF(1.LE.NE.15) GO TO 711
WRITE(KO,274) (Y(J),J=1,11),(YD(J),J=13,21,2)
WRITE(KD,277) (1)
CONTINUE
PRINT FFORMAT( ' /TH,SHIME,FB,5X,'SECS' )
WRITE(KO,299) K
PRINT FFORMAT(1H2,3SA, 'PASS NO ',12)
WRITE(KO,200) (TY(J),J=1,11),(TY(J),J=13,21,2)
WRITE(KD,224) (EL(J),J=1,11),(EL(J),J=13,21,2)
WRITE(KO,235) (EPH(J),J=1,11),(EPH(J),J=13,21,2)
WRITE(KO,189) (ECRE(E,J),J=1,11),(ECRE(J),J=13,21,2)
WRITE(KO,236) (EL(J),J=1,11),(EL(J),J=13,21,2)
WRITE(KD,233) (STRES(E,J),J=1,11),(STRES(J),J=13,21,2)
WRITE(KD,234) (10*ELASIC STRAIN,16F7.3)
WRITE(KD,189) (10*CREEP STRAIN,16F7.3)
WRITE(KD,235) (10*PLASTIC STRAIN,16F7.3)
WRITE(KD,236) (13H MECH. STRAIN,X,16F7.3)
WRITE(KO,233) (10 STRESS,9X,16F7.2)
CONTINUE
WRITE(DD,667) J=1,21
WRITE(665) CONTINUE
WRITE(DD,987) IPL=1,21
WRITE(STRP) STRES(IPL)
WRITE(TPL) TY(J)
WRITE(TMP) STRES(I)
WRITE(TMP) STRES(I)
C **** STRESS RELIEVING BY UNIFORM HEATING ****
C
0212 IF(ISREV EQ 0.0) GO TO 9090
0213 NSREV=NSREV+1
0214 WRITE(172)
0215 172 FORMAT(1X,'STRESS RELIEVING...
0216 WRITE(183) ICREEP
0217 183 FORMAT(1X,'ICREEP=',I5,' (0 IF CREEP IS IGNORED,1 IF INCLUDED)'
0218 WRITE(184) TSREV
0219 184 FORMAT(1X,'STRESS RELIEVING TEMPERATURE (DEG F) ',F10.2)
0220 WRITE(185) NUP,TIUP,NSDOAK,TISK,MON,TIDN
0221 185 FORMAT(1X,'TEMPERATURE HISTOGRAM PARAMETERS ',/3X,'NUMBER OF STEPS',3X,'STEP SIZE IN SECONDS',/3X,'HEATING',5X,15X,'FO.2/37X',/3X,'HOLDING',5X,15X,'FO.1/37X',/3X,'COOLING',5X,15X,'FO.2/37X')
0222 WRITE(210) (YD(J),J=1,11),((YD(J),J=13,21,2)
0223 DO 331 J=1,NSREV
0224 331 Y(J)=Y(J)+1
0225 IF(J.GT.NUP) GO TO 3331
0226 DO 3019 J=1,21
0227 3019 TY(J)=TY(J)+TSREV
0228 T(J)=T(J)+TIUP+FLOAT(I)/F(NUP)*TSREV
0229 CALL STRESS(I,ISREV)
0230 DO 3099 J=1,21
0231 3099 STRH(J)=STRES(J)
0232 GO TO 3331
0233 IF(J.GT.(NUP+NSDOAK)) GO TO 3332
0234 DO 302 J=1,21
0235 302 TY(J)=TY(J)+TSREV
0236 T(J)=T(J)+TIUP+FLOAT(NUP)+TISK+FLOAT(I,1)-NUP
0237 IF(ICREEP.EQ.1) GO TO 3021
0238 CALL STRESS(I,ISREV)
0239 GO TO 3301
0240 CALL STCAP(I,5)
0241 DO 3302 J=1,21
0242 3302 STRS(J)=STRES(J)
0243 GO TO 3332
0244 DO 303 J=1,21
0245 303 TY(J)=TY(J)+TSREV+FLOAT(NSREV+1)/FLOAT(NUP)*TSREV
0246 INI=I-1-NUP-NSDOAK
0247 T(J)=T(J)+TIUP+FLOAT(NUP)+TISK+FLOAT(NSDOAK)+TIDN+FLOAT(INU)
C C *** PRINT TEMPERATURES (DEG F), STRAINS (X1000), STRESSES (KSI) C

0240 IF(NPC.EQ.2) GO TO 194
0250 IF (I.LE.5) GO TO 332
0251 WRITE (KO,274)(YD(J),J=1,11),(YD(J),J=13,21,2)
0252 WRITE(KO,277) (I(15)
0253 WRITE(KO,200)(TY(J),J=1,11),(TY(J),J=13,21,2)
0254 WRITE (KO,234)(EL(J),J=1,11),(EL(J),J=13,21,2)
0255 WRITE (KO,235)(EPH(J),J=1,11),(EPH(J),J=13,21,2)
0256 WRITE (KO,189)(ECREEP(J),J=1,11),(ECREEP(J),J=13,21,2)
0257 WRITE (KO,239)(EM(J),J=1,11),(EM(J),J=13,21,2)
0258 WRITE (KO,236)(EX(J),J=1,11),(EX(J),J=13,21,2)
0259 WRITE (KO,233)(SRES(J),J=1,11),(SRES(J),J=13,21,2)
0260 WRITE(KO,233)(SRES(J),J=1,11),(SRES(J),J=13,21,2)
0261
0262 194 TPL(I1+1)=TY(I)
0263 TME(I1+1)=TME(I)
0264 SSS1(I1+1)=SSS1(I)
0265 SSS1(I1+1)=SSS1(I)
0266 SSS1(I1+1)=SSS1(I)
0267 SSS1(I1+1)=SSS1(I)
0268 SSS1(I1+1)=SSS1(I)
0269 EME1(I1+1)=EME1(I)
0270 EME1(I1+1)=EME1(I)
0271 EME1(I1+1)=EME1(I)
0272 EME1(I1+1)=EME1(I)
0273 EME1(I1+1)=EME1(I)
0274 EME1(I1+1)=EME1(I)
0275 331 CONTINUE
0276 C C Plotting Phase
0277 C USE CALCOMP PLOTTER SUBROUTINES ON IBM VM/370
0278 9090 IF(NPC.EQ.0) GO TO 9991
0279 CALL PLOTS (120,100M.25)
0280 C C Welding Part
0281 C C Plot the first N1,N2,N3 time steps
0282 C C FOR TEMPERATURES, STRESSES AND STRAINS RESPECTIVELY
0283 C CALL PLOT(3.0,0.0,3)
0284 CALL SYMBOL(7.15,3.05,05.10.0,0.1)
0285 CALL SYMBOL(7.43.0,07.05.0,0,0.1)
0286 CALL SYMBOL(7.42.0,07.05.0,0,0.1)
0287 CALL SYMBOL(7.15,25.05.4,0.0,0.1)
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0331 CALL SYMBOL(7.15,1.05,.095,0.0,-1)
0332 CALL SYMBOL(7.4,1.0,7.,'AT 3.0 INCHES',0.0,13)
0333 CALL PLOT(3.0,0.3,12)
0334 CALL PICTUR(7.05,0.,'TIME (HOURS)',12)
1' STRESSES (KSI), 15, TIME, SSS1, NSREV1, 05.1. TIME, SSS59, NSREV1, 05.2.
2TIME, SSS11, NSREV1, 05.3, TIME, SSS15, NSREV1, 05.4, TIME, SSS18, NSREV1.
0335 CALL PLOT(3.0,0.0,3)
0336 CALL SYMBOL(7.15,3.05,.05).1.00,-1)
0337 CALL SYMBOL(7.430,0,.'AT 0.5 INCHES',.00,13)
0338 CALL SYMBOL(7.15,2.05,.05,2.00,-1)
0339 CALL SYMBOL(7.425,0,.'AT 1.0 INCHES',.00,13)
0340 CALL SYMBOL(7.15,2.05,.05,3.00,-1)
0341 CALL SYMBOL(7.420,0,.'AT 1.5 INCHES',.00,13)
0342 CALL SYMBOL(7.15,5.5,.05,4.00,-1)
0343 CALL SYMBOL(7.415,0,.'AT 2.0 INCHES',.00,13)
0344 CALL SYMBOL(7.15,5.05,.05,5.00,-1)
0345 CALL SYMBOL(7.410,0,.'AT 3.0 INCHES',.00,13)
0346 CALL PLOT(3.0,0.0,3)
0347 CALL PICTUR(7.05,0.,'TIME (HOURS)',12)
1' MECHANICAL STRAINS (X 1000), 28, TIME, ENH7, NSREV1, 05.1
34, TIME, ENH11, NSREV1, 05.5
0348 CALL PLOT(3.0,0.0,3)
0349 CALL SYMBOL(7.15,3.05,.05,0.0,0,-1)
0350 CALL SYMBOL(7.430,0,.'AFTER WELDING',.00,13)
0351 CALL SYMBOL(7.15,2.55,.05,1.00,-1)
0352 CALL SYMBOL(7.425,0,.'AFTER HEATING',.00,13)
0353 CALL SYMBOL(7.15,2.05,.05,2.00,-1)
0354 CALL SYMBOL(7.420,0,.'AFTER SOAKING',.00,13)
0355 CALL SYMBOL(7.15,3.95,.05,3.00,-1)
0356 CALL SYMBOL(7.415,0,.'AFTER COOLING',.00,13)
0357 CALL PLOT(3.0,0.0,3)
0358 CALL PICTUR(7.05,0.,'TRANSVERSE DISTANCE (INCHES)',28)
1'REMAINS STRAINS (KSI), 24, YD, STRL1, 21, 05.0, YD, STRL21, 21. 05.1.
0359 CALL ENDPIT (11.0,0.0,999)
0360 CALL GO TO 888
0361 CONTINUE
0362 STOP
0363 END
0001 SUBROUTINE TEMP1(X,T,K) 
0002 C ***** TEMPERATURE DISTRIBUTION DURING WELDING ***** 
0003 C 
0004 DIMENSION YB(8) 
0005 COMMON /EM(TY(21),TEMP(10),CAP(10),RH(10),COND(10),TREFN(10,21) 
0006 COMMON /ME(EM(10),Y(10),ALPH(10),SLOP(10) 
0007 COMMON /ST(RAT(20),T(2000),EX(21),EM(21),EPN(21),SURES(21),EL(21) 
0008 COMMON /ME(Y(21),V(21)) 
0009 COMMON /PF/Y(21),YTHM(10,21) 
0010 COMMON /SC/Y(21),THICK 
0011 COMMON /PC/Y(21),TMAX,TMAX,MPH,MPH 
0012 COMMON /AI/KI,KO 
0013 INTEGER YS*YPN(K) 
0014 DO 70 J=1,21 
0015 YT*YD(J) 
0016 YT*YD(J) 
0017 DO 20 NN=1,4 
0018 NP=2+NN 
0019 N=NN+NN 
0020 YB(NN)=Y+YD(NN)*YD(NN)*YD(NN) 
0021 YB(NN)=Y+YD(NN)*YD(NN)*YD(NN) 
0022 CONTINUE 
0023 DO 410 T=TMAX 
0024 IF(R<0.001) 410,410,411 
0025 410 T=TMAX 
0026 GO TO 11 
0027 411 CONN=COND(4) 
0028 CA=CAP(4) 
0029 RH=RHO(4) 
0030 HDN=HDN(4) 
0031 TO=TREFN(K,J) 
0032 TT=0 
0033 CONTINUE 
0034 10 CONTINUE 
0035 10 IT=IT+1 
0036 CONN=CON+YTHM(K,J)/THICK 
0037 RH=RH+YTHM(K,J)/THICK 
0038 RV2=RV2+HCN(10,CON/THICK 
0039 RV2=RV2+HCN(10,CON/THICK 
0040 RV=SQRT(RV2) 
0041 EE=0 
0042 DO 45 JJ=1,8 
0043 YY=Y(Y(JJ)) 
0044 R=SQRT((X1+YY))/Y 
0045 ZZ=RV*Y 
0046 Z=1.0+Z1+Z 
0047 CALL RBS(Z,Z,EKR,B) 
0048 CALL RBS(Z,Z,EKR,B) 
0049 EE=EE+B 
0050 45 CONTINUE
0051          TN+CT+EK
0052  44     TN=TN+TREN(K,J)
0053     IF(TN.GT.TMAX) TN=TMAX
0054     AB=ABS(TN-T0)
0055     IF(AB<0.5)11,12
0056  12     TM=(TD+TN)/2.0
0057     IF(TT.GT.90) GO TO 998
0058     TO+TN
0059     CON=FILLIN(TM,TEMP,COND,10)
0060     HCON=FILLIN(TM,TEMP,HCOT,10)
0061     CA=FILLIN(TM,TEMP,CAP,10)
0062     RH=FILLIN(TM,TEMP,RHO,10)
0063     GO TO 10
0064  11     TY(J)=TN
0065     GO TO 888
0066  998     WRITE(90,298) J,TM,TO,TN
0067     298     FORMAT(/1X,22HTEMP DOES NOT CONVERGE ,J+','I2,' TM,TO,TN','
0068     3F10.2,'****** USED IN *****')
0069     CON=CONTINUE
0070     CONTINUE
0071     RETURN
0072     END
SUBROUTINE RBES(ZZ,Z,EK,B)

IF(ZZ.LT.155.0) 42,42,43

43 0=Z/ZZ

GO=O=O

GOQ=GOQ=O

IF((Z-ZZ).LT.-170.0) GO TO 45

ER=ER-(2-ZZ)*(1.253314-.0733235-.0218956-.0106244-.005320-.001550)/SQRT(ZZ)

405878 *QQ=QQ-.0025154*QQ*QQ+.0005320*QQ+.001550/SQRT(ZZ)

B=O=O

RETURN

42 CALL RBES(ZZ,B)

IF(Z.LT.-15.0) Z=-15.0

ER=B*EXP(Z)

RETURN

45 ER=O=O

RETURN

END
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STRESS

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0051
SPS(J)=0
0052
HE(J)+1
0053
GO TO 61
0054
49 EYN(J)=SY(J)/A(J)
0055
61 EPLA(J)=.0
0056
NC=0
0057
666 CONTINUE
0058
NC=NC+1
0059
IF (NC GT 20) GO TO 989
0060
DO 66 J=1,21
0061
HTE(J)=H(J)+T(J)+EPLA(J)+ECR(J)
0062
63 HTE(J)=HTE(J)+Y(J)
0063
CALL QDQRY(Y,0,0,HTE,21,EYN,ERROR)
0064
CALL QDQRY(Y,0,0,HTE,21,TEH,ERROR)
0065
DO 65 J=1,21
0066
IF (NEDGE GE 1) GO TO 650
0067
65 IF (K.NE.1) GO TO 652
0068
IF (Y(J).GE.TMAX) GO TO 650
0069
IF (T(J).LE.TIX) GO TO 650
0070
652 CONTINUE
0071
EX(J)=A+TeH
0072
GO TO 651
0073
650 EX(J)=(AT-J*J+J+21)+EYN(A2+Y(J)*A3)+EYN
0074
651 EML(J)=EX(J)+T(J)+AU(J)
0075
57 IF (EPD(J)) B0,81,81
0076
58 IF (EM(J)) B2,83,83
0077
59 IF (EM(J)) B4,85,85
0078
60 IF (EM(J)) B6,87,87
0079
61 IF (ABS(EML(J)) GT ABS(EYN(J)))
0080
62 IF (ABS(EML(J)) LE ABS(EYN(J)))
0081
63 IF (ABS(EML(J)) LT ABS(EYN(J)))
0082
64 IF (ABS(EML(J)) .GT 8) GO TO 831
0083
65 GO TO 65
0084
66 IF (ABS(EML(J)) .GT 822) GO TO 832
0085
67 IF (ABS(EML(J)) .GT 832) GO TO 833
0086
68 IF (ABS(EML(J)) .GT 833) GO TO 834
0087
69 IF (ABS(EML(J)) .GT 834) GO TO 835
0088
70 IF (ABS(EML(J)) .GT 835) GO TO 836
0089
71 IF (ABS(EML(J)) .GT 836) GO TO 837
0090
72 IF (ABS(EML(J)) .GT 837) GO TO 838
0091
73 IF (ABS(EML(J)) .GT 838) GO TO 839
0092
74 IF (ABS(EML(J)) .GT 839) GO TO 840
0093
75 IF (ABS(EML(J)) .GT 840) GO TO 841
0094
76 IF (ABS(EML(J)) .GT 841) GO TO 842
0095
77 IF (ABS(EML(J)) .GT 842) GO TO 843
0096
78 IF (ABS(EML(J)) .GT 843) GO TO 844
0097
79 IF (ABS(EML(J)) .GT 844) GO TO 845
0098
80 IF (ABS(EML(J)) .GT 845) GO TO 846
0099
81 IF (ABS(EML(J)) .GT 846) GO TO 847
0100
82 IF (ABS(EML(J)) .GT 847) GO TO 848
GO TO 666

89 CONTINUE

GO 33 J=1,21

EPD(J)*EPN(J)

SIRES(J)+YP(J)+*E(J)+(EM(J)-EPN(J)-ECR(J))/000.0

X(J)+EX(J)+YOUT

E(J)+(EM(J)-EPN(J)-ECR(J))+YOUT

EM(J)+(EM(J))*YOUT

33 EPN(J)+EPN(J)+YOUT

GO TO 888

999 WRITE(KO,298)

298 FORMAT(1X,24HSTRAIN DOES NOT CONVERGE)

CONTINUE

CONTINUE

RETURN

END.
DO 72 J=1,21
   EINT(J)+HJ+(TAU(J)+ECR(J)+DECRP(J)+EPN(J))
   EINT(Y(J)+EINT(J))+Y(J)
   CALL QMOD(Y,0,0,EINT,21,EIN,IER)
   CALL QMOD(Y,0,0,EINT,21,EIN,IER)
   DO 65 J=1,21
      IF(NEDGE.GE.1) GO TO 650
      EX(J)*A1+EIN
      GO TO 651
   650 EX(J)+(A1+Y(J)+A2+Y(J)*A3)+EIN
      EM(J)*EX(J)-TAU(J)
      EL(J)*EM(J)-EPN(J)-ECR(J)
      STRES(J)+HJ+Y(J)+EL(J)+1000
      SS+STRES(J)
      CALL CREPLO(SS,II,10,DE)
      DECRP(J)=DE/YP
      CONTINUE
       65 CONTINUE
       DO 88 J=1,21
       IF(ABS(DECRP(J)-DECRP(J)) GT 0.0+ABS(DECRP(J))) 88,88,86
       CONTINUE
       GO TO 89
       86 DO 87 J=1,21
       DECRP(J)=DECRP(J)
       GO TO 666
       87 CONTINUE
       89 DO 99 J=1,21
       ECR(J)-ECR(J)+DECRP(J)
       ECRP(J)+ECR(J)+YOUT
       EM(J)+EM(J)+YOUT
       EL(J)+EL(J)+YOUT
       EX(J)+EX(J)+YOUT
       EPN(J)+EPN(J)+YOUT
       GO TO 688
       99 WRITE(KO,238)
       238 FORMAT(1X,'**** CREEP STRAIN DOES NOT CONVERGE -- -- -- --')
       CONTINUE
       RETURN
       END
SUBROUTINE CREPLO(S11,10,DE)

C  *** UNIAXIAL CREPE LAW FOR 304 STAINLESS AT 1100 DEG F  ***
C
F=5.436E-05*(ABS(S))**1.843
R=5.929E-05*EXP(0.2029*ABS(S))
G=6.73E-09*(SINH(0.1479*ABS(S)))**3.0
DEF=(EXP(-R+11)-EXP(-R+10)+G*(10-11))
IF (S.GE.0.0) GO TO 1

DE=DE
RETURN
END
FUNCTION FILIN(x, AB, OR) W
C
C PARABOLIC INTERPOLATION
C
DIMENSION AB(N+1, OR+1)

1 X1 = x
2 Y1 = (x-2) * (x-3) * (x-4) * (x-5) / 24
3 Y2 = (x-3) * (x-4) * (x-5) * (x-6) / 24
4 Y3 = (x-4) * (x-5) * (x-6) * (x-7) / 24
5 Y4 = (x-5) * (x-6) * (x-7) * (x-8) / 24
6 Y5 = (x-6) * (x-7) * (x-8) * (x-9) / 24
7 Y = Y1 + (x-x1) * (Y2-Y1) + (x-x1) * (x-x2) * (Y3-Y2) +
8 (x-x1) * (x-x2) * (x-x3) * (Y4-Y3) +
9 (x-x1) * (x-x2) * (x-x3) * (x-x4) * (Y5-Y4) +
10 (x-x1) * (x-x2) * (x-x3) * (x-x4) * (x-x5) * (Y6-Y5)
11 RETURN
END
SUBROUTINE BES(X,B)
DIMENSION C(6), CI(6), CK(6)
DATA C/ 0.029154, 0.058787, 0.010624, 0.018956, 0.0783235, 1.253014/
DATA CI/ 0.360768, 0.2659732, 1.206749, 3.089942, 3.515622, 1.1/
DATA CK/ 0.001075, 0.002529, 0.0348993, 23.09875, 4227842, 5772156/

IF (X) 10, 20, 0006
10 B = 999 RETURN
20 IF (X < 1.) 30, 50, 50
30 XI = ALOG5 (5*X)
40 X5 = X**2/4
50 X5I = X**2/14 0625
60 S1 = 0.045813
70 S5 = 0.0000740
80 DO 35 J = 1, 6
90 35 S1 = S1 * S5 * CI(L)
100 DO 15 J = 1, 6
110 15 B = B * S1 * XI
120 RETURN
130 S0 X = SQRT(X)*EXP(X)
140 S0 X2 = X / X
150 S0 X = SQRT(X)*EXP(X)
160 DO 55 S5 = 0.0053208
170 S0 S5 = S5 * 1.6
180 DO 55 S5 = 0.0053208
190 S5 = S5 * 1.6
200 DO 55 S5 = 0.0053208
210 B = B/X
220 RETURN
230 END
SUBROUTINE QUDR(ARG,H,VAL,N,Q,IER)
  DIMENSION ARG(22),VAL(22)
  IER=0
  Q=0
  IF(N-2) 10,20,50
     IER=1
     RETURN
  10 RETURN
  IF(ARG(N) NE ARG(N)) GO TO 40
  20 RETURN
  30 IER=2
  40 Q=(ARG(N)-ARG(1))/VAL(1)+VAL(N)/2.
  50 IF(H.NE.Q) GO TO 100
  60 IF(ARG(J) NE ARG(J-1)) GO TO 80
  70 IER=2
     N=N-1
     IF(J.GT.N) GO TO 90
  80 DD TO K-J,N
  90 ARG(K)=ARG(K+1)
  100 VAL(K+1)=VAL(K+1)
  110 J=J+1
  120 IF(J.LE.N) GO TO 60
  130 IF(N-2) 10,20,100
  140 I=1
  150 I=I+2
  160 IF((N-1).LE.3) GO TO 150
  170 IF(H.NE.Q) GO TO 140
  180 H=ARG(1)/ARG(1)-H1
  190 H=H*(1+1.2*(H1/(H1+D)+VAL(1)+2+(H1/(H1+D)+H1/(H1-D)))+VAL(1)+2)
  200 IF(LT(N-2)) GO TO 110
  210 RETURN
C ADD THE AREA BETWEEN ARG(J) AND ARG(J-1)
C
  220 IF(H.NE.Q) GO TO 120
  230 H1=ARG(1)/ARG(1)-H1
  240 H=H*(1+1.2*(H1/(H1+D)+VAL(1)+2+(H1/(H1+D)+H1/(H1-D)))+VAL(1)+2)
  250 IF(LT(N-2)) GO TO 110
  260 RETURN
C EQUAL INTERVAL
C
  270 D=Q
  280 H=H
  290 GO TO 130
  300 GO TO 150
C ADD THE LAST THREE AREAS
C
  310 H=ARG(N)-ARG(3)/2
  320 D=ARG(N)-ARG(N)-H1
  330 D2=ARG(N)-H2
  340 Q=H1/3+((1+2.*D2)/(H1+D)+VAL(N)+4+1+H1/(D2+D1))
  350 Q=H1/3+((1+2.*D2)/(H1+D)+VAL(N)+4+1+H1/(D2+D1))
  360 Q=H1/3+((1+2.*D2)/(H1+D)+VAL(N)+4+1+H1/(D2+D1))
  370 Q=H1/3+((1+2.*D2)/(H1+D)+VAL(N)+4+1+H1/(D2+D1))
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<td>17420</td>
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<td>------</td>
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<p>| 1 | 10 | 50 | 10 | 360 | 360 | 360 | 1100 |</p>
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<td>EDGE WELDING OF 304 PLATE AND STRESS RELIEVING AT 500 DEG F</td>
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APPENDIX D

LISTINGS OF DATA ACQUISITION AND REDUCTION PROGRAMS
**FORTRAN IV V02.5 PAGE 001**

**PROGRAM SMPLS.FOR**

**PROGRAM TO TRANSFER ASCII DATA SERIALLY BETWEEN**

**MINC-23 AND DAYTRONIC 9635 COMPUTER INTERFACE MODULE**

**NOW SET TO SAMPLE ONLY 10 CHANNELS:**

**$65 TO 09 FOR STRAIN GAGES**

**$10 TO 14 FOR T/C's**

**CHANGE IX VALUES ACCORDING TO WHAT YOU SAMPLE**

**(BUFFER SIZE IN CIN SET TO 20 CHARACTERS)**

```
0001 LOGICAL*1 BUF1(4), FNAME(16), BUF(20), BUF3(4)
0002 LOGICAL*1 BUF2(6), IX(2,10), BOUT(90)
0003 LOGICAL*1 BOUT(9,10), VAL(9,10)
0004 DIMENSION T(3000)
0005 EQUIVALENCE (BOUT(1), VAL(1,1))
0006 INTEGER*4 ITM1, ITM3
0007 DATA BUF1/"L","O","C","r","s",15/
0008 DATA BUF2/"H","N","I","x","s",15/
0009 DATA BUF3/"U","N","L","s",15/
0010 DATA IX/"0","5","0","0","7","0","B","0",9/

0011 NCRL=4
0012 NCRL2=6
0013 NCRL3=4
0014 IFLAG1=0
0015 IFLAG2=0
0016 IFLAG3=0

**OUTPUT FILE INITIALIZATION**

0017 TYPE *, 'WHAT IS THE NAME OF THE OUTPUT FILE'
0018 ACCEPT 701,(FNAME(1), I=1,14)
0019 FORMAT(1AA1)
0020 FNAME(15)=0
0021 OPEN(UNIT=15,NAME=FNAME,TYPE='NEW',ACCESS='SEQUENTIAL',
1FORM='FORMATTED')

**TIME STEPS DEFINITION**

0022 TYPE *, 'ENTER TIME LIMITS FOR EVERY INTERVAL'
0023 TYPE *, 'ENTER TIME STEP SIZE FOR EVERY INTERVAL'
0024 ACCEPT *, T1, T2, T3
0025 TYPE *, 'NOTE THAT TIME STEPS MUST BE GREATER THAN 4.5 SECS !!!'
```
FORTRAN IV

0028      TYPE *, ' (FOR 5 STRAIN GAGES AND 5 THERMOCOUPLES)
0029      ACCEPT *,T11,T12,T13
0030      N=1
0031      T1=T11
0032      T1(I)=0.0
0033      IF(I(N).GE.T1) T1=T12
0034      IF(I(N).GE.T2) T1=T13
0035      IF(I(N).GE.T3) GO TO 41
0036      T(N+1)=T(N)+T1
0037      N=N+1
0038      GO TO 40
0039      CONTINUE
0040      TYPE 702 *(I(I,K),J,K=1,N)
0041      702      F00M=(1H,1S,/1X,8F10.2)
0042      TYPE *, *** ARE TIME STEPS OK ?? (YES=1,NO=0) ***
0043      ACCEPT *, IYE
0044      IF (IYE.EQ.0) GO TO 100

C     SAMPLING PHASE

C     PAUSE ' TYPE A CARRIAGE RETURN TO START SAMPLING !!'
0049      TYPE *, ' TYPE A SECOND CARRIAGE RETURN TO STOP !!'

C     GET INITIAL TIME

0050      CALL GTHM(IH0)
0051      CALL CTHM(IH0,IHO,ISHO,ISO,ITO)

C     LOOP FOR ALL TIME STEPS

0052      DO 121 JJ=1,N
0053      121      CALL GTHM(IH1)
0054      CALL CTHM(IH1,IH1,IH1,IS1,I11)
0055      TIM=(IH1-IHO)*3600.+(IH1-IHO)*60.+ (IS1-ISO)+(I1-IITO)/60.
0056      IF(I1.IEQ.1) GO TO 1
0057      CALL GHTM(IH1)
0058      CALL CHTM(IH1,IH1,IH1,IS1,I11)
0059      TIM=(IH1-IHO)*3600.+(IH1-IHO)*60.+ (IS1-ISO)+(I1-IITO)/60.

C     SEND 'LOC' (9635 MEMORY FREEZE) COMMAND

0060      DO 901 JJ=1,J
0061      901      CALL COUT (BUF1, NCH1, IFLAG1)
0062      IF (IFLAG1.EQ.1) GO TO 901
0063      JJ=JJ-1
0064      TYPE 718,JJ,TIM
0065      F00MAT(3X,1S,1F10.3)

C     LOOP FOR ALL CHANNELS

0066      DO 103 JL=1,10
0067      CALL CNRS
0068      DO 104 J=1,20
0069      CALL CNR2(JL)
0070      CALL CNR2(JL)
0071      104      BUF(JL)= ' '
0072      BUF2(4)=IX(1,JL)
0073 BUF2(5)=IX(2,JL)
CCC SEND "CHN X" (CHANNEL SAMPLING) COMMAND

0074 CALL COUT (BUF2,NCHR2,IFLAG2)
0075 IF (IFLAG2.EQ.1) GO TO 902
0077 IFL=0
0078 NCNT=0
CCC INPUT SERIAL DATA FROM 9635

0079 CALL CIN(BUF,1FL,NCNT)
0080 IF (IFL.EQ.0) GO TO 900
CCC GET RID OF LF,CR OR UREADABLE CHARACTERS

0082 DO 11 I=4,12
0083 JLI=(JL-1)*9+I-3
0084 IF (BUF(JLI).NE.'15') GO TO 112
0086 BUF(I)_=''
0087 112 IF (BUF(I).NE.'0') GO TO 11
0089 BUF(I)_=''
0090 BBUF(JLI)=BUF(I)
0091 103 CONTINUE
CCC SEND "UNL" (9635 MEMORY UNFREEZE) COMMAND

0092 CALL COUT(BUF3,NCHR3,IFLAG3)
0093 IF (IFLAG3.EQ.1) GO TO 903
CCC SET DATA TO PROPER FORMAT

0095 DO 93 I=1,10
0096 JY=9
0097 DO 92 IL=1,9
0098 ILL=9-IL+1
0099 IF (VAL(ILL,I).EQ.,')') GO TO 92
0101 BOT(JY,I)=VAL(ILL,I)
0102 JY=JY-1
0103 92 CONTINUE
0104 DO 94 JI=1,J9
0105 94 CONTINUE
CCC WRITE THE DATA TO THE DISC AND DISPLAY SOME IN THE TERMINAL

0107 WRITE (15,703)ITIM,((BOT(IL,I),IL=1,9),I=1,10)
0108 703 FORMAT (1X,F10.3,4X,10(1X,9A1))
0109 TYPE 704,((BOT(IL,I),IL=1,9),I=1,3),((BOT(M1,M),ML=1,9),M=6,8)
0110 704 FORMAT (15X,6(1X,9A1))
CCC CHECK IF A CARRIAGE RETURN HAS BEEN TYPED TO STOP SAMPLING

0111 IY=ITIM()
0112 IF (IY.GE.0) GO TO 122
0114 CONTINUE
0115 END OF TIME STEPS LOOP
0116 GO TO 125
0117 TYPE **,'DO YOU WANT TO CONTINUE SAMPLING IN THE SAME FILE ??'
0118 TYPE **,'(YES=1, NO=0)'
0119 ACCEPT **,IYST
0120 IF (IYST.NE.0) GO TO 121
0121 CLOSE (UNIT=15)
0122 STOP
0123 END
C************ .................................................................
C
C SEND.FOR
C
C PROGRAM TO SEND CONTROL COMMANDS
C TO THE 9635 DAYTRONIC MODULE
C
C EXAMPLE: LOC (FOR MEMORY FREEZE)
C UNL (FOR MEMORY UNFREEZE)
C RST X (TO RESET CHNXL X TO MILLIVOLTS)
C ZRO X (TO SET CHNXL X TO ZERO)
C
C COMMAND TEXT CAN BE UP TO 9 CHARACTERS
C (CARRIAGE RETURN IS ADDED BY THE PROGRAM)
C
C************ .................................................................
C
0001   LOGICAL*1 BUF1(10)
0002   DATA BUF1(10)/"15/
0003   1   TYPE *, 'WHAT DO YOU WANT TO BE SENT?'
0004   ACCEPT 11,(BUF1(I),I=1,9)
0005   11  FORMAT(9A1)
0006   N1=10
0007   I1=0
0008   2   CALL COUT(BUF1,N1,I1)
0009   IF(I1.EQ.1) GO TO 2
0010   TYPE *, 'DO YOU WANT TO SEND MORE? (YES=1, NO=0)'
0011   ACCEPT 12,IY
0012   12  FORMAT(I1)
0013   IF(IY.EQ.1) GO TO 1
0014   STOP
0015   END
PROGRAM TO COMPENSATE FOR TEMPERATURE INDUCED APPARENT STRAIN AND GAGE FACTOR VARIATION OR TO SCREEN SOME MEASUREMENTS FOR THE CASE OF VERY LARGE INPUT FILES READING ARE INPUT FROM FILE 'FLIN' IN INTEGER FORMAT (FORMATS 702 OR 703) CORRECTED STRAINS ARE OUTPUT TO FILE 'FLOUT' IN REAL FORMAT (FORMATS 704 OR 705)

LOGICAL*1 FLIN(16),FLOUT(16)
DIMENSION IVSG(10),IVTC(10),STR(10),TEMP(10),STRAIN(10)

I/O FILES INITIALIZATION AND DATA INPUT

TYPE *, 'WHAT IS THE NAME OF THE INPUT DATA FILE ?'
ACCEPT 701,(FLIN(I),I=1,14)
FORMAT (16A1)
OPEN (UNIT=15,NAM€=FLIN,TYPE='OLD',ACCESS='SEQUENTIAL',
FORM='FORMATTED')

TYPE *, 'WHAT IS THE NAME OF THE OUTPUT DATA FILE ?'
ACCEPT 701,(FLOUT(I),I=1,14)
OPEN (UNIT=16,NAM€=FLOUT,TYPE='NEW',ACCESS='SEQUENTIAL',
FORM='FORMATTED')

TYPE *, 'ENTER NUMBER OF STRAIN GAGE CHANNELS (MAX=10)'
ACCEPT *,NSG

TYPE *, 'ENTER NUMBER OF THERMOCOUPLE CHANNELS (MAX=10)'
ACCEPT *,NTC

NCH=NSG+NTC

IF NSG IS GREATER THAN NTC THE PROGRAM WILL ASSUME THAT THE LAST (NSG-NTC) STRAIN GAGES ARE AT THE SAME TEMPERATURES AS THE FIRST NTC OF THEM

TYPE *, 'ENTER NUMBER OF RUNS '

ACCEPT * , NR

TYPE *, 'EVERY HOW MANY POINTS DO YOU WANT TO READ AND WRITE ?'
ACCEPT *,NJ

NCOUNT=NJ

TYPE *, 'DO YOU WANT TO COMPENSATE FOR TEMPERATURE EFFECTS ?'

TYPE *, (YES=1,NO=0)

ACCEPT *,ITMP

IF NO COMPENSATION IS REQUIRED THE 'SCREENED' DATA
C WILL BE WRITTEN IN THE OUTPUT FILE
C WITH THE SAME FORMAT AS IN THE INPUT FILE
C
0025 TYPE *, 'DO YOU WANT TO CHANGE THE SIGN OF THE STRAIN READINGS ?'
0026 TYPE *, '(YES=1, NO=0)'
0027 ACCEPT *, ISGN
C DEPENDING ON THE CONNECTIONS OF THE BRIDGE COMPLETION CIRCUITS
C IN THE STRAIN GAGE CONDITIONER SIGN CHANGE OF S.G. READINGS
C MIGHT BE NEEDED

0028 DO 100 I = 1, NR
0029 IF (NSG, GT, 5) GO TO 2
0031 READ (15, 702) TIM, (IVSG(J), J = 1, NSG), (IVTC(J), J = 1, NTC)
0032 702 FORMAT (1X, F10.3, 4X, 10(1X, 19))
0033 GO TO 3
0034 2 READ (15, 703) TIM, (IVSG(J), J = 1, NSG), (IVTC(J), J = 1, NTC)
0035 703 FORMAT (1X, F10.3, 4X, 10(1X, 19)/15X, 10(1X, 19))
0036 IF (ISGN, NE, 1) GO TO 300
0038 DO 30 J = 1, NSG
0039 30 IVSG(J) = IVSG(J)
0040 NCOUNT = NCOUNT - 1
0041 IF (NCOUNT, NE, 0) GO TO 100
0043 DO 101 I = 1, NSG
0044 STR(I) = IVSG(I)
0045 IF (I, GT, NTC) GO TO 102
0047 TEMP(I) = IVTC(I)
0048 GO TO 103
0049 102 TEMP(I) = IVTC(I) - NTC
0050 103 IF (TEMP, NE, 1) GO TO 101
0052 CALL COMP(TEMP(I), STR(I), STRAIN(I))
0053 101 CONTINUE
0054 IF (TEMP, NE, 1) GO TO 104
0056 IF (NSG, GT, 5) GO TO 21
0058 WRITE (16, 704) TIM, (STRAIN(J), J = 1, NSG), (TEMP(J), J = 1, NTC)
0059 704 FORMAT (1X, F10.3, 4X, 10(1X, F9.2))
0060 GO TO 31
0061 21 WRITE (16, 705) TIM, (STRAIN(J), J = 1, NSG), (TEMP(J), J = 1, NTC)
0062 705 FORMAT (1X, F10.3, 4X, 10(1X, F9.2), 15X, 10(1X, F9.2))
0063 GO TO 31
0064 104 IF (NSG, GT, 5) GO TO 22
0066 WRITE (16, 702) TIM, (IVSG(J), J = 1, NSG), (IVTC(J), J = 1, NTC)
0067 GO TO 31
0068 22 WRITE (16, 703) TIM, (IVSG(J), J = 1, NSG), (IVTC(J), J = 1, NTC)
0069 31 NCOUNT = NJ
0070 100 CONTINUE
0071 WRITE (UNIT=15)
0072 CONTINUE
0073 STOP
0074 END
**FORTRAN IV**

**V02.5**

```
C*********************************************************
C  COMP.FOR
C*********************************************************
C SUBROUTINE TO CORRECT ERRORS IN
C STRAIN MEASUREMENTS DUE TO TEMPERATURE-INDUCED
C APPARENT STRAIN AND GAGE FACTOR VARIATION
C
C NOTE THAT THE FORMULAS USED FOR APPARENT STRAIN
C OR GAGE FACTOR VARIATION VERSUS TEMPERATURE
C APPLY ONLY TO THE STRAIN GAGES USED
C (M-H WK-09-062AP-350 LOT#K14FE01)
C BONDED ON 304 STAINLESS STEEL
C
C STRAIN IN MICROSTRAIN AND TEMPERATURE IN DEGREES F
C*********************************************************

0001 SUBROUTINE COMP(T,SIN,SOUT)
0002  CHECK IF TEMPERATURE IN ACCEPTABLE LIMITS
0003  IF ((T.LT.0.),0,(T.GT.550.)) GO TO 100
0004  APPARENT STRAIN CALCULATION (-360 DEG F < TEMP < 500 DEG F)
0005  EAPP=-8.4+1.39*T-4.63E-3*T**2+8.57E-6*T**3-9.33E-9*T**4
0006  PERCENT VARIATION OF GAGE FACTOR (0 DEG F < TEMP < 500 DEG F)
0007  SGROOM=2.01
0008  SGCLBR=2.0
0009  IF (T.GT.200.) GO TO 1
0010  DSG=0.17-0.69*T/200.
0011  GO TO 3
0012  1  GO TO 2
0013  DSG=-0.52-1.18*(T-200.)/200.
0014  GO TO 3
0015  DSG=-1.70-1.03*(T-400.)/100.
0016  SG=SGROOM*(1+DSG/100.)
0017  STRAIN CORRECTION
0018  SOUT=(SIN-EAPP)*SGCLBR/SG
0019  RETURN
0020  SOUT=SIN
0021  RETURN
0022 END
```
FORTRAN IV
V0.2.5
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***************************************************************************

LOGICAL FNAME(16),DNUM(16)

DIMENSION VAL(300,15),TIM(300),PLO(300)

DIMENSION IVSG(10),IVTC(10),VSG(10),UTC(10)

TYPE *, 'WHAT IS THE NAME OF THE INPUT DATA FILE ?'

ACCEP 701,(FNAME(I),I=1,14)

FORMAT (14A1)

OPEN(UNIT=15,NAME=FNAME,TYPE='OLD',ACCESS='SEQUENTIAL',
1FORM='FORMATTED')

TYPE *, 'ARE DATA IN INTEGER FORMAT ? (YES=1,NO=0)'

TYPE *, 'NOTE : ORIGINAL READINGS ARE IN INTEGER FORMAT BUT,'

TYPE *, 'AFTER COMPENSATION ARE STORED IN REAL FORMAT'

ACCEPT *,IINT

ACCEPT *,INT

TYPE *, 'ENTER NUMBER OF STRAIN GAGE CHANNELS (MAX=10)'

ACCEPT *,NSG

TYPE *, 'ENTER NUMBER OF THERMOCOUPLE CHANNELS (MAX=5)'

ACCEPT *,NTC

NCH=NSG+NTC

TYPE *, 'ENTER NUMBER OF RUNS (MAX=300)'

ACCEPT *,NR

READ INPUT DATA FILE

DO 100 J=1,NR

IF (INT(NF,1)) GO TO 1

IF (NSG.GT.5) GO TO 2

READ (15,702)TIM(J),IVSG(I),I=1,NSG),IVTC(I),I=1,NTC)

GO TO 3

2 READ (15,703)TIM(J),IVSG(I),I=1,NSG),IVTC(I),I=1,NTC)

GO TO 3

3 DD 301 IS=1,NSG

VAL(J,IS)=IVSG(IS)

DO 302 IT=1,NTC

VAL(J,IT+NSG)=IVTC(IT)

GO TO 100

1 IF (NSG.GT.5) GO TO 21

READ (15,704)TIM(J),IVSG(I),I=1,NSG),UTC(I),I=1,NTC)

GO TO 31

21 READ (15,705)TIM(J),IVSG(I),I=1,NSG),UTC(I),I=1,NTC)

DO 311 IS=1,NSG

VAL(J,IS)=VSG(IS)

DO 312 IT=1,NTC

VAL(J,IT+NSG)=UTC(IT)

GO TO 100

31 CONTINUE

PLOTTING PHASE (USE OF PLTSVK PLOTTING PACKAGE)

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0042 DO 110 I=1,NCH
0043 DO 11 J=1, NR
0044 11 FLO(J)=VAL(J,I)
0045 TYPE *, 'ENTER OPTION (2 FOR SAME PARAMETERS AS PREVIOUS GRAPH)'
0046 ACCEPT *, IDPT
0047 CALL GRAPHIC(TIM, FLO, NR, IDPT)
0048 CONTINUE
0049 CONTINUE
0050 FORMAT(1X, F10.3, 4X, 10(1X, I9))
0051 FORMAT(1X, F10.3, 4X, 10(1X, F9.2))
0052 FORMAT(1X, F10.3, 4X, 10(1X, F9.2))
0053 FORMAT(1X, F10.3, 4X, 10(1X, F9.2))
0054 CLOSED (UNIT=15)
0055 STOP
0056 END