PLASTICITY-BASED DISTORTION ANALYSIS
FOR FILLET WELDED THIN PLATE T-JOINTS

DISSER TATION

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By

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ABSTRACT

The characteristic relationship between cumulative plastic strains and angular distortion of fillet welded thin plate T-joints was studied using numerical analysis. A 3D thermo-elastic-plastic analysis incorporating the effects of moving heat and non-linear material properties was performed to obtain the characteristic cumulative plastic strain distributions and angular distortion. The procedure of plasticity-based distortion analysis (PDA) was developed to map each cumulative plastic strain component into elastic models using equivalent thermal strains. PDA determined the quantitative individual angular distortions induced by cumulative plastic strains, demonstrating their contribution to the total angular distortion and the unique relationship between cumulative plastic strains and distortion.

PDA was used to investigate the effects of external restraints and thermal management techniques on the relationship between cumulative plastic strains and angular distortion of T-joints and T-tubular connections, and the limitation of the 2D model application in the distortion analysis of T-joints. It was shown that PDA was a very effective tool in investigating the relationship between cumulative plastic strains and distortion.
The following significant findings were observed in this study.

1) Distortion was uniquely determined under the specified cumulative plastic strain distribution.

2) The transverse cumulative plastic strain produced bend-down angular distortion in T-joints. Vertical and longitudinal components induced relatively small bend-down and bend-up angular distortion, respectively. Most bend-up angular distortion produced by the xy-plane shear cumulative plastic strain existed in and around the welded region.

3) Angular distortion was reduced by increasing the degree of external restraint. External restraint mainly controlled individual angular distortion induced by the transverse cumulative plastic strain.

4) Heat sinking increased angular distortion of T-joints. Heat sinking affected individual angular distortions induced by the nominal components of cumulative plastic strains.

5) TIG pre-heating resulted in the reduction of angular distortion of T-joints. TIG pre-heating mainly controlled individual angular distortion induced by xy-plane shear cumulative plastic strain.

6) Angular distortion in T-tubular connections was induced by the transverse, vertical and xy-plane shear cumulative plastic strains. Heat sinking effectively reduced angular distortion.

7) The relationship between cumulative plastic strains and angular distortion in T-joints obtained from the 2D and 3D models was inherently different.
Dedicated To
My father
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NOMENCLATURES

A  Sectional area of groove of a butt joint
A*  Area of plastic zone
A,B,C,a,b,c  Parameters defining double ellipsoidal distribution
C1, C2  Material Constants
C0  Initial kinematic hardening modulus
\(c_f, c_b, r_f, r_b\)  Parameters defining double ellipsoidal distribution
D  Diameter of the electrode (mm)
D1, D2  Integration constants
E  Young’s modulus
EPA  Elastic-plastic analysis
h  Plate thickness
I  Current (Amps)
Ic  Second moment of inertia
PDA  Plasticity-based distortion analysis
R  Incompatibility
\(t_{total}\)  Total scanning time (sec)
v  Welding speed
V  Voltage (Volt)
W  Weight of deposited metal per unit weld length (gr/cm)

W_A  Weight of metal deposited in backing pass (gr/cm)

(x, y, z)  Local coordinates

(X, Y, Z)  Global coordinates

(x, y, ξ)  Moving coordinates

q_n  Heat input per unit run (cal/cm)

q(0)  Maximum value of the power density at the center

φ  Angular distortion

φ  Coefficient ( = 1.68 × 10^{-5})

η  Arc efficiency

γ  The rate of kinematic hardening hardening

ψ  Accuracy of the PDA procedure (%)

θ_{ij}(x, y, z)  Temperature distribution along ij direction

α_{ij}  Anistropic thermal expansion coefficient along ij direction

δ_{PDA}^{total}  Total angular distortion determined from PDA (mm)

δ_i  Individual angular distortions (mm)

δ_{EPA}  Total angular distortion determined from EPA (mm)

\bar{e}^{pl}  Equivalent plastic strain

\varepsilon_{ij}  Total strain components

\varepsilon_{ij}^{th}  Thermal strain components
\[ \sum e''_{ij} \quad \text{Cumulative plastic strain components} \]

\[ \sum e'_{i}^{\text{avg}} \quad \text{Averaged cumulative plastic strain at node i of element #2} \]

\[ \sum e'_{i}^{j} \quad \text{Extrapolated cumulative plastic strain at node i of element # j} \]

\[ \sum e''_{i}^{th} \quad \text{Equivalent thermal strain at node i of element #2} \]
CHAPTER 1

INTRODUCTION

1.1 Research Background

A fillet weld is the most common weld type used in the fabrication of structural members in automobile, shipbuilding, petrochemical and other industries. Fillet welded joints suffer from various welding distortion patterns, such as angular distortion, transverse shrinkage, longitudinal distortion and buckling as shown in Figure 1.1. Of the above mentioned distortions, especially, angular distortion has significant impact on fabrication accuracy influencing the productivity and quality of the welded structures. Angular distortion can reduce the quality of welded structures because of misalignment in adjacent joints during fabrication, and after fabrication undesirable appearance and change of structure stiffness. It can also increase the cost of fabrication due to rework such as fairing, cutting, attaching, fitting, gap filling, etc.

Recently, many industries have been seeking application of light materials, such as aluminum and magnesium alloys, to structural members to reduce the weight of
structures, and increase the performance of structures in terms of fuel efficiency and recycling. However, due to higher thermal expansion coefficient, low stiffness/strength ratio, and low softening temperature of aluminum and magnesium alloys, control of welding-induced distortion in these connections becomes a critical issue. These light materials can be applicable in structural frame members and panels consisting of T-plate and T-tubular connections. T-plates and T-tubular connections are built by fillet welds in which angular distortion is one of the major concerns regarding precise prediction and control.

Numerous efforts have been devoted to predict distortion, investigating the characteristics of the generation mechanism of distortion using analytical, experimental, and numerical approaches. Especially, numerical approach using the finite element method has been highlighted because of the development of powerful computers and finite element codes. Remarkable progress was made in prediction of distortion by Ueda [Ref. U1, U2], who was a founder applying the concept of inherent strain method in determination of residual stresses and distortion. Inherent strain method is based on the unique relationship between inherent strains (cumulative plastic strains) and distortion, which implies that distortion can be uniquely determined using elastic analysis if inherent strains are known. In this method, there are some critical issues to be addressed. The first is how to evaluate the unique relationship between inherent strain and distortion. To date, numerical procedures incorporating inherent strains (cumulative plastic strains) directly have not been reported, but many applications of inherent strain method have been carried out to predict distortion without proving the unique relationship between inherent strains and distortion. Reasonable good agreements with results of weld tests have been
reported with certain constrains. The second issue is how to accommodate inherent strains in elastic analysis. Most of the efforts have been focused on determination of equivalent forces and moments, which are obtained from integration of inherent strains related to a distortion pattern of concern. The third issue is how to accommodate the effect of external restraint and thermal management techniques on inherent strains when distortion control plans are required. In order to take into consideration the above-mentioned issues in the inherent strain method, better understanding of the behavior of the inherent strains and their relationship with distortion is necessary.

For angular distortion in welded connections, major research has been focused on angular distortion in a butt weld, attempting to explain the mechanism generating angular distortion. It has been generally believed that non-uniformly distributed cumulative plastic strain (inherent strain) in the transverse direction to the weld line caused angular distortion. Based on this understanding, some simplified elastic models predicting angular distortion have been developed, which use the equivalent forces and moments. At the same time, many researchers have been investigating the characteristics of cumulative plastic strains using analytical, experimental and numerical approaches.

In contrast to angular distortion in butt joints, little research on angular distortion in fillet welded joints using the inherent strain method has been reported. The majority of studies have been experimental in characterizing the pattern of angular distortion. The generation mechanism of angular distortion in fillet welded T-joints was studied based on the results of the finite element method [Ref. K1, M4, O2]. The observation of temperatures and distortions during welding suggests that angular distortion is caused by the contraction source located above the neural axis of the flange plate (horizontal
member). This explanation is very similar to that of butt joints. In fact, many predictions of angular distortion in fillet welded structures, such as panel structures in ship building, have been done by using equivalent forces and moments calculated from butt joints. It is usually assumed that the amount of angular distortion is not affected by the presence of the web plate (vertical member). Recently, some research in analysis of distortion has been focused on finding the specific relation between the cumulative plastic strains and distortions [Ref. H3]. It has been reported that the transverse cumulative plastic strain varies with time and location during heating and cooling. In order to derive a simplistic formulation, the linearized transverse plastic strain distribution is assumed and the effect of other plastic strain components on the angular distortion is additive. However, these results are applicable to butt joints, not fillet welded joints.

Compared to butt joints, fillet welded T-joints have a different configuration including weldment deposited and a web erected on a flange, even though it is constructed by the same welding parameters. One question that can be raised is whether the relationship between cumulative plastic strains and angular distortion holds true in butt joints and in fillet welded joints. Figure 1.2 represents typical deformation patterns inducing bend-up angular distortion. One is a bending deformation related to the gradient of transverse cumulative plastic strain (or transverse inherent strain). This pattern has been well known as a major source resulting in angular distortion. The other is a shear deformation including pure and rotational shear deformation. To date, contribution of shear cumulative plastic strains to angular distortion has not been investigated by researchers.
Unlike butt joints, the effect of shear cumulative plastic strains in fillet welded T-joints may be significant in angular distortion when the web plate is highly constrained.

Many types of distortion control plans have been developed and applied to reduce transverse shrinkage, angular distortion, longitudinal distortion and buckling [Ref. C1, M3-6, O2, P1]. Validity of their effectiveness has been demonstrated by experimentation and numerical simulation. However, no detailed study has been performed to investigate the effect of external restraint and thermal management techniques on the relationship between cumulative plastic strains and angular distortion in fillet welded T-joints. For example, heat sinking has been known as an effective means to reduce buckling distortion [Ref. M 5-6]. However, it may actually increase angular distortion in constrained fillet welded T-joints. In order to develop a realistic distortion control plan, quantitative understanding of their effect on the cumulative plastic strains becomes necessary.

The 2D elastic-plastic analysis has been used in the prediction of an angular distortion of fillet welded T-joints because it allowed the development of a model with more detailed joint configuration within appropriate calculation time [Ref. M4]. The different characteristics of heat flow and stiffness between 2D and 3D models may affect transient stress fields and the final distortion pattern. To date, no study has been performed to investigate the difference between 2D and 3D elastic-plastic analyses of T-joints in terms of distribution pattern of cumulative plastic strains and the relationship between cumulative plastic strains and distortion.
In order to investigate the above-mentioned issues concerning the relationship between cumulative plastic strains and angular distortion, the plasticity-based distortion analysis (PDA) procedure is proposed and used in this study.

1.2 Scope of Current Research

The research subject addressed in this dissertation is to characterize the relationship between the cumulative plastic strains and angular distortion in fillet welded, thin plate T-joints (Magnesium alloy AZ 91 C, 3.2mm thickness in flange and web). Figure 1.3 shows a symmetric half model of fillet welded T-joints, assuming symmetric welding on both sides of the joint.

A 3D welding simulation for T-joints using the finite element method (FEM) is performed to find the cumulative plastic strain fields and angular distortion under the given sets of welding parameters. The moving source with a double ellipsoidal heat distribution is employed in a half symmetry part. Uncoupled thermal and mechanical analysis is carried out. The heat input is determined by calibrating heat input to match the molten pool boundary to the pre-designed fillet size. The validity of numerical procedure is demonstrated by comparing the numerical results with experimental results obtained from weld tests of an aluminum T-joint (4 mm plate thickness) performed by Ohata and others [Ref. O2].

The investigation of the relationship between cumulative plastic strains and angular distortion is performed by plasticity-based distortion analysis (PDA). PDA is an elastic analysis procedure with material properties at room temperature and prescribed incompatible strain fields (e.g. plastic strains or thermal strains). The incompatible strain
fields are prescribed using equivalent thermal strains in PDA. The fields of equivalent thermal strains are obtained by a mapping method that converts cumulative plastic strains obtained from a 3D elastic-plastic analysis into equivalent thermal strains using the anistropic thermal expansion coefficient and the corresponding temperature field. In order to investigate the relative contribution of each cumulative plastic strain component to angular distortion in T-joints, six linear elastic analyses are carried out independently. From each linear elastic analysis, six individual angular distortions are calculated. From results obtained from six linear elastic analyses, new knowledge about the relationship between cumulative plastic strains and angular distortion in T-joint is obtained. The final angular distortion is obtained by adding six individual angular distortions from six independent linear elastic analyses and compared with the final angular distortion obtained from the 3D elastic-plastic analysis. Using this information, a unique relationship between cumulative plastic strains and angular distortion is demonstrated. The effectiveness of elastic modeling is also demonstrated for engineering applications.

Validity of the unique relationship between cumulative plastic strains and angular distortion is demonstrated by other welding situations, such as external restraints and thermal management techniques, which may change the characteristics of cumulative plastic strain distributions.

With the same 3D finite element model for T-joint, the effect of external restraint on angular distortion and the relationship between cumulative plastic strains and angular distortion is investigated. Elastic-plastic analysis with temperature evolution obtained from the 3D thermal analysis is carried out with differing degrees of external restraint. External restrain is applied by fixing boundaries during welding and cooling at different
locations in the flange of a T-joint, and then removing them after cooling is completed. Cumulative plastic strain fields from elastic-plastic analysis with differing degrees of external restraint are used in PDA to calculate individual angular distortions induced by each cumulative plastic strain component.

Distortion mitigation methodologies using thermal management, such as heat sinking and/or TIG pre-heating, are simulated by the elastic-plastic analysis. Heat sinking and preheating have an inherent difference. Heat sinking reduces the effect of heat on the base metal, and TIG pre-heating provides additional heating by an auxiliary heat source. The effect of heat sinking is simulated by applying a relatively high convection coefficient to dissipate heat rapidly on the bottom of the T-joint. In case of the TIG pre-heating, two heat sources are running in the joint vicinity. TIG runs ahead of the GMAW arc at a certain distance on the bottom of the joint. The heat magnitude is controlled to prevent melting in the joint. In each case, PDA is carried out to investigate the effect of thermal management on the relationship between cumulative plastic strains and angular distortion quantitatively.

Applicability of PDA and validity of the unique relationship between cumulative plastic strains and angular distortion are investigated in thin wall T-tubular connections. The 3D elastic-plastic analysis is performed to generate the characteristic cumulative plastic strain field in two cases, with and without heat sinking. The effect of heat sinking is considered by applying a relatively high convection boundary on the inside wall of the flange tube, which is similar to a flange plate in T-joints.
The relationship between cumulative plastic strains and angular distortion in T-tubular connection is investigated using PDA. The effect of heat sinking in T-joint and T-tubular connections on angular distortion is compared using results from PDA.

The limitation of 2D modeling in angular distortion analysis of the T-joint is investigated using PDA. For 2D analysis, a 3D double ellipsoidal heat distribution is scanned on the weld region to generate the same nugget and maximum peak temperature distribution pattern as 3D analysis. 2D PDA is carried out to determine the characteristic relation between cumulative plastic strains and angular distortion. The results from 2D and 3D PDA are compared and used to address the limitation of the 2D model application in the prediction of angular distortion in T-joints.

1.3 Research Objectives

This study is primarily concerned with the identification of the relationship between cumulative plastic strains and angular distortion in fillet welded thin plate T-joints using plasticity-based distortion analysis, PDA. More specifically, the objectives are as follows:

1. Demonstrate the unique relationship between cumulative plastic strains and distortion.

2. Develop the procedure of elastic-plastic welding distortion analysis to generate the appropriate cumulative plastic strain fields in fillet welded thin plate T-joints.

3. Develop plasticity-based distortion analysis (mapping procedure) to transform the cumulative plastic strains into equivalent thermal strains for PDA.
4. Investigate the contribution of each cumulative plastic strain component to angular distortion in fillet welded T-joints, and characterize the relationship between cumulative plastic strains and angular distortion.

5. Investigate the effect of external restraint and thermal management methods, such as heat sinking and TIG pre-heating on relationship between cumulative plastic strains and angular distortion in fillet welded T-joints.

6. Investigate the contribution of each cumulative plastic strain component to angular distortion in thin wall T-tubular connections, and explain the cause of angular distortion.

7. Investigate the effect of heat sinking on the relationship between cumulative plastic strains and angular distortion in thin wall T-tubular connections.

8. Investigate the limitation of 2D model in predicting angular distortion in T-joints.
(a) Transverse shrinkage    (b) Angular distortion            (c) Longitudinal distortion

Figure 1.1 Typical distortion patterns in T-joints

(a) Bending deformation    (b) Shear deformation

Figure 1.2 Typical deformation patterns related to bend-up angular distortion
Figure 1.3 Symmetric half finite element model for T-joints
CHAPTER 2

LITERATURE REVIEW

In this chapter, a literature review of various methods for predicting distortion induced by welding was made in order to understand the general trend of research and the limitation of their applications. The main distortion type concerned is angular distortion, especially in fillet welded T-joints.

2.1 Analytical and Experimental Distortion Analysis

Most of the analytical and experimental distortion analyses carried out in the 1950s and 60s produced simple formulae employing the effect of welding parameters, type of material and geometry on distortion. It has been realized that the application of simple formulae should be used with careful attention because each formula was developed for a very specific case.
2.1.1 Angular distortion in butt joints

As analytical formulations, Okerblom [Ref. O1] proposed a model predicting angular distortion of butt joints. It was assumed that most distortion occurred during cooling. Using simple elasticity and plasticity theories, angular distortion can be determined by the assumed bead shape, the temperature range between solidification and room temperature, thermal expansion coefficient and yield stress of material. This model has been used in many studies developing the simplistic formula [Ref. Z1, F2]. Very recently, Son, et. al. [Ref. S1] developed an analytical formula for welding-induced angular distortion of butt welds using the model of an elliptical cylindrical inclusion with eigen-strain or inherent strain in an infinite laminated plate theory. Using this explicit formula containing material properties, plate thickness, and heat input, angular distortion could be determined without considering the correction factor from weld tests unlike other formulae. The predicted angular distortion was very well fitted with the test results, especially the GTAW test results.

As empirical formulations, Kirillov [Ref. K1] proposed an experimental formula predicting angular distortion in single-pass butt joints by incorporating the residual curvature and the size of plastic deformation. Considering the relationship between the residual curvature and the size of plastic deformation, angular distortion was described as a function of heat input per unit run and thickness of the plate:

\[ \phi = \phi \frac{q_o}{h^2} \]  

(2.1)
where \( \phi \) is the coefficient \((=1.68 \times 10^{-5}\ \text{radian/(cal/cm}^3)\)), \( q_n/h < 500 \), \( q_n\) (cal/cm) is heat input per unit run, and \( h \) (cm) is thickness of the plate. This formula implies that the ratio of heat input and rigidity of a joint is related to angular distortion.

Watanabe and Satoh’s formula [Ref. W1] included the effect of welding parameters, electrode and plate thickness:

\[
\phi = C_1 \left( \frac{A}{h^2} \right) \left[ 2 \cdot \exp \left( -\frac{C_2}{h^{3/2}} \left( \frac{W}{W_A} \right)^{3/4} \right) - \exp \left( -\frac{C_2}{h^{3/2}} \right) \right] \\
\]

\[
C_1 \propto \frac{1}{w_0} \left( \frac{\eta VI}{D^{0.25} \sqrt{V}} \right), \quad C_2 \propto \left( \frac{\eta VI}{D^{0.25} \sqrt{V}} \right)
\]

where \( A = \) sectional area of groove of butt joint (cm\(^2\)), \( h = \) thickness of plate (cm), \( W = \) weight of deposited metal per unit weld length (gr/cm), \( W_A = \) weight of metal deposited in backing pass (g/cm), \( D = \) diameter of the electrode (mm), \( \eta = \) arc efficiency, \( V = \) voltage (Volt), \( I = \) current (Amps) and \( v = \) welding speed (cm/sec). Compared to the formula, Equation (2.1), this included more various factors.

Masubuchi [Ref. M3] describes the cause of angular distortion in butt welded joints: “Angular distortion occurs when the transverse shrinkage is not uniform in the thickness direction”. This implies that the distribution pattern of the transverse shrinkage is governed by arc characteristics, electrode characteristics and joint characteristics which are included in Equation (2.2). This basic understanding of the relationship between transverse shrinkage and angular distortion has been applied to many applications of welding distortion analysis regardless of the type of joints.
2.1.2 Angular distortion in fillet welded T-joints

Hirai and Nakamura [Ref. H1] investigated angular distortion experimentally for steel fillet welded joints, and provided a graph to predict angular distortion for fillet welded T-joints with different thickness. Taniguchi [Ref. T1] applied the same method to aluminum alloy fillet welds. In both results, angular distortion was prescribed as a function of plate thickness, and weight of electrode consumed per weld length, which means that angular distortion is related with the rigidity of joints (plate thickness) and welding parameters (weight of electrode deposited). Results also showed that the maximum angular distortion occurred at a certain thickness range, which means if plates were thinner or thicker than this thickness range, less angular distortion would occur. This implies that angular distortion in fillet welded T-joints is related with not only the temperature gradient along a flange plate thickness, but also rigidity of joints.

Watanabe and Satoh [Ref. W1] proposed a formula predicting angular distortion of fillet welded T-joints including the effect of welding parameters, electrode and plate thickness which is similar to Equation (2.2):

\[
\phi = C_1 \left( \frac{I}{h \sqrt{vh}} \right)^{1.5} \exp \left( -C_2 \frac{I}{h \sqrt{vh}} \right) \quad (2.3)
\]

\[
C_1 \propto \left( \frac{\eta V}{D^{0.25}} \right)^{2.5}, \quad C_2 \propto \frac{\eta V}{D^{2.5}}
\]

where \( h \) = thickness of plate, \( D \) = diameter of an electrode, \( \eta \) = arc efficiency, \( V \) = voltage, \( I \) = current and \( v \) = welding speed.
The basic concepts of the formulation of Equation (2.2) and (2.3) are similar, which means that they may believe that the cause of angular distortion in butt joints and fillet welded T-joints is similar.

2.2 Numerical Distortion Analysis

2.2.1 2D and 3D thermal-mechanical distortion analysis

Numerical distortion analysis consists of two parts: thermal analysis and elastic-plastic analysis. In general, these two analyses are performed separately because heat generation during plastic deformation is small enough to be negligible compared to the heat input from the arc.

In thermal analysis, thermal material properties, such as thermal conductivity, specific heat and density might be dependent upon temperature. Friedman [Ref. F1] included the effect of the solid-liquid phase transformation using latent heat as well.

It is very important to develop the heat source model incorporating the physical behavior of the arc. As a simplistic approach, some researchers [Ref. R1] adopted temperature evolution from analytical models, such as a semi-infinite solid with point heat source model proposed by Rosenthal [Ref. R3], without performing a numerical thermal analysis. For a 2D model, a ramped heat source was developed in order to consider the effects of heating and cooling during the movement of the arc. Lee [Ref. L2] applied the heat separating into surface and body flux to a nugget shape obtained from the weld tests. Han [Ref. H3] applied only body flux on the nugget, calibrating the amount of heat to match the nugget and the maximum peak temperature boundary with a
melting temperature. However, it is difficult to obtain the temperature during heating and cooling because the heating time is calculated by unit thickness and welding speed.

In order to overcome this difficulty, Michaleris, et. al [Ref. M5] introduced a new 2D thermal model by scanning a 3D moving heat source onto the 2D finite element model, and obtained an accurate temperature evolution during heating and cooling. Dika and et. al. [Ref. D1] simulated multipass welded butt joints using a 2D thermal analysis and a 3D elastic-plastic analysis to reduce the calculation time by introducing a mapping of 2D thermal solutions to the 3D elastic-plastic model.

The most accurate temperature evolution can be obtained from a 3D thermal analysis. Tekriwal and Mazumber [Ref. T2-3] developed a 3D transient heat transfer model using the commercial finite element code, ABAQUS. A double ellipsoidal body flux distribution proposed by Goldak [Ref. G1] was used and applied as the moving heat by using user-subroutine, DFLUX. They also simulated the effect of deposition of filler metal by adding element groups step by step. The weld length was very short because of the limitation of the computation capability. In order to reduce the calculation time, a 3D instantaneous heat source model in which the entire deposition of a weld metal occurs simultaneously has been used. Kim, et. al. [Ref.K2] simulated fillet welded T-joints using 3D models with an instantaneous heat source and a moving heat source. They found that a model with the instantaneous heat source predicted larger angular distortion than a model with the moving heat source. Recently, due to the development of more powerful computers, more realistic 3D thermal analysis incorporating the moving heat source and the detailed geometric configuration of the welded structures has been carried out in many studies.
In the elastic-plastic analysis, the behavior of material significantly impacts on the determination of distortion and residual stress. In welding situations, materials experience a severe variation of temperature from room temperature to melting or evaporation temperature. Therefore, the dependency of material properties, such as Young’s modulus, Poisson’s ratio, yield strength, strain hardening and thermal expansion coefficient, on temperature might be taken into consideration. Especially, stress and strain relaxation at melting temperature has been reported as one of the causes of the discrepancy of distortions from the numerical simulations and the weld tests. Total relief of plastic strains at above melting temperature has been considered in some analysis [Ref. F1, P2, M4, H3]. Han [Ref. H3] developed the user-subroutine, UMAT using ABAQUS to describe the relaxation of all types of stresses and strains after melting. Angular distortion predicted by incorporating the effect of stresses/strains relaxation had a good agreement with that obtained from weld tests.

The deposition of a weld metal might have an affect on the transient stress in multi-pass welding sequence. Rybicki, et. al. [Ref. R1] considered the non-welded parts as the artificial regions with zero stiffness. ABAQUS provides a command, MODEL CHANGE by which the set of elements can be activated or deactivated. Han [Ref. H3] included this in his relaxation model where the set of elements in the nugget was deactivated during heating, and then activated after the temperature reached the melting temperature. In order to simulate the effect of moving heat, the element group located in front of the arc should be deactivated. Tekriwal and Maxumber [Ref. T2-3] used ABAQUS to simulate the deposition of the weld metal in a 3D thermal-mechanical model.
In general, two types of mechanical models have been used in the residual stress and distortion analysis: a 2D generalized plane strain model and a 3D solid model. The generalized plain strain model has been used in many applications, especially for multipass welded joints, because of the relatively short calculation time compared to the 3D solid model. However, some discrepancy between distortions predicted by the generalized plain strain model and that obtained from weld tests has been reported [Ref. D1, H3, H4]. One of the approaches to reduce this discrepancy is to consider the effect of stress and strain relaxation during melting [Ref. H3, H4]. Dika, et. al. [Ref. D1] simulated multipass welded butt joints using the generalized plane strain model, concluding that a 2D model was not capable of predicting the accumulated shrinkage occurring after the first pass. Ma, et. al. [Ref. M4] developed a 2D generalized plane strain finite element model to simulate residual stresses and angular distortion in fillet welded T-joints. The residual stress distributions computed by the 2D generalized plane strain model showed very good agreement with those of the 3D model. Based on this, the 2D model was used in various parameter studies. However, it should be pointed out that there was no data to show the accuracy of the 2D model in terms of distortion. To date, no clear explanation of the cause of this discrepancy has been reported.

Similar to the 3D solid model, a 3D instantaneous heat source mechanical model and a 3D moving heat source mechanical model were developed. The 3D instantaneous heat source mechanical model called the inherent shrinkage model proposed by Tsai [Ref. T4], is a simplified approach, which is similar to the 2D generalized plane strain model. Kim, et. al. [Ref. K2] simulated fillet welded T-joints using the 3D instantaneous heat source mechanical model and the 3D moving heat source mechanical model. In terms of
angular distortion predicted, the 3D instantaneous heat sources mechanical model generated more angular distortion than the 3D moving heat source mechanical model. Bachorski, et. al. [Ref. B1] predicted angular distortion of butt joints using a 3D instantaneous heat source mechanical model considering only cooling part of temperature evolution, from the elevated temperature (900°C for carbon steel weldments) to room temperature, without a heating-up thermal cycle. They concluded that the accuracy of the predicted angular distortions was very sensitive to the assumed shrinkage volume which could be the volume of the nugget or the heat affected zone. Compared to the results obtained from weld tests, a more accurate angular distortion was predicted in the case where the volume of the heat affected zone was used as the shrinkage volume.

The 3D moving heat source mechanical model has been accepted as the most reliable model in the prediction of distortion, but its application has been limited because of the tremendous calculation time required and the need for a powerful computer processor. Tekriwal and Mazumber [Ref. T2-3] simulated butt welded joints using the 3D moving heat source mechanical model, investigating various effects of modeling parameters on residual stresses and distortions. Unfortunately, the reasonable deformation shape was not obtained because the weld line was too short. Dika, et. al. [Ref. D1] simulated multipass welded butt joints using the 3D moving heat source mechanical model with the temperature evolution mapped from the 2D thermal analysis. The 3D moving heat source mechanical model described better accumulated shrinkage during multi-pass welding than the 2D generalized plane strain model.
Even though there is no sufficient data proving the accuracy, distortions predicted by the 3D moving heat source mechanical model may be used as the base line results when weld tests are not available.

2.2.2 Simplified distortion analysis

Residual stresses and distortions after welding are caused by the cumulative plastic strains accumulated during plastic deformation. The characteristics of the cumulative plastic strains are strongly dependent upon the history of plastic deformation. In order to develop a simplified distortion analysis model reducing the calculation time with reasonable accuracy, better understanding of the history and the final distribution pattern of plastic strains is necessary. So far, two types of the simplified distortion analysis have been developed and used in prediction of welding-induced distortions.

One is the “inherent shrinkage model” proposed by Tsai [Ref. T4]. Based on the rigorous observation of numerical simulation results, it was assumed that the most important part of temperature evolution was the cooling region, and the sequential deposition of weldment was negligible. Park [Ref. P3] verified the inherent shrinkage model in the prediction of angular distortion in aluminum T-joints. He also applied this model in the investigation of welding sequence effects on aluminum panel structures to find the optimal sequence reducing welding-induced distortion. Kim, et. al. adopted this model by applying an instantaneous heat source on the entire weldment of T-joint. More angular distortion with the instantaneous heat source was obtained than with the moving heat source.
Bachorski, et. al. [Ref. B1] developed a thermal-elastic model employing the concept of the inherent shrinkage model. They ignored the heating-up thermal cycle, considering only shrinkage occurring during cooling. Three different shrinkage volumes tested were determined from the joint preparation geometry, thermal model with split heat source and the actual welded fusion zone. A model with the actual welded fusion zone predicted better angular distortion than others. Han [Ref. H3] developed a fully elastic shrinkage model by introducing a shrinkage volume calculated by integrating cumulative plastic strain existing on the plastic zone. He addressed the effect of longitudinal plastic strain on angular distortion which should be taken into consideration in the analysis.

The other is the “inherent strain model” developed by Ueda [Ref. U1]. Ueda and his co-workers [Ref. U2] claimed that welding-induced distortion could be determined by a linear elastic model if the magnitude and distribution pattern of the inherent strains were known. Luo, et. al. [Ref. L1] developed an elastic model to predict welding induced-residual stresses and distortions using the inherent strain concept. As in other application of the inherent strain model, Jang, et. al. [Ref. J1] determined the equivalent forces and moments obtained from the characteristic inherent strain distribution in order to predict the deformation induced by line heating. Son, et. al. [Ref. S1] adopted this concept to derive the analytical solution of angular distortion.

2.3 Distortion Control

Distortion induced by welding has been regarded as a critical issue in terms of performance, quality and productivity. Mashubuchi [Ref. M3] summarized methods of
distortion-reduction in welded joints based on research he had performed. He reviewed the general distortion-reduction methods in terms of weldment dimension, joint design, welding process, multipass welding, constraints, welding sequence, intermittent welding and peening. More detailed discussion on the effects of external restraints and thermal-pattern alteration was presented. Pavlovsky and Masubuchi [Ref. P1] reviewed the various distortion control methods studied by U.S.S.R. researchers. Conrardy and Dull [Ref. C1] reviewed the distortion control techniques applicable in thin ship panel structures. To reduce buckling, modifying panel design, applying intermittent welding, reducing heat input and applying thermal tensioning were recommended. Restraint, back-bending and back-side line heating were recommended as techniques reducing angular distortion.

Recently, the finite element method has been used in the investigation of the performance of various distortion control techniques, and has provided the fundamental understanding of distortion mechanism and the effects of distortion control techniques on the distortion pattern. Park [Ref. P3] developed a model to predict thin plate panel distortion, and simulated the effect of welding sequence on the reduction of distortion. Ohata, et. al [Ref. O2] introduced the TIG preheating method to reduce the angular distortion in fillet welded aluminum thin plates, and performed weld tests and finite element analysis to evaluate its effectiveness. Michaleris and his co-workers [Ref. M5-6] verified thermal tensioning in the reduction of buckling in panel structures using tests and finite element analysis. Ma, et. al. [Ref. M4] simulated the effects of weld sequence, working table and external restraint on angular distortion of fillet welded T-joints using a 2D finite element analysis. Han [Ref. H3] simulated the effect of side heating, heat
sinking and their combination on the distribution pattern of longitudinal plastic strain associated with longitudinal compressive stress causing buckling using a 2D finite element analysis.
CHAPTER 3

NUMERICAL 3D THERMAL-ELASTIC-PLASTIC ANALYSIS
FOR PREDICTING DISTORTION IN FILLET WELDED THIN PLATE T-JOINTS

To date, many numerical models using the finite element method have been developed to predict distortion induced by welding. A 2D model has been used in many applications because of its advantages, e.g. shortening calculation time, more detail in the model of geometry configuration. On the other hand, the application of a 3D model has been limited because it requires tremendous calculation time. Dike [Ref. D1] concluded that the 3D model with the effect of moving heat successfully predicted angular distortion induced by gas metal arc multipass welds, but a 2D model and 3D model with an instantaneous heat did not perform well. Kim [Ref. K2] demonstrated the effect of a moving heat source on angular distortion of fillet welded T-joints by comparing angular distortion obtained from a 3D moving heat source model and a 3D instantaneous heat source model that is similar to a 2D model. Therefore, in this dissertation, a 3D thermal-elastic-plastic analysis with a moving heat source is chosen as a baseline analysis predicting the appropriate distortions and distribution of cumulative plastic strains.
In this chapter, angular distortion in fillet welded T-joints is predicted using a 3D thermal-elastic-plastic analysis with a moving heat source, and considered as a baseline angular distortion which is used in the evaluation of plasticity-based distortion analysis (PDA) procedure described in chapter 4. The corresponding distribution patterns of each cumulative plastic strain are obtained and converted into the equivalent thermal strains in PDA as well.

A 3D thermal-elastic-plastic analysis was performed using ABAQUS 5.8-14. In a real welding situation, interaction between thermal, metallurgical and mechanical behaviors of material occurs. If all interactions are accommodated in the analysis, a coupled analysis should be carried out, which requires a complex algorithm and tremendous computing time. In this dissertation, a simpler model that incorporates most of the significant physical behaviors of material is used with the following assumptions and limitations.

- Heat generation from plastic deformation is negligible compared to the heat input from the arc.
- A nugget shape implies the characteristics of fluid flow and heat diffusion.
- Interaction between stress and metallurgical phase transformation during liquidation and solidification is not considered.
- The effect of phase transformation from solid to liquid and liquid to solid is considered using latent heat.
- Stress and strain relaxation over melting temperature is not considered.
- The effect of deposition of a filler metal is not considered.
Figure 3.1 represents a numerical procedure of the decoupled thermal-elastic-plastic analysis. In thermal analysis, temperature evolution over all nodes is obtained for the given welding parameters, material and joint configuration. Evaluation of the results can be performed by comparing the temperature evolution at given locations with the experimental results, or comparing the nugget shape from the weld tests with the predicted boundary of the molten pool. Those evaluation methods require weld tests, which may not be available in all cases. In this study, a simple way of heat input calibration is proposed for fillet welded thin plate T-joints where the weld test results are not available. Elastic-plastic mechanical analysis is sequentially carried out with temperature evolutions retrieved from thermal analysis under the specified boundary conditions, such as symmetry boundary and restraints from jigs and fixtures, and calculates residual stresses and distortions.

3.1 Thermal Analysis

Thermal analysis consists of the following basic procedures: modeling material properties, modeling moving heat source, developing a finite element model, heat input calibration and performing the solution procedure.

3.1.1 Modeling material properties

The material that is used in this dissertation is a magnesium alloy, AZ91 C. When thermal analysis is transient rather than static, then density, specific heat and thermal conductivity are needed. In this analysis, the temperature dependency of material properties is considered except for density as shown in Figure 3.2. It is assumed that
density was constant over the entire temperature range, and thermal conductivity over liquidus temperature, 595°C, and specific heat over 1127°C were constant. The latent heat, solidus and liquidus temperature are 3.73E5 J/(kg·°C), 470°C and 595°C, respectively. Welding parameters for 3.2 mm thickness plate T-joint with fillet welds are Voltage = 13 Volt, Current = 110 Amps, Weld speed = 10 mm/sec [Ref. A2].

3.1.2 Modeling moving heat source

The effect of the moving heat source was incorporated via user-subroutine in ABAQUS, DFLUX [Ref. A1]. In order to describe the motion of the arc, the moving coordinate system was defined. Figure 3.3 defines the moving coordinate system. The moving coordinate system can be defined with weld speed, time and fixed global coordinate system (X,Y,Z).

The local coordinates system (x,y,z) is defined by translation and rotation transformation at location “A” which is the starting point of welding.

\[
\begin{bmatrix}
    x \\
    y \\
    z
\end{bmatrix} = \begin{bmatrix}
    \cos 45 & \sin 45 & 0 \\
    -\sin 45 & \cos 45 & 0 \\
    0 & 0 & 1
\end{bmatrix} \begin{bmatrix}
    X - X_0 \\
    Y - Y_0 \\
    Z - Z_0
\end{bmatrix}
\]  

(3.1)

where \(X_0, Y_0, Z_0\) are coordinate values of point A in the global coordinate system. The moving coordinate is fixed at the arc position. Point “ C ” located in the weldment can be described by two different coordinate systems.
One is the moving coordinate system \((x,y,\xi)\), and the other is the local coordinate system \((x,y,z)\) defined in equation (3.1). From Figure 3.3, the relation between the two coordinate systems can be found as

\[
\xi = Z_t - Vt
\]  

(3.2)

where \(V = \) weld speed, \(t = \) current weld time.

In the moving coordinate system, heat distribution does not change with respect to time.

As a heat distribution pattern, the double ellipsoidal distribution proposed by Goldak [Ref. G1] was used. The double ellipsoidal heat distribution can be derived from the ellipsoidal heat distribution. The Gaussian distribution of the power density in an ellipsoid with the center at \((0,0,0)\) and semi-axes \(a, b, c\) which are parallel to coordinate axes \(x, y, \xi\) shown in Figure 3.3 can be described by:

\[
q(x, y, \xi) = q(0)e^{-A\xi^2}e^{-Bz^2}e^{-C\xi^2}
\]  

(3.3)

where \(q(0) = \) maximum value of the power density at the center.

Under the conservation of energy, total heat can be written as:

\[
Q = \eta VI = 4\int_0^\infty \int_0^\infty \int_0^\infty q(0)e^{-A\xi^2}e^{-Bz^2}e^{-C\xi^2} \, dx \, dy \, \xi
\]  

(3.4)
where \( \eta = \text{arc efficiency}, \ V = \text{voltage}, \ I = \text{current}. \)

Maximum value of the power density at the center can be obtained from Equation (3.4):

\[
q(0) = \frac{2Q\sqrt{ABC}}{\pi\sqrt{\pi}}
\]

(3.5)

Constants, \( A, \ B \) and \( C \) can be determined by assumption that the power density reduces to 0.05 \( q(0) \) at the surface of the ellipsoid.

\[
A = \frac{3}{a^2}, \ B = \frac{3}{b^2}, \ C = \frac{3}{c^2}
\]

(3.6)

From Equation (3.3), (3.4) and (3.6), an ellipsoidal distribution of heat density can be written as:

\[
q(x, y, \xi) = \frac{6\sqrt{3}\eta VI}{abc\pi \sqrt{\pi}} \exp\left(-\frac{3x^2}{a^2} - \frac{3y^2}{b^2} - \frac{3\xi^2}{c^2}\right)
\]

(3.7)

From observation of a moving arc, it was found that the gradient of the temperature at the front and rear of the arc did not match with the results obtained from Equation (3.7). In order to overcome this limitation, a double ellipsoidal distribution was proposed. In this distribution, heat density (body flux) distributions at the front and rear of the arc are separately described as:
\[ q(x, y, \xi) = \frac{6\sqrt{3}r_f \eta V I}{abc_f \pi \sqrt{\pi}} \exp\left(-\frac{3x^2}{a^2} - \frac{3y^2}{b^2} - \frac{3\xi^2}{c_f^2}\right) \text{ [Front region]} \quad (3.8) \]

\[ q(x, y, \xi) = \frac{6\sqrt{3}r_b \eta V I}{abc_b \pi \sqrt{\pi}} \exp\left(-\frac{3x^2}{a^2} - \frac{3y^2}{b^2} - \frac{3\xi^2}{c_b^2}\right) \text{ [Rear region]} \quad (3.9) \]

Each parameter in equation (3.8) and (3.9) are found to provide the best correspondence between temperatures obtained from Equation (3.8) and (3.9) and the experiments. Nguyen, et al. [Ref. N1] derived the relation between each parameter in Equation (3.8) and (3.9). Based on this relation, parameters in Equation (3.8) and (3.9) for fillet welded T-joints with 3.2 mm of weld leg in fillet welds:

- \( a = b = 3.2 \sin 45 \)
- \( c_f = a, \quad c_b = 2a \)
- \( r_f = \frac{2c_f}{c_f + c_b}, \quad r_f = \frac{2c_b}{c_f + c_b} \)

3.1.3 Developing a finite element model

A half of T-joint was modeled because it was assumed that the two fillet welds were carried out simultaneously. Quadratic brick elements with 20 nodes, DC3D20 in ABAQUS, were used. The total number of elements and nodes were 6100 and 30068, respectively. Figure 3.4 shows the finite element model used in thermal analysis for a fillet welded T-joint, with a flange, 100 mm × 200 mm × 3.2mm, and a web erected on the flange plate, 100 mm × 100 mm × 3.2mm.
The effect of heat diffusion by radiation was ignored, and only the natural convection boundary was described on the free surface of the T-joint. The film coefficients used for the convection boundary at different temperature are shown in Figure 3.5.

3.1.4 Heat input calibration

In order to predict distortion patterns induced by welding, it is critical to obtain a reasonable temperature evolution without the significant loss of accuracy. For fillet welded joints, it was proposed that heat input calibration would be carried out by matching the boundary of molten pool with the pre-design fillet size. Figure 3.6 shows temperature evolutions obtained from thermal analysis with heat input calibrated by the pre-designed fillet size at specific points on an aluminum T-tubular connection. The predicted temperature evolutions were compared with the test results provided by Volvo, an automobile company [Ref. J2]. Even though only one measured data was available, very good agreement was observed.

In order to obtain the boundary of the molten pool, a user-subroutine, UVARM [Ref. A1], was developed to calculate the maximum peak temperature over all nodes. Figure 3.7 shows a map of the maximum peak temperature calculated by UVARM without any correction factor. With the given welding parameters, the finite element model predicted a very good weld profile without any calibration of heat input. It should be noted that the deposition of a filler metal was not considered in this analysis.
3.1.5 Solution procedure and output control

Thermal analysis for T-joints consists of two steps: welding and cooling. Total welding and cooling time were 10.32 and 3600 seconds, respectively. The automatic time stepping option was turned on to reduce the calculation time. With the output control option, temperature evolution on all nodes was saved at each time increment during the solution procedure.

3.2 Elastic-Plastic Mechanical Analysis

The main purpose of elastic-plastic mechanical analysis is to obtain the characteristic cumulative plastic strain distribution patterns and the associated angular distortion for fillet welded thin plate T-joints.

In elastic-plastic mechanical analysis, the same finite element model used in thermal analysis was used. The temperature evolution was retrieved from thermal analysis.

3.2.1 Material properties

In thermal-elastic-plastic analysis, it has been know that the most difficult part is to obtain the appropriate material properties at the elevated temperature. Especially, for special and rare alloys, it is very hard to find the material database applicable at the elevated temperature. For the magnesium alloy, AZ91 C, only a few material properties at the elevated temperature are available from metal handbooks.
Dependency of elastic modulus, yield strength and tensile strength on temperature may give a significant impact on cumulative plastic strains and distortion patterns in the elastic-plastic mechanical analysis.

In this study, mechanical material properties were modeled by statistical assessment of the applicable material properties of magnesium alloys at elevated temperatures. Figures 3.8-10 show mechanical properties depending on temperature. Non-linear kinematic strain hardening proposed by Chabochi [Ref. A1, C2] was used. Back-stress evolution is used to define the kinematic strain hardening model which is:

\[ \alpha = \frac{C_0}{\gamma}(1 - e^{-\gamma \bar{\varepsilon}^{pl}}) \]  

(3.5)

where \( C_0 \) = material constant representing initial kinematic hardening modulus, \( \gamma \) = material constant determining the rate at which the kinematic hardening modulus decreases with increasing plastic deformation, and \( \bar{\varepsilon}^{pl} \) = equivalent plastic strain.

The material constants used in Equation (3.5) are summarized in Table 3.1. It was assumed that thermal strain over melting temperature was constant. Thermal expansion coefficients associated with this were modified as shown in Figure 3.10.

3.2.2 Boundary conditions

The top edge of the web plate, \( y = 100\text{mm} \) in Figure 3.3, was fixed during welding and cooling. Symmetric boundary conditions were applied in Y-Z plane as shown in Figure 3.3.
3.2.3 Results

Figure 3.11 shows the deformed shape after welding and cooling. Significant bend-up angular distortion is observed. Angular distortion can be interpreted by displacement in the Y-direction. The maximum angular distortion along the free edge of the flange plate is $\delta = 1.05$ mm at its mid-span, $Z = 50$mm. It is shown that angular distortion at the weld stop is smaller than at the weld start.

Six components of cumulative plastic strains are plotted in Figure 3.12 with the same color contour spectrum. It has been reported that transverse cumulative plastic strain shown in Figure 3.12 (a) is mainly related to angular distortion when it has the gradient through thickness of plate [Ref. K2, O2]. Longitudinal component has been realized as a main source related to longitudinal bending and buckling. To date, three shear components shown in Figure 3.12 (d), (e) and (f) have not been highlighted in any distortion analysis. As shown in Figure 1.2, the shear deformation associated with cumulative shear plastic strains may result in angular distortion. Therefore, if a significant amount of shear cumulative plastic strains exists, angular distortion induced by shear components can be significant.

3.3 Evaluation of Thermal-Elastic-Plastic Analysis Procedure

In the numerical analysis, some uncertainty comes from material properties at the elevated temperature, plasticity model, boundary conditions, numerical errors, etc. Therefore, the numerical analysis procedure should be evaluated by test results.
It should be noted that there is also uncertainty from the measurement procedure, such as calibration of measuring instruments and precision of equipment, data scattering and inconsistent test conditions, etc.

In this section, the procedure of thermal-elastic-plastic analysis used in the previous section is evaluated by comparing it with results from weld tests performed by Ohata for aluminum (AL5008-O) T-joint with 4 mm thickness [Ref. O2].

3.3.1 Dimensions and welding parameters

In the referenced welding test, the material was AL5083-O aluminum alloy rolled flat plates and extrusions. Figure 3.13 shows the geometry of the fillet welded T-joint specimen tested. Thicknesses of the flange plate and web plate were 4 mm and 3 mm, respectively. Fillet welding was performed by single-pass, one-side GMAW to join the web to the flange. The filler wire was 1.2 mm diameter 5183 wire. Heat input applied was 1058 J/cm with welding speed of 30 mm/sec.

In the numerical analysis, the length of the T-joint is reduced from 500 mm to 100 mm in order to reduce the calculation time.

3.3.2 Thermal analysis

Figure 3.14 shows a finite element model with a one-side fillet weld, and locations where angular distortions are measured. The type of element was DC3D20, and the number of nodes and elements were 27382 and 5475, respectively. Material properties for AL 5082-O used in thermal analysis are represented in Figure 3.15. The natural convection boundaries were described on the free surface of T-joint. Two cases
were simulated in order to observe the effect of preheating on angular distortion. One was without preheating and the other with a preheating temperature of 200 °C. Preheating was considered by defining the initial temperature field.

From thermal analysis with arc efficiency of 0.85, the molten zone is matched with the designed fillet size as shown Figure 3.16 for two cases. A larger nugget and a wider heated zone are achieved in the case with preheating.

3.3.3 Elastic-plastic mechanical analysis

The same finite elements were used in thermal and elastic-plastic mechanical analyses. The type of element was C3D20R. As mechanical boundary conditions, the top free surface of a web was completely fixed during welding and cooling. Figure 3.17 shows the mechanical material properties of AL5083-O. Parameters used in kinematic strain hardening are summarized in Table 3.2. In order to compare with test results, vertical displacements at the free edge of the flange were monitored.

Figure 3.18 shows the deformed shape in two cases, with and without preheating. The averaged angular distortions for the cases with and without preheating are 0.42 mm ~ 0.62 mm and 0.27 mm ~ 0.36 mm, respectively. Figure 3.19 compares angular distortions obtained from both weld tests and simulations. It is shown that numerical simulations predict less angular distortions than weld tests. Considering various above-mentioned uncertainties and the small discrepancy between angular distortions from weld tests and simulations, it can be concluded that the procedure of the numerical simulation is appropriate to use in the prediction of angular distortion and engineering measures associated with welding distortion.
Figure 3.1 Schematic diagram for thermal-elastic-plastic analysis procedure
Figure 3.2 Thermal material properties of magnesium alloy, AZ 91 C

Figure 3.3 Definition of a moving coordinate system in fillet welded T-joints
Figure 3.4 A finite element model for T-joints

Figure 3.5 Natural convection (film) coefficients depending on temperature
Figure 3.6 Results of thermal analysis for aluminum T-tubular connection

(a) Moving arc

(b) Temperature evolutions obtained from thermal analysis and weld tests
Figure 3.7 Maximum peak temperature map in the T-joint

<table>
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<th>Temp (°C)</th>
<th>21</th>
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<th>225</th>
<th>325</th>
<th>400</th>
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<td>600</td>
<td>10</td>
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<td>1</td>
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<tr>
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<td>27</td>
<td>27</td>
<td>27</td>
<td>27</td>
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</tr>
</tbody>
</table>

Table 3.1 Material constants for nonlinear kinematic hardening of magnesium alloy
Figure 3.8 Yield and tensile strength of magnesium alloy
Figure 3.9 Elastic modulus and elongation of magnesium alloy
Figure 3.10 Coefficients of thermal expansion and Poisson’s ratio of magnesium alloy
Figure 3.11 Deformed shape of the T-joint after welding
Figure 3.12 Cumulative plastic strain maps in the T-joint

Red: 0.002 ~ , Blue: ~ -0.002

48
Figure 3.13 Dimensions of fillet welded aluminum T-joints

Figure 3.14 A finite element model for aluminum T-joints, and locations measuring angular distortion
\[ \theta_{\text{solidus}} = 591^\circ C, \quad \theta_{\text{liquidus}} = 638^\circ C \]

\[ \text{Latent Heat} = 3.8846 \times 10^5 \frac{J}{kg \cdot ^\circ C} \]

Figure 3.15 Thermal material properties of AL5083-O
Figure 3.16 Maximum peak temperature maps in aluminum T-joints

(a) Without preheating

(b) Preheating with 200 °C

Cross section at z= 50 mm

Cross section at z= 50 mm
Figure 3.17 Mechanical material properties of AL5083-O
A case without preheating
\[ \delta_1 = 0.16 \text{ mm} \sim 0.31 \text{ mm}, \quad \delta_2 = 0.68 \text{ mm} \sim 0.83 \text{ mm}, \quad \delta_{\text{avg}} = 0.42 \text{ mm} \sim 0.62 \text{ mm} \]

A case with preheating
\[ \delta_1 = 0.12 \text{ mm} \sim 0.21 \text{ mm}, \quad \delta_2 = 0.41 \text{ mm} \sim 0.50 \text{ mm}, \quad \delta_{\text{avg}} = 0.27 \text{ mm} \sim 0.36 \text{ mm} \]

Figure 3.18 Angular distortions in aluminum T-joints
Figure 3.19 Comparison of angular distortions obtained from numerical simulations and weld tests for aluminum T-joints [Ref. O2]

<table>
<thead>
<tr>
<th>Temp (°C)</th>
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<th>204</th>
<th>260</th>
<th>316</th>
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<td>( \gamma )</td>
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<td>7.2</td>
<td>7.2</td>
<td>7.2</td>
<td>7.2</td>
</tr>
</tbody>
</table>

Table 3.2 Material constants for nonlinear kinematic hardening of aluminum alloy, AL5038-O
From a mechanical viewpoint, distortion and residual stress induced in the welded structures after welding can be regarded as the resultant of incompatible strains consisting of thermal strains, plastic strains, creep strains and others. In this study, it is assumed that only plastic strains exist as incompatible strains after welding because creep would not be expected due to fast cooling, and no thermal strains after completion of cooling. Therefore, it can be expected that if plastic strains do not exist after welding, no residual stress and distortion occur. In other words, understanding the cumulative plastic strain distribution after welding can be regarded as a linchpin of residual stress and distortion analysis.

Ueda and his co-workers [Ref. U1-2, M4, Y1] defined the characteristic distribution of inherent strains, and applied it to predict the residual stress induced by welding. Based on experimental, theoretical and numerical studies, they defined inherent strain distributions using a trapezoid curve pattern, and predicted the residual stress by
performing an elastic analysis, in which inherent strains were replaced by an equivalent distributed load. Others [Ref. J1, L1, S1] applied this method to predict welding-induced distortion. This type of inherent strain approach was based on the assumption that the residual stress and distortion being analyzed should be correctly predicted by the specifically selected component of inherent strains. For example, if the longitudinal inherent strain were used in the prediction of the longitudinal residual stress or distortion, other inherent strain components should not affect the longitudinal residual stress or distortion, or their effect should be small enough to be negligible. This may not be true for constrained T-joints. Therefore, it is critical to understand the relationship cumulative plastic strains have with distortion and with residual stress in the application of the inherent strain approach.

In terms of angular distortion, it has been believed that angular distortion is induced by the transverse cumulative plastic strain which is distributed non-uniformly through the thickness of the plate. This statement may be true in the case of butt-welded joints, but based on the observation of the history of angular distortion and cumulative plastic strains, Han [Ref. H3] found that angular distortion in butt joint resulted from not only the transverse cumulative plastic strain, but also the longitudinal component. On the other hand, for fillet welded T-joints which have more complex geometric configuration than butt welded joints, only a few studies [Ref. K1, M4, O4] have been performed to figure out the angular distortion mechanism using numerical and experimental analysis. In these explanations of distortion mechanism using the change of the location of shrinkage source, there was an obvious analogy between the mechanism of angular distortion and the mechanism of longitudinal distortion. It seems those researchers
believed the gradient of transverse shrinkage resulted in angular distortion in fillet welded joints like butt welded joints. For the application of the inherent strain approach to T-joints, Yuan and Ueda [Ref. Y1] used it to predict longitudinal residual stress in T-joints and I-beam cross section joints, but no further research on the prediction of angular distortion of T-joints has been followed. Probably some difficulties or misunderstandings may restrict the application of the inherent strain approach to the prediction of angular distortion in fillet welded T-joint.

As mentioned above, it is critical to postulate the characteristic relationship between cumulative plastic strains and angular distortion in order to provide better quantitative understanding of the angular distortion mechanism. In this chapter, a new approach analyzing the relationship between cumulative plastic strains and angular distortion in fillet welded T-joints, dubbed as Plasticity-Based Distortion Analysis (PDA), is proposed and applied to the investigation of the relationship between cumulative plastic strains and angular distortion in fillet welded thin plate T-joints.

4.1 Relationship between Cumulative Plastic Strains and Angular Distortion: Using Analytical Solution for Simple Bending Cases

An analytical solution can give significant insight into the understanding of the relationship between cumulative plastic strains and distortion. In terms of angular distortion, it has been understood that the variation of the transverse cumulative plastic strain through thickness of the plate causes angular distortion. Figure 4.1 shows the transverse cumulative plastic strain distributed non-uniformly through the thickness, in the y-direction, and uniformly distributed along the width, in the x-direction.
Supposing the transverse cumulative plastic strain is uniform along the weld line, in the z-direction, the transverse total strain can be written as:

$$
\epsilon'_{xx}(y) = \frac{\sigma_{xx}(y)}{E} + \epsilon'^{th}_{xx}(y)(= 0) + \sum \epsilon^p_{xx}(y)
$$

(4.1)

where $\epsilon'_{xx}$, $\epsilon'^{th}_{xx}$, $\sum \epsilon^p_{xx}$ are total strain, thermal strain and cumulative plastic strain, respectively, in the transverse (x) direction. After completing welding and cooling, thermal strains are zero. It is assumed that other stresses are negligible because their effect on angular distortion is insignificant. This total strain field should satisfy the condition of compatibility:

$$
\frac{\partial^2 \epsilon'_{xx}}{\partial y^2} + \frac{\partial^2 \epsilon'_{yy}}{\partial x^2} - \frac{\partial^2 \epsilon'_{xy}}{\partial x \partial y} = \frac{\partial^2 \epsilon'_{xx}}{\partial y^2} = 0
$$

(4.2)

Substitute Equation (4.2) into (4.1):

$$
\frac{1}{E} \frac{\partial^2}{\partial y^2} \sigma_{xx}(y) + \frac{\partial^2 \sum \epsilon^p_{xx}}{\partial y^2} = 0
$$

(4.3)

Moriguchi defined second term in Equation (4.3) as R “incompatibility” [Ref. M1]:

$$
R = -\frac{\partial^2 \sum \epsilon^p_{xx}(y)}{\partial y^2}
$$

(4.4)
Stress can be determined by the double integration of Equation (4.3) with respect to y:

\[
\sigma_{xx}(y) = -E \int_0^1 \frac{\partial^2 \sum \varepsilon_{xx}^p(y)}{\partial y^2} dy + C_1 y + C_2 = -E \sum \varepsilon_{xx}^p(y) + C_1 y + C_2 \tag{4.5}
\]

Stress represented by Equation (4.5) should satisfy force and moment equilibrium conditions:

\[
\int_{-\frac{h}{2}}^{\frac{h}{2}} \sigma_{xx}(y) dy = -E \int_{-\frac{h}{2}}^{\frac{h}{2}} \varepsilon_{xx}^p(y) dy + C_2 h = 0
\]

\[
\int_{-\frac{h}{2}}^{\frac{h}{2}} \sigma_{xx}(y) y dy = -E \int_{-\frac{h}{2}}^{\frac{h}{2}} \varepsilon_{xx}^p(y) y dy + \frac{1}{12} C_1 h^3 = 0
\tag{4.6}

Integration constants can be determined by solving Equation (4.6)

\[
C_1 = \frac{12}{h^3} E \int_{-\frac{h}{2}}^{\frac{h}{2}} \sum \varepsilon_{xx}^p(y) dy
\]

\[
C_2 = E \int_{-\frac{h}{2}}^{\frac{h}{2}} \varepsilon_{xx}^p(y) dy
\tag{4.7}

Combine Equation (4.1), (4.5) and (4.7), total strain can be rewritten by

\[
\varepsilon_{xx}'(y) = \frac{1}{E} \left(-E \sum \varepsilon_{xx}^p(y) + C_1 y + C_2 \right) + \sum \varepsilon_{xx}^p(y)
\]

\[
= \frac{1}{E} \left(C_1 y + C_2 \right)
\]

\[
= \frac{12}{h^3} \int_{-\frac{h}{2}}^{\frac{h}{2}} \varepsilon_{xx}^p(y) dy \cdot y + \frac{1}{h} \int_{-\frac{h}{2}}^{\frac{h}{2}} \varepsilon_{xx}^p(y) dy
\tag{4.8}
Equation (4.8) represents linear distribution of the total strain which is directly related to distortion. The first term of Equation (4.8) represents the amount of angular distortion. This angular distortion can be interpreted by

\[ \phi = \frac{1}{I_{zz}} \cdot y_c \cdot A^* \quad \text{(4.9)} \]

where \( \phi \) = angular distortion (rad), \( I_{zz} \) = the second moment of inertia, \( y_c \) = the location of centroid of plastic strain zone and \( A^* \) = area of plastic strain. From Equation (4.9), it can be expected that if the distribution pattern of the plastic strain is known, angular distortion can be uniquely determined under the given boundary and geometry.

However, even in simple cases, such as a bead-on plate, it is not easy to describe transverse cumulative plastic strain distribution with an explicit function. Some researches [Ref. J1, S1] assuming the shape of the inherent strain zone as a semi-circle or hemisphere, used that assumption in the calculation of equivalent forces and moments. In this model, only nominal cumulative plastic strains were considered to describe shrinkage. Han [Ref. H1] determined shrinkage volume by integrating cumulative plastic strains over the plastic zone. The effect of the longitudinal cumulative plastic strain on angular distortion was additive, which explains the discrepancy between angular distortion from the weld tests and angular distortion predicted by shrinkage volume including only the transverse cumulative plastic strain. It may be possible that this discrepancy might result from numerical error in integration.
Therefore, it is necessary to understand the relationship between cumulative plastic strains and distortion prior to performing the simplified distortion analysis, such as the inherent strain approach.

4.2 Plasticity-Based Distortion Analysis (PDA)

Plasticity-based distortion analysis (PDA) is a numerical procedure to predict welding-induced distortion by directly mapping the characteristic cumulative plastic strains into the elastic models, instead of applying equivalent forces and moments. One of the advantages of this approach is to incorporate all cumulative plastic strain components in predicting distortion, and investigate the relationship between each cumulative plastic strain and a distortion type of concern. Especially, for fillet welded T-joints which are complex in geometric configuration, PDA becomes a more powerful tool.

4.2.1 General procedure of PDA

Figure 4.2 shows the flow chart of PDA consisting of three parts:

- Part 1: Thermal-elastic-plastic analysis, determining the characteristic cumulative plastic strain distributions of all six components.
- Part 2: Elastic analysis, calculating the individual distortions corresponding to the mapped individual cumulative plastic strains. Repeating six times with all components of cumulative plastic strains in the case of a 3D model, 4 times for a generalized plane strain model.
- Part 3: Post processing, obtaining the total distortion by adding the individual distortions induced by each cumulative plastic strain component.

In Part 1, PDA requires the appropriate distributions of all cumulative plastic components from thermal-elastic-plastic analysis mentioned in the previous chapter for the given welding condition. At this point, it is assumed that distortion predicted by thermal-elastic-plastic analysis is correct. Therefore corresponding cumulative plastic strains obtained are also reasonable, and can be used to predict distortion using an elastic model.

Part 2 is an elastic analysis. Each cumulative plastic strain distribution obtained from thermal-elastic-plastic analysis can be mapped into elastic models as the equivalent incompatible strain fields. In mapping procedure, temperature or state variables can be used to incorporate cumulative plastic strains. In this study, temperature and the corresponding thermal expansion coefficient were used. For the 3D model, six components of cumulative plastic strains were mapped one by one into six elastic models using corresponding temperature fields, and six individual distortions were determined from six elastic analyses.

In Part 3, the relationship between cumulative plastic strains and distortion can be explained with quantitative measure, and the mapping efficiency can be obtained. Each individual distortion represents the contribution of each cumulative plastic strain to a total distortion. Based on this information, the role of each cumulative plastic strain on the specific distortion pattern can be analyzed. In addition, a total distortion can be calculated by adding six individual distortions, comparing the total distortion with the distortion
obtained from thermal-elastic-plastic analysis in order to check the accuracy of PDA procedure. If the accuracy does not satisfy the required accuracy in terms of engineering application, the finite element model should be remodeled by increasing the number of nodes and elements or increasing the shape function order of elements to increase mapping efficiency.

4.2.2 Mapping method

After completing heating and cooling, six components of cumulative plastic strains exist as incompatible strains and result in distortion. As incompatible strains, the equivalent thermal strain induced by thermal expansion, the plastic strain, or other state variables causing straining of materials, may be taken into consideration. In this study, the equivalent thermal strain was used as the incompatible strain replacing the effect of the cumulative plastic strain.

For isotropic materials, thermal expansion coefficients in all directions are the same, so it may not describe the characteristic of plastic strains, such as material’s incompressibility:

$$\sum e_{xx}^p + \sum e_{yy}^p + \sum e_{zz}^p = 0$$

Therefore, it is impossible to describe the characteristics of cumulative plastic strains using isotropic thermal expansion coefficients. ABAQUS [Ref.A1] provides anistropic thermal expansion with six independent thermal expansion coefficients including nominal and shear expansions. Using anistropic thermal expansion coefficients and the
corresponding temperature fields, each cumulative plastic strain component can be mapped independently into six elastic models. For example, the transverse cumulative plastic strain, \( \sum \varepsilon^p_{xx}(x, y, z) \) can be mapped by using a temperature field calculated by Equation (4.11).

\[
\theta_{xx}(x, y, z) = \frac{\sum \varepsilon^p_{xx}(x, y, z)}{\alpha_{xx}}
\]  

\[
\alpha_{xx} = \text{constant, } \alpha_{yy} = \alpha_{zz} = \alpha_{xy} = \alpha_{xz} = \alpha_{yz} = 0
\]  

where, \( \theta \) = temperature, \( \alpha \) = anisotropic thermal expansion coefficient. Other temperature fields associated with other cumulative plastic strain components can be obtained in the same way as described in Equation (4.11).

In order to obtain corresponding temperature fields, a FORTRAN program was developed to retrieve all cumulative plastic strains over all nodes from thermal-elastic-plastic analysis, and save them into a data file [See Appendix A]. In the elastic-plastic analysis, an output control option to save cumulative plastic strains to a file with extension, [file_name.fil], was set up to make an average temperature at nodes. Using the data file containing cumulative plastic strain values at all nodes, corresponding temperature values at all nodes were calculated using User-Subroutine, UTEMP [See Appendix A] and Equation (4.11).

Distortion associated with only one component of cumulative plastic strain can be determined by the elastic analysis with mechanical material properties at room temperature and boundary conditions identical to that of elastic-plastic analysis. In case
of 3D analysis, six individual distortions corresponding to six cumulative plastic strains are obtained by six independent elastic analyses. As a final step of PDA, the total distortion induced by all cumulative plastic strains is obtained by the addition of the individual distortions calculated from six independent elastic analyses:

\[ \delta_{\text{PDA}}^{\text{total}} = \sum_{i=1}^{6} \delta_i \]  

(4.12)

where, \( \delta_{\text{PDA}}^{\text{total}} \) = the total distortion, \( \delta_i \) = the individual distortion with only \( i^{th} \) component of cumulative plastic strains. The accuracy of the PDA procedure can be determined by comparing distortion from elastic-plastic analysis with the total distortion obtained from PDA using Equation (4.12).

\[ \psi = \frac{\delta_{\text{PDA}}^{\text{total}}}{\delta_{\text{EPA}}} \times 100(\%) \]  

(4.13)

This accuracy of PDA may be affected by some factors, such as the non-linearity of geometry and material or the mapping accuracy. One factor is the material non-linearity during plastic deformation. During incremental plastic deformation, elastic modulus is continuously updated to incorporate the change of plastic modulus describing the strain hardening. ABAQUS uses the modified Newton-Raphson iteration in which stresses are determined by the updated elastic modulus in order to determine yielding. Therefore, after completing the plastic deformation, the updated elastic modulus may be
different from the initial elastic modulus. However, in welding situations, the non-linearity of material during plastic deformation may not significantly affect on angular distortion measured at the locations far away from the plastic zone. The geometry non-linearity comes from a large deformation. In this study, the small deformation theory was used. Therefore, the effect of the geometric non-linearity on angular distortion can be considered as negligible.

Mapping accuracy may also affect the accuracy of PDA procedure. If the cumulative plastic strains were not mapped precisely, especially in the case of high non-linear distortion, the individual angular distortions calculated from PDA and the total distortion would be incorrect. More discussion concerning mapping accuracy is contained in the next section.

In this study, it was assumed that the reasonable accuracy of PDA procedure was in the range of 90% to 100% in view of engineering application.

4.2.3 Evaluation of mapping accuracy

In order to consider cumulative plastic strains as precisely as possible, mapping efficiency should be checked. The mapping method proposed in this study has a few intrinsic problems which reduce the accuracy of mapping. One problem is from the averaged value of cumulative plastic strain at nodes during retrieving cumulative plastic strains obtained from elastic-plastic analysis. Figure 4.3 shows how to calculate the averaged cumulative plastic strains at the nodes. In the numerical calculation, all measures of elements are calculated at integration points and extrapolated to calculate
nodal value. Therefore, averaged cumulative plastic strains are the values obtained by averaging nodal cumulative plastic strains from all elements connected at the nodes. As shown in Figure 4.3, when only three elements are connected, the averaged cumulative plastic strains at the nodes in element #2 are calculated by:

\[
\begin{align*}
\sum e_i^{avg} &= \frac{\sum e_j^1 + \sum e_i^2}{2} \\
\sum e_j^{avg} &= \frac{\sum e_j^2 + \sum e_i^3}{2} \\
\sum e_k^{avg} &= \frac{\sum e_k^2 + \sum e_i^3}{2} \\
\sum e_l^{avg} &= \frac{\sum e_l^1 + \sum e_i^2}{2}
\end{align*}
\]  

(4.14)

where \(\sum e_i^{avg}\) = the averaged cumulative plastic strain at node \(i\) of element #2, \(\sum e_i^j\) = extrapolated cumulative plastic strain at node \(i\) of element #\(j\). The average cumulative plastic strains calculated by Equation (4.14) are mapped into elements by thermal strains using Equation (4.11) and pre-defined anistropic thermal expansion coefficients. In the numerical procedure, thermal strain of linear elements is constant throughout an element, being determined by thermal expansion coefficients and averaged temperatures applied at all nodes connected with the element. Therefore, thermal strains at the nodes and the integration points of element #2 mapped by Equation (4.14) becomes

\[
\sum e_i^{th} = \sum e_j^{th} = \sum e_j^{th} = \sum e_j^{th} = \frac{\sum e_i^{avg} + \sum e_j^{avg} + \sum e_k^{avg} + \sum e_l^{avg}}{4}
\]

(4.15)
From Equation (4.14) and (4.15), if cumulative plastic strain is uniformly distributed, it can be expected that 100% efficiency of mapping can be guaranteed. However, the distribution of cumulative plastic strains will not be uniform in and around the welded region. In order to increase mapping efficiency, the difference in cumulative plastic strains between adjacent elements should be small, which means that the smaller the size of the element better mapping efficiency is insured. Quadratic elements can also increase the mapping efficiency because thermal strain is linear in an element [Ref. A2].

4.2.4 Evaluation of accuracy of PDA procedure

Two examples were selected to investigate the characteristics of the accuracy of PDA procedure determined by comparing two distortions obtained from elastic-plastic analysis and PDA. The first was a 2D simple bending model with temperature gradient along the y-axis defined by function, \( \theta(y) = 595y^4 \), and uniform along the x-axis as shown in Figure 4.4. After heating, a tested coupon was cooled down to room temperature. For this model, four cases were investigated with different strain hardening models and boundary conditions as followings:

Case 1 : With strain hardening during plastic deformation and simple support B.C.
Case 2 : Without strain hardening during plastic deformation and simple support B.C.
Case 3 : With strain hardening during plastic deformation and fixed ends B.C.
Case 4 : Without strain hardening during plastic deformation and fixed ends B.C.
Only a half of the coupon was modeled with symmetric boundary condition at \( x = 0 \) mm. After performing elastic-plastic analysis and PDA, all displacements in the y-direction at \( x = 0 \) mm were plotted and compared in Figure 4.5. For Case 1 and 2, 98% accuracy is obtained in both cases. 100% accuracy is obtained for Case 3 and 4. It can be conclude that the accuracy of PDA procedure is not significantly affected by material non-linearity, slightly depending on the distribution pattern of the cumulative plastic strains which affect mapping accuracy.

For the second example, a fillet welded thin plate T-joint was tested using different size and the order of shape function of elements as followings:

\[ \text{T-A : Fine meshed with linear elements (8 nodes for each element) - Figure 4.6} \]
\[ \text{T-B : Coarse meshed with quadratic elements (20 nodes for each element) - Figure 4.7} \]

Two models were tested using elastic-plastic analysis and PDA. Angular distortions at the end of a flange were plotted and compared in Figure 4.8. The accuracy of PDA procedure for T-A and T-B are 75% and 98%, respectively. Even though T-A has more elements and nodes (12060 element and 56786 nodes, for T-B, 6100 elements and 30068 nodes), poor accuracy of PDA is shown compared to T-B due to the character of thermal strain. Therefore, it is recommended to use a quadratic type of element with 20 nodes 3D brick elements that have smoother shape function allowing a better mapping of the cumulative plastic strains. All analyses for fillet welded thin plate T-joints were performed with T-B with 98% accuracy. More details about PDA for T-joints will be explained in section 4.3.
4.3 PDA for Fillet Welded Thin Plate T-joints

For fillet welded thin plate T-joints, a finite element model with the accuracy of PDA procedure of 98% was used in elastic-plastic analysis and PDA. As material properties for magnesium alloy, elastic modulus and Poisson’s ratio at room temperature were 4.3E04 (MPa) and 0.35, respectively.

4.3.1 Mapping cumulative plastic strains

Six components of cumulative plastic strains obtained from elastic-plastic analysis were mapped one by one into six elastic models. Figure 4.9 shows the cumulative plastic strain contours obtained from elastic-plastic analysis and thermal strain contours mapped in PDA. The range of color spectrum is the same for all contour plots: maximum band = +0.002 ~ max., minimum band = min. ~ -0.002. Even though there are some differences in magnitude, the general distribution patterns are close to each other.

4.3.2. Angular distortion patterns induced by each cumulative plastic strain

From six elastic analyses, deformed shapes related to each cumulative plastic strain were obtained and plotted in Figure 4.10. What was expected and previously believed based on the observation of butt welded joints was that transverse cumulative plastic strain, $\Sigma\varepsilon_{xx}^p$, would cause bend-up angular distortion. However, as shown in Figure 4.10 (a), PDA shows that the transverse cumulative plastic strain results in bend-down angular distortion. The vertical cumulative plastic strain, $\Sigma\varepsilon_{yy}^p$, also generates bend-down angular distortion.
A slight bend-up angular distortion is produced by the longitudinal cumulative plastic strain, \( \sum \epsilon_{zz}^p \), which may be mainly related to longitudinal bending and buckling.

All three of the above-mentioned components are nominal components that have been taken into consideration in distortion analysis. However, the other three shear cumulative plastic strain components have never been highlighted in distortion analysis. As shown in Figure 4.10 (d), xy-plane shear cumulative plastic strain, \( \sum \epsilon_{xy}^p \), produces the most bend-up angular distortion. Figure 4.10 (e) and (f) show that the other shear cumulative plastic strains are not related to angular distortion.

In order to clearly show the contribution of each cumulative plastic strain to the total angular distortion, averaged individual and total angular distortions from PDA and angular distortion from elastic-plastic analysis were plotted using a bar chart as shown in Figure 4.11. It shows clearly the relationship between cumulative plastic strains and angular distortion quantitatively: transverse and vertical cumulative plastic strains result in bend-down angular distortion, and longitudinal and xy-plane shear cumulative plastic strains generate bend-down angular distortion, and most bend-up angular distortion is induced by xy-plane shear cumulative plastic strain.

Therefore, without considering the effect of xy-plane shear cumulative plastic strain, the conventional inherent strain model incorporating only the transverse cumulative plastic strain may not predict the correct angular distortion pattern in fillet welded T-joints.
4.3.3 Total angular distortion calculated by PDA

The accuracy of PDA was evaluated by comparing the total angular distortion calculated by the addition of six individual distortions obtained from six independent elastic analyses with angular distortion obtained from elastic-plastic analysis. The averaged angular distortions along the length of a flange were measured from each elastic model and added together. The averaged total angular distortion from PDA is 0.99 mm, and the averaged angular distortion from elastic-plastic analysis is 1.02 mm as shown in Figure 4.11. Using Equation (4.13), the accuracy is 98%, which implies that the relationship between cumulative plastic strains and angular distortion is unique, which means that angular distortion can be uniquely determined by the given cumulative plastic strains. It can also be said that the application of elastic models with material properties at room temperature and the cumulative plastic strains associated is valid in engineering application.

4.4 Summary

From PDA for fillet welded thin plate T-joints, the following results were obtained.

- The relationship between cumulative plastic strains and angular distortion is unique.
- Plasticity-base distortion analysis (PDA) was proved as an effective tool to investigate the relationship between cumulative plastic strains and angular distortion.
New knowledge about the angular distortion mechanism for fillet welded T-joints using PDA procedure was addressed:

1) xy-plane shear cumulative plastic produces most bend-up angular distortion, and other shear components are not related with angular distortion.

2) Transverse and vertical cumulative plastic strains result in bend-down angular distortion.

3) Longitudinal cumulative plastic strain produces a slight bend-up angular distortion.

It was demonstrated that the application of an elastic model using material properties at room temperature and the cumulative plastic strains associated is valid in distortion analysis.

In the case of using the inherent strain model in prediction of angular distortion in T-joints, the right angular distortion pattern cannot be obtained by transforming only the transverse cumulative plastic strain into equivalent forces and moments. The effect of transverse, vertical, longitudinal and xy-plane shear cumulative plastic strains should be employed in calculation of equivalent forces and moments.
Figure 4.1 Schematic illustration of angular distortion associated with the transverse cumulative plastic strain
Figure 4.2 Schematic diagram for plasticity-base distortion analysis (PDA)
Figure 4.3  Numbering of nodes and elements

Figure 4.4  Temperature distribution and boundary conditions for simple bending cases
Figure 4.5 Comparison of displacements calculated by elastic-plastic analysis (EPA) and plasticity-based distortion analysis (PDA) for the 2D model
Figure 4.6 Finely meshed model with linear elements

Figure 4.7 Coarsely meshed model with quadratic elements
Figure 4.8 Effect of element type and size on angular distortion
Figure 4.9 Comparison of cumulative plastic strains and equivalent thermal strains
Figure 4.10 Deformed shapes associated with cumulative plastic strains
Figure 4.11 Averaged angular distortions calculated by EPA and PDA
Many techniques have been developed to minimize distortion induced by welding, such as external restraining, pre-heating, auxiliary side heating, heat sinking, etc. In general, most of the distortion mitigation techniques have been developed according to conventional understanding to explain their effectiveness in distortion control, and then evaluated by comparing with test results. These conventional understandings may include not only theoretical and mathematical knowledge, but also generally accepted knowledge from experience or analogy. For example, the concept of pre-deformation is based on direct intuition after observing the distortion pattern: welding-induced deformation is compensated by a counter-deformation formed in joints prior to welding. The other example can be heat control techniques applying pre-heating or side heating in order to reduce the temperature gradient. Once the basic idea is tested and evaluated, a number of parametric studies may follow to find the optimum condition and explain the effect of mitigation parameters.
However, no rigorous studies have been carried out to investigate how these techniques affect the relationship between cumulative plastic strains and distortion. Very recently, Han [Ref. H3] investigated how heat sinking and side heating affect the longitudinal cumulative plastic strain, and explained the effectiveness of a method based on the relationship between the longitudinal cumulative plastic strain and the longitudinal residual stress associated with buckling.

In this chapter, the main focus is to investigate the effect of external restraints and two specific thermal management techniques on the relationship between cumulative plastic strains and angular distortion for fillet welded thin plate T-joints by using plasticity-based distortion analysis. The purpose of this practice is to demonstrate the effectiveness of PDA in understanding the characteristics of angular distortion in fillet welded T-joints, and evaluate the unique relationship between cumulative plastic strains and distortion.

5.1 Effect of External Restraints on Angular Distortion in T-joints

External restraining including pre-deformation has been widely known as a useful technique to reduce angular distortion in welded structures. In the application of pressure vessel or large scale pipe welding, initial expansion has been adopted to reduce radial shrinkage. In the case of T-joints, it has been reported that restraint and back-bending are effective means to reduce angular distortion [Ref. C1].

In this section, the effect of external restraints on angular distortion is investigated by using 3D thermal-elastic-plastic analysis. PDA is also performed to
investigate the effect of external restraints on the characteristic relationship between cumulative plastic strains and angular distortion discussed in Chapter 4.

5.1.1 Thermal analysis

External restraint was applied on the flange plate of the T-joint. Some amount of heat loss through the contact surface between the fixtures and the flange can be expected to occur, but in this study, it was assumed that heat loss through the contact surface would be negligible. From the heat diffusion viewpoint, there was no change in temperature evolution due to the external restraints applied. Therefore, it is not necessary to conduct thermal analysis in these cases. Temperature evolution used in chapter 4 was directly retrieved in the following elastic-plastic analysis.

5.1.2 Elastic-plastic analysis

It has been reported that angular distortion is affected by the degree of external restraint [Ref. S2, M3]. In other words, a higher degree of restraint reduces a greater amount of angular distortion. In this study, external restraint was applied along the flange plate with fixed boundary conditions. The degree of restraint is inversely proportional to the distance between the weld line and the line of the fixed boundary. Figure 5.1 shows locations of external restraints. Three cases designated as Case A, Case B and Case C, have differing degrees of external restraint. The locations applying the fixed boundary conditions for Case A, Case B and Case C, were x = 100 mm, 50 mm, and 24.7 mm, respectively. So Case C has the highest degree of external restraint of the three cases. These fixed boundary conditions were applied during heating and cooling, and then
removed after cooling was completed. The final angular distortion becomes displacements at the free edge of the flange after removing external restraints. Therefore, from these simulations, we can obtain the characteristic cumulative plastic strain distribution patterns and angular distortion associated with differing degrees of external restraint, both of which can be used in PDA procedure investigating the effect of external restraints on angular distortion of T-joints.

Figure 5.2 shows deformed shapes after removing the external restraints with the same scaling factor of deformation. The averaged angular distortions which occurred at the free end of the flange are plotted in Figure 5.3. From Figures 5.2 and 5.3, it can be said that at higher restraints, less angular distortion is expected. For Case C, a minute angular distortion is produced with external restraints at $x = 24.7$ mm very close to the boundary of the plastic zone.

Considering the same boundary condition after finishing heating and cooling, the difference in angular distortions among the three cases may result from the change in cumulative plastic strain distributions due to external restraint. By plotting cumulative plastic strains and comparing them, some effect of external restraint on the distribution patterns of cumulative plastic strains can be observed. However, if only these results are considered, it is difficult to explain the effect of external restraint on the relationship between cumulative plastic strains and angular distortion. Therefore, PDA was carried out to observe the effect of external restraint on the characteristic relationship between cumulative plastic strains and angular distortion.
5.1.3 Plasticity-based distortion analysis

Cumulative plastic strains from the three cases with differing degrees of external restraints were saved and mapped into T-joint using equivalent thermal strains in PDA procedure. Individual and total angular distortions for Case A, Case B, and Case C, are plotted in Figures 5.4, 5.5 and 5.6, respectively. They show no change in the basic pattern of relationship between cumulative plastic strains and angular distortion addressed in Chapter 4: bend-down angular distortion due to transverse/vertical cumulative plastic strains, bend-up angular distortion due to longitudinal/xy-plane shear cumulative plastic strains and no angular distortion by other shear cumulative plastic strains.

In order to investigate the effect of external restraints on the relationship between cumulative plastic strains and angular distortion, individual and total angular distortions for the three cases and a case without external restraints are plotted together in Figure 5.7. Reasonable accuracy of PDA procedure is shown for all cases, which implies that the unique relationship between cumulative plastic strains and angular distortion is valid under external restraint. The major change due to external restraints is observed in individual angular distortion induced by the transverse cumulative plastic, $\sum \varepsilon_{xx}^p$. It is shown that more bend-down angular distortion induced by the transverse cumulative plastic strain is produced by higher external restraint. The largest bend-down angular distortion is observed in Case C, which has the highest external restraint. Figure 5.8 shows the change of the transverse cumulative plastic strain distributed on the flange plate. There is no change in the distribution on the web plate, but the area with positive plastic strain (red colored region) on the top surface of the flange expands with an increasing the degree of external restraint.
The region with positive plastic strain is within the plastic zone where the compressive longitudinal plastic strain exists. Some parts of the positive plastic strain may be generated to satisfy incompressibility under the plastic deformation. However, the transverse or bending restraints may not significantly affect the distribution of the longitudinal cumulative plastic strain because the longitudinal stress is sensitive to the longitudinal restraint or the rigidity in the longitudinal direction. Therefore, considering the distribution pattern of the positive transverse plastic strain, yielding may occur due to high tensile transverse stress which is generated by external restraint preventing bend-up angular and transverse shrinkage during cooling. In general, high restraint produces more cumulative plastic strain because high stress resulting from high restraint causes earlier yielding than low restraint. Therefore, it may be said that as a higher external restraint is applied, higher and wider positive transverse cumulative plastic strain is produced. This can explain the reason why more bend-down angular distortion occurs with the higher external restraint because the role of the positive transverse cumulative plastic strain bends down the flange plate.

For the vertical cumulative plastic strains, $\sum \varepsilon_{yy}^{p}$, only slight change in angular distortion is shown. In general, less bend-down angular distortion is induced by higher external restraint. No change in angular distortion induced by the longitudinal cumulative plastic strain occurs. This may imply that external restraint applied on the flange would not affect the distribution patterns of the longitudinal cumulative plastic strain as previously discussed. For angular distortion induced by the xy-plane shear cumulative plastic strain, $\sum \varepsilon_{xy}^{p}$, no change in the calculated individual angular distortion is shown except in Case C with external restraint close to the boundary of the plastic zone. Figure
5.9 shows clearly how the distribution of the xy-plane shear cumulative plastic strain is affected by external restraint. There is no change in the distribution pattern for Case A, Case B and the case without external restraint. For Case C, it is shown that external restraint close to the boundary of the plastic zone disturbs the distribution of the xy-plane shear cumulative plastic strain. The change in individual angular distortions induced by other shear components is negligible.

Based on results obtained from PDA, it can be concluded that the reduction of angular distortion by external restraint applied on a flange results from an increase of bend-down angular distortion induced by the transverse cumulative plastic strain. The region with positive transverse cumulative plastic strain causing bend-down angular distortion is expanded by increasing the degree of external restraint. PDA also gives the quantitative explanation about the effect of external restraint on the relationship between cumulative plastic strains and angular distortion. PDA reflects sensitive changes of cumulative plastic strains on angular distortion by separating the contribution of each cumulative plastic strain to angular distortion component by component, effectively explaining the unique relationship between cumulative plastic strain and angular distortion.

5.2 Effect of Thermal Management Techniques on Angular Distortion in T-joints

In this study, two thermal management techniques were selected: heat sinking and TIG pre-heating. They are inherently different in terms of heat control. Heat sinking reduces the heat affected zone by applying a cooling chamber beneath the bottom of the flange.
On the other hand, TIG pre-heating increases the heat affected zone by pre-heating which is carried out by running TIG ahead of GMAW on the bottom surface of the flange.

It has been reported that heat sinking is an effective way to reduce buckling [Ref. M6] and TIG pre-heating reduces the angular distortion in T-joints [Ref. O2]. Most research has been focused on showing their effectiveness by comparing angular distortions in cases with and without thermal management techniques using weld tests and numerical simulations. Recently, Han [Ref. H4] investigated the relationship between cumulative plastic strains and buckling for butt joints. However, it is still unknown how thermal management techniques affect the relationship between cumulative plastic strains and angular distortion in T-joints. Therefore, the effect of thermal management techniques on the relationship between cumulative plastic strains and angular distortion was investigated using PDA procedure.

5.2.1 Thermal analysis

The effect of heat sinking was simulated by applying the relatively low film coefficient, 1.0E-2 Watt/(mm² °C) with temperature 21°C, on the bottom surface of the flange within x = 0 mm ~ 24.8 mm as shown in Figure 5.10. TIG pre-heating was modeled by running TIG 50 mm ahead of GMAW at the same speed as GMAW on the bottom surface of the flange as shown in Figure 5.11. The heat for TIG was one third of that of GMAW, 477 Watt ( = 110 volt × 13 amps × 1/3 ) with heat input calibration factor of 0.6.

It can be expected that the nugget will be smaller with heat sinking than with TIG pre-heating because pre-heating raises the temperature in and around the weld region.
Figure 5.12 shows maximum peak temperature maps calculated by User-Subroutine, UVARM in ABAQUS. The region with a higher temperature than liquidus temperature can be interpreted as the nugget. Compared to the nugget without thermal management, heat sinking generates a smaller nugget and TIG pre-heating gives a larger nugget.

Heat management techniques affect not only the size of the heat affected zone, but also the rate of heating and cooling which can be related to the instantaneous gradient of temperature along the plates. In general, heat sinking results in a higher temperature gradient due to a higher cooling rate, and facilitates achieving a uniform temperature on the plates compared to the case without thermal management. Contrarily, TIG pre-heating increases the maximum peak temperature and reduces the temperature gradient. Figure 5.13 compares temperature evolutions on three points located on the top surface of the flange, x = 4.8, 9.8 and 24.8 mm. Temperature gradient can be represented by the distance of each curve. The heating and cooling rate can also be determined by calculating the slope of the curves. For heat sinking, significant change in the magnitude of the maximum peak temperature, the temperature gradient and the rate of cooling can be observed. TIG pre-heating does not give significant changes in the temperature gradient and the cooling rate compared with the case without thermal management.

During heating, the effect of TIG pre-heating is shown by two spikes on each curve. Maximum peak temperatures for each curve are also increased due to the effect of pre-heating, which results in a wider nugget and heat affected zone.

Based on the results of thermal analysis, it can be said that the two thermal management techniques, heat sinking and TIG pre-heating, give the opposite impact on not only temperature evolution and profile, but also angular distortion.
5.2.2 Elastic-plastic analysis

All boundary conditions for the two cases were the same as those of the case without the external restraint described in section 5.1.2. The temperature evolutions for the two cases calculated by thermal analysis were retrieved and applied to elastic-plastic analysis.

Figure 5.14 shows deformed shapes for the three cases with the same magnification scale factor: Case 1 = without thermal management, Case 2 = heat sinking, Case 3 = TIG pre-heating. The average angular distortion calculated for Case 1, 2 and 3 are 1.04 mm, 2.75 mm and 0.5 mm, respectively, and plotted in Figure 5.15. Heat sinking increases angular distortion. A reduction of angular distortion is shown when TIG pre-heating is applied. This opposite effect between the two thermal management techniques on angular distortion was expected after observing the results of thermal analysis.

In order to do a more detailed investigation, cumulative plastic strain maps for the three cases are plotted in Figure 5.16. For Case 1 and 3, a similar distribution pattern of the cumulative plastic strains is shown. On the other hand, a significant reduction of the size of the plastic zone is obtained by heat sinking. The distribution pattern of the transverse cumulative plastic strain for Case 2 is different from those of Case 1 and 3. For Case 2, the transverse cumulative plastic strain on the top surface of the flange is wider than on the bottom of the flange, which may cause bend-up angular distortion and eventually reduce the amount of bend-down angular distortion. For the vertical cumulative plastic strain, the reduced size of the plastic strain zone in the flange may affect angular distortion as well. Even though there is significant change in the distribution pattern of the longitudinal cumulative plastic strain, the resultant effect may
be small because the longitudinal component is not as sensitive as other components on angular distortion. For the xy-plane shear cumulative plastic strain, no significant change may be expected.

In Case 3, it is not clear what causes the reduction of angular distortion because there is no significant difference of cumulative plastic strain distributions between Case 1 and 3. Compared to other components, it is observed that the distribution of the xy-plane shear cumulative plastic strain is affected by TIG pre-heating. TIG pre-heating increases the area of the positive shear strain region (red colored region) on the bottom (or corner) of the flange and the area of the negative shear strain region on the top of the flange. However, it is difficult to explain why the change of the xy-plane shear cumulative plastic strain causes the reduction of angular distortion.

5.2.3 Plasticity-based distortion analysis

As discussed above, it is difficult to figure out the relationship between cumulative plastic strains and angular distortion using only the results from the elastic-plastic analysis. PDA was performed to investigate the effect of thermal management techniques on the relationship between cumulative plastic strains and angular distortion.

Individual and total angular distortions are plotted and compared for Case 1, 2 and 3 on Figure 5.17. No matter what types of thermal management techniques are applied, the basic characteristic relationship between cumulative plastic strains and angular distortion is not changed: transverse and vertical cumulative plastic strains result in bend-down angular distortion, and longitudinal and xy-plane shear cumulative plastic strains generate bend-down angular distortion. Comparing the total angular distortion obtained
from PDA procedure with angular distortion from elastic-plastic analysis, it is also demonstrated that the unique relationship between cumulative plastic strains and angular distortion is valid under the different thermal management techniques.

Figure 5.17 shows that heat sinking increases bend-up individual angular distortions, reducing bend-down angular distortion, and ultimately causing an extreme increase in the total angular distortion. The main cause of the increase of total angular distortion comes from the reduction of bend-down angular distortions induced by transverse and vertical cumulative plastic strains. A relatively small increase of angular distortion due to the longitudinal and xy-plane shear cumulative plastic strains is shown. These results obtained from PDA are similar to the ones discussed in section 5.2.2 based on the maps of cumulative plastic strains, but provide more quantitative information than those from elastic-plastic analysis.

On the other hand, TIG pre-heating does not significantly change individual angular distortions except the one induced by the xy-plane shear cumulative plastic strain. It is shown that the reduction of the total angular distortion by TIG pre-heating is mainly related to the reduced bend-up angular distortion induced by the xy-plane shear cumulative plastic strain only. In order to reduce bend-up angular distortion, the negative xy-plane shear strain resulting in bend-down angular distortion must act more dominantly to reduce bend-up angular distortion than the positive one. Therefore, it can be said that the wider the region of negative xy-plane shear cumulative plastic strain (blue colored region) on the top surface of the flange as shown in Figure 5.16, the smaller bend-up angular distortion.

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Based on results from PDA, it can be concluded that heat sinking increases the total bend-up angular distortion by reducing individual bend-down angular distortions induced by transverse and vertical cumulative plastic strains, and TIG pre-heating reduces the total bend-up angular distortion by reducing individual bend-up angular distortion induced by the xy-plane shear cumulative plastic strain. It is also demonstrated that the unique relationship between cumulative plastic strains and angular distortion is valid under thermal management. In addition, in terms of optimizing the welding induced-distortion, the selection of thermal management techniques should be carefully performed. Based on this study, heat sinking can help to reduce buckling, but may result in more angular distortion. On the other hands, TIG pre-heating can reduce angular distortion, but it may possibly result in buckling because TIG pre-heating generates a wider plastic zone [Ref. H3].
Apply restraint during welding and cooling, and then remove it

Case A: $x = 100$ mm  
Case B: $x = 50$ mm  
Case C: $x = 24.7$ mm

Figure 5.1 Locations applied external restraints
Figure 5.2 Deformed shapes after removing external restraints

Magnification factor: × 10

Figure 5.2 Deformed shapes after removing external restraints
Figure 5.3 Comparison of averaged angular distortion for the cases with differing degrees of external restraint.
Figure 5.4 Averaged angular distortions calculated by EPA and PDA for Case A.
Figure 5.5 Averaged angular distortions calculated by EPA and PDA for Case B
Figure 5.6 Averaged angular distortions calculated by EPA and PDA for Case C
Figure 5.7 Comparison of averaged angular distortions calculated by EPA and PDA for the cases with differing degrees of external restraints
Figure 5.8 Transverse cumulative plastic strain maps for the cases with differing degrees of external restraint.

(a) Without external restraint
(b) Case A
(c) Case B
(d) Case C

Red color: +0.002, Blue color: -0.002
Figure 5.9 xy-plane shear cumulative plastic strain maps for the cases with differing degrees of external restraint

Red color: +0.002 ~ , Blue color: ~ - 0.002

(a) Without external restraint
(b) Case A
(c) Case B
(d) Case C

Figure 5.9 xy-plane shear cumulative plastic strain maps for the cases with differing degrees of external restraint
Figure 5.10 Schemes of heat sinking applied to T-joint

Figure 5.11 Scheme of TIG pre-heating applied to T-joints
Figure 5.12 Comparison of nugget shapes obtained from thermal analyses with different thermal management techniques
Figure 5.13 Comparison of temperature evolutions for the cases with different thermal management techniques

(a) Without thermal management

(b) With heat sinking

(c) With TIG pre-heating
Figure 5.14 Comparison of deformed shapes for the cases with different thermal management techniques

(a) Without thermal management

(b) With heat sinking

(c) With TIG pre-heating

Magnification factor = $\times 10$

Figure 5.14 Comparison of deformed shapes for the cases with different thermal management techniques
Figure 5.15 Comparison of averaged total angular distortions obtained from EPA for the cases with different thermal management techniques
Figure 5.16 Comparison of typical cumulative plastic strain distribution patterns in the cases with different thermal management techniques.
Figure 5.17 Comparison of averaged angular distortions calculated by EPA and PDA for the cases with different thermal management techniques.
CHAPTER 6

APPLICATIONS OF PLASTICITY-BASED DISTORTION ANALYSIS

In previous chapters, the effectiveness of plasticity-based distortion analysis and the unique relationship between cumulative plastic strains and angular distortion have been demonstrated for fillet welded thin plate T-joints. In this chapter, the focus is to extend its application into the investigation of the relationship between cumulative plastic strains and angular distortion of other types of T-connections.

Fillet welded thin wall T-tubular connections are chosen as one of these applications. It is well known that the transverse cumulative plastic strain distributed on the top surface of a flange tube generates transverse shrinkage resulting in angular distortion. However, no detailed information about the contribution of other cumulative plastic strains to angular distortion has been reported.

Another application of PDA is investigating the limitation of a 2D T-joint model in the prediction of angular distortion. Previously, 2D models have been developed and used in residual stress and distortion analyses because of some advantages, such as reducing the calculation time and allowing more detailed weld cross section modeling.
However, there is no clear information explaining the difference between 2D and 3D models in terms of the relationship between cumulative plastic strains and angular distortion. The 3D model is considered as the baseline providing more accurate results.

6.1 Fillet Welded Thin Wall T-Tubular Connections

A T-tubular connection is constructed by joining two rectangular tubes with 3.2 mm thickness as shown Figure 6.1. Detailed dimensions for this connection are shown in Figure 6.1. The material was a magnesium alloy. In the joining process, two butt and two fillet welds are necessary, and their sequence may have an affect on residual stress and distortion patterns. From the observations of previous studies, angular distortion induced by butt welds is negligible [Ref. J2]. Therefore, it was assumed that the region joined by butt welds was initially connected, and the two fillet welds were carried out simultaneously with the same weld direction and speed. Figure 6.2 shows a half symmetric finite element model of the T-tubular connection.

As the distortion mitigation techniques, heat sinking with air mist blowing into the flange tube was proposed by Jung and Tsai in a previous study [Ref. J2]. The effectiveness of the proposed heat sinking is investigated by elastic-plastic analysis and PDA procedure.

6.1.1. Thermal analysis

For the fillet welds, the leg size of the fillet welds was 3.2 mm which was equal to the thickness of the tubes. The welding parameters of GMAW, current, voltage and weld speed were 110 Amps, 13 Volt and 10 mm/sec, respectively.
A double ellipsoidal moving heat source was employed. Heat input was calibrated to match the fusion boundary with the pre-designed fillet size. The heat input calibration factor was 0.7.

In order to simulate the effect of heat sinking, a forced convection boundary was applied on the inner surface of the flange tube where the water mist was flowing. The film coefficient for the forced convection was $1.0 \times 10^{-2} \frac{W}{mm^2\cdot C}$, and the temperature of the air mist was 21 °C. All other surfaces had the natural convection boundary used in T-joint analysis.

Figure 6.3 compares maximum peak temperature maps for the cases with and without heat sinking. The significant reduction of the heated region, especially on the top surface of the flange tube, is obtained by heat sinking, and may give an effect on the distribution of cumulative plastic strains and distortion. Figure 6.4 shows how the maximum peak temperature is distributed on the top surface of the flange tube in both cases. It shows the heated region is very localized by heat sinking, and generally in most regions of the flange tube, the maximum peak temperature does not exceed room temperature except in the welded region.

6.1.2. Elastic-plastic analysis

For both cases, the elastic-plastic analysis was performed to obtain the characteristic distribution of cumulative plastic strains and angular distortion.
On the yz-plane at x = -25 mm, the symmetry boundary conditions were applied, and the fixed boundary conditions were described on the free edge of the web tube at y = 75 mm during welding and cooling.

The average angular distortions for the cases with and without heat sinking at the free end of the flange tube are 0.37 mm and 0.28 mm, respectively. Approximately a 24% reduction of angular distortion is obtained by heat sinking. In order to find the key factor causing the reduction of angular distortion, the distribution patterns of each cumulative plastic strain were investigated. Figure 6.5 and 6.6 compare the distribution of four cumulative plastic strains, $\sum \varepsilon_{xx}^p$, $\sum \varepsilon_{yy}^p$, $\sum \varepsilon_{zz}^p$, $\sum \varepsilon_{xy}^p$ that may be mainly related to angular distortion. Due to heat sinking, a slight reduction of the plastic strain zone of all components is shown. In order to compare them in detail, cumulative plastic strains distributed on the top surface of the flange tube are plotted in Figure 6.7. In general, the size of the plastic zone is slightly reduced, but more significant reduction is observed in the longitudinal cumulative plastic strain, $\sum \varepsilon_{zz}^p$, which are the same results addressed by Han [Ref. H3].

Based on this observation of the distribution pattern of cumulative plastic strains, the reduction of angular distortion due to heat sinking may be affected by the change of the distribution pattern of cumulative plastic strains, but it is still difficult to explain the quantitative effect of heat sinking on each cumulative plastic strain and angular distortion.

6.1.3. Plasticity-based distortion analysis

In order to explain the characteristic relationship between cumulative plastic strains and angular distortion and the effect of heat sinking on this relationship for T-
tubular connections, plasticity-based distortion analysis was performed. For both cases with and without heat sinking, cumulative plastic strains were mapped into finite elements of T-tubular connection using equivalent thermal strains.

Figure 6.8 compares individual and total angular distortions obtained from PDA procedure and angular distortion determined from elastic-plastic analysis for both cases, showing the quantitative contribution of each cumulative plastic strain on angular distortion. Reasonable accuracy of PDA procedure is obtained in both cases.

Unlike T-joints, the transverse cumulative plastic strain, $\sum \varepsilon_{xx}^p$ induces bend-up angular distortion, being the most dominant cause resulting in angular distortion. For T-joints, angular distortion associated with the transverse component is mainly related to its gradient through the thickness of the flange. Therefore, if the gradient of the transverse cumulative plastic strain were not as significant, angular distortion might be minimal. On the other hand, for T-tubular connections, angular distortion mainly results from transverse shrinkage on the top surface of the flange tube far away from the neutral axis of the cross section of the flange tube, but its gradient may produce localized bending of the top surface of the flange tube.

Shear cumulative plastic strain in xy-plane, $\sum \varepsilon_{xy}^p$, results in bend-up angular distortion, but it is not as much as that of the transverse cumulative plastic strain. Vertical cumulative plastic strain, $\sum \varepsilon_{yy}^p$ also produces bend-up angular distortion, but it is relatively small compared to those of transverse and xy-plane shear cumulative plastic strains. Individual angular distortions induced by other components are small enough to be negligible.
Therefore, it can be said that angular distortion in T-tubular connections is mainly related to the transverse cumulative plastic strain, and the effect of the xy-plane shear cumulative plastic strain on angular distortion cannot be negligible. So far, most studies have been focused on developing a simple formula to predict angular distortion of T-tubular connection by accommodating only the transverse cumulative plastic strain. Based on results of PDA, it is expected that angular distortion predicted by only including the transverse cumulative plastic strain will be underestimated. Therefore, some modification might be necessary to include the effect of shear cumulative plastic strain.

Figure 6.8 also shows the effect of heat sinking on the characteristic relationship between cumulative plastic strains and angular distortion. Except for angular distortion induced by the transverse cumulative plastic strain, heat sinking does not have an affect on other individual angular distortions. Even though heat sinking slightly reduces the plastic zone size of the transverse component as shown in Figure 6.7, a significant reduction of angular distortion is achieved. In Chapter 5, it was observed that heat sinking mainly reduces individual angular distortions induced by nominal cumulative plastic strain components, such as transverse, vertical and longitudinal. However, for T-tubular connections, only individual angular distortion induced by transverse cumulative plastic strain is affected by heat sinking. This implies that the changes of the distribution of vertical and longitudinal cumulative plastic strains may not have a significant affect on individual angular distortions and total angular distortion. For shear cumulative plastic strains, heat sinking does not affect their individual angular distortions as in T-joints.
It can be concluded that PDA provides a quantitative understanding of the contribution of each cumulative plastic strain to angular distortion, explaining the effect of heat sinking on the characteristic relationship between cumulative plastic strains and angular distortion, and demonstrating that the unique relationship between cumulative plastic strains and angular distortion is valid in T-tubular connections.

6.2 2D Modeling for T-Joints

A 2D model was developed for T-joints. In this section, the limitation of the application of a 2D distortion analysis for T-joints is mainly discussed in terms of the relationship between cumulative plastic strains and angular distortion. All dimensions of the 2D model were the same as those of the cross section in the 3D model.

One of the thermal management techniques, heat sinking with uniform cooling along the weld line, was simulated to investigate its effect on the characteristics of the relationship between cumulative plastic strains and angular distortion.

Using PDA, the quantitative investigation of the difference between the 2D and 3D models was conducted.

6.2.1 Thermal analysis

Figure 6.9 shows a symmetric half finite element model of the T-joint meshed by 8 nodes 2D elements, DC2D8 [Ref. A1]. Except for the symmetry line, natural convection boundary conditions were described on the free edges of the T-joint. For the case with heat sinking, the forced convection boundary conditions were applied on the bottom edge of the flange within $0 \text{ mm} \leq x \leq 25.7 \text{ mm}$ as shown in Figure 6.9.
The conventional heat model for the 2D thermal analysis is a ramped heat source accommodating the moving effect of the arc as shown in Figure 6.10 [Ref. L2]. The total scanning time of body or surface fluxes is determined by welding speed and unit thickness.

\[ t_{\text{total}} = \frac{\text{unit thickness}}{\text{weld speed}} \]  

(6.1)

In generally, the ramped time has been assumed to be 20 ~ 30 % of the total scanning time. In Figure 6.10, the dotted line represents the ramped heat sources. The total energy can be determined by integrating surface or body fluxes within the scanning time, Equation (6.1). Therefore, the energy calculated from each of the two heat source models is identical. However, there is one critical problem with this ramped heat source model because the scanning time is only dependent upon weld speed. It has been reported that there are difficulties in obtaining correct temperature evolution using a lamped heat source resulting in a relatively higher cooling rate due to the short scanning time [Ref. M5, M6]. It was also reported that the temperature evolution impacted the maximum peak temperature distribution governing the size of the plastic zone [Ref. H3]. Therefore, it is very important to use the correct temperature evolution and the maximum peak temperature distribution in the prediction of distortion.

In this study, a 3D double ellipsoidal body flux was scanned in order to obtain a similar maximum peak temperature in 2D and 3D models, which was proposed by Michaleris, et. al. [Ref. M5, M6]. Considering that the 2D model does not have heat
diffusion along the weld line, the heat input calibration is necessary. Figure 6.11 compares the maximum peak temperature distributions on the top surface of the flange for the 2D and 3D models. The heat input of the 2D model was 82.5% of the 3D model’s. The maximum peak temperature distribution on the weld cross section is also compared in Figure 6.12 (a) and (b). The predicted nugget size and maximum peak temperature distribution pattern obtained from the 2D model show a good agreement with those from the 3D model.

On the other hand, for the case with heat sinking, the maximum peak temperature maps from the 2D and 3D models are different as shown in Figure 6.12 (c) and (d). The 2D model predicts a larger nugget than the 3D model. This discrepancy between the two models may be responsible for the differences in heat diffusion behavior along the weld direction. In order to achieve a similar nugget shape, a very high film coefficient of 1.0 was defined on the region where heat sinking was applied. However, the general distribution pattern shown on Figure 6.12 (e) is so different from that of Figure 6.12 (d) that it can not be used. In this study, the temperature evolution obtain from the 2D model with the forced convection boundary conditions, film coefficient, 1.0E-2 Watt/(mm² °C) with temperature 21°C, was used in the elastic-plastic analysis.

6.2.2 Elastic-plastic analysis

In the 2D elastic-plastic analysis, the generalized plane strain element with reduced integration option, CGPE10R, was used [Ref. A1]. For both cases with and without heat sinking, elastic-plastic analysis was performed to obtain the characteristic distribution of cumulative plastic strains and angular distortion. At the edge at x = 0mm,
the symmetry boundary condition was applied, and the fixed boundary condition was
described on the top edge of the web. Temperature evolutions for both cases were
retrieved from thermal analysis.

The average angular distortion for both cases with and without heat sinking at the
free edge of the flange are 0.392 mm and 0.522 mm, respectively, whereas the 3D model
predicts 2.75 mm and 1.04 mm, respectively. For the case without heat sinking, the 2D
model predicts smaller angular distortion than the 3D model even though the maximum
peak temperature profiles from both models are similar. Figure 6.13 compares the
cumulative plastic strains map obtained from 2D and 3D elastic-plastic analyses. Except
for the longitudinal component, totally different distribution patterns are shown. For the
case with heat sinking, the 2D and 3D models predict the opposite effect of heat sinking
on angular distortion. The 2D model predicts reduced angular distortion by applying heat
sinking. On the other hand, in 3D model, heat sinking increases angular distortion. Figure
6.14 compares the cumulative plastic strains obtained from the 2D and 3D models in the
case with heat sinking. The reduction of the plastic strain zone is clearly shown in both
models due to heat sinking, but there is no significant change in their general distribution
pattern.

Based on these results, it can be said that the 2D model for T-joints may predict
an underestimated angular distortion, and give the wrong interpretation of the effect of
heat sinking on angular distortion based on the assumption that the 3D model would
predict more realistic angular distortion.
6.2.3 Plasticity-based distortion analysis

With results from the elastic-plastic analysis only, it is hard to explain why the 2D and 3D models predict different angular distortion, especially in view of the relationship between cumulative plastic strains and angular distortion. In order to explain their relationship quantitatively, a 2D PDA was carried out. FORTAN programs calculating the equivalent thermal strains are attached in APPENDIX B.

Figure 6.15 shows individual and total angular distortions obtained from PDA for the 2D and 3D models in the case without heat sinking. Individual angular distortions obtained from the 2D model are different from those of the 3D model except angular distortion induced by the longitudinal cumulative plastic strain which has a similar distribution pattern in both 2D and 3D models as shown in Figure 6.13. Unlike the 3D model, bend-up angular distortion in the 2D model is induced by the transverse cumulative plastic strain, and its magnitude is small compared to that of the 3D model. This opposite relation may result in the different effect of heat sinking on angular distortion in the 2D and 3D models. Unlike other nominal components, individual angular distortions induced by the longitudinal component are the same in both 2D and 3D models, which is expected because the distribution patterns of the longitudinal cumulative plastic strain are similar as shown in Figure 6.13 (c). Like the 3D model, the xy-plane shear cumulative plastic strain produce most bend-up angular distortion in the 2D model, but its magnitude is much smaller than that of the 3D model.

Figure 6.16 compares individual and total angular distortions calculated from the 2D PDA for the cases with and without heat sinking. The total angular distortion is reduced by heat sinking in the 2D model, which is different from the results obtained
from the 3D model: the angular distortion is increased by heat sinking. Most of the effects of heat sinking are shown in individual angular distortions induced by the transverse and xy-plane shear cumulative plastic strains. For the transverse component, heat sinking reduces angular distortion, which is a similar result of the 3D model as shown in Figure 5.17. However, the reduction of bend-down angular distortion increases the total bend-up angular distortion in the 3D model, but the reduced bend-up angular distortion decreases the total bend-up angular distortion in the 2D model. For the vertical cumulative plastic strain, the significant reduction of bend-down angular distortion is shown in the 3D model, but no change is evident in the 2D model. For the longitudinal component, the same results are shown in the 2D and 3D models: heat sinking increases bend-up angular distortion. Since the distribution of the longitudinal cumulative plastic strain is mainly dependent on the maximum peak temperature profile along the transverse direction where the effect of the moving heat is minor, a similar result is obtained from the 2D and 3D models. In the 2D model, a significant reduction of bend-up angular distortion induced by the xy-plane shear cumulative plastic strain is observed, but there is a relatively small reduction in the 3D model.

Based on the results, it can be said that cumulative plastic strains and the relationship between cumulative plastic strains and angular distortion obtained from 2D and 3D models for fillet welded T-joints are inherently different except those associated with longitudinal cumulative plastic strain. Therefore, the 2D model in the prediction of angular distortion in T-joints may not be applicable. The 2D model may predict less angular distortion than the 3D model, resulting in incorrect interpretation of the effect of the external restraining and thermal management techniques which control the
distribution pattern of cumulative plastic strains. It is also proved that the distribution pattern of the longitudinal cumulative plastic strain is strongly dependent upon the maximum peak temperature distribution [Ref. H3]. Therefore, the 2D model may be applicable only to cases predicting the longitudinal residual stress and longitudinal buckling which are related to the distribution pattern of the longitudinal cumulative plastic strain.
Figure 6.1 Dimensions of T-tubular connections
Figure 6.2 A finite element model for T-tubular connections
Figure 6.3 Maximum peak temperature maps for the cases with and without heat sinking
Figure 6.4 Comparison of maximum peak temperature profile on the top surface of the flange tube for the cases with and without heat sinking
Figure 6.5 Comparison of typical cumulative plastic strain maps for the cases with and without heat sinking [Part 1]
Figure 6.6 Comparison of typical cumulative plastic strain maps for the cases with and without heat sinking [Part 2]
Figure 6.7 Comparison of typical cumulative plastic strain distributions for the cases with and without heat sinking
Figure 6.8 Averaged angular distortions calculated by EPA and PDA for T-tubular connections with and without heat sinking
Figure 6.9 Scheme of a 2D model scanning a 3D moving heat

Figure 6.10 Scheme of a ramped heat input model
Figure 6.11 Comparison of maximum peak temperature distributions calculated from 2D and 3D models

2D heat input = 82.5% of heat input in 3D
Figure 6.12 Comparison of nugget shapes obtained from 2D and 3D thermal analyses for the cases with and without heat sinking

(a) 2D without heat sinking  
(b) 3D without heat sinking  
(c) 2D with heat sinking  
(d) 3D with heat sinking  
(e) 2D with heat sinking with very high convection coefficient
Figure 6.13 Comparison of cumulative plastic strain distributions obtained from 2D and 3D models for the case without heat sinking.
Figure 6.14 Comparison of cumulative plastic strain distributions obtained from 2D and 3D models for the case with heat sinking.
Figure 6.15 Comparison of averaged individual angular distortions obtained from 2D and 3D models for the case without heat sinking
Figure 6.16 Comparison of averaged individual angular distortions obtained from a 2D model for the cases with and without heat sinking
7.1 Conclusions

In this dissertation, the plasticity-based distortion analysis (PDA) procedure was developed and applied to investigate the relationship between cumulative plastic strains and angular distortion of fillet welded thin plate T-joints and thin wall T-tubular connections. The following conclusions are drawn from the results of this work.

1) Relation between cumulative plastic strains and distortion

From a simple analytical example, it was explained that distortion was uniquely determined under the specified cumulative plastic strain remaining after welding. For welded joints with complex geometric configuration, such as fillet welded T-joints and T-tubular connections, PDA procedure mapping cumulative plastic strains into the joint predicts reasonable angular distortion in all case studies, which implies that the relationship between the cumulative plastic strains and angular distortion is unique.
2) Application of an elastic model

As a simplified analysis tool, an inherent strain approach predicting residual stress and distortion with an elastic model and equivalent forces and moments has been used. PDA demonstrated that the elastic model with material properties at room temperature and cumulative plastic strains was valid in the prediction of distortion in engineering applications.

3) Relationship between cumulative plastic strains and angular distortion in T-joints

Based on the results obtained from PDA, new knowledge about the relationship between cumulative plastic strains and angular distortion in T-joints was addressed. The transverse cumulative plastic strain, having been known as a source inducing bend-up angular distortion, produced bend-down angular distortion in T-joints. The vertical and longitudinal components induced relatively small bend-down and bend-up angular distortion, respectively. Most bend-up angular distortion produced by the xy-plane shear cumulative plastic strain existed in and around the welded region. Other shear components did not affect angular distortion. This new knowledge may provide useful guidelines for the development of an inherent strain approach-based simplified model predicting angular distortion of T-joints.

4) Effect of external restraint on the relationship between cumulative plastic strains and angular distortion in T-joints

As expected, angular distortion was reduced by increasing the degree of external restraint applied on the flange of T-joints. PDA showed that the reduction of angular
distortion by external restraint resulted mainly from the increase of bend-down angular distortion induced by the transverse cumulative plastic strain. The effect of external restraint on other cumulative plastic strains was small enough to be negligible.

5) Effect of heat sinking on the relationship between cumulative plastic strains and angular distortion in T-joints

Heat sinking increased angular distortion. PDA showed that heat sinking controlled mainly the nominal cumulative plastic strains, such as transverse, vertical and longitudinal components. Bend-down angular distortion induced by the transverse and vertical components was reduced by heat sinking, which resulted in the increase of angular distortion. For other components, the change of angular distortion was relatively small, thus negligible.

6) Effect of TIG pre-heating on the relationship between cumulative plastic strains and angular distortion in T-joints

TIG pre-heating resulted in the reduction of angular distortion. PDA showed that TIG pre-heating did not affect the contribution of nominal components to angular distortion, but mainly controlled the contribution of the xy-plane shear cumulative plastic strain existing in and around the welded region. The reduction of angular distortion resulted from the reduction of bend-up angular distortion which was induced by the xy-plane shear cumulative plastic strain.
7) Relationship between each cumulative plastic strain and angular distortion in T-tubular connections

It has been known that transverse shrinkage associated with transverse cumulative plastic strain resulted in angular distortion in T-tubular connections. PDA showed that this was correct, but other components, such as vertical and xy-plane shear components, should be taken into consideration as sources of angular distortion. Unlike T-joints, heat sinking was an effective distortion mitigation technique, reducing bend-up angular distortion induced by the transverse cumulative plastic strain. Angular distortions induced by other components were not sensitive to heat sinking. The consistent results in T-joints and T-tubular connections were observed, and it was determined that heat sinking mainly controlled the contribution of the nominal cumulative plastic strains to angular distortion.

8) Limitation of a 2D model in the prediction of angular distortion in T-joints

Based on results obtained from thermal-elastic-plastic analysis and PDA, it was shown that the cumulative plastic strain distribution patterns and the relationship between cumulative plastic strains and angular distortion in T-joints obtained from 2D and 3D models were inherently different except those associated with the longitudinal cumulative plastic strain. For example, the transverse cumulative plastic strain produced bend-down angular distortion in the 3D model, but it produced bend-up angular distortion in the 2D model. Less angular distortion was predicted by the 2D model than the 3D model. The opposite effect of heat sinking on angular distortion was shown in the 2D and 3D models: heat sinking increased angular distortion in the 3D model, but reduced angular distortion in the 2D model. Therefore, the 2D model is not applicable in the prediction of angular
distortion in T-joints. The 2D model may be used in the prediction of longitudinal residual stress and longitudinal buckling which are strongly dependent upon the distribution of the longitudinal cumulative plastic strain.

7.2 Future Work

The fundamental characteristic relationship between cumulative plastic strains and angular distortion for fillet welded T-joint and T-tubular connections was found using the PDA procedure. For future research, the following studies are recommended:

- Investigate the effect of welding parameters, material properties and joint configuration on the characteristic relationship between cumulative plastic strains and angular distortion in T-joints using PDA.
- Expand the application of PDA to other types of welded joints, such as multi-pass welded butt and T-joints, pipes, panel structures and tubular connections.
- Develop a thermal-elastic-plastic model to predict angular distortion including more detailed material behaviors, such as stress-strain relaxation and deposition of filler metals, investigating their effect on the characteristic relationship between cumulative plastic strains and angular distortion in T-joints.
- Develop a simplified distortion model predicting angular distortion of T-joints and T-tubular connections using the characteristic relationship between cumulative plastic strains and angular distortion obtained from PDA.
REFERENCES


[H3]. Han, M.S. 2002. Fundamental studies on welding-induced distortion in thin plate. Ph.D. Dissertation, The Ohio State University, Columbus, Ohio. USA.

[H4]. Hong, J.K. 1996. Study of numerical methodologies for multi-pass welding distortion analysis. Ph.D. Dissertation, The Ohio State University, Columbus, Ohio. USA.


APPENDIX A

3D MAPPING PROGRAMS

1) Read cumulative plastic strains in “mech.fil” file, and write them on “plastic.out”

C:\abaqus make job=plasticstrain3D
C:\plasticstrain3D

File name: “plasticstrain3D.for”

C
C ABAQUS 5.8-14
C
PROGRAM POST
C
C INCLUDE 'ABA_PARAM.INC'
character*80 fname
dimension array(513), jrray(nprec,513), lrunit(2,1)
equivalence(array(1), jrray(1,1))
open(unit=3,
1 file=c:\plastic.out,
1 status='UNKNOWN')
c
File initialization
c
fname='mech'
c
nr : the number of result files
c
nr = 1

c lrunit(1,k1) : Fortran unit to read k1th result file, 1-4, 8, 14-30
c lrunit(2,k1) = 1: Ascii, lrunit(2,k1) = 2, Binary

149
c
LRUNIT(1,1)=8
LRUNIT(2,1)=2
LOUTF=0
CALL INITPF(FNAME,NRU,LRUNIT,LOUTF)
JUNIT=8
CALL DBRNU(JUNIT)
c
Read records from result file
c
Note: For this operation, plastic strains in fil file should be written
c by: *EL FILE,ELSET= ***, position=AVERAGED AT NODES
c
DO 110 K1=1,99999
CALL DBFILE(0,ARRAY,JRCD)
KEY=JRRAY(1,2)
IF(JRCD.NE.0) GOTO 100
IF(KEY.EQ.1) THEN
c
JNODE : node number
c
JNODE=JRRAY(1,3)
c
KET=22 for reading plastic strains
c
else if (key.eq.22) then
XPE11= ARRAY (3)
XPE22= ARRAY (4)
XPE33= ARRAY (5)
XPE12= ARRAY (6)
XPE13= ARRAY (7)
XPE23= ARRAY (8)
WRITE(3,1000)JNODE,XPE11,XPE22,XPE33,XPE12,XPE13,XPE23
1000 FORMAT(I5,2X,6(E15.7,2X))
ENDIF
110 CONTINUE
100 CONTINUE
STOP
END

2) User-subroutine, “UTEMP”

SUBROUTINE
UTEMP(TEMP,MSECPT,KSTEP,KINC,TIME,NODE,COORDS)
C
INCLUDE 'ABA_PARAM.INC'
C
C XPLCOMP (NUMNODE,7): NUMNODE = TOTAL NUMBER OF NODES
C
DIMENSION TEMP(MSECPT), TIME(2), COORDS(3), XPLCOMP(30016,7)
C
C Thermal strain = 1.0 E-5 * (Temp-0) = plastic strain
C Temp = plastic strain/1.0E-5

C In main program,
C Total time = 1.0 sec
C Anistropic thermal expansion coefficient = 1.0E-5
C Initial temperature = 0
C Reference temperature = 0

C
OPEN(UNIT=3, 1 FILE='C:\PLASTIC.OUT', STATUS='UNKNOWN')
C
C NUMNODE=30016
IF(IXNODE.EQ.0) THEN
  IXNODE=IXNODE+1
  TEMP(1)=0.D0
  XPLCOMP=0.D0
  DO 20 K2=1,NUMNODE
    READ(3,*) (XPLCOMP(K2,J1),J1=1,7)
    INODE=INT(XPLCOMP(K2,1))
    WRITE(4,1001)INODE,(XPLCOMP(K2,J1),J1=2,7)
 20 CONTINUE
CLOSE(UNIT=3)
ELSE
ENDIF
C
C ICOMP = 1 (PE11), 2(PE22), 3(PE33), 4(PE12), 5(PE13), 6(PE23)
C
ICOMP=1
TEMP(1)=XPLCOMP(NODE,ICOMP+1)/1.0E-5*TIME(2)
RETURN
END
APPENDIX B

2D MAPPING PROGRAMS

1) Read cumulative plastic strains in “mech.fil” file, and write them on “plastic.out”

C:\abaqus make job=plasticstrain2D
C:\plasticstrain2D

File name: “plasticstrain2D.for”

C
C ABAQUS 5.8-14
C
C PROGRAM POST
C
C INCLUDE ’ABA_PARAM.INC’
CHARCTER*80 FNAME
DIMENSION ARRAY(513), JRAY(NPREDN,513), LRUNIT(2,1)
EQUIVALENCE(ARRAY(1), JRAY(1,1))
OPEN(UNIT=3,
1     FILE=C:\plastic.out,
1     STATUS=’UNKNOWN’)

C File initialization
C
FNAME=’mech’
C
NRU=1
C
LRUNIT(1,k1) : Fortran unit to read k1th result file, 1-4, 8, 14-30
C
LRUNIT(2,k1) = 1: Ascii, LRUNIT(2,k1) =2, Binary
LRUNIT(1,1)=8
LRUNIT(2,1)=2
LOUTF=0
CALL INITPF(FNAME,NRU,LRUNIT,LOUTF)
JUNIT=8
CALL DBRNU(JUNIT)

Read records from result file

Note: For this operation, plastic strains in fil file should be written
by: *EL FILE,ELSET= ***, position=AVERAGED AT NODES

DO 110 K1=1,99999
CALL DBFILE(0,ARRAY,JRCD)
KEY=JRRAY(1,2)
IF(JRCD.NE.0) GOTO 100
IF(KEY.EQ.1) THEN
JNODE=JRRAY(1,3)
else if (key.eq.22) then
XPE11= ARRAY (3)
XPE22= ARRAY (4)
XPE33= ARRAY (5)
XPE12= ARRAY (6)
WRITE(3,1000)JNODE,XPE11,XPE22,XPE33,XPE12
1000  FORMAT(I5,2X,4(E15.7,2X))
ENDIF
111 CONTINUE
101 CONTINUE
STOP
END

2) User-subroutine, “UTEMP”

SUBROUTINE
UTEMP(TEMP,MSECPT,KSTEP,KINC,TIME,NODE,COORDS)
C
INCLUDE 'ABA_PARAM.INC'
C
C XPLCOMP (NUMNODE,5): NUMNODE = TOTAL NUMBER OF NODES
C
DIMENSION TEMP(MSECPT), TIME(2), COORDS(3), XPLCOMP(1216,5)
C
--- Thermal strain = 1.0 E-5 *(Temp-0) = plastic strain
C Temp = plastic strain/1.0E-5
C
C In main program,
C Total time = 1.0 sec
C Anisotropic thermal expansion coefficient = 1.0E-5
C Initial temperature = 0
C Reference temperature = 0
C
---
OPEN(UNIT=3,
1 FILE='C:\PLASTIC.OUT',  STATUS='UNKNOWN')
C
C
C NUMNODE=1216
IF(IXNODE.EQ.0) THEN
   IXNODE=IXNODE+1
   TEMP(1)=0.D0
   XPLCOMP=0.D0
   DO 20 K2=1,NUMNODE
      READ(3,*) (XPLCOMP(K2,J1),J1=1,5)
      INODE=INT(XPLCOMP(K2,1))
      WRITE(4,1001)INODE,(XPLCOMP(K2,J1),J1=2,5)
   1001   FORMAT(2X,I5,2X, 4(E15.7,2X))
   20      CONTINUE
   CLOSE(UNIT=3)
ELSE
   ENDIF
C
C ICOMP = 1 (PE11), 2 (PE22), 3 (PE33), 4 (PE12)
C
ICOMP=1
TEMP(1)=XPLCOMP(NODE,ICOMP+1)/1.0E-5*TIME(2)
RETURN
END