NOTE TO USERS

This reproduction is the best copy available.

UMI[®]

**A STUDY ON THE COMBINED EFFECT OF THE WELD
LENGTH AND LEG SIZE ON THE CAPACITY OF STEEL
PLATE TENSION MEMBERS**

Submitted by

Alain-Philippe Beauger, B. Eng

Carleton University, Ottawa, Ontario, Canada

A thesis submitted to the Faculty of Graduate Studies and
Research in partial fulfillment of the requirements for the
degree of

Master of Applied Science

Ottawa-Carleton Institute for Civil Engineering

Department of Civil and Environmental Engineering

Carleton University

Ottawa, Ontario, Canada

January, 2010

Copyright© Alain-Philippe Beauger, 2010

Library and Archives
Canada

Published Heritage
Branch

395 Wellington Street
Ottawa ON K1A 0N4
Canada

Bibliothèque et
Archives Canada

Direction du
Patrimoine de l'édition

395, rue Wellington
Ottawa ON K1A 0N4
Canada

Your file *Votre référence*
ISBN: 978-0-494-63818-7
Our file *Notre référence*
ISBN: 978-0-494-63818-7

NOTICE:

The author has granted a non-exclusive license allowing Library and Archives Canada to reproduce, publish, archive, preserve, conserve, communicate to the public by telecommunication or on the Internet, loan, distribute and sell theses worldwide, for commercial or non-commercial purposes, in microform, paper, electronic and/or any other formats.

The author retains copyright ownership and moral rights in this thesis. Neither the thesis nor substantial extracts from it may be printed or otherwise reproduced without the author's permission.

AVIS:

L'auteur a accordé une licence non exclusive permettant à la Bibliothèque et Archives Canada de reproduire, publier, archiver, sauvegarder, conserver, transmettre au public par télécommunication ou par l'Internet, prêter, distribuer et vendre des thèses partout dans le monde, à des fins commerciales ou autres, sur support microforme, papier, électronique et/ou autres formats.

L'auteur conserve la propriété du droit d'auteur et des droits moraux qui protègent cette thèse. Ni la thèse ni des extraits substantiels de celle-ci ne doivent être imprimés ou autrement reproduits sans son autorisation.

In compliance with the Canadian Privacy Act some supporting forms may have been removed from this thesis.

While these forms may be included in the document page count, their removal does not represent any loss of content from the thesis.

Conformément à la loi canadienne sur la protection de la vie privée, quelques formulaires secondaires ont été enlevés de cette thèse.

Bien que ces formulaires aient inclus dans la pagination, il n'y aura aucun contenu manquant.

Canada

ABSTRACT

An experimental study has been conducted to determine the combined effect of the weld length and leg size on the capacity of steel plate tension members. The specimens consisted of single and double plate (flat bar) being welded longitudinally only along two parallel edges to the gusset plates. Six weld configurations with the combination of weld length to plate width ratios (L/w) of 0.5, 1.0, 1.5 and 2.0, and the weld leg size to plate thickness ratios (s/t) of 0.63 and 1.0 were considered. Compared to the test results, provisions in the current Canadian and American design specifications were found to significantly underestimate the capacity at an L/w ratio less than 1.0 and slightly overestimate the capacity at an L/w ratio greater than or equal to 2.0 of the full weld size specimens. The maximum capacity of the full weld size specimens was achieved at a L/w ratio slightly above 1.0, but the full efficiency of the specimen was not achieved even at an L/w ratio of 2.0. However, design provisions for tension members overestimate the capacity of the partial weld size specimens since only the effect of the L/w ratio has been considered.

Along with the experimental study, a finite element analysis parametric study was also conducted for a wide range L/w and s/t ratios that take into account the combined effect of the weld length and leg size. Results of the finite element analyses were used in developing the equations to predict the capacity of welded plate tension members after being calibrated with the test data.

ACKNOWLEDGEMENTS

The author wishes to thank his supervisor for this thesis, Dr. Heng Aik Khoo for his guidance, support, patience, and advice throughout this research. Its completion could not have happened without it.

I would also like to thank my parents, my family, and my friends for their support and encouragement during the period of this research. I want to thank the technicians of the Civil and Environmental Engineering laboratory at Carleton University for their help. This research is funded by the Natural Sciences and Engineering Research Council of Canada (NSERC).

TABLE OF CONTENTS

Chapter	Page
CHAPTER 1 INTRODUCTION	1
1.1 Objective of the thesis.....	2
1.2 Methodology	2
1.3 Organization of the thesis	4
CHAPTER 2 LITERATURE REVIEW	6
2.1 Design of tension members and connections.....	6
2.1.1 Canadian design provisions.....	7
2.1.2 American design provisions	10
2.1.3 Weld design	11
2.2 Shear lag.....	12
2.2.1 Bolted and riveted connections	14
2.2.2 Welded connections.....	15
2.3 Numerical simulation for welded tension members	17
CHAPTER 3 TESTING PROGRAM	22
3.1 Objective	22
3.2 Specimen detail.....	23
3.3 Specimen measurement and designation	23
3.4 Test setup and instrumentation.....	24
3.5 Test procedure	25

3.6	Material properties	25
CHAPTER 4	EXPERIMENTAL RESULTS	35
4.1	Material tests.....	35
4.2	Specimen test results	36
4.2.1	Double plate specimens – full weld size	38
4.2.2	Partial weld size specimens.....	38
4.2.3	Single plate specimens	39
4.3	Strain distribution.....	40
4.4	Analysis of design guidelines	41
CHAPTER 5	NUMERICAL ANALYSIS AND PARAMETRIC STUDY ...	54
5.1	Material properties	54
5.2	Finite element modeling	56
5.2.1	Finite element modeling of the flat bar	56
5.2.2	Finite element modeling of the weld.....	57
5.2.3	Failure criteria	58
5.2.4	Mesh refinement.....	59
5.3	Finite element model validation	62
5.4	Parametric study.....	65
5.4.1	Parametric study of full weld tension plate members	66
5.4.2	Parametric study of partial weld tension plate members	68
CHAPTER 6	CONCLUSIONS AND RECOMMENDATIONS.....	100

6.1	Conclusions	100
6.2	Recommendations	102
	REFERENCES.....	104
	APPENDIX A: TESTING PROGRAM	107
	APPENDIX B: ADDITIONAL EXPERIMENTAL RESULTS	116
	APPENDIX C: DETERMINATION OF TRUE STRESS VERSUS TRUE PLASTIC STRAIN RELATIONSHIP	133
	APPENDIX D: ADDITIONAL DATA	147

LIST OF TABLES

Table	Page
3.1 Nominal dimensions of test specimens.....	28
3.2 Average dimensions of specimens.....	29
4.1 Material properties	44
4.2 Test results	45
4.3 Comparisons of the average efficiency between full and partial weld size tests	46
4.4 Comparisons of the average efficiency between single and double plate specimens.....	46
5.1 True stress – true plastic strain values for the flat bar, 12.7 and 7.9 mm thick gusset plates	74
5.2 True stress – true plastic strain values for the weld material.....	75
5.3 Change in efficiency between different mesh refinements with first crack failure criterion.....	76
5.4 Change in efficiency between different mesh refinements with crack propagation analyses	76
5.5 Comparison between the predicted and experimental efficiencies for partial weld size specimens ($s/t = 0.63$).....	77
5.6 Comparison between the predicted and experimental efficiencies for full weld size specimens ($s/t = 1.0$)	77

5.7	Effect of element thickness on the efficiency obtained through the numerical simulation.....	78
5.8	Full weld size tension plate member efficiency.....	78
5.9	Partial weld size tension plate member efficiency.....	79
5.10	Corrected and uncorrected ratios of predicted partial over full weld size specimen efficiency at L/w ratios of 1.0 and 2.0.....	80
5.11	Normalized efficiency ratio for the interpolation curve with the fracture limit of 0.55.....	81
5.12	Normalized efficiency ratio for the interpolation curve with the fracture limit of 0.79.....	82
5.13	Comparisons between predicted and AWS (1931) test efficiencies for partial weld size tension plate specimens	83
A.1	Measured dimensions of the first flat bar for single and double plate specimens	108
A.2	Measured dimensions of the second flat bar for double plate specimens.....	109
A.3	Measured dimensions of the top gusset plate	110
A.4	Measured dimensions for the bottom gusset plate.....	111
A.5	Measured dimensions of welds in the first flat bar for single and double plate specimens	112

A.6	Measured dimensions of welds in the second flat bar of double plate specimens.....	113
B.1	Dynamic and static stresses for tension coupons of the flat bar, 12.7 and 7.9 mm thick gusset plates.....	118
C.1	C and n values at each iteration used in deriving the true stress–true plastic strain curve for the tension coupons.....	138
C.2	Constants used in generating the true stress – true plastic strain curve for the tension coupons.....	138
C.3	C and n values used in deriving the true stress – true plastic strain relationship for the weld material.....	139
C.4	True stress – true plastic strain values for weld material.....	140
D.1	Test results from the AWS 2122 series	148
D.2	Test results from the AWS 2133 series	149
D.3	Test results from the AWS 2144 series	150
D.4	Test results from the AWS 2155 series	151
D.5	Test results from the AWS 2166 series	152
D.6	Test results from the AWS 2322 series	153
D.7	Test results from the AWS 2333 series	154
D.8	Test results from the AWS 2344 series	155
D.9	Test results from the AWS 2355 series	156
D.10	Test results from the AWS 2366 series	157

D.11	Test results from the AWS 2143 series	158
D.12	Test results from the AWS 2153 series	159
D.13	Test results from the AWS 2163 series	160
D.14	Test results from the AWS 2343 series	161
D.15	Test results from the AWS 2353 series	162
D.16	Test results from the AWS 2363 series	163
D.17	Average test results for each AWS series.....	164
D.18	Test results of Easterling and Giroux plate specimens	165

LIST OF FIGURES

Figure	Page
1.1 Plate tension plate member welded longitudinally along only two parallel edges	5
2.1 Shear lag in a steel tension member [adopted from Abi-Saad et al. (2006)]	21
2.2 Eccentricity of a steel angle and doubly symmetric section [adopted from Kulak et al. (1987)]	21
3.1 Isometric view of a welded specimen.....	30
3.2 Top and side view of a welded plate specimen.....	31
3.3 Location of strain gauges.....	32
3.4 Test setup	33
3.5 Cross-sectional measurements of tension coupons.....	34
4.1 Test efficiencies with predicted CSA and AISC values	47
4.2 Average stress versus average LVDT displacement for full weld size double plate specimens	47
4.3 Transverse tension fracture across the cross-section, shown for d15R	48
4.4 Longitudinal shear failure along the weld typical for specimens with $L/w = 0.5$, shown for d05.....	48
4.5 Comparison of the stress versus average LVDT displacement for double plate specimens with s/t ratios of 1.0 and 0.63	49

4.6	Longitudinal shear failure typical of specimens with $L/w = 1.0$ and $s/t = 0.63$, shown for d10a	49
4.7	Comparison of the stress versus average LVDT displacement between double and single plate specimens with L/w ratio of 1.0 and 2.0.....	50
4.8	Strain in single plate specimens prior to the onset of yielding at LVDT displacement of 3 mm.....	50
4.9	Strain in full and partial weld size single plate specimens prior to the onset of yielding at LVDT displacement of 3 mm.....	51
4.10	Efficiencies of full size weld specimens.....	51
4.11	Efficiencies of full and partial weld plate specimens without modification for weld size	52
4.12	Average efficiencies of full and partial weld plate specimens without modification for weld size	52
4.13	Efficiencies of full and partial weld size specimens, modified using Eq. (4.2)	53
4.14	Average test efficiencies of full and partial weld specimens, modified using Eq. (4.2).....	53
5.1	True stress versus true plastic strain curve for flat bar, gusset plates and weld.....	84
5.2	Finite element model with boundary conditions.....	84
5.3	Discretized weld for specimens with $s/t = 1.0$ (unrefined).....	85

5.4	Discretized weld for specimens with $s/t = 0.63$ (unrefined).....	85
5.5	First mesh configuration with mesh refinement Scheme 1.....	86
5.6	Second mesh configuration with modification to the coarse mesh for the specimens with an L/w ratio less than 1.0	87
5.7	Third mesh configuration with extended mesh refinement for specimens with an s/t ratio less than 1.0 and L/w greater than and equal to 1.0.....	87
5.8	Top view of mesh refinement for Scheme 1, $0.476 \times 0.595 \times 0.595$ mm (width x length x thickness).....	88
5.9	Top view of mesh refinement for Scheme 2, $0.318 \times 0.397 \times 0.397$ mm (width x length x thickness).....	89
5.10	Top view of mesh refinement for Scheme 3, $0.238 \times 0.298 \times 0.298$ mm (width x length x thickness).....	90
5.11	Predicted mode of failure for specimens with $L/w = 0.5$ and $s/t = 1.0$	91
5.12	Predicted mode of failure for specimens with $L/w \geq 1.0$ and $s/t = 1.0$	91
5.13	Average stress versus LVDT displacement curves for specimens with $L/w = 2.0$ and $s/t = 0.63$	92
5.14	Average stress versus LVDT displacement curves for specimen with $L/w = 1.0$ and $s/t = 0.63$	92
5.15	Average stress versus LVDT displacement curves for specimen with $L/w = 0.5$ and $s/t = 1.0$	93

5.16	Average stress versus LVDT displacement curves for specimen with L/w = 1.0 and s/t = 1.0	93
5.17	Average stress versus LVDT displacement curves for specimen with L/w = 1.5 and s/t = 1.0	94
5.18	Average stress versus LVDT displacement curves for specimen with L/w = 2.0 and s/t = 1.0	94
5.19	Modified mesh with smaller element thickness at s/t = 1.0.....	95
5.20	Test and predicted efficiency versus L/w ratio	95
5.21	Comparison between predicted and average test efficiencies for full weld size specimens.....	96
5.22	Comparison between predicted and average test efficiencies with modified effective plate width for full weld size specimens.....	96
5.23	Comparison between predicted and test efficiency with modified effective plate width for full weld size specimens.....	97
5.24	Test and predicted efficiency of full and partial weld size specimens for various s/t ratios with fracture limit of 0.55.....	97
5.25	Test and predicted efficiency of full and partial weld size specimens for various s/t ratios with fracture limit of 0.79.....	98
5.26	Corrected ratio of the partial over full weld size specimen efficiency ($U/U_{s/t=1}$) _c versus weld leg size ratio (s/t) for L/w ratios of 1.0 and 2.0	98
5.27	Interpolation curve based on the fracture limit of 0.55.....	99

5.28	Interpolation curve based on the fracture limit of 0.79.....	99
A.1	Identification of fillet weld 1 to 4 on the strain gauge side of the flat bar.....	114
A.2	Identification of fillet weld 5 to 8 on the non-strain gauge side of the flat bar	115
B.1	Engineering stress versus engineering strain curves for FB 102 x 12.7 mm tension coupons	119
B.2	Engineering stress versus engineering strain curves for FB 76 x 4.8 mm tension coupons	119
B.3	Engineering stress versus engineering strain curves for FB 102 x 7.9 mm tension coupons	120
B.4	Average stress versus LVDT displacement for double plate specimens with $L/w = 0.5$ and $s/t = 1.0$	121
B.5	Weld failure for double plate specimens d05 and d05R	121
B.6	Average stress versus LVDT displacement for double plate specimens with $L/w = 1.0$ and $s/t = 1.0$	122
B.7	Plate failure for double plate specimens d10 and d10R.....	122
B.8	Average stress versus LVDT displacement for double plate specimens with $L/w = 1.0$ and $s/t = 0.63$	123
B.9	Weld failure for double plate specimens d10a and d10aR.....	123
B.10	Average stress versus LVDT displacement for double plate specimens with $L/w = 1.5$ and $s/t = 1.0$	124

B.11	Plate failure for double plate specimens d15 and d15R.....	124
B.12	Average stress versus LVDT displacement for double plate specimens with $L/w = 2.0$ and $s/t = 1.0$	125
B.13	Plate failure for double plate specimens d20 and d20R.....	125
B.14	Average stress versus LVDT displacement for double plate specimens with $L/w = 2.0$ and $s/t = 0.63$	126
B.15	Weld failure for double plate specimens d20a and d20aR.....	126
B.16	Average stress versus LVDT displacement for single plate specimens with $L/w = 0.5$ and $s/t = 1.0$	127
B.17	Weld failure for single plate specimens s05 and s05R	127
B.18	Average stress versus LVDT displacement for single plate specimens with $L/w = 1.0$ and $s/t = 1.0$	128
B.19	Plate failure for single plate specimens s10 and s10R.....	128
B.20	Average stress versus LVDT displacement for single plate specimens with $L/w = 1.0$ and $s/t = 0.63$	129
B.21	Weld failure for single plate specimens s10a and s10aR.....	129
B.22	Average stress versus LVDT displacement for single plate specimens with $L/w = 1.5$ and $s/t = 1.0$	130
B.23	Plate failure for single plate specimens s15 and s15R.....	130
B.24	Average stress versus LVDT displacement for single plate specimens with $L/w = 2.0$ and $s/t = 1.0$	131

B.25	Plate failure for single plate specimens s20 and s20R.....	131
B.26	Average stress versus LVDT displacement for single plate specimens with $L/w = 2.0$ and $s/t = 0.63$	132
B.27	Weld failure for single plate specimens s20a and s20aR.....	132
C.1	Tension coupons measurement locations.....	141
C.2	Finite element mesh for the cylindrical coupon analysis.....	142
C.3	Engineering stress versus change in cross-sectional area for FB 76 x 4.8 mm	143
C.4	True stress versus true plastic strain relationship for FB 76 x 4.8 mm.....	143
C.5	Engineering stress versus change in cross-sectional area for FB 102 x 12.7 mm	144
C.6	True stress versus true plastic strain relationship for FB 102 x 12.7 mm.....	144
C.7	Engineering stress versus change in cross-sectional area for FB 102 x 7.9 mm	145
C.8	True stress versus true plastic strain relationship for FB 102 x 7.9 mm.....	145
C.9	True stress versus true plastic strain relationship for weld material.....	146

LIST OF SYMBOLS

A	-	current cross-sectional area
A_{cor}	-	Measured cross-sectional area taken from the corners of a rectangular coupon after necking
A_e	-	reduced effective area of the cross-section due to shear lag
A_f	-	average cross-sectional area of a tension coupon at fracture
A_g	-	gross cross-sectional area
A_{gv}	-	gross area in shear
A_m	-	area of effective fusion face
A_{mid}	-	Measured cross-sectional area taken from the middle of a rectangular of a rectangular coupon after necking
A_n	-	net cross-sectional area
A_{ne}	-	effective net cross-sectional area reduced due to shear lag
A_{n1}	-	area of the elements directly connected to transverse welds
A_{n2}	-	area of the elements connected along two parallel edges
A_{n3}	-	area of the elements connected by a single longitudinal weld
A_o	-	original cross-sectional area of a tension coupon
A_w	-	area of effective weld throat
AWS	-	American Welding Society
C	-	parameter that relates the true stress to the true plastic strain
E	-	modulus of elasticity

$f(L/w, s/t)$	- reduction function to convert from full to partial weld size efficiency based on the weld length ratio
$f_{u1}(s/t)$	- function to approximate the relationship between $(U/U_{s/t=1})_c$ and s/t ratios at $L/w = 1.0$
$f_{u2}(s/t)$	- function to approximate the relationship between $(U/U_{s/t=1})_c$ and s/t ratios at $L/w = 2.0$
FB	- flat bar
F_D	- dynamic stress
FEM	- finite element model
F_s	- static stress
F_u	- ultimate strength of steel
F_y	- yield strength of steel
$g(L/w_s, s/t)$	- reduction function to convert from full to partial weld size efficiency based on the modified weld length ratio
L	- length of the longitudinal weld
L/w	- weld length ratio
L/w_s	- modified weld length ratio
L_G	- length of the gusset plate
L_s	- total length of the flat bar
n	- parameter that relates the true stress to the true plastic strain
P_n	- tensile resistance of steel (AISC)

P_{utest}	- test load at failure
P_{u-FEM}	- maximum load achieved in the finite element analysis
s	- weld leg size
s/t	- weld size ratio
t	- thickness of the cross-section
t_G	- thickness of the gusset plate
T_r	- tensile resistance of steel (CSA)
U	- shear lag factor
U_{AISC}	- predicted efficiency from ANSI/AISC 360-05 (2005)
U_{CSA}	- predicted efficiency from CSA S16-01 (2005)
U_e	- test efficiency from AWS
U_F	- predicted efficiency for full weld size plate tension members
$U_{FEM(avg)}$	- weighted average efficiency of full weld size tension plate specimen from the finite element analysis
U_L	- lower bound efficiency using first crack failure criterion
U_m	- predicted efficiency of partial weld size plate tension members based on experimental results
U_n	- net section efficiency
U_P	- predicted efficiency for partial weld size plate tension members
$U_{s/t=1}$	- efficiency of full weld size specimens
U_t	- efficiency factor

U_{ta}	-	average test efficiency of all specimens with same L/w and s/t ratios
U_{te}	-	experimental test efficiency
U_u	-	upper bound efficiency using crack propagation analysis procedure
w	-	width of the cross-section
w_n	-	width of the net cross-section
w_s	-	modified effective plate width
$\bar{X}$	-	eccentricity of the connection
X_u	-	weld electrode ultimate strength
$(1-A/A_o)$	-	change in cross-sectional area
$(U/U_{s/t=1})_c$	-	corrected ratio of the partial over full weld size specimen efficiency
$(U/U_{s/t=1})_n$	-	normalized ratio of the partial over full weld size specimen efficiency
δ_{corr}	-	ratio of the difference between stroke and extensometer loading control
Δ_{FEM}	-	drop in efficiency at $s/t = 0.63$ for the numerical simulation
Δ_{peak}	-	average LVDT displacement at peak load
Δ_{test}	-	drop in efficiency at $s/t = 0.63$ for the experimental tests
ΔF_{ED}	-	extensometer control load (stress) drop
ΔF_{SD}	-	stroke control load (stress) drop
ϵ_o	-	engineering strain prior to the removal of the extensometer
ϵ^e	-	engineering strain
ϵ^p	-	true plastic strain

ε^t	- true strain
ε_{eq}^p	- equivalent plastic strain
ε_f^p	- uniaxial equivalent plastic strain at fracture
ε_{ij}^p	- plastic strain tensor
ϕ	- resistance factor
θ	- angle between the axis of the weld and the load vector
σ^e	- engineering stress
σ^t	- true stress
σ_i^e	- engineering stress at $(1-A/A_o)_f$ in the i^{th} iteration
σ_{i-1}^e	- engineering stress at $(1-A/A_o)_f$ in the $(i-1)^{th}$ iteration
σ_f^e	- engineering stress at fracture

CHAPTER 1 INTRODUCTION

Steel tension members have many applications in structural engineering, and are commonly used as chords, diagonals, and vertical members in trusses, or as cross-bracing against lateral loads. However, the effects of shear lag which cause the uneven stress distribution and stress concentration around the connection may prevent these members from realizing their full capacities. Therefore, it is important to gain a better understanding of shear lag and other factors that affect the performance of steel tension members. However, provisions in the current design specifications may not be representative of the actual performance of tension members of certain structural shapes. One of these consists of plates longitudinally welded with parallel fillet welds, as shown in Fig. 1.1.

Material properties of the steel, the shape and dimensions of the cross section, and the connection length have been found to affect the ultimate capacity of tension members. Effects of these factors have been incorporated in the existing design provisions. However, the effect of the weld leg size on the capacity of the tension member has not been addressed except through the weld capacity calculation. Easterling and Giroux (1993) have tested longitudinally welded plates only along two edges and with the fillet weld leg size equal to the plate thickness at a weld length to plate width ratios (L/w) of 1.4 and 1.7. On the other hand, tests commissioned by the American Welding Society (AWS 1931) investigated plate specimens with a weld leg size smaller than the plate thickness at an L/w ratio of 1.0 to 1.6. The efficiency of the specimen was lower in the

tests by AWS (1931) compared to those by Easterling and Giroux (1993). These results show that the capacity of welded plate tension members is affected by the weld leg size.

There is a lack of information on the effect of varying the weld leg size on the capacity of welded plate tension members at various weld lengths. Thus, it will be useful to study and quantify the combined effect of the fillet weld leg size and length on the capacity of plate tension members so that design guidelines that are more representative of their performance can be developed.

1.1 Objective of the thesis

The objective of this thesis is to study and quantify the combined effect of the weld size and length on the ultimate capacity of plate specimens longitudinally welded along only two parallel edges. Single and double plate specimens will be investigated experimentally at a weld length ratio (L/w) of 0.5 to 2.0 together with two weld leg sizes. A finite element analysis parametric study will also be carried out to complement the experimental test results. Together, results from the tests and numerical simulations will be used to develop the recommendations to improve on the provisions in the design standards for welded plate tension members.

1.2 Methodology

A total of 24 welded single plate and double plate specimens with six different configurations were tested. The six weld configurations consist of four configurations with full weld size (fillet weld size equal to the thickness of the plate) at the weld length to plate width ratios (L/w) of 0.5, 1.0, 1.5, and 2.0, and two configurations with the weld

leg size to plate thickness ratio (s/t) of 0.63 at the L/w ratios of 1.0 and 2.0. Only one flat bar size of FB 76 x 4.8 (3" x 3/16") was used as the tension member. The flat bar was welded to the gusset plates, which were placed into the hydraulic grip of the testing machine. Thus, the ends of the test specimen can be considered to be restrained against rotation. All the tests were conducted under stroke control.

Tension coupon tests were conducted to determine the material properties and the true stress-true plastic strain relationship of the steel to be used in analyzing the experimental results and carrying out the finite element analysis parametric study. However, the material properties of the weld were not measured. Test results were analyzed and studied using the actual measured mechanical properties of the tension member (FB 76 x 4.8). Results from the tests are combined with the experimental results from other research to study the performance of the welded plate tension members and compare it to the provisions in the current design specifications.

A finite element analysis parametric study was conducted using ABAQUS (Simulia, 2008) finite element software package. In the parametric study, the weld length to plate width ratio (L/w) varied from 0.5 to 3.0 and the weld size to plate thickness ratio (s/t) varied from 0.5 to 1.0. Results from the numerical analyses were calibrated with those from the tests. Effects of the weld length and weld size ratios were assessed using results of the parametric study. Based on the study, equations were developed to predict the capacity (efficiency) of the welded plate tension members in terms of the weld length and weld size ratios.

1.3 Organization of the thesis

Chapter 2 consists of a brief literature review of past research conducted experimentally and numerically in both welded and bolted steel tension members. Factors that have been found to affect the performance of tension members are discussed.

Chapter 3 outlines the experimental program along with the equipment used and the methodologies followed in conducting the tests on the welded plate specimens and the tension coupons.

Chapter 4 presents the experimental results of all the specimens tested. The test results are combined with the experimental results from other research to study the behaviour of welded plate specimens. In the process, a tentative relationship is developed between the performance of the tension member and the weld size.

Chapter 5 describes the finite element analysis parametric study, and the procedure used in calibrating the analysis results with those from the tests. Results from the numerical analyses were analyzed and equations were developed to predict the performance of the welded tension members due to the combined effect of the leg size and length of the weld.

Chapter 6 provides conclusions of the research along with the recommendations for future studies in this area. Additional results from the tests and numerical analyses are presented in the appendices.

Figure 1.1 Plate tension member longitudinally welded only along two parallel edges

CHAPTER 2 LITERATURE REVIEW

Properly designed connections are essential to the safety and the stability of steel structures. When designing steel tension members, the effect of shear lag on the cross-section has a dramatic impact on the overall performance of that structural member as it causes a higher stress concentration around the connection. This may cause the member to fail prior to reaching the full capacity of the cross-section. Over the course of the last century, extensive research has been carried out in this particular field in order to ensure that designs perform up to the established standards, but also to improve on their efficiency. Recent research on the topic has shifted towards gaining an even greater understanding of all the variables that affect the performance of tension members in order to accurately predict their behaviour.

However, the effect of all the factors that impact the performance of steel tension members, such as the combination effect of the length and size of the weld, has not been sufficiently explored. Furthermore, the existing design provisions are not as accurate or as efficient in all cases, and improvements could be made. This chapter will be examining past research in the design of steel tension members, and the effect of shear lag on the performance of tension members along with factors that affect the performance of steel tension members, such as the length and size of the weld.

2.1 Design of tension members and connections

In steel design, there are three types of connection methods used: bolts, rivets, and welds, and each have their own design and fabrication challenges. Rivets are fasteners

which consist of a smooth cylindrical shaft and a head. They are fitted tightly to punched or drilled holes, and the rivets are forced through where the tail of the connector expands to secure the connection. Bolted connections involve a threaded metal rod attached to a head, and it is fastened to the connection using high strength nuts. Bolts have been more commonly used than rivets.

Despite the higher costs associated with high strength bolts compared to rivets, they are still preferred given that the installation cost of rivets is higher and bolts have greater load carrying capacity. Welding involves the joining of two metal pieces through the use of a filler and intense heat, which fuses the pieces together. The use of either bolted or welded connection depends on where the fabrication process takes place. If the structural member needs to be installed on-site, then the use of high strength bolts is preferred due to their ease of installation over welding. Furthermore, there is a lack of a controlled environment for welding on-site. However, if the structural members are being fabricated in the shop, then welding becomes the preferred method.

2.1.1 Canadian design provisions

In tension members as per the Canadian Standard CAN/CSA-S16-01 (2005), the effective resistance is to be determined by the lesser of

$$T_r = \phi A_g F_y, \quad (2.1)$$

$$T_r = 0.85\phi A_n F_u, \text{ and} \quad (2.2)$$

$$T_r = 0.85\phi A_{ne} F_u \quad (2.3)$$

where

$$\phi = 0.90,$$

- A_g = gross cross-sectional area,
 F_y = yield strength of the steel,
 A_n = net cross-sectional area,
 F_u = ultimate strength of the steel, and
 A_{ne} = effective net cross-sectional area reduced due to shear lag.

The first expression accounts for the case where the gross cross-section has yielded, which causes excessive elongation of the member. The second and third equations account for the fracture of the cross-section near or at the connection. The main difference between these two lies in the reduced net effective cross-sectional area that takes into account the effect of shear lag on the connection. Unlike bolted tension members, structural members connected using welds do not lose any of their gross area since there is no need for drilled or punched holes. Past research conducted by Davis and Boomsliiter (1934) that compared the performance of welded connections to bolted connections has shown that the increased ultimate strength of welded tension members is in proportion to the higher effective cross-sectional area. In CAN/CSA-S16-09 (2009), Eqs. (2.2) and (2.3) are replaced with

$$T_r = \phi_u \left[U_t A_n F_u + 0.6 A_{gv} \frac{(F_y + F_u)}{2} \right], \text{ and} \quad (2.4)$$

$$T_r = \phi_u A_{ne} F_u \quad (2.5)$$

where

- ϕ_u = 0.75
 U_t = efficiency factor
 A_{gv} = gross area in shear.

Equation (2.5) in CAN/CSA-S16-09 is identical to Eq. (2.3) in CAN/CSA-S16-01 for a tension plate longitudinally welded only with fillet welds along two edges, the tensile resistance for the steel tension members will be the smallest value obtained from Eqs. (2.1) and (2.3) for CAN/CSA-S16-01 and from Eqs. (2.1) and (2.5) for CAN/CSA-S16-09.

In welded connections, the effective net area, A_{ne} , is calculated using

$$A_{ne} = A_{n1} + A_{n2} + A_{n3} \quad (2.6)$$

where

A_{n1} = area of the elements directly connected to the transverse welds,

A_{n2} = area of elements connected along two parallel edges, and

A_{n3} = area of elements connected by a single longitudinal weld.

A_{n2} is dependent on the length of the weld in relation to the element width, while A_{n3} is dependent on the connection eccentricity and the length of the weld. The expression for the effective area of a tension member connected by a transverse weld is given by

$$A_{n1} = wt \quad (2.7)$$

where w is the width and t is the thickness of the element. Equation (2.7) shows that there is no reduction in the effective area of a cross-section of an element that is connected with a weld that is perpendicular to the direction of loading. When the element is connected by two parallel welds, the reduced effective area due to shear lag is dependent on the relationship between the length of the weld and the width of the element that can be expressed as

$$A_{n2} = 1.00wt \quad \text{for } L \geq 2w, \quad (2.8)$$

$$A_{n2} = 0.50wt + 0.25Lt \quad \text{for } 2w > L > w, \quad (2.9)$$

$$A_{n2} = 0.75Lt \quad \text{for } w > L \quad (2.10)$$

where L is the length of the weld in the longitudinal direction. As shown in Eqs. (2.8) to (2.10), the effective area is dependent on the weld length relative to the width of the element. By providing at least twice the cross-sectional width in weld length, the entire cross-section can be considered to be effective at resisting the tensile load. Equations (2.9) and (2.10) show that as the length of the weld decreases, the effective cross-sectional area that is effective at resisting the tensile load reduces.

The reduced effective area for elements connected by a single longitudinal weld can be calculated using

$$A_{n3} = \left(1 - \frac{\bar{x}}{L}\right) \quad \text{for } L \geq w, \text{ and} \quad (2.11)$$

$$A_{n3} = 0.50Lt \quad \text{for } w > L \quad (2.12)$$

where $\bar{x}$ is the eccentricity of the outstanding leg. The expression in Eq. (2.11) is similar to the factor proposed by Chesson and Munse (1963) based on the experimental study on bolted and riveted tension members.

2.1.2 American design provisions

The American specifications for the design of steel tension members have similar provisions to account for the effect of shear lag. In the AISC Specification for Structural Steel Building ANSI/AISC 360-05 (AISC 2005), the effective net area for a cross-section connected by welds may have to be reduced. The reduction factor depends on factors such as the length of weld in relation to the distance between the welds, and the type of

cross-section that is under tension. For welded connections, the effective tensile resistance of a steel member is governed by

$$P_n = \phi_t F_y A_g, \text{ and} \quad (2.13)$$

$$P_n = \phi_n F_u A_e \quad (2.14)$$

where

A_e = reduced effective area of the cross-section due to shear lag,

ϕ_t = resistance factor of 0.9 for Eqs. (2.13), and

ϕ_n = resistance factor of 0.75 for Eqs. (2.14).

Similar to CAN/CSA-S16-01 (2005), two possible failure modes exist for tension members of either through yielding of the gross cross-section or rupture of the net section according to Eqs. (2.13) and (2.14) respectively. The reduced effective area of the cross-section can be written as

$$A_e = UA_n \quad (2.15)$$

where U is the shear lag factor. For plates where the tension load is transmitted by two longitudinal welds only, the shear lag factor is given by

$$U = 1.00 \quad \text{for } L \geq 2w, \quad (2.16)$$

$$U = 0.87 \quad \text{for } 2w \geq L \geq 1.5w, \text{ and} \quad (2.17)$$

$$U = 0.75 \quad \text{for } 1.5w > L \geq w. \quad (2.18)$$

2.1.3 Weld design

Failure of the fillet weld occurs due to shear. There are two possible failure modes involving the longitudinal fillet weld. The first case involves the shear failure of

the base metal, and the other case being the shear failure of the weld metal. The Canadian specification CAN/CSA S16-09 (2009) expresses these two scenarios using

$$V_r = 0.67\phi_w A_m F_u \quad \text{for the base metal failure, and} \quad (2.19)$$

$$V_r = 0.67\phi_w A_w X_u \quad \text{for the weld metal failure} \quad (2.20)$$

where

$$\phi_w = 0.67,$$

$$A_m = \text{area of effective fusion face,}$$

$$A_w = \text{area of effective weld throat,}$$

$$X_u = \text{electrode ultimate strength, and}$$

The lesser of Eqs. (2.19) and (2.20) will govern the design capacity for the welded connection. The area of the effective fusion face is normally taken as the weld leg size multiplied by the length of the weld, while the area of the effective weld throat is taken as the weld leg size over the square root of two, multiplied by the weld length.

2.2 Shear lag

Shear lag is one of the many factors that affect the performance of tension members. Shear lag is defined as the non-uniform stress distribution in tension members that is observed adjacent to the connection where not all elements of the cross section are directly connected as shown in Fig. 2.1. The effects of shear lag are associated with all types of cross-sections, where the elements that are directly attached to the connection are immediately effective while elements not directly connected lag behind in the overall strength contribution. The higher stress concentration adjacent to the connection causes a reduction in the overall strength of the tension member. Through the years, many

experiments and analyses have been carried out on various structural members and connections in order to gain a better understanding on the effect of shear lag and to develop a more efficient design guideline that is more representative of their performance. Many of the current design provisions are derived from studies and design guidelines that were developed by Chesson and Munse (1963). Chesson and Munse performed an extensive review of past experiments to go along with their own experiments to carefully look at various factors that affect the performance of bolted and riveted tension members. It was observed that localized yielding may occur, particularly around the connection, while stresses in other parts of the cross-section would be below the yield level of steel. For many of these sections, failure would occur at the net section. Based on the data obtained from strain gauges installed at various locations along the member, unequal straining in the webs and the outstanding angle legs were observed. The further away from the connection, the more uniform the stress distribution is over the cross-section. It was found the effect of shear lag to be negligible at a distance greater than 3 times the member width.

When evaluating the various specimens tested, the main basis for comparison was the net section efficiency. The net section efficiency can be expressed as

$$U_n = \frac{P_{\text{utest}}}{A_n F_u} \quad (2.21)$$

where

P_{utest} = maximum test load,

A_n = net cross-sectional area of the tension member, and

F_u = ultimate strength of the steel.

Chesson and Munse observed that efficiencies were rarely greater than 90%. Factors that were found to impact the efficiency of tension members were the ductility of the fastener and the connected material, the length of the connection, the fastener pattern, and the geometry of the cross-section relative to the bolt layout. Only some of the studies on the effect of shear lag will be briefly discussed in the following sections, with an emphasis on welded tension members.

2.2.1 Bolted and riveted connections

The initial experiments were initiated due to the failures of tension members across the United States. Chesson and Munse (1963) tested various configurations of tension members that included plates, angles, rolled I-shaped members, and various built-up cross-sections. For I-shaped members, it was found that the greater quantity of material concentrated in the web (the non-connected part of the tension member), the lower the efficiency will be. It was also found that increasing the connection length increases the efficiency of the tension member. Chesson and Munse observed that failure would first occur in the flanges at the net section in the first row of bolts, where the crack would then propagate completely across the entire member. This crack would eventually move into the web. Similar observations were made with regards to steel angles and their “webs”, the non connected portion of the member, where it was found that the outstanding leg was not fully effective in resisting the load.

Based on the tests conducted by Chesson and Munse, along with a review of previous tests conducted with bolted or riveted tension members, Chesson and Munse observed that the strength of the members decreased with an increasing eccentricity ($\bar{x}$),

but increased with an increasing connection length (L). Therefore, a simple relationship known as the shear lag factor to account for the effect of shear lag on the efficiency of tension members was developed. This relationship can be expressed as

$$U = \left(1 - \frac{\bar{x}}{L}\right) \quad (2.22)$$

The eccentricity of the connection is defined as the distance from the face of the gusset plate to the centroid of the section under loading. In the case of doubly symmetric cross-sections, the eccentricity is defined as the distance from the face of the gusset plate to the center of gravity of the half section, as shown in Fig. 2.2. The shear lag factor acts as a reduction factor that is multiplied to the net area to form the effective net area of a tension member.

$$A_{ne} = \left(1 - \frac{\bar{x}}{L}\right) A_n \quad (2.23)$$

2.2.2 Welded Connections

One of the first major studies carried out in the area of welded tension members was conducted in 1931 by the American Welding Society (AWS 1931). A large number of flat bar specimens consisting of one or two plates welded to the gusset plates were tested, including the configuration with only parallel longitudinal welds. The flat bar specimens had varying configurations with the weld length ratio (L/w) ranging from 0.7 to 1.6, and weld size ratio (s/t) ranging from 0.5 to 1.0, where s is the weld leg size. The purpose of that study was to determine the safe working stresses for tension members. However, the L/w and s/t ratios were not treated as independent parameters in the investigation, as there were no instances where the s/t ratio was held constant while the L/w ratio varied, and vice versa. Even when these two parameters had been considered

separately, different cross-section sizes were used for different L/w ratios. The s/t ratio of 1.0 was used for all specimens with an L/w ratio below 1.0, and a decreasing s/t ratio with a higher L/w ratio at 1.0 and above. For this reason, it was difficult to precisely quantify the effect of s/t and L/w ratios on the connection efficiency from the results of these tests. Nevertheless, the capacity was found to be affected by both L/w and s/t ratios. Most of the specimens failed along the throat of the weld, and with a few exceptions that failed in the plate. With the exception of a selected few, most of the specimens achieved the efficiency of less than 85%.

Tests on welded steel angles were conducted by Davis and Boomsitter (1934). Based on the data obtained from the strain gauges, the stress distribution was found to be non-uniform along the cross-section. Tests performed by Gibson and Wake (1942) confirmed the presence of non-uniform stress, which caused the member to fracture prior to reaching its full strength. It was also observed that the weld became the critical element susceptible to failure when the length of the weld was less than the distance between the two parallel welds, which corresponds to an L/w ratio of less than 1.0. Easterling and Giroux (1993) conducted a small number of tests on plates welded only along two longitudinal edges. The weld size s/t ratio remained fixed at 1.0 while the weld length L/w ratio varied from 1.4, and 1.7. Specimens at both L/w ratios achieved a similar average efficiency of 97%. This indicates that there is very little strength improvement to be gained as the length of the weld is increased above 1.4 times the width of the cross-section. For specimens welded transversely along the full width of the plate in addition to the longitudinal welds, Easterling and Giroux found the full efficiency of the plate was achieved. Besides flat bars, Easterling and Giroux also investigated

welded angle and channel connections. In all cases, a high stress concentration was found near the connection.

Abi-Saad and Bauer (2006) analytically studied the effect of shear lag in welded tension members. A series of equations were developed to account for shear lag in various weld configurations and structural shapes. For elements connected by longitudinal welds along two parallel edges, the effective net area is given by

$$A_{n2} = 1.00 \quad \text{for } L \geq \frac{w}{2 \tan \theta}, \text{ and} \quad (2.24)$$

$$A_{n2} = 2(\tan \theta) Lt \quad \text{for } \frac{w}{2 \tan \theta} > L \quad (2.25)$$

with θ taken as 30° . However, Eqs. (2.24) and (2.25) have not been compared to test results of welded plates with an L/w ratio less than 1.4. There are many other experimental and analytical studies on welded tension members that have been conducted. Some of these include studies on welded angles by Uzoegbo (1998), and on slotted tubular members by Korol et al. (1994), Martinez-Saucedo et al. (2006) and Zhao et al. (2009).

2.3 Numerical simulation for welded tension members

There have been numerous numerical simulations conducted on welded connections using finite element methods in an attempt to study the behaviour of steel tension members and the effects of shear lag. Numerical simulation provides a cost effective way to conduct a large number of numerical tests to study various parameters, as opposed to conducting physical experiments.

Easterling and Giroux (1993) combined the physical testing with the numerical simulation using ANSYS. Models of all the specimens tested in the laboratory (plates, angles, channels) were developed using 4-node isoparametric plane stress elements for the plates, and 3D shell elements for the channels and angles. The purpose of the numerical simulation was to compare the stresses and displacements of the finite element models to those of the physical experiments. The analysis was confined to the linear elastic behaviour of the members only. The weld was not discretized for the analysis, but was modelled as an elastic spring. The determination of the spring constant was carried out through trial and error where many models were analyzed with different weld stiffnesses until the results from the analysis provided the closest correlation to the test data. The comparison of the results from the test and simulation indicated that the model accurately predicted the stresses at the middle of the cross-section near the connection, however it underestimated the stresses near the weld.

Girard et al. (1996) developed a finite element model to study the effects of shear lag on welded HSS sections. The cross-section was discretized using 6 node triangular elastoplastic thin shell elements, however the HSS corner was not explicitly modelled. Similar to the previous study, the deformation and failure of the weld were not considered in the analysis. The analysis only used a very simple bi-linear stress-strain curve to model the material behaviour. It was also mainly restricted to the linear elastic range with a limited linear isotropic strain-hardening. However, an artificial displacement limit was imposed in their simulations. Consequently, the numerical analyses were unable to accurately predict the behaviour of the tension member. Martinez-Saucedo et al. (2006) conducted non-linear numerical simulations using the true

stress-true strain curve to describe the behaviour of the circular hollow section (CHS), the gusset plate, and the weld. The weld was also discretized in this study using 3D solid elements, and was assigned the same material properties as the CHS. The fracture limit was obtained by trial and error until the numerical simulation equivalent plastic strain replicated the test results. The simulation also used the “death feature” of the ANSYS software to model crack propagation. Once the strain within an element had exceeded the equivalent plastic strain limit, the “death feature” eliminates that element and causes the load to be redistributed to the rest of the member. The finite element analysis was able to replicate the shear lag phenomenon, and the associated higher stresses and. However, since the weld was oversized compared to the CHS thickness, none of the failure was along the weld.

In the study of HSS sections by Zhao et al. (2009), the analysis was carried out using a combination of 3D shell and solid elements. Most of the model was constructed using shell elements. However, the critical region of the cross-section where failure was expected to occur was modeled with solid elements in order to provide a more accurate modeling, since solid elements can better capture necking and strain localization. Due to the weld being oversized, it was assumed that failure would occur in the HSS. Therefore, the weld was modelled as a rigid beam element connecting the nodes in the HSS section and the gusset plate. The numerical simulation was carried out using the true stress-true plastic strain curves and the critical equivalent plastic strains of the HSS and gusset plates, determined from the tension coupon tests. In general, it was found that the shape of the material stress-strain curve has very little effect on the net section efficiency. The

numerical simulation was able to give a reasonably good prediction of the failure mode and the crack path of the member.

Figure 2.1 Shear lag in a steel tension member [adopted from Abi-Saad et al. (2006)]

Figure 2.2 Eccentricity of a steel angle and doubly symmetric section [adopted from Kulak et al. (1987)]

CHAPTER 3 TESTING PROGRAM

From the literature review on tests conducted by the American Welding Society (AWS 1931), and Easterling and Giroux (1993), it was found that there is a need for a more systematic study on the tension capacity of steel plates longitudinally welded along two parallel edges due to the effects of the weld length (L/w) and the weld size (s/t) ratios. To this end, one plate cross-section size was used in this experimental program with L/w and s/t ratios being treated as independent parameters to be investigated. In tests conducted by the American Welding Society (AWS 1931), the single plate specimen was found to have a lower capacity than a double plate specimen. Thus, both single and double plate configurations were also tested in this experimental program. This chapter will provide in detail the test setup, procedure, equipment, and specimen that were used in this testing program.

3.1 Objective

The objective of the testing program is to determine the efficiency of single and double plate tension members longitudinally welded only along parallel edges under monotonic tension loading. These tests were conducted to investigate the effects of the weld length and size ratios on the performance of the tension members. The weld length ratio (L/w) being the ratio of the longitudinal weld length (L) over the plate width (w) and the weld size ratio (s/t) being the ratio of the fillet weld leg size (s) to the plate thickness (t). Results from the tests will also be used to calibrate the models and results of the finite element analysis parametric study.

3.2 Specimen detail

The specimens were divided into two groups. One group consists of specimens fabricated as a single plate while the other group consists of two plates, as shown in Figs. 3.1 and 3.2. Flat bars were used to fabricate the specimens. Six weld configurations were considered for each group. There were a total of twelve specimens in each group, with two specimens for each configuration. All single plate specimens were fabricated with FB 76 x 4.8 mm (3" x 3/16") and connected to FB 102 x 7.9 mm (4" x 5/16") gusset plates, while the double plate specimens were fabricated with FB 76 x 4.8 mm connected to FB 102 x 12.7 mm (4" x 1/2") gusset plates. All flat bars were CAN/CSA G40-20/21 grade 300W steel.

The six weld configurations consist of weld length ratios (L/w) of 0.5, 1.0, 1.5 and 2.0 with the weld size ratio (s/t) at 1.0, and weld length ratios of 1.0 and 2.0 with a weld size of 3 mm or weld size ratio of 0.63. The clear span between the gusset plates was kept constant at approximately 375 mm in order to minimize the non-uniform stress distribution at one end from affecting the other. Screws were welded at the ends of the gusset plates to allow for the placement of linear variable displacement transducers (LVDTs) to measure the deformation of the specimen. The nominal dimensions and the configurations of the specimen tested are provided in Table 3.1.

3.3 Specimen measurement and designation

The average measured dimensions of all specimens FB 76 x 4.8 along with the average weld dimensions are provided in Table 3.2. All specimen designations as shown in Table 3.2 are in the form $\alpha\beta\beta aR$, where α is s for single plate specimens or d for

double plate specimens, and β is 05, 10, 15, or 20 for weld length (L/w) ratios of 0.5, 1.0, 1.5, and 2.0. The parameter "a" is only added if the specimen has a partial leg size with the weld size ratio (s/t) of 0.63, while "R" represents the duplicate specimen of each configuration. Thus, a double plate specimen with the weld size ratios (s/t) of 1.0 and a weld length ratio (L/w) of 1.0 is designated as d10, and its duplicate is designated as d10R. For the specimen with the same configuration as d10 but with a smaller weld size, it is designated as d10a.

Due to a fabrication error, the clear span of specimen s20R was longer than other specimens with the same weld configuration. However, this error was not expected to have a significant impact on the results. Therefore, no additional specimen of that configuration was fabricated to match the nominal dimensions of the specimen. Detailed measurements of all the specimens are provided in Appendix A.

3.4 Test setup and instrumentation

The test setup and instrumentation for the specimens are shown by Figs 3.3 and 3.4. The tests for both the specimens and tension coupons were carried out in the 500 kN MTS810 material testing machine. Both ends of the gusset plates were gripped tightly by the MTS 647 hydraulic wedge grip, leaving approximately 50 mm spacing between the grip and the plates at both ends. Thus, both ends of the specimens can be considered to be restrained against rotation. As shown in Fig. 3.3, each specimen had three strain gauges installed, with two along the section where the longitudinal welds end, and another one at the mid-span. Two LVDTs were also installed on each side of the specimen.

3.5 Test procedure

Variable loading rates were used depending on the stage where the test was at. A stroke rate of 1 mm per minute was used from the beginning of the test until the peak load was reached. After the peak load, the stroke rate was increased to 2 mm per minute until failure of the specimen. The machine stroke, load, strain gauge and LVDT readings were recorded automatically at every five seconds for specimen d20R. However, that was changed to one reading every second to ensure that a sufficient amount of data would be recorded for the remaining specimens, especially those that were expected to fail at a lower deformation. At regular intervals, the loading was paused for approximately 20 seconds to allow for static readings to be taken.

The frequency at which the static readings were recorded was dependent on the size of the member and the weld. For all specimens with the exception of those with an L/w ratio of 0.5 and specimens with an L/w ratio of 1.0 combined with an s/t ratio of 0.63, the first static reading was taken at the onset of yielding, and subsequently at every 3.0 mm stroke. For the other specimens, the first static reading was taken at the point where the load-deformation curve was observed to deviate from a straight line, and every 1.5 mm stroke displacement thereafter to ensure that a sufficient number of static readings could be obtained prior to the failure along the weld.

3.6 Material properties

Tension coupons were fabricated and tested in accordance to ASTM-E8-04 (ASTM 2004) to obtain the material properties of the specimens to evaluate the test results and to carry out the finite element analyses. Rectangular coupons were cut

longitudinally from the flat bars. Four coupons were tested for each flat bar. Tension coupon tests for FB 76 x 4.8 and FB 102 x 12.7 were conducted. Tests for FB 102 x 7.9 were carried out by another student in a separate experimental study, and were not repeated.

A 50.8 mm gauge length axial extensometer was used in all tests to measure the longitudinal strain until necking occurred, after which it was removed in order to measure the change in cross-sectional dimensions needed in establishing the true stress versus true strain relationship of the steel. Of the four coupons for each material, two had the loading controlled by the strain rate of the axial extensometer and the other two by the stroke rate of the machine. A strain rate of 10^{-4}s^{-1} and the stroke rate of 0.00508 mm/s were used respectively with the axial extensometer control and stroke control.

The recording frequency was ten readings per second during the elastic phase of the test, and four readings per second after yielding had started. The data collected by the data acquisition system consisted of stroke, load, and strain. The loading was paused at regular intervals for approximately 45 to 60 seconds in order to take the static readings and cross-sectional measurements of the coupon. The first static reading was taken at the onset of yielding, and with three subsequent readings taken within the yield plateau region. This was followed by another measurement at the onset of strain hardening. Between the start of strain hardening and the peak load, static readings were taken at approximately every 1.5% strain. After the peak load, static readings were taken at about every 1.0% strain.

Due to the presence of the extensometer, great care had to be taken when measuring the cross-section with a caliper without disturbing the extensometer. Once

necking was observed on the coupon, the loading was switched from axial extensometer control to stroke control for the test with the axial extensometer control. At this point, the extensometer was removed to allow the cross-sectional dimensions to be measured precisely using a micrometer. Two measurements for both the width and the thickness as shown by Fig. 3.5 were taken. One measurement was at the edge of the cross-section and the other at the mid-point of the sides. After failure, the final elongation of the coupon over a 50.8 mm gauge length was measured.

Table 3.1 Nominal dimensions of test specimens

Config- uration	Type	Specimen	Weld size (s) (mm)	Gusset plate			Flat bar (FB 76 x 4.8)	
				Width (mm)	Length (L _G) (mm)	Thickness (t _G) (mm)	Length (L _s) (mm)	Weld length (L) (mm)
1	Single	s05	5	101.6	173	7.9	461	38
2	Single	s10	5	101.6	211	7.9	537	76
3	Single	s15	5	101.6	249	7.9	614	114
4	Single	s20	5	101.6	287	7.9	690	152
5	Double	d05	5	101.6	173	12.7	461	38
6	Double	d10	5	101.6	211	12.7	537	76
7	Double	d15	5	101.6	249	12.7	614	114
8	Double	d20	5	101.6	287	12.7	690	152
9	Single	s10a	3	101.6	211	7.9	537	76
10	Single	s20a	3	101.6	287	7.9	690	152
11	Double	d10a	3	101.6	211	12.7	537	76
12	Double	d20a	3	101.6	287	12.7	690	152

Table 3.2 Average dimensions of specimens

Specimen	Width (w) (mm)	Thickness (t) (mm)	Length (L _s) (mm)	Weld size (s) (mm)	Weld length (L) (mm)
s05	76.3	4.7	460	5.1	39.6
s05R	76.4	4.7	462	5.2	39.9
s10	76.4	4.7	536	5.6	75.8
s10R	76.3	4.7	538	5.4	76.0
s10a	76.1	4.7	537	3.0	77.1
s10aR	76.2	4.8	537	3.2	75.5
s15	75.8	4.7	614	5.1	114.8
s15R	76.5	4.7	613	5.3	115.1
s20	76.5	4.7	690	5.1	151.9
s20R	76.4	4.7	738	4.8	153.6
s20a	76.3	4.8	690	3.2	152.9
s20aR	76.3	4.7	690	3.4	152.2
d05	76.4	4.6	462	5.1	38.1
d05R	76.3	4.7	462	5.0	38.3
d10	76.4	4.7	537	5.3	79.5
d10R	76.5	4.7	537	5.2	78.8
d10a	76.2	4.6	536	2.9	77.4
d10aR	76.3	4.7	690	3.2	76.4
d15	76.2	4.7	615	5.1	116.1
d15R	76.3	4.7	614	5.1	116.6
d20	76.3	5.1	690	5.1	151.9
d20R	76.3	4.7	689	5.3	152.9
d20a	76.2	4.6	691	2.8	151.1
d20aR	76.3	4.7	690	3.0	151.3

Figure 3.1 Isometric view of a welded plate specimen

Figure 3.2 Top and side view of a welded plate specimen

Figure 3.3 Location of strain gauges

Figure 3.4 Test setup

Figure 3.5 Cross-sectional measurements of tension coupons

CHAPTER 4 EXPERIMENTAL RESULTS

Following the procedures outlined in Chapter 3, tension coupons and welded specimens were tested under quasi-static loading. The specimens were investigated for the net section efficiency, failure mode and general behaviour with respect to the weld length and size. In the following discussions, both the weld length and size are expressed in the non-dimensionalized form as the L/w and s/t ratios.

4.1 Material tests

Results from the coupon tests are summarized in Table 4.1. It has been observed that when the machine was put on pause to take static readings, the load drop was higher in tests where the loading is controlled using the axial extensometer strain compared to that controlled using the stroke displacement. Since stroke control was used in testing of the welded specimens, a correction has been applied to static readings from coupons tested using axial extensometer strain control using the ratio of the load drop at the static readings after switching to stroke control over the load drop prior to the switch. This correction converts the static readings from the tests that used extensometer control to those equivalent to stroke control. The material engineering stress versus strain curves of the gusset plates and flat bar along with a more detailed explanation of the correction procedure employed to convert the static readings from the axial extensometer control tests are provided in Appendix B. Table 4.1 shows the material properties based on the static readings from tests with the stroke control method of loading along with those

which had the axial extensometer control method of loading with the values converted to the equivalent of the stroke control method of loading.

4.2 Specimen test results

As mentioned in Chapter 2, the performance of a tension member can be defined by the efficiency expressed by Eq. (2.21). Given that there is no loss in cross-sectional area for welded connections, the net cross-sectional area is equal to the gross cross-sectional area. Therefore, the test efficiency can be rewritten as

$$U_{te} = \frac{P_{utest}}{A_g F_u} \quad (4.1)$$

where P_{utest} is the static maximum load attained in the test, A_g is the measured gross cross-section area of the test specimen, and F_u is the static ultimate tensile strength obtained from the material test. Even though the capacity of some of the test specimens was governed by weld failure, the weld efficiency was not calculated and used in the data analysis. Instead, the capacity of those specimens that experienced weld failure was calculated as an equivalent plate tension efficiency so that the capacity of the tension member can be studied in terms of the L/w ratio.

The measured efficiencies of all the specimens tested were calculated, and those results are summarized in Figure 4.1, and along with the LVDT displacement at the peak load in Table 4.2. Additional test data are shown in Appendix B. Equations from CAN/CSA-S16-09 (CSA 2009) and ANSI/AISC 360-05 (2005) for the tension member and Eq. (2.19) for weld failure along the fusion surface are also shown in Fig. 4.1 without the resistance factor. The resistance factor is applied to the weld strength Eq. (2.19) to improve upon the design safety margin. Therefore, it is removed when calculating the

actual weld strength. Since the failure observed was along the fusion surface, only values from the base metal failure equation are shown on Fig. 4.1. Results of the tests are compared to the net section efficiency prescribed by both the Canadian and American specifications. When the full weld size was provided ($s/t = 1.0$), both CSA-S16-09 and ANSI/AISC 360-05 significantly underestimate the efficiency at an L/w ratio less than or equal to 1.5. The underestimation is about 20% at an L/w of 1.0 and about 40% at 0.5. It can also be observed that there is no significant improvement in the efficiency when the length of the weld is greater than the width of the cross-section, whereas the provisions in both Canadian and American design specifications predict that there is a considerable efficiency improvement from an L/w ratio of 1.0 to 2.0. According to the design provisions, the full efficiency can be achieved at an L/w ratio greater than or equal to 2.0. However, the maximum efficiency reached by any of the specimens was only 97%. Based on the comparisons with the test results, it shows that there is room to improve on Eqs. (2.8) to (2.10) and (2.16) to (2.18) in the design specifications.

Conversely, provisions in both CSA-S16-09 and ANSI/AISC 360-05 over predict the capacity of specimens with a partial weld size ($s/t = 0.63$). The capacity is overestimated by about 10 % and 15% at L/w ratios of 1.0 and 2.0 respectively. Plotting Eq. (2.19) using the measured or specified ultimate strength of the tension member (FB 76 x 4.8), it can be seen in Fig. 4.1 that the capacity of every specimen was below that for the base metal weld failure. In the current tests, all specimens either fractured transversely across the tension member or along the fusion surface of either the tension member or the gusset plate as indicated in Table 4.1. Thus, it can be assumed that weld metal failure did not occur. On the other hand, fracture occurred in the weld metal along

the minimum throat of the fillet weld in the tests by AWS (1931). Nevertheless, results of the tests show that shear lag has an effect on the net section efficiency regardless of whether fracture was along the weld or transversely across the tension member.

4.2.1 Double plate specimens – full weld size

The load versus deformation relationship of the double plate specimens with the full weld size can be seen in Fig. 4.2. As shown in Figs. 4.1 and 4.2, there are only minor differences in the ultimate load and the displacement at peak load for the tension members with an s/t ratio equal to 1.0 and an L/w ratio ranging from 1.0 to 2.0. Furthermore, there is no improvement in the net section efficiency at an L/w ratio greater than 1.5. On the other hand, specimens with an L/w of 0.5 have a considerably different capacity where it can be seen that their efficiency is significantly lower than specimens with an L/w ratio of 1.0. Specimens with an L/w ratio greater than or equal to 1.0 experienced net section fracture of the tension member near the weld, as shown in Fig. 4.3, while specimens at the L/w of 0.5 experienced shear failure of the longitudinal weld along the fusion surface of the gusset plate, as shown in Fig. 4.4. However, the weld failure may have been initiated at the fusion surface of the tension member as there was a small crack in the tension member at the end of the weld.

4.2.2 Partial weld size specimens

The load versus deformation curves of both full and partial weld size double plate specimens are shown in Fig. 4.5. It can be seen that both full and partial weld size specimens have the same load versus deformation curve until the partial weld size

specimens failed. Taking the average for each weld configuration, Table 4.3 shows the comparison of the average test efficiency between full weld size and partial weld size specimens. Compared to specimens with the full weld size, specimens with the partial weld size had a lower efficiency. The reduction was found to be around 25% at an L/w ratio of 1.0 and 10% at an L/w ratio of 2.0. Specimens with an L/w ratio of 2.0 fractured across the tension member similar to the full weld size specimens with an L/w greater than or equal to 1.0. On the other hand, specimens with an L/w equal to 1.0 and an s/t equal to 0.63 experienced shear failure of the longitudinal weld along the fusion surface of the tension member. Figure 4.6 shows the weld failure of specimen d10a along the fusion surface of the tension member. Results of the tests indicate that it is essential to consider the effect of the weld leg size on the net section efficiency of tension members.

4.2.3 Single plate specimens

Unlike tests by AWS (1931), the performance of single plate specimens was found to be almost the same as double plate specimens, as can be seen in Fig. 4.1 and Table 4.4. In the current tests, the specimen ends were restrained against rotation. However, there may be a more noticeable difference in the performance of the single and double plate specimens if the ends are pinned connected. Nevertheless, this was not investigated in the current study. Figure 4.7 also shows that the differences in the peak average stress and displacement at the peak load are small between the double and single plate specimens. The single plate specimens also exhibited similar mode of failure as the double plate specimens. For the full weld size specimens, fracture occurred across the tension member with the weld length ratio (L/w) of 1.0 or greater, and fractured along the

fusion surface of the tension member with the weld length ratio of 0.5. With a partial weld size ($s/t = 0.63$), specimens having an L/w ratio of 2.0 fractured across the tension member, while those having an L/w ratio of 1.0 fractured along the fusion surface on the tension member.

4.3 Strain distribution

The effect of shear lag can be assessed based on the strain distribution across the section near the connection. Comparisons of the strain readings for single plate specimens are shown in Figs 4.8 and 4.9, and the layout of strain gauges shown in Fig. 3.3. For the full weld size single plate specimens, it can be seen in Fig. 4.8 that the strain for s05R closer to the edge of the plate at G3 is much larger than at the middle of the plate at G2. This indicates that the effect of shear lag may be significant for s05R. On the other hand, there is no clear difference in the strain readings at G2 and G3 for s10R, s15R and s20R, implying that the effect of shear lag is small. This is reflected in the small difference in the test efficiency when an L/w ratio was higher than 1.0. Figure 4.9 shows the measured strain of the full and partial weld size single plate specimens at the LVDT displacement of 3 mm prior to the onset of yielding. There is only a small difference in the strain at G2 and G3 for all the specimens even though the failure mode is different between some of the specimens, where s10R fractured across the tension member and s10aR along the weld. Thus, the effect on the strain distribution across the width is more dependent on the weld length than the leg size.

4.4 Analyses of design guidelines

Test results from AWS (1931), and Easterling and Giroux (1993) are included in the analysis to study the performance of the welded plate tension members even though fracture occurred along the minimum throat of the weld in most of the specimens by AWS (1931). In the current test program, the weld leg size is small relative to the width of the tension member. However in the tests by AWS (1931), the weld size was up to 25% of the tension member width. Thus, a modified specimen width is used to calculate the weld length ratio to account for the large weld leg size relative to the tension member width. The modified width is taken as the total width of the tension member plus twice the leg size of the weld

$$w_s = w + 2s \quad (4.1)$$

where s is the weld leg size. Thus, the modified weld length ratio is taken as L/w_s . Data from current tests, results from AWS (1931), and Easterling and Giroux (1993), are shown in Figs. 4.10 and 4.11 with respect to the modified weld length ratio. Figure 4.10 shows results from only full weld size specimens, while Fig. 4.11 shows results from both full and partial weld size specimens. Only results from specimens with the clear span of at least twice the plate width are included. All test data are compared to the predicted net section efficiency given by Eqs. (2.8) to (2.10) from CSA S16-09 (CSA 2009) and Eqs. (2.16) to (2.18) from ANSI/AISC 360-05 (AISC 2005). In Fig. 4.10 for full weld size specimens, it can be seen that Eqs. (2.8) to (2.10) from CSA S16-09 significantly under predict the efficiency for most tests with an L/w_s ratio less than 1.5. A similar trend is observed for Eqs. (2.16) to (2.18) from ANSI/AISC 360-05. When test results of the partial weld size specimens are included, Fig. 4.11 shows that the

efficiency of many partial weld size specimens is lower than the predicted efficiency by CSA S16-09 (2009) and ANSI/AISC 360-05 (2005). The average efficiency for each weld configuration is plotted in Fig. 4.12 to provide a clearer illustration of the difference between the theoretical and actual efficiency of the tests. It is clear that the weld size ratio has a significant effect on the net section efficiency of the tension member. Thus, a modification to the net section efficiency equation that can partly account for the effect of the weld leg size has been developed. The modification could take the form

$$U_m = U_F \left(\frac{L}{w_s} \right) h \left(\frac{L}{w_s}, \frac{s}{t} \right) \quad (4.2)$$

where $U_F(L/w_s)$ is the efficiency of the full weld size tension member and $h(L/w_s, s/t)$ is a function that takes into account the effect of the weld leg size.

The function $h(L/w_s, s/t)$ has to be such that its value is 1.0 when the weld size ratio (s/t) is 1.0. A simple function of $h(s/t)$ taken as

$$h \left(\frac{s}{t} \right) = \left(\frac{s}{t} \right)^c \quad (4.3)$$

with c equal to 0.25 has been found to reduce the scatter of the test results for various weld size ratios. Figure 4.13 and 4.14 show the test efficiency divided by Eq. (4.3). It can be seen in Figs. 4.13 and 4.14 that the modification has reduced the scatter and raised most of the partial weld size specimens test efficiency to be above that predicted by CSA S16-09 (2009) and ANSI/AISC 360-05 (2005). However, more test data from partial weld size specimens are required for the development of a more accurate function to account for the effect of the weld leg size since most of the test data for the partial weld size specimens are clustered around an L/w_s ratio of 1.0. In the absence of a more detailed study, Eq. (4.3) can be applied to the net section efficiency calculation in the

current design provisions for tension members to take into account at least partly the effect due to the weld leg size.

Table 4.1 Material properties

Coupon	A_o/A_f	Elongation (%)	Yield strength (MPa)	Ultimate strength (MPa)	Young's modulus (GPa)
FB 76 x 4.8	2.31	36.1%	328	492	210
FB 102 x 7.9	2.21	33.7%	306	508	200
FB 102 x 12.7	2.41	32.7%	355	560	213

Table 4.2 Test results

Specimen	L/w	s/t	P _{utest} (kN)	Δ _{peak} (mm)	U _{te}	U _{te} / U _{csa}	U _{te} / U _{AISC}	Failure
s05	0.5 ³	1.00 ⁴	95.4	5.3	0.54	1.44	-	weld ¹
s05R	0.5	1.00	86.9	3.3	0.49	1.31	-	weld ¹
s10	1.0	1.00	163.7	34.3	0.92	1.23	1.23	plate
s10R	1.0	1.00	155.2	25.8	0.88	1.17	1.17	plate
s10a	1.0	0.63	123.5	10.9	0.70	0.93	0.93	weld ¹
s10aR	1.0	0.63	123.1	10.6	0.68	0.91	0.91	weld ¹
s15	1.5	1.00	163.1	34.8	0.92	1.06	1.06	plate
s15R	1.5	1.00	172.1	50.7	0.96	1.10	1.11	plate
s20	2.0	1.00	167.1	38.6	0.95	0.95	0.95	plate
s20R	2.0	1.00	166.1	38.5	0.93	0.93	0.93	plate
s20a	2.0	0.63	148.4	20.5	0.82	0.82	0.82	plate
s20aR	2.0	0.63	148.4	20.4	0.84	0.84	0.84	plate
d05	0.5	1.00	183.1	2.1	0.53	1.40	-	weld ²
d05R	0.5	1.00	189.2	2.7	0.54	1.44	-	weld ²
d10	1.0	1.00	329.6	34.8	0.93	1.24	1.24	plate
d10R	1.0	1.00	323.2	31.2	0.91	1.21	1.21	plate
d10a	1.0	0.63	220.3	0.9	0.63	0.85	0.85	weld ¹
d10aR	1.0	0.63	245.7	1.1	0.69	0.92	0.92	weld ¹
d15	1.5	1.00	327.9	36.8	0.94	1.07	1.08	plate
d15R	1.5	1.00	340.3	42.9	0.97	1.11	1.12	plate
d20	2.0	1.00	331.3	34.6	0.94	0.94	0.94	plate
d20R	2.0	1.00	324.4	38.3	0.92	0.92	0.92	plate
d20a	2.0	0.63	309.8	26.0	0.89	0.89	0.89	plate
d20aR	2.0	0.63	301.3	20.4	0.85	0.85	0.85	plate

Notes:

1. Weld failure on the fusion surface of the tension member
2. Weld failure on the fusion surface of the gusset plate
3. Value for L/w ratio calculated based on nominal dimensions
4. Value for s/t ratio calculated based on nominal dimensions

Table 4.3 Comparisons of the average efficiency U_{te} between full and partial weld size tests

Specimen	L/w	Average U_{te}		
		Full weld (a)	Partial weld (b)	Ratio (b)/(a)
s10	1.0	0.90	0.69	0.77
s20	2.0	0.94	0.83	0.89
d10	1.0	0.92	0.66	0.72
d20	2.0	0.93	0.87	0.94

Table 4.4 Comparisons of the average efficiency U_{te} between single and double plate specimens

Specimen	L/w	s/t	Average U_{te}		
			Single plate (a)	Double plate (b)	Ratio (a)/(b)
x05	0.5	1.00	0.52	0.53	0.97
x10	1.0	1.00	0.90	0.91	1.00
x10a	1.0	0.63	0.69	0.66	1.04
x15	1.5	1.00	0.94	0.95	0.99
x20	2.0	1.00	0.94	0.93	1.01
x20a	2.0	0.63	0.83	0.87	0.96

Figure 4.1 Test efficiencies with predicted CSA and AISC values

Figure 4.2 Average stress versus average LVDT displacement for full weld size double plate specimens

Figure 4.3 Transverse tension fracture across the cross-section, shown for d15R

Figure 4.4 Longitudinal shear failure of the weld along the fusion surface of the gusset plate, typical for double plate specimens with $L/w = 0.5$, shown for d05

Figure 4.5 Comparison of the stress versus average LVDT displacement for double plate specimens with s/t ratios of 1.0 and 0.63

Figure 4.6 Longitudinal shear failure typical of specimens with $L/w = 1.0$ and $s/t = 0.63$, shown for d10a

Figure 4.7 Comparison of the stress versus average LVDT displacement between double and single plate specimens with L/w ratio of 1.0 and 2.0

Figure 4.8 Strain in single plate specimens prior to the onset of yielding at LVDT displacement of 3 mm

Figure 4.9 Strain in full and partial weld size single plate specimens prior to the onset of yielding at LVDT displacement of 3 mm

Figure 4.10 Efficiencies of full weld specimens

Figure 4.11 Efficiencies of full and partial weld plate specimens without modification for weld size

Figure 4.12 Average efficiencies of full and partial weld size specimens without modification for weld size

Figure 4.13 Efficiencies of full and partial weld size specimens, modified using Eq. (4.2)

Figure 4.14 Average efficiencies of full and partial weld size specimens modified with Eq. (4.2)

CHAPTER 5 NUMERICAL ANALYSIS AND PARAMETRIC STUDY

A parametric study was conducted to predict the combined effect of the length and size of the fillet weld on the efficiency of the tension member for various configurations of flat bar tension members welded only with parallel longitudinal fillet welds. Finite element models for the parametric study were developed based on the flat bar specimens tested. Procedures used in determining the material properties for the numerical simulation and calibrating the finite element models, together with the discussions on the results of the numerical analyses are also presented in this chapter. All finite element analyses were carried out using the finite element software package ABAQUS (Simulia 2008).

5.1 Material properties

The finite element model is assumed to deform according to the incremental flow plasticity theory with isotropic hardening. To carry out the analysis, the true stress-true plastic strain relationship of the flat bar and gusset plates has to be determined. The material properties were derived from the coupon tests using the method developed by Zhao (2005). The procedure used, along with steps taken to determine the true stress-true plastic strain curves of all the materials used in the numerical simulation are presented in Appendix C. The true stress-true plastic strain relationship for the flat bar, gusset plates, and weld used in the analyses can be seen in Fig. 5.1, and the associated values used to generate these curves in Tables 5.1 and 5.2.

The equivalent plastic strain at fracture can be defined as

$$\varepsilon_f^p = \ln\left(\frac{A_o}{A_f}\right) \quad (5.1)$$

with the equivalent plastic strain defined as

$$\int d\varepsilon_{eq}^p = \int \sqrt{\frac{2}{3}} d\varepsilon_{ij}^p d\varepsilon_{ij}^p \quad (5.2)$$

where ε_{ij}^p is the plastic strain tensor, A_o is the original cross-sectional area of the coupon, and A_f is the final average cross-sectional area at fracture of the coupon. Although Eq. (5.1) is only valid when the coupon is under uniaxial loading, it is still a good approximation of the actual strain at fracture when the state of stress is no longer uniaxial due to necking. Based on Eq. (5.1), the true plastic strain at fracture for the flat bar, 7.9 and 12.7 mm gusset plates were found to be 0.79, 0.84, and 0.84.

The properties of the weld used in the analysis were based on the average of all-metal tension coupon test results of butt welded 15.9 mm (5/8") thick plates by Naqvi (2004). The true stress-true plastic strain curve of the weld used in the analysis is also shown in Fig. 5.1, while the process of calculating the curve is presented in Appendix C. The equivalent plastic strain at fracture determined with Eq. (5.2) was found to be 0.85 for the weld based on the test data from Naqvi (2004). In the study by Naqvi (2004), the weld metal was found to be more brittle than the ASTM A572 Grade 50 plates tested. Thus in the numerical simulation, the equivalent plastic strain at fracture for the weld was taken as no greater than the smallest equivalent plastic strain at fracture among the flat bar and gusset plates. Therefore, in the parametric study, the fracture limit of the weld was the same as the flat bar. The elastic modulus was taken as 200 GPa for the flat bar, the 12.7 mm gusset plate and weld, and 213 GPa for the 7.9 mm gusset plate, while the Poisson's ratio is taken as 0.3 for all four materials.

5.2 Finite element modeling

The tension specimen was modeled using eight node solid continuum elements (C3D8) and six node triangular prism continuum elements (C3D6). Since one of the parameters being studied is the size of the weld and its effect on the efficiency of the tension members, the weld was discretized using a combination of solid rectangular and triangular prism elements. The following sections describe in detail how the specimens were modeled.

5.2.1 Finite element modeling of the flat bar and the gusset plate

The unrefined region of the model consists of 4 layers of elements for the gusset plate and 4 to 6 layers for the flat bar, with the width of the cross-section orientated along the x-direction, the thickness along the y-direction, and the longitudinal length along the z-direction. The axis orientation and boundary conditions are shown in Fig. 5.2. Due to the symmetry of the cross-section, only a quarter of the specimen was explicitly modeled for the single plate specimens, and one-eighth of the specimen for the double plate specimens. This was done through the use of a symmetric boundary (XSYMM) condition about the y-z plane applied to the nodes at the edge of the model, which corresponds to the centerline of the actual specimen being constrained against any displacement in the x-direction, and the nodes at the mid-span of the specimen were constraint against any displacement in the z-direction with the imposed symmetric boundary condition (ZSYMM).

For the double plate specimens, only half of the gusset plate thickness was modeled using the same number of elements through the thickness as the single plate

specimens. Thus a symmetric boundary condition (YSYMM) was applied to all the nodes representing the mid-thickness plane of the gusset plate. In the unrefined region of the mesh, the elements of the flat bar up to the end of the fillet weld have a horizontal dimension of 3.81 mm along the width and 4.76 mm along the length of the flat bar, while the elements in the remainder of the specimen have a length of 4.53 mm. In the test, there was a spacing of approximately 50.8 mm (2") from the end of the flat bar to the grip of the testing apparatus. Therefore, the incremental axial displacement (Δz) was equally applied to the nodes at the end of the gusset plate at an approximate distance of 50.8 mm away from the end of the flat bar to simulate the loading condition of the actual tests. The nodes along the top and bottom edges of the gusset plate at the grip boundary were constrained against any displacement in the y-direction and sliding in the x-direction in order to simulate the effect of the grip. Nevertheless, it was found that constraining all the nodes at the end of the gusset plate against sliding had no impact on the results.

5.2.2 Finite element modeling of the weld

The weld, shown in Fig. 5.3 for the unrefined mesh, was discretized using a combination of rectangular and triangular solid elements. This modeling approach follows past studies conducted by Kim et al. (2000) where the weld was constructed in a similar fashion. The weld was modeled with elements that are 4.76 mm long along the length, and a quarter of the weld leg size along the width and thickness of the tension member. For the partial weld size model, the thickness of the flat bar was formed with an additional two layers of elements added on top of the existing four layers of elements for

the full weld size member. However, the number of elements that formed the cross-section of the weld remained the same for both the full and partial weld size specimens, but the thickness of the elements was adjusted accordingly to model the reduced weld size. Similarly, as shown in Fig. 5.4, the thickness of elements in the flat bar within the height of the weld leg size was also reduced. The nodes on the side and bottom faces of the weld were constrained to have the same displacements as the nodes in the flat bar and gusset plate to model fusion of the surface by the weld.

5.2.3 Failure criteria

Two specimen failure criteria were explored for the numerical analyses using user defined subroutines in ABAQUS. For both failure criteria, the equivalent plastic strain limit at fracture is assumed to be the strain limit for crack initiation. In the first failure criterion, the subroutine would immediately terminate the analysis when the equivalent plastic strain limit at fracture has been exceeded in any element within the model. It is assumed that upon the initiation of the first crack, the load capacity would subsequently begin to drop from that point onwards. Thus, the load at the first crack initiation would represent the ultimate capacity of the tension specimen. For the second failure criterion, the subroutine does not terminate the analysis at the instance the equivalent plastic strain at the integration point has reached the limit. Instead, the analysis is allowed to continue, but with the elastic modulus reduced by a factor of 10^{-4} and the stresses set to zero at the integration point where the equivalent plastic strain limit has been exceeded in order to simulate the crack development and material failure at that point. This allows the load to redistribute itself to the rest of the member that is still effective. Thus, the load may

continue to increase even after the development of cracks due to the non-uniform stress distribution over the cross-section of the tension member until a sufficient number of elements have failed.

5.2.4 Mesh refinement

A mesh refinement was implemented in regions of high stress concentration near the end of the weld so that the model could more accurately capture the stress and strain variation in the tension member. In addition to the mesh refinement, three general mesh configurations were developed to model for various configurations of the tension specimens depending on the weld length (L/w) and the weld size (s/t) ratios. For the first mesh configuration as shown in Fig. 5.5, the dimension of the designated refinement zone was around three times the thickness of the plate in both horizontal directions from the end of the fillet weld in order to minimize the influence of the unrefined mesh on the stress and strain distributions at the location of the highest stress concentration near the end of the weld. At the interface, nodes of the refined mesh that coincide with the corner nodes of the unrefined mesh element were constrained to have the same displacements, while the displacements of the remaining refined mesh nodes were constrained to the deformed surface of the coarse element using the bi-linear MPC constraint.

A second mesh configuration shown in Fig. 5.6 was developed for the specimens with a weld length ratio (L/w) less than 1.0 in order to ensure that there was a better transition between the fine and coarse meshes as the weld length becomes shorter. The size of the refinement zone was kept the same as the first mesh configuration. However, the width and length of the coarse mesh elements in the region beyond the mesh

refinement that is 19 mm in the direction towards the grip and 14 mm towards the mid-length was reduced by half but the element height stayed the same as the coarse element in the first mesh configuration.

The third mesh configuration, as shown in Fig. 5.7, was developed for tension specimens with a partial weld leg size and L/w ratio greater than or equal to 1.0 to ensure that the high stresses do not transition into the coarse mesh as the crack propagates along the weld. The refinement zone was extended by approximately 24 mm in the direction towards the grip while the same element size as the first configuration.

Even though no extensive mesh study was carried out to find the optimum mesh size, a number of refinement schemes have been considered and analyzed. The material properties and the fracture limits used in the limited mesh study have been defined in Section 5.1. Numerical simulations were carried out for the full size weld single plate specimens with two mesh refinement schemes and both failure criteria. Scheme 1 of the mesh refinement had eight elements through the height of the weld, with 0.476 and 0.595 mm for the width and length in the x and z directions. A more refined Scheme 2 had twelve elements through the height of the weld, and the width and length of 0.318 and 0.397 mm. Both mesh refinement schemes are shown in Figs. 5.8 and 5.9 respectively. Results of the analyses are shown in Tables 5.3 and 5.4 with the predicted efficiency calculated as

$$U = \frac{P_{u-FEM}}{A_g F_u} \quad (5.3)$$

where P_{u-FEM} is the maximum load achieved in the analysis, A_g is the gross cross-sectional area of the flat bar, and F_u is the ultimate stress of the flat bar determined from the material tests. Table 5.3 shows that the predicted tension capacity at crack initiation

reduces by close to 10% going from Scheme 1 to 2, while the reductions shown in Table 5.4 for the analyses that consider crack propagation are less than 2%. A finer mesh allows a steeper strain gradient to be modeled in the region at the end of the fillet weld. As a result, crack initiation was predicted to occur earlier with Scheme 2 mesh refinement. Thus, the mesh refinement is expected to have a greater impact on the predicted tension capacity based on the load at crack initiation rather than the peak load attained when the crack is allowed to propagate and the stress is able to redistribute around the region of high stress concentration.

Due to the heavy computational time required to conduct the numerical analysis with crack propagation, no additional analyses were carried out for a finer mesh with the crack propagation procedure. The additional computational time required to model crack propagation is due to the additional effort needed to reach solution convergence when the crack has grown in any time increment step as there is considerable stress redistribution after additional material failed. Nevertheless, additional analyses with the first crack failure criterion were carried out with mesh refinement Scheme 3 shown in Fig. 5.10. Mesh refinement Scheme 3 has half the element size of Scheme 1. It can be seen in Table 5.3 that there is still a significant difference in the predicted capacity of the tension member going from Scheme 2 to 3. Based on the limited mesh study conducted, it is obvious that the solution has not converged with either refinement Schemes 1 or 2. However, only Scheme 1 and 2 mesh refinements were used in the subsequent parametric study due to the heavy computational time required in the analysis with a mesh refinement finer than Scheme 2. Thus, the parametric study will only be used in

predicting the trend in the behaviour of the welded plate tension member to complement the test results.

5.3 Finite element model validation

As outlined in Section 5.2.3, two different failure criteria were used to determine the ultimate capacity of the tension member in the finite element analyses. The fracture limit was initially calibrated to be 0.4 to match the experimental results using analyses with crack propagation for full weld size single plate specimens ($s/t = 1.0$) and mesh refinement Scheme 1 while keeping other material properties as outlined in Section 5.1 unchanged. The numerical simulation was able to closely replicate the test results in terms of the efficiency, the displacement at peak load, and the mode of failure for specimens with an L/w ratio of 1.0, 1.5 and 2.0, but not at the ratio of 0.5. Instead of fracturing along the fusion face, the failure of the specimen at an L/w ratio of 0.5 was predicted to be along the minimum throat of the weld. The predicted failure mode of these specimens can be seen in Figs. 5.11 and 5.12, shown for the L/w ratios of 0.5 and 1.0.

However, using the fracture limit calibrated for the full weld size specimens, the finite element analyses significantly overestimated the capacity of the partial weld size specimens at an s/t ratio of 0.63, and were unable to replicate the failure mode of all the partial weld size specimens tested. The differences between the test and predicted results may be due partly to some of the following factors. These factors being the mesh used may not be fine enough, the material properties used may not be representative of the actual weld and heat affected zone, and the fracture criterion used may not be appropriate

although the criterion has been used successfully in other studies by various researchers. As stated earlier, due to the heavy computational time required, a finer mesh has not been considered. The fracture criterion used was based on a fixed equivalent plastic strain limit calculated according to Eq. (5.2). This criterion does not take into account the effect of hydrostatic stress on the ductility of steel, which Hancock and Mackenzie (1976) found to have an inverse effect on the plastic strain at fracture. However, the use of other fracture criteria has not been explored. In addition, the actual weld and heat affected zone material properties of the test specimens have not been determined in the current study. In view of the above limitations, results from the finite element analyses were calibrated to fill the gap in the test results and to provide the prediction in the overall trend in the performance of the tension member. Nevertheless, future studies should consider other fracture criteria and additional material tests to determine the actual properties of the weld and heat affected zone.

Since the preliminary study found that the numerical simulation was unable to replicate the failure mode of partial weld size specimens, no analysis with crack propagation was considered in the parametric study of the partial weld size tension members. Analyses for the single plate partial weld size specimens ($s/t = 0.63$) with the refinement Scheme 2 were carried out with two fracture limits of 0.55 and 0.79 for the flat bar to predict the capacity based first crack initiation failure criterion. The 0.55 fracture limit was selected to give a lower bound prediction when compared to the average test capacity, while the 0.79 limit was the measured value from the tension coupon test. Comparisons of the test and predicted efficiency are shown in Table 5.5. Using the fracture limit of 0.79, the analysis closely predicted the capacity of the

specimens with an L/w ratio of 2.0, but overestimated the efficiency at an L/w ratio of 1.0 by 4%. On the other hand, with the fracture limit of 0.55, the analysis underestimated the efficiency at an L/w ratio of 2.0 by 7%, but only slightly underestimated the test efficiency at an L/w ratio of 1.0. Comparisons of the test and predicted average stress versus deformation of tension specimens with an s/t ratio of 0.63 and L/w ratios of 1.0 and 2.0 are shown in Figs. 5.13 and 5.14. It can be seen that there is a good agreement between the test and predicted average stress versus deformation curves up to the predicted point of failure.

For the full weld size model, using the mesh refinement Scheme 1 with the failure limit set at 0.79, it was found that analyses with crack propagation were able to predict an upper bound tensile capacity of the specimens, while the load at the first crack initiation of the same analyses gives a lower bound prediction of the tensile capacity. Analyses were also carried out for two double plate specimens at the L/w ratios of 0.5 and 2.0. Comparisons between test and predicted results for the full weld size specimens are shown in Table 5.6 and Figs. 5.15 to 5.18. It can be seen that there is a good agreement between the test and predicted average stress versus deformation curves, except close to the experimental peak load for specimens with an L/w ratio of 0.5. As shown in Table 5.6, there is no significant difference between results of single or double plate specimens. For this reason, the subsequent parametric study was carried out only with single plate specimen models.

Due to the way the model was constructed, the element thickness in the plate and the weld was smaller in the partial weld size model than the full weld size model. Therefore, an additional model was constructed to study the effect of changing the

element height while maintaining the same horizontal dimensions in the x and z directions. As shown in Fig. 5.19, the model was based on the mesh configuration of the full weld size specimen with mesh refinement Scheme 1, but the element thickness in the refined mesh has been reduced by raising the number of through thickness elements to 12 in order to match closely the element thickness of the partial weld size model with an s/t ratio of 0.63. Analyses were carried out for specimens with L/w ratios of 1.0 and 2.0 using the same material properties as outlined in Section 5.1 and a fracture limit of 0.79 with the first crack failure criterion. Based on the results obtained from these simulations shown in Table 5.7, it can be seen that the variation in element thickness employed in the modeling of the partial weld size specimens has no significant impact on the predicted ultimate capacity. This shows that it is acceptable to compare results of the partial weld size model at various s/t ratios even though the model at each s/t ratio will have a different element height.

5.4 Parametric study

The parametric study of the welded plate tension member was divided into two phases, with phase one examining the behaviour of plate specimens with the full weld leg size ($s/t = 1.0$), and phase two examining the behaviour of plate specimens with a partial weld leg size ($s/t < 1.0$). Analyses of the full weld size specimens were carried out with Scheme 1 mesh refinement along with a failure limit of 0.79 for both the first crack failure criterion and the crack propagation analysis. On the other hand, the analyses of partial weld size specimens utilized Scheme 2 mesh refinement along with both fracture

limits of 0.55 and 0.79, but using only the first crack failure criterion. In all the analyses, fracture was predicted to initiate in the flat bar at the end of the fillet weld.

5.4.1 Parametric study of full weld size tension plate members

In the parametric study of full weld size tension plate members, two separate analyses were conducted for each tension specimen for an L/w ratio ranging from 0.5 to 3.0. One analysis was conducted using the first crack failure criterion and the other using the crack propagation procedure. Results of the analyses are plotted in Fig. 5.20 together with the average of the single and double plate specimen tests. Results from both failure criteria show that the efficiency increases linearly up to an L/w ratio of about 0.6, but after which the rate of increase starts to decrease. The maximum efficiency is achieved at an L/w ratio slightly above 1.0. It can be seen that the first crack failure criterion provides a lower bound estimation while the crack propagation analysis provides an upper bound estimation of the capacity. Thus, a weighted average of the upper and the lower bound solutions can be used in predicting the efficiency of the tension member. A weighted average function that predicts an efficiency lower than the test average at all the L/w ratios has been developed.

$$U_{\text{FEM}(\text{avg})} = 0.175U_u + 0.825U_L \quad (5.4)$$

where $U_{\text{FEM}(\text{avg})}$ is the weighted average efficiency of a full weld size tension plate member, U_u is the upper bound efficiency using crack propagation analysis and U_L is the lower bound efficiency using the first crack failure criterion. Results from Eq. (5.4) are shown in Table 5.8 along with the test average in Fig. 5.21. Using the weighted

average results of Eq. (5.4), a piecewise linear relationship can be developed for the predicted efficiency and L/w ratio, which can be expressed as

$$U_F = \left(\frac{L}{w} \right) \quad \text{for } L \leq 0.6w, \text{ and} \quad (5.5)$$

$$U_F = 0.20 + 0.66 \left(\frac{L}{w} \right) \leq 0.94 \quad \text{for } L > 0.6w \quad (5.6)$$

where U_F is the predicted efficiency for full weld size plate tension member. When compared to other experimental results in Fig. 5.21, Eqs. (5.5) and (5.6) provide a good estimate of the test efficiency by Easterling and Giroux (1993). However, this is not the case for the test results by AWS (1931) where most of the average test efficiencies are below the predicted values because the weld leg size of some of these tests was quite large relative to the plate width. Using the modification outlined in Section 4.4 for the width of the flat bar on all the tests and numerical results, Fig. 5.22 shows that a better correlation can be obtained between the efficiency and the modified weld length ratio. Using the modified weld length ratio, Eqs (5.5) and (5.6) are transformed into

$$U_F = 1.12 \left(\frac{L}{w_s} \right) \quad \text{for } L \leq 0.54w_s, \text{ and} \quad (5.7)$$

$$U_F = 0.20 + 0.74 \left(\frac{L}{w_s} \right) \leq 0.94 \quad \text{for } L > 0.54w_s \quad (5.8)$$

where w_s is the modified plate width defined by Eq. (4.1). The constants in Eqs. (5.7) and (5.8) are obtained by applying a factor of 1.12 to Eqs. (5.5) and (5.6), which is the ratio of the modified width over the actual width of the plate model used in the analyses. It can be seen that the finite element analysis results agree well with the experimental results using the modified weld length ratio. After the modification, all the average test

efficiencies are higher, but not significantly higher than the predicted values given by Eqs. (5.7) and (5.8).

Comparisons of all test data for full weld size specimens along with the numerical analysis results, and Eqs. (5.7) and (5.8) are shown in Fig. 5.23. Results from current tests and tests by Easterling and Giroux (1993) have a small scatter and agree well with Eqs. (5.7) and (5.8). However, results by AWS (1931) show a considerably larger scatter as these tests were not conducted in one institution using a single welding procedure, but across a number of institutions using different welding procedures. Nevertheless, most of the test data fall above Eqs. (5.7) and (5.8) even though tests by AWS (1931) all failed along the throat of the weld. The proposed Eqs. (5.5) to (5.8) were developed from results of the finite element analyses that predicted the location of fracture initiation occurring at the flat bar and calibrated with data from the tests at an L/w ratio of 0.5 that failed along the weld fusion surface. Thus, these equations can be considered to predict the tension member efficiency due to weld failure along the fusion surface when the L/w ratio is low. Nevertheless in other situations, the capacity of the tension member can be governed by the weld failure along the minimum throat of the weld. Thus, additional data from tests at low L/w ratios with known actual weld material properties will be needed to perform a more thorough validation of the proposed equations.

5.4.2 Parametric study of partial weld size tension plate members

The parametric study of partial weld size tension plate members was conducted for an L/w ratio ranging from 0.5 to 3.0 and an s/t ratio ranging from 0.5 to 0.85 using the first crack failure criterion with fracture limits of 0.55 and 0.79, and mesh refinement

Scheme 2. Analyses were also carried out for specimens with an s/t ratio of 1.0 using the mesh configuration and refinement of the partial weld size model so that comparisons can be made between results of full and partial weld size specimens in a consistent manner. Results from the analyses are presented in Table 5.9 and plotted together with the test average from the single and double plate specimens in Figs. 5.24 and 5.25 for the fracture limits of 0.55 and 0.79 respectively. Only analysis results for s/t ratios of 0.63, 0.85, and 1.0 are shown in Figs. 5.24 and 5.25. The predicted efficiency for the partial weld size specimens can be seen to follow the same trend as the full weld size specimens where there is an initial linear relationship with the L/w ratio, which is followed by a reduced rate of increase. Overall, the predicted efficiency increases with the s/t ratio for the same L/w ratio. Thus, with a lower s/t ratio, the maximum efficiency is achieved at a larger L/w ratio. However, the maximum efficiency of the partial weld size specimen approaches but never achieves the maximum efficiency of the full weld size specimen. Comparisons of the test and predicted efficiencies clearly show that the analyses cannot accurately simulate the partial weld size effect. Thus, results from the finite element analyses of partial weld size specimens cannot be used directly in predicting the efficiency of the tension member.

In order to analyze the effect of the weld size (s/t) ratio on the efficiency, the ratio of the predicted partial over the full weld size specimen efficiency at a given L/w ratio was first calculated. Due to the lack of accuracy in the predicted efficiency, this ratio was further calibrated against the test results so that the predicted partial over full weld size specimen efficiency ratio from the numerical analyses at the weld leg size ratio (s/t) of

0.63 would match that from the tests. The corrected ratio after the calibration can be expressed as

$$\left(\frac{U}{U_{\frac{s}{t}=1}} \right)_c = \left[1 - \frac{\Delta_{\text{Test}}}{\Delta_{\text{FEM}}} \left(1 - \frac{U}{U_{\frac{s}{t}=1}} \right)_{\text{FEM}} \right] \quad (5.9)$$

where U is the efficiency, the subscript FEM denotes results from the finite element analyses, Δ_{Test} and Δ_{FEM} are the test and predicted drop in the ratio of the efficiency at an s/t ratio of 0.63 over the efficiency at an s/t ratio of 1.0, which are expressed as

$$\Delta_{\text{Test}} = 1 - \left(\frac{U_{\frac{s}{t}=0.63}}{U_{\frac{s}{t}=1.0}} \right)_{\text{Test}} \quad \text{and} \quad (5.10)$$

$$\Delta_{\text{FEM}} = 1 - \left(\frac{U_{\frac{s}{t}=0.63}}{U_{\frac{s}{t}=1.0}} \right)_{\text{FEM}} \quad (5.11)$$

Corrected and uncorrected ratios of the partial over the full weld size specimen efficiency for the L/w ratios of 1.0 and 2.0 are listed in Table 5.10, and the corrected ratio plotted against s/t ratio in Fig. 5.26. Corrections at an L/w ratio of 1.0 was developed from analysis results with the fracture limit at 0.55 and at an L/w ratio of 2.0 with the fracture limit at 0.79 since the predicted efficiency at an s/t ratio of 0.63 is closer to the average test results using the respective fracture limit at each of the corresponding L/w ratio. From the plots in Fig. 5.26, it can be seen that there is a strong relationship between the $(U/U_{s/t=1})_c$ and s/t ratios. These relationships can be approximated with

$$f_{ul} \left(\frac{s}{t} \right) = 1 - 1.81 \left(1 - \frac{s}{t} \right)^2 \quad \text{for } L/w = 1.0, \text{ and} \quad (5.12)$$

$$f_{u2}\left(\frac{s}{t}\right) = 1 - 0.63\left(1 - \frac{s}{t}\right)^2 \quad \text{for } L/w = 2.0, \quad (5.13)$$

as can be seen in Fig. 5.26. Since Eqs. (5.12) and (5.13) are only valid at the L/w ratios of 1.0 and 2.0, it is necessary to interpolate between these two equations in order to predict the efficiency of a partial weld size specimen at an L/w ratio other than 1.0 and 2.0.

In order to establish the interpolation procedure, the partial over the full weld size specimen efficiency ratio was normalized for each s/t ratio such that the normalized efficiency ratio is equal to 1.0 when the L/w ratio is 2.0 and 0.0 when the L/w ratio is 1.0. The normalized efficiency ratio for each s/t ratio is expressed as

$$\left(\frac{U}{U_{s=1}}\right)_n = \left[\left(\frac{U}{U_{s=1}}\right) - \left(\frac{U}{U_{s=1}}\right)_{\frac{L}{w}=1.0} \right] \left(\frac{1}{U_r}\right) \quad \text{with} \quad (5.14)$$

$$U_r = \left(\frac{U}{U_{s=1}}\right)_{\frac{L}{w}=2.0} - \left(\frac{U}{U_{s=1}}\right)_{\frac{L}{w}=1.0} . \quad (5.15)$$

When using Eq. (5.14), the s/t ratio is held constant while the L/w ratio is varied from 0.5 to 3.0. Thus, four separate interpolation curves are generated for the s/t ratios considered in the parametric study. The normalized ratios are shown in Fig. 5.27 and Table 5.11 for fracture limit of 0.55, and Fig. 5.28 and Table 5.12 for the fracture limit of 0.79. Figures 5.27 and 5.28 show that the curves for both s/t ratios of 0.63 and 0.85 are almost the same for an L/w ratio greater than 1.0, and the normalized efficiency ratio remains constant at 1.0 for an L/w ratio greater than 1.5. Between the L/w ratios of 1.0 and 1.5, the relationship between the normalized efficiency and L/w ratios may be approximated by a

linear function since no numerical analysis was carried out for the L/w ratios between 1.0 and 1.5. There is a difference between the curves for the s/t ratios of 0.63 and 0.85 and those for s/t ratios of 0.5 and 0.75 because no numerical analysis was conducted at an L/w ratio of 1.5 for s/t ratios of 0.5 and 0.75. As a result, the curves for s/t ratios of 0.5 and 0.75 go directly from the normalized efficiency ratio of 0 at an L/w ratio of 1.0 to 1.0 at an L/w ratio of 2.0. Since 0.75 is between 0.63 and 0.85, and 0.50 is close to 0.63, results at the s/t ratios of 0.63 and 0.85 are assumed to be representative of those at s/t ratios of 0.5 and 0.75. Thus, no additional analysis was carried out for an L/w ratio of 1.5 at the s/t ratios of 0.5 and 0.75. Based on the curves in Figs. 5.27 and 5.28 for the s/t ratios of 0.63 and 0.85, a linear function is used to interpolate the predicted partial to full weld size specimen efficiency ratio between L/w ratios of 1.0 and 1.5, which can simply be expressed as

$$f\left(\frac{L}{w}, \frac{s}{t}\right) = f_{u1}\left(\frac{s}{t}\right) + 2\left(\frac{L}{w} - 1.0\right)\left[f_{u2}\left(\frac{s}{t}\right) - f_{u1}\left(\frac{s}{t}\right)\right] \leq f_{u2}\left(\frac{s}{t}\right) \quad (5.16)$$

where $f_{u1}(s/t)$ and $f_{u2}(s/t)$ are functions defined by Eqs. (5.12) and (5.13). Equation (5.16) is applicable for an L/w ratio greater or equal to 1.0, but may be extended to a L/w ratio slightly below 1.0. Multiplying Eq. (5.16) by Eqs. (5.5) and (5.6) for U_F , the predicted efficiency for the partial weld size tension plate members can be expressed to be

$$U_P = U_F f\left(\frac{L}{w}, \frac{s}{t}\right). \quad (5.17)$$

By applying a factor of 1.12 for the ratio of the modified over actual plate width of the full weld size finite element model, Eqs. (5.16) and (5.17) can be rewritten in terms of the modified weld length ratio (L/w_s) as

$$g\left(\frac{L}{w_s}, \frac{s}{t}\right) = f_{u1}\left(\frac{s}{t}\right) + 2\left[1.12\left(\frac{L}{w_s}\right) - 1.0\right]\left[f_{u2}\left(\frac{s}{t}\right) - f_{u1}\left(\frac{s}{t}\right)\right] \leq f_{u2}\left(\frac{s}{t}\right) \text{ and} \quad (5.18)$$

$$U_p = U_F g\left(\frac{L}{w_s}, \frac{s}{t}\right). \quad (5.19)$$

Comparisons of predicted efficiency using Eq. (5.19) and the average test results for the partial weld size specimens by AWS (1931) are shown in Table 5.13. Overall, there is a reasonably good agreement between the test and predicted efficiencies with the predicted efficiency being generally more conservative.

According to the finite element analyses and the proposed equations, the maximum efficiency of the partial weld size tension member will never achieve the maximum efficiency of a full weld size tension member regardless of the L/w ratio. This needs to be verified with additional tests at an L/w ratio greater than 2.0 for partial weld size specimens to confirm if the maximum achievable efficiency is affected by the weld leg size since no specimen with an L/w ratio greater than 2.0 has been tested. Similar to the full weld size tension member, the predicted efficiency by Eq. (5.19) for weld failure can be considered as failure along the fusion surface based on the location of fracture initiation predicted by the numerical analyses and the mode of failure associated with the test data used in calibrating Eq. (5.12). For test specimens at an L/w ratio of 1.0 and s/t ratio of 0.63, the weld failure was found to be along the fusion surface on the tension member. In the finite element analyses, fracture was predicted to initiate at the flat bar, and not at the throat of the weld. However, the proposed equations still need additional test data at various s/t and L/w ratios to be validated, especially from tests with the known actual mechanical properties of the weld and heat affected zone..

Table 5.1 True stress-true plastic strain values for the flat bar, 12.7 mm and 7.9 mm gusset plates

FB 76 x 4.8		FB 102 x 12.7		FB 102 x 7.9	
True plastic strain (mm/mm)	True stress (MPa)	True plastic strain (mm/mm)	True stress (MPa)	True plastic strain (mm/mm)	True stress (MPa)
0.00000	333.0	0.00000	293.0	0.00000	342.0
0.02006	349.0	0.00169	293.8	0.00143	343.7
0.03736	418.9	0.00661	314.9	0.00569	358.9
0.07517	497.5	0.01434	329.5	0.00839	381.0
0.10660	535.2	0.01820	356.2	0.01803	434.7
0.13310	557.7	0.02776	401.1	0.02741	476.7
0.15898	577.9	0.03727	435.1	0.03699	507.9
0.16000	578.6	0.07475	517.9	0.07444	583.7
0.18000	590.6	0.10653	557.3	0.10181	616.8
0.20000	601.4	0.13310	579.4	0.13749	644.7
0.24000	620.4	0.17166	606.3	0.16000	658.7
0.30000	644.2	0.20000	622.8	0.18000	669.7
0.40000	675.7	0.22000	633.2	0.20000	679.7
0.50000	700.9	0.24000	642.7	0.24000	697.3
0.60000	722.1	0.30000	667.7	0.30000	719.4
0.70000	740.4	0.40000	701.0	0.32000	725.9
0.85000	764.0	0.50000	727.6	0.40000	748.7
1.00000	784.3	0.60000	750.0	0.50000	772.1
1.20000	807.7	0.70000	769.3	0.60000	791.8
1.40000	827.9	0.80000	786.5	0.70000	808.8
2.50000	908.3	0.93134	497.6	0.80000	823.8
4.00000	978.0	1.03694	511.4	0.85000	830.7
		1.40000	551.1	1.00000	849.5
				1.20000	871.0
				1.40000	889.6

Table 5.2 True stress-true plastic strain values for the weld material

Weld	
True plastic strain (mm/mm)	True stress (MPa)
0.00000	486.6
0.03070	486.5
0.07517	561.5
0.10660	597.1
0.13310	621.7
0.15898	642.6
0.16000	643.3
0.18000	657.7
0.20000	671.0
0.24000	694.7
0.30000	725.3
0.40000	767.2
0.50000	801.8
0.60000	831.3
0.70000	857.2
0.80000	880.3
0.86000	893.2
1.00000	920.6
1.20000	954.9
1.40000	985.0

Table 5.3 Change in efficiency between different mesh refinements with first crack failure criterion

Specimen	L/w	s/t	Efficiency			$\frac{U_{(8)} - U_{(12)}}{U_{(12)}}$	$\frac{U_{(12)} - U_{(16)}}{U_{(16)}}$
			Scheme 1 $U_{(8)}$	Scheme 2 $U_{(12)}$	Scheme 3 $U_{(16)}$		
s05	0.5	1.0	0.50	0.46	0.44	10%	4%
s10	1.0	1.0	0.84	0.80	0.78	4%	3%
s20	2.0	1.0	0.92	0.87	0.82	7%	5%

Table 5.4 Change in efficiency between different mesh refinements with crack propagation analyses

Specimen	L/w	s/t	Efficiency		$\frac{U_{(8)} - U_{(12)}}{U_{(12)}}$
			Scheme 1 $U_{(8)}$	Scheme 2 $U_{(12)}$	
s05	0.5	1.0	0.59	0.58	2%
s10	1.0	1.0	1.00	1.00	0%
s20	2.0	1.0	1.00	1.00	0%

Table 5.5 Comparison between the predicted and experimental efficiencies for partial weld size specimens ($s/t = 0.63$)

Specimen	L/w	s/t	Efficiency				$\frac{U_u}{U_{ta}}$	$\frac{U_L}{U_{ta}}$	$\frac{U_{avg}}{U_{ta}}$
			Test average U_{ta}	Numerical Analyses					
				First crack ($\epsilon_f^P=0.79$) U_u	First crack ($\epsilon_f^P=0.55$) U_L	Average U_{avg}			
s10a	1.0	0.63	0.69	0.72	0.67	0.70	1.04	0.97	1.01
s20a	2.0	0.63	0.83	0.83	0.77	0.80	1.00	0.93	0.96

Table 5.6 Comparison between the predicted and experimental efficiencies for full weld size specimens ($s/t = 1.0$)

Specimen	L/w	s/t	Efficiency				$\frac{U_u}{U_{ta}}$	$\frac{U_L}{U_{ta}}$	$\frac{U_{avg}}{U_{ta}}$
				Numerical Analyses					
				Test average (U _{ta})	Crack prop. (U _u)	First crack (U _L)			
s05	0.5	1.00	0.52	0.59	0.50	0.55	1.13	0.96	1.05
s10	1.0	1.00	0.90	1.00	0.84	0.92	1.11	0.93	1.02
s15	1.5	1.00	0.94	1.00	0.92	0.96	1.06	0.98	1.02
s20	2.0	1.00	0.94	1.00	0.92	0.96	1.06	0.98	1.02
d05	0.5	1.00	0.52	0.59	0.49	0.54	1.13	0.94	1.04
d20	2.0	1.00	0.94	1.00	0.94	0.97	1.06	1.00	1.03

Table 5.7 Effect of element thickness on the efficiency obtained through the numerical simulation

Test	L/w	s/t	Element thickness (mm)	P_{u-FEM} (N)	U
fb2010-08	2.0	1.00	0.595	82395	0.92
fb2010-08t	2.0	1.00	0.397	81807	0.92
fb1010-08	1.0	1.00	0.595	74634	0.84
fb1010-08t	1.0	1.00	0.397	74831	0.84

Table 5.8 Full weld size tension plate member efficiency

Specimen	L/w	s/t	Efficiency		
			Crack prop. U_u	First crack U_L	$U_{FEM(avg)}$
fb0510-08	0.50	1.00	0.59	0.50	0.52
fb0610-08	0.63	1.00	0.75	0.60	0.63
fb0810-08	0.75	1.00	0.88	0.70	0.73
fb1010-08	1.00	1.00	1.00	0.84	0.86
fb1310-08	1.25	1.00	1.00	0.91	0.93
fb1510-08	1.50	1.00	1.00	0.92	0.93
fb1810-08	1.75	1.00	1.00	0.92	0.93
fb2010-08	2.00	1.00	1.00	0.92	0.94
fb2510-08	2.50	1.00	1.00	0.92	0.94
fb3010-08	3.00	1.00	1.00	0.93	0.94

Table 5.9 Partial weld size tension plate member efficiency

Specimen	L/w	s/t	Efficiency		
			First crack ($\epsilon_f^p=0.79$) U_u	First crack ($\epsilon_f^p=0.55$) U_L	U_{avg}
fb0506-12	0.50	0.63	0.35	0.35	0.35
fb0509-12	0.50	0.85	0.42	0.40	0.41
fb0510-12	0.50	1.00	0.46	0.43	0.44
fb1005-12	1.00	0.50	0.66	0.61	0.63
fb1006-12	1.00	0.63	0.72	0.67	0.70
fb1008-12	1.00	0.75	0.76	0.72	0.74
fb1009-12	1.00	0.85	0.78	0.74	0.76
fb1010-12	1.00	1.00	0.80	0.76	0.78
fb1506-12	1.50	0.63	0.82	0.77	0.80
fb1509-12	1.50	0.85	0.86	0.79	0.82
fb1510-12	1.50	1.00	0.86	0.79	0.83
fb2005-12	2.00	0.50	0.80	0.75	0.78
fb2006-12	2.00	0.63	0.83	0.77	0.80
fb2008-12	2.00	0.75	0.85	0.79	0.82
fb2009-12	2.00	0.85	0.86	0.80	0.83
fb2010-12	2.00	1.00	0.87	0.80	0.83
fb2506-12	2.50	0.63	0.83	0.78	0.80
fb2509-12	2.50	0.85	0.86	0.80	0.83
fb2510-12	2.50	1.00	0.87	0.80	0.83
fb3005-12	3.00	0.50	0.81	0.76	0.78
fb3006-12	3.00	0.63	0.83	0.78	0.80
fb3008-12	3.00	0.75	0.85	0.79	0.82
fb3009-12	3.00	0.85	0.86	0.80	0.83
fb3010-12	3.00	1.00	0.87	0.80	0.84

Table 5.10 Corrected and uncorrected ratios of predicted partial over full weld size specimen efficiency at L/w ratios of 1.0 and 2.0

Specimen	L/w	s/t	Δ_{test}	Δ_{FEM}	U	$U/U_{s/t=1}$	$(U/U_{s/t=1})_c$
fb1005-12	1.0	0.50	0.255	0.117	0.61	0.80	0.56
fb1006-12	1.0	0.63	0.255	0.117	0.67	0.88	0.74
fb1008-12	1.0	0.75	0.255	0.117	0.72	0.94	0.87
fb1009-12	1.0	0.85	0.255	0.117	0.74	0.98	0.96
fb1010-12	1.0	1.00	0.255	0.117	0.76	1.00	1.00
fb2005-12	2.0	0.50	0.088	0.045	0.75	0.94	0.89
fb2006-12	2.0	0.63	0.088	0.045	0.77	0.97	0.94
fb2008-12	2.0	0.75	0.088	0.045	0.79	0.99	0.97
fb2009-12	2.0	0.85	0.088	0.045	0.80	1.00	0.99
fb2010-12	2.0	1.00	0.088	0.045	0.80	1.00	1.00

Table 5.11 Normalized efficiency ratio for the interpolation curve with the fracture limit of 0.55

Specimen	L/w	s/t	U_L	$U_{s/t=1}$	$U/U_{s/t=1}$	$(U/U_{s/t=1})_n$
fb1005-12	1.0	0.50	0.61	0.76	0.80	0.00
fb2005-12	2.0	0.50	0.75	0.80	0.94	1.00
fb3005-12	3.0	0.50	0.76	0.80	0.94	0.98
fb0506-12	0.5	0.63	0.35	0.43	0.82	-0.79
fb1006-12	1.0	0.63	0.67	0.76	0.88	0.00
fb1506-12	1.5	0.63	0.77	0.79	0.97	0.99
fb2006-12	2.0	0.63	0.77	0.80	0.97	1.00
fb2506-12	2.5	0.63	0.78	0.80	0.97	0.98
fb3006-12	3.0	0.63	0.78	0.80	0.97	0.96
fb1008-12	1.0	0.75	0.72	0.76	0.94	0.00
fb2008-12	2.0	0.75	0.79	0.80	0.99	1.00
fb3008-12	3.0	0.75	0.79	0.80	0.98	0.93
fb0509-12	0.5	0.85	0.40	0.43	0.94	-2.34
fb1009-12	1.0	0.85	0.74	0.76	0.98	0.00
fb1509-12	1.5	0.85	0.79	0.79	1.00	1.00
fb2009-12	2.0	0.85	0.80	0.80	1.00	1.00
fb2509-12	2.5	0.85	0.80	0.80	0.99	0.86
fb3009-12	3.0	0.85	0.80	0.80	0.99	0.86

Table 5.12 Normalized efficiency ratio for the interpolation curves with the fracture limit of 0.79

Specimen	L/w	s/t	U_u	$U_{s/t=1}$	$U/U_{s/t=1}$	$(U/U_{s/t=1})_n$
fb1005-12	1.0	0.50	0.66	0.80	0.81	0.00
fb2005-12	2.0	0.50	0.80	0.87	0.92	1.00
fb3005-12	3.0	0.50	0.81	0.87	0.93	1.06
fb0506-12	0.5	0.63	0.35	0.46	0.78	-1.90
fb1006-12	1.0	0.63	0.72	0.80	0.89	0.00
fb1506-12	1.5	0.63	0.82	0.86	0.95	0.99
fb2006-12	2.0	0.63	0.83	0.87	0.96	1.00
fb2506-12	2.5	0.63	0.83	0.87	0.95	0.97
fb3006-12	3.0	0.63	0.83	0.87	0.95	0.97
fb1008-12	1.0	0.75	0.76	0.80	0.94	0.00
fb2008-12	2.0	0.75	0.85	0.87	0.98	1.00
fb3008-12	3.0	0.75	0.85	0.87	0.98	0.95
fb0509-12	0.5	0.85	0.42	0.46	0.92	-1.68
fb1009-12	1.0	0.85	0.78	0.80	0.97	0.00
fb1509-12	1.5	0.85	0.86	0.86	0.99	0.96
fb2009-12	2.0	0.85	0.86	0.87	1.00	1.00
fb2509-12	2.5	0.85	0.86	0.87	0.99	0.88
fb3009-12	3.0	0.85	0.86	0.87	0.99	0.88

Table 5.13 Comparisons between predicted and AWS (1931) test efficiencies for partial weld size tension plate specimens

AWS Series	L/w_s	s/t	$f_{u1}(s/t)$	$f_{u2}(s/t)$	$g(L/w_s, s/t)$	Efficiency			
						Predicted		Test average U_e	Ratio $\frac{U_P}{U_e}$
						Full weld size U_F	Partial weld size U_P		
2143	0.80	0.75	0.89	0.96	0.87	0.79	0.69	0.69	0.99
2153	1.07	0.60	0.71	0.90	0.78	0.94	0.74	0.75	0.99
2163	1.07	0.50	0.55	0.84	0.66	0.94	0.62	0.66	0.94
2343	0.80	0.75	0.89	0.96	0.87	0.79	0.69	0.78	0.89
2353	1.07	0.60	0.71	0.90	0.78	0.94	0.74	0.81	0.91
2363	1.07	0.50	0.55	0.84	0.66	0.94	0.62	0.75	0.83

Figure 5.1 True stress versus true plastic strain curve for flat bar, gusset plates and weld

Figure 5.2 Finite element model with boundary conditions

Figure 5.3 Discretized weld for specimens with $s/t = 1.0$ (unrefined)

Figure 5.4 Discretized weld for specimens with $s/t = 0.63$ (unrefined)

Figure 5.5 First mesh configuration with mesh refinement Scheme 1

Figure 5.6 Second mesh configuration with modification to the coarse mesh for the specimens with an L/w ratio less than 1.0

Figure 5.7 Third mesh configuration with extended mesh refinement for specimens with an s/t ratio less than 1.0 and L/w greater than and equal to 1.0

Figure 5.8 Top view of mesh refinement for Scheme 1, 0.476 x 0.595 x 0.595 mm (width x length x thickness)

Figure 5.9 Top view of mesh refinement for Scheme 2, 0.318 x 0.397 x 0.397 mm (width x length x thickness)

Figure 5.10 Top view of mesh refinement for Scheme 3, 0.238 x 0.298 x 0.298 mm (width x length x thickness)

Figure 5.11 Predicted mode of failure for specimen with $L/w = 0.5$ and $s/t = 1.0$

Figure 5.12 Predicted mode of failure for specimens with $L/w \geq 1.0$ and $s/t = 1.0$

Figure 5.13 Average stress versus LVDT displacement curves for specimens with $L/w = 2.0$ and $s/t = 0.63$

Figure 5.14 Average stress versus LVDT displacement curves for specimen with $L/w = 1.0$ and $s/t = 0.63$

Figure 5.15 Average stress versus LVDT displacement curves for specimen with $L/w = 0.5$ and $s/t = 1.0$

Figure 5.16 Average stress versus LVDT displacement curves for specimen with $L/w = 1.0$ and $s/t = 1.0$

Figure 5.17 Average stress versus LVDT displacement curves for specimen with $L/w = 1.5$ and $s/t = 1.0$

Figure 5.18 Average stress versus LVDT displacement curves for specimen with $L/w = 2.0$ and $s/t = 1.0$

Figure 5.19 Modified mesh with a smaller element thickness at $s/t = 1.0$

Figure 5.20 Test and predicted efficiency versus the L/w ratio

Figure 5.21 Comparison between predicted and average test efficiencies for full weld size specimens

Figure 5.22 Comparison between predicted and average test efficiencies with modified effective plate width for full weld size specimens

Figure 5.23 Comparison between predicted and test efficiency with modified effective plate width for full weld size specimens

Figure 5.24 Test and predicted efficiency of full and partial weld size specimens for various s/t ratios with fracture limit of 0.55

Figure 5.25 Test and predicted efficiency of full and partial weld size specimens for various s/t ratios with fracture limit of 0.79

Figure 5.26 Corrected ratio of the partial over full weld size specimen efficiency $(U/U_{s/t=1})_c$ versus weld leg size ratio (s/t) for L/w ratios of 1.0 and 2.0

Figure 5.27 Interpolation curve based on the fracture limit of 0.55

Figure 5.28 Interpolation curve based on the fracture limit of 0.79

CHAPTER 6 CONCLUSIONS AND RECOMMENDATIONS

6.1 Conclusions

Based on the results from the experimental and numerical analyses, the following conclusions have been drawn:

- 1) In the tests, both single and double plate specimens achieved almost the same efficiency. However, all current tests were conducted with the ends restrained against rotation. There may be a larger difference in the efficiency between single and double plate specimens if the ends were pinned.
- 2) For test specimens with the weld leg size equal to the plate thickness, there was no change in the efficiency after an L/w ratio of 1.5, and the efficiency improvement was small after an L/w ratio of 1.0. The average efficiency improvement was only around 3% from an L/w ratio of 1.0 to 1.5. However, only a maximum of 94% average efficiency has been achieved at an L/w ratio greater than 1.5 and not a 100%.
- 3) The weld leg size relative to the plate thickness has been found to have a significant effect on the efficiency of welded plate tension members. At an L/w ratio of 2.0 and s/t ratio of 0.63, the test specimens only manage to attain an average efficiency of 85%, which was about 10% lower than the efficiency of the specimens with a full weld size even though specimens of both weld sizes have the same mode of failure across the width of the plate. At an L/w ratio of 1.0, the average efficiency of the specimens at an s/t ratio of 0.63 was around 24% lower than the efficiency of the full weld size specimens.

- 4) Compared to the test results for full weld size specimens, the current design provisions for welded plate tension members slightly overestimate the capacity at a L/w ratio greater than 2.0, but significantly underestimate the capacity at a L/w ratio less than or equal to 1.0. Since there is no consideration of the weld leg size, current design provisions significantly overestimate the capacity of the specimens with an s/t ratio of 0.63.
- 5) With the same s/t and L/w ratios, the efficiency was found to decrease with a larger weld leg size relative to the plate width based on results from other studies. Taking the modified width as the sum of the plate width and twice the weld leg size, a better correlation can be achieved between the efficiency and the modified weld length ratio (L/w_s) than with the regular L/w ratio.
- 6) From the parametric study for the full weld size specimens, it was found that the efficiency reaches a maximum at an L/w ratio slightly larger than one, and is linearly dependent on an L/w ratio up to a ratio of around 0.6
- 7) In the parametric study of the partial weld size tension members with an s/t ratio greater 0.5, the efficiency of the specimens with the same L/w ratio was found to decrease with the s/t ratio. Conversely, an L/w ratio at which point the maximum efficiency is reached was found to vary inversely to the s/t ratio. The specimens were predicted to achieve the maximum efficiency at around an L/w ratio of 2.0 for an s/t ratio of 0.5. However, the maximum predicted efficiency for the partial weld size tension members was lower and did not achieve that for the full weld size specimens even at a relatively large L/w ratio of 3.0.

- 8) Although the finite element analyses cannot accurately predict the efficiency and mode of fracture of the tests, results of the analyses were able to predict a useful prediction in the overall trend in the performance of the tension members. Equations that can provide a good estimate of the efficiency have been developed for the full weld size plate tension members and for partial weld size plate tension members with $s/t \geq 0.5$ and $L/w \geq 1.0$ from the results of the analyses after calibrating with those from the tests.

6.2 Recommendations

- 1) Current tests only included specimens up to an L/w ratio of 2.0. Since the parametric study found that the maximum efficiency decreases with the s/t ratio, more tests at an L/w ratio greater than 2.0 are needed to verify if and to what extent the maximum efficiency is affected by the s/t ratio. Additional tests of partial weld size specimens at an L/w ratio less than 2.0 will also be useful in helping to quantify the combined effect of the weld length and leg size.
- 2) Although the finite element analyses in the parametric study can be used to predict the trend of the performance, they were unable to accurately predict the mode of failure and efficiency of the tests. Other fracture criteria that can better account for the effect of hydrostatic tension should be explored and used in the future finite element analysis studies of welded tension plate members.
- 3) A proper finite element mesh study has not been carried out in the current study. However, the preliminary study showed that analyses with crack propagation procedure seem to require less mesh refinement. Thus, a mesh study to find the

optimum mesh configuration and refinement scheme to be run with the crack propagation procedure will allow a better finite element analysis parametric study to be conducted.

- 4) When the L/w ratio is large, the tension members fracture across the width of the plate. In this situation, the actual material properties of the weld and heat affected zone are not really needed in assessing the performance of the tension members. However at a low L/w ratio, the capacity is governed by weld failure. In the current tests, material properties of the flat bar were used since the failure was found to be along the fusion surface. However in tests by AWS (1931), failure was mainly along the minimum throat of the weld and the capacity was found to be also affected by the L/w_s ratio. Thus, the material properties of the weld and heat affected zone of the test specimens should be determined. Knowing the actual material properties of the weld and heat affected zone will allow for a better assessment of the combined effect of the plate width, plate thickness, weld length and weld leg size on the performance of welded plate tension members.
- 5) In the current experimental study, tension members were only tested with the ends restraint against rotation.. Tests should be carried out for single plate specimens with pinned end condition to investigate the effect of load eccentricity on the performance. Similarly, specimens with various plate thicknesses can also be tested to investigate the effect of load eccentricity.

REFERENCES

1. Abi-Saad, G. and Bauer, D. 2006. Analytical Approach for Shear Lag in Welded Tension Members. *Canadian Journal of Civil Engineering*, 33(4):384-394.
2. AISC. 2005. Specification for Structural Steel Buildings, ANSI/AISC 360-05, American Institute of Steel Construction (AISC), Chicago, Illinois.
3. American Bureau of Welding (AWB). 1931. Report of Structural Steel Welding Committee. American Welding Society. New York.
4. Chesson, E. and Munse, W.H. 1963. Riveted and Bolted Joints: Net Section Design. *Journal of the Structural Division, ASCE*, 89(1): 107-126.
5. CISC. 2006. Handbook of Steel Construction, Ninth Edition, Canadian Institute of Steel Construction, Willowdale, Ontario, Canada.
6. CSA. 2005. Limit States Design of Steel Structures, CAN/CSA-S16-01. Including Supplement S16-05. Canadian Standards Association, Toronto, Ontario, Canada.
7. CSA. 2009. Design of Steel Structures, CAN/CSA-S16-09. Canadian Standards Association, Mississauga, Ontario, Canada.
8. Davis, R.P. and Boomsliter, G.P. 1934. Tensile Tests of Welded and Riveted Structural Members. *Journal of the American Welding Society*, 13(4): 21-27.
9. Easterling, W.S. and Giroux, L.G. 1993. Shear Lag Effects in Steel Tension Members. *Engineering Journal, AISC*, 30(3): 77-89.
10. Fisher, J.W. and Yoshida, N. 1970. Large Bolted and Riveted Shingle Splices. *Journal of the Structural Division, ASCE*, 96(9): 1903-1918.

11. Gibson, G.J. and Wake, B.T. (1942), An Investigation of Welded Connections for Angle Tension Members. *Journal of the American Welding Society, Welding Research Supplement*, 21(1): 44-49.
12. Girard, C., Picard, A. and Fafard, M. 1995. Finite Element Modelling of the Shear Lag Effects in an HSS Welded to a Gusset Plate. *Canadian Journal of Civil Engineering*, 22(4): 651-659.
13. Hancock, J.W. and Mackenzie, A.C. 1976. On the Mechanisms of Ductile Failure in High-Strength Steels Subjected to Multi-Axial Stress-States. *Journal of Mechanics and Physics of Solids*, 24(2-3):147-169.
14. Kim, T., Whittaker, A.S., Gilani, A.S.J., Bertero, V.V., and Takhirov, S. M. 2000. Cover-Plate and Flange-Plate Reinforced Steel Moment-Resisting Connections. *Pacific Earthquake Engineering Research Center, PEER*, 2000(7):1-307.
15. Korol, R.M., Mirza, F.A. and Mirza, M.Y. 1994. Investigation of Shear-lag in Slotted HSS Tension Members. *Proceedings of the VIth International Symposium on Tubular Structures*, Melbourne, Australia, pp. 473-479.
16. Korol, R.M. 1996. Shear Lag in Slotted HSS Tension Members. *Canadian Journal of Civil Engineering*, 23(6):1350-1354.
17. Kulak, G.L., Fisher, J.W. and Struik, J.H.A. 1987. *Guide to Design Criteria for Bolted and Riveted Joints*, Second Edition. John Wiley & Sons, Inc, New York.
18. Kulak, G.L. and Grondin, G.Y. 2005. *Limit States Design in Structural Steel*, Seventh Edition, Canadian Institute of Steel Construction, Toronto, Ontario, Canada.

19. Martinez-Saucedo, G., Packer, J.A. and Willibald, S. 2006. Parametric Finite Element Study of Slotted End Connections to Circular Hollow Sections. *Engineering Structures*, 28(14): 1956-1971.
20. Naqvi, Z. 2004. Constitutive Properties of Weld Metal and Heat-Affected Zone of butt welded ASTM A572 Steel Plates. Masters Thesis, Carleton University, Ottawa, Ontario, Canada.
21. Simulia. 2008. ABAQUS Analysis User's Manual. Version 6.8. Volumes I – V. Simulia, Providence, Rhode Island.
22. Uzoegbo, H.C. 1998. Shear lag in Steel Angles: An Investigation of the South African Standards. *Journal of Constructional Steel Research*, 46:1-3, Paper No. 348.
23. Zhao, R. 2005. A Study on Slotted Square and Rectangular Hollow Structural Section Connections. Masters Thesis, Carleton University, Ottawa, Ontario, Canada.
24. Zhao, R., Huang, R., Khoo, H.A. and Cheng, J.J.R. 2009. Parametric Finite Element Study on Slotted Rectangular and Square HSS Tension Connections. *Journal of Constructional Steel Research*, 65(3): 611-621.

APPENDIX A TESTING PROGRAM

Detailed measured dimension of the test specimens are provided below.

A.1 Specimen detail

As outlined in Chapter 3, a total of 24 welded plate specimens were tested. The measured dimensions of the first flat bar where the strain gauges were installed are provided in Table A.1 while measurements of the second flat bar for double plate specimens are provided in Table A.2. Three measurements were taken using calipers and a micrometer for the width and the thickness of the plate(s) at the connected edge of the top gusset plate (1), at the mid-span of the flat bar (2), and at the connected edge of bottom gusset plate (3). The total length (L_s) of each flat bar is also included in Tables A.1 and A.2. Measurements of the top and bottom gusset plates are provided in Tables A.3 and A.4 respectively, where the width and thickness measurements were taken at the clamped edge (1), at mid-length (2), and at the connected edge (3). The total length (L_G) of each gusset plate is also included in Tables A.3 and A.4.

Measurements of each fillet weld length and size were also recorded. Each fillet weld was assigned a number based on its position on the specimen, as shown in Figs. A.1 and A.2. The measurement of the weld leg size was taken at the mid-length of the weld as it was more representative of the actual weld size as opposed to the ends where the size was generally larger. The measured dimensions of the weld are listed in Table A.5 for the first flat bar, and Table A.6 for the second flat bar in double plate specimens.

Table A.1 Measured dimensions of the first flat bar for single and double plate specimens

Specimen	Flat bar width (mm)			Flat bar thickness (mm)			Length (L _s) (mm)	Area (mm ²)
	1	2	3	1	2	3		
s05	76.25	76.23	76.21	4.71	4.70	4.72	460	358.8
s05R	76.45	76.37	76.41	4.78	4.66	4.69	462	359.9
s10	76.35	76.33	76.26	4.73	4.71	4.71	536	359.7
s10R	76.29	76.29	76.32	4.72	4.71	4.72	538	360.0
s10a	76.24	76.29	76.26	4.73	4.71	4.71	537	359.7
s10aR	76.33	76.27	76.26	4.77	4.73	4.73	537	362.0
s15	76.45	76.39	76.39	4.78	4.64	4.70	614	359.9
s15R	76.47	76.40	76.58	4.72	4.71	4.73	613	361.3
s20	76.42	76.52	76.41	4.70	4.75	4.74	690	361.5
s20R	76.41	76.28	76.33	4.72	4.71	4.71	738	359.9
s20a	76.42	76.24	76.30	4.72	4.69	4.69	690	358.7
s20aR	76.40	76.44	76.46	4.70	4.72	4.72	690	360.3
d05	76.41	76.32	76.30	4.62	4.57	4.56	462	349.9
d05R	76.25	76.24	76.24	4.58	4.56	4.60	461	349.2
d10	76.38	76.40	76.33	4.56	4.64	4.54	537	349.8
d10R	76.50	76.51	76.46	4.64	4.71	4.62	537	356.2
d10a	76.40	76.43	76.38	4.49	4.50	4.55	537	344.8
d10aR	76.40	76.47	76.39	4.67	4.66	4.69	690	357.1
d15	76.14	76.13	76.17	4.56	4.46	4.56	615	344.7
d15R	76.13	76.25	76.21	4.54	4.57	4.57	614	347.5
d20	76.29	76.20	76.23	4.65	4.46	4.48	690	345.4
d20R	76.27	76.26	76.25	4.61	4.59	4.59	689	350.5
d20a	76.31	76.36	76.34	4.56	4.50	4.50	692	345.0
d20aR	76.40	76.47	76.39	4.67	4.66	4.69	690	357.1

Table A.2 Measured dimensions of the second flat bar for double plate specimens

Specimen	Flat bar width (mm)			Flat bar thickness (mm)			Length (L _s) (mm)	Area (mm ²)
	1	2	3	1	2	3		
d05	76.38	76.35	76.23	4.58	4.59	4.59	461	350.1
d05R	76.3	76.29	76.34	4.66	4.6	4.64	462	353.6
d10	76.41	76.38	76.34	4.67	4.57	4.65	537	353.6
d10R	76.43	76.38	76.45	4.69	4.63	4.64	537	355.6
d10a	76.33	76.55	76.29	4.71	4.56	4.65	534	354.4
d10aR	76.37	76.39	76.4	4.62	4.6	4.64	690	352.9
d15	76.27	76.19	76.15	4.63	4.56	4.44	614	346.2
d15R	76.34	76.28	76.28	4.54	4.55	4.55	613	346.9
d20	76.3	76.28	76.24	4.49	4.51	4.61	689	346.0
d20R	76.29	76.44	76.22	4.59	4.47	4.56	689	346.5
d20a	76.22	76.28	76.34	4.51	4.52	4.5	690	344.0
d20aR	76.37	76.39	76.4	4.62	4.6	4.64	690	352.9

Table A.3 Measured dimensions of the top gusset plate

Specimen	Width (mm)			Thickness (mm)			Length (L _G) (mm)
	1	2	3	1	2	3	
s05	101.83	101.67	101.63	7.99	7.89	7.94	171
s05R	101.82	101.76	101.81	7.89	7.89	7.98	173
s10	101.76	101.74	101.75	8.03	7.95	8.00	210
s10R	101.77	101.75	101.68	7.85	7.88	7.95	210
s10a	101.72	101.71	101.75	7.93	7.86	7.91	210
s10aR	101.89	101.65	101.62	7.92	7.91	7.92	209
s15	101.98	101.83	101.80	7.97	7.95	7.87	115
s15R	101.86	102.07	102.14	7.97	7.99	7.99	247
s20	101.76	101.81	101.65	7.88	7.89	7.95	286
s20R	101.74	101.67	101.69	7.93	7.88	7.92	286
s20a	101.88	101.80	101.82	7.94	7.93	7.98	286
s20aR	101.84	101.78	101.89	7.94	7.93	7.88	286
d05	101.69	101.52	101.48	7.69	7.66	7.74	171
d05R	101.52	101.47	101.34	12.63	12.63	12.78	170
d10	101.83	101.63	101.55	12.76	12.74	12.91	211
d10R	101.18	101.55	101.56	12.72	12.68	12.72	210
d10a	101.46	101.45	101.31	12.89	12.76	12.85	210
d10aR	101.55	101.25	101.39	12.67	12.64	12.68	209
d15	101.36	101.31	101.33	12.84	12.79	12.80	248
d15R	101.67	101.48	101.30	12.64	12.64	12.76	249
d20	101.28	101.30	101.31	12.84	12.62	12.77	284
d20R	101.22	101.21	101.24	12.72	12.72	12.81	285
d20a	101.34	101.45	101.44	12.67	12.70	12.70	284
d20aR	101.60	101.47	101.34	12.72	12.65	12.72	287

Table A.4 Measured dimensions for the bottom gusset plate

Specimen	Width (mm)			Thickness (mm)			Length (L _G) (mm)
	1	2	3	1	2	3	
s05	101.76	101.73	101.72	8.02	7.91	7.88	171
s05R	102.14	101.90	101.94	7.92	7.87	7.88	171
s10	101.60	101.76	101.77	8.01	7.96	8.01	210
s10R	101.57	101.59	101.69	7.98	7.89	7.92	210
s10a	101.69	101.68	101.74	7.89	7.90	7.99	210
s10aR	101.65	101.62	101.76	7.97	7.91	7.95	210
s15	101.85	101.82	101.81	7.88	7.86	7.90	115
s15R	101.84	101.87	101.99	7.97	7.92	7.95	247
s20	101.70	101.79	101.74	7.98	7.95	7.90	286
s20R	101.68	101.78	101.74	7.99	7.91	7.89	286
s20a	101.84	101.89	101.82	7.93	7.87	7.77	286
s20aR	101.89	101.87	101.91	7.99	7.97	7.94	286
d05	101.53	101.41	101.46	7.72	7.62	7.69	170
d05R	101.08	101.33	101.48	12.78	12.63	12.62	170
d10	101.53	101.52	101.66	12.89	12.65	12.61	210
d10R	101.48	101.42	101.56	12.73	12.65	12.66	211
d10a	101.22	101.43	101.49	12.71	12.64	12.73	210
d10aR	101.45	101.36	101.27	12.71	12.65	12.64	210
d15	101.14	101.08	101.25	12.80	12.78	12.81	248
d15R	101.68	101.24	101.42	12.74	12.62	12.60	249
d20	101.12	101.32	101.37	12.76	12.716	12.68	284
d20R	101.25	101.18	101.48	12.81	12.72	12.63	285
d20a	101.45	101.55	101.54	12.75	12.66	12.69	284
d20aR	101.56	101.45	101.30	12.70	12.74	12.64	286

Table A.5 Measured dimensions of welds in the first flat bar for single and double plate specimens

Specimen	Weld 1		Weld 2		Weld 3		Weld 4	
	Size (mm)	Length (mm)	Size (mm)	Length (mm)	Size (mm)	Length (mm)	Size (mm)	Length (mm)
s05	5.01	38.4	5.01	39.4	5.42	38.7	-	42.0
s05R	5.22	43.8	5.04	42.0	5.07	36.5	5.31	37.3
s10	5.06	76.4	5.82	76.4	5.61	76.1	5.86	74.2
s10R	5.27	74.6	5.46	76.4	5.19	76.4	5.62	76.4
s10a	3.22	75.0	3.38	77.9	2.79	77.6	2.79	78.0
s10aR	3.72	73.7	2.97	75.5	2.87	76.1	3.43	76.9
s15	5.05	116.7	5.17	112.7	5.31	115.7	4.92	114.1
s15R	5.31	114.9	4.99	114.1	5.51	116.8	5.53	114.7
s20	5.21	149.0	5.32	149.8	5.09	154.0	4.77	154.9
s20R	4.80	153.4	5.27	153.3	4.80	154.1	4.52	153.6
s20a	2.88	153.9	3.43	152.5	2.97	151.8	3.51	153.5
s20aR	3.79	151.6	2.99	153.4	3.15	154.2	3.54	149.8
d05	5.28	37.4	5.26	37.6	5.18	39.3	4.92	39.5
d05R	4.95	38.4	4.85	36.4	4.89	37.0	5.35	33.8
d10	5.61	78.7	5.34	75.9	5.30	82.6	5.51	79.0
d10R	5.32	80.7	5.18	78.0	5.48	83.0	5.00	79.3
d10a	2.45	77.7	3.12	77.6	2.43	79.7	3.07	75.1
d10aR	3.00	76.6	2.97	76.7	3.12	76.3	3.94	74.7
d15	5.77	118.5	4.93	115.6	5.57	117.2	5.01	113.2
d15R	4.73	120.8	4.92	117.1	4.81	120.7	4.74	116.2
d20	4.80	152.2	5.42	151.4	4.83	153.3	5.16	149.5
d20R	5.66	153.2	5.47	153.1	5.34	154.1	4.84	152.4
d20a	2.94	153.0	2.45	153.1	2.53	149.0	2.98	150.8
d20aR	2.72	153.7	3.13	146.9	2.78	151.9	3.32	152.3

Table A.6 Measured dimensions of welds in second flat bar of double plate specimens

Specimen	Weld 5		Weld 6		Weld 7		Weld 8	
	Size (mm)	Length (mm)	Size (mm)	Length (mm)	Size (mm)	Length (mm)	Size (mm)	Length (mm)
d05	5.12	37.8	5.14	39.1	5.20	39.0	4.93	35.5
d05R	4.79	41.0	5.22	38.8	5.27	43.3	4.85	37.5
d10	5.25	78.4	4.97	81.7	5.55	80.4	4.47	79.3
d10R	5.90	74.9	5.12	78.5	4.68	78.6	5.10	77.5
d10a	3.11	75.8	2.81	76.8	3.16	78.7	2.77	77.5
d10aR	2.55	76.5	3.05	78.1	3.06	77.4	3.83	74.7
d15	4.58	115.7	5.01	116.3	4.81	116.4	5.13	115.8
d15R	5.64	114.3	4.94	113.0	5.42	117.8	5.34	112.6
d20	4.92	151.3	5.13	152.8	5.49	152.7	4.89	152.1
d20R	5.49	153.0	5.36	153.8	5.32	151.0	5.19	152.5
d20a	3.16	150.8	2.45	151.4	2.85	152.7	3.38	148.4
d20aR	2.68	150.0	3.44	149.9	2.41	151.9	3.52	153.9

Figure A.1 Identification of fillet weld 1 to 4 on the strain gauge side flat bar

Figure A.2 Identification of fillet weld 5-8 on the non-strain gauge flat bar

APPENDIX B ADDITIONAL EXPERIMENTAL RESULTS

More detailed information on the correction applied to the static stresses from the coupon tests using the extensometer loading control, along with detailed test results from all the coupons and specimens are presented in this appendix.

B.1 Tension coupon test results

As outlined in Chapter 4, all test specimens used the stroke displacement to control the loading rate, while some of the coupons had the loading controlled with the extensometer displacement. When the loading machine was put on pause to take static readings, tests that used extensometer control exhibited a significantly higher load drop than tests that used stroke control. Thus, a correction was applied to the static stress readings from the coupon tests that used extensometer control in the form of a ratio. That ratio is taken as the load drop after switching to stroke control with the removal of the extensometer over the load drop prior to switching to stroke control, and is expressed as

$$\delta_{\text{corr}} = \frac{\Delta F_{\text{SD}}}{\Delta F_{\text{ED}}} \quad (\text{B.1})$$

where ΔF_{SD} is the average load drop of the first two static readings after switching to stroke control with the removal of the extensometer, and ΔF_{ED} is the average load drop of the last two static readings prior to switching to stroke control. The correction using the ratio is applied to the load drop at static readings such that

$$F_{\text{S}} = F_{\text{D}} - \delta_{\text{corr}} \Delta F_{\text{E}} \quad (\text{B.2})$$

where F_s is the static load after applying the correction for stroke control, ΔF_E is the load drop when the machine is put on pause to take the static readings under extensometer control, and F_D is the dynamic load. The correction according to (B.2) is applied to the measured static load (stress) in coupon tests that used extensometer control. The ratio used in the correction and the corrected yield and ultimate stresses are presented in Table B.1. It can be seen that the ratio only varies slightly among coupons of the same material, and the corrected yield and ultimate stresses are consistent with those from tests that used stroke control. The average stress versus strain curve for all coupons can be seen in Figs. B.1 to B.3. After the extensometer was removed, the engineering strain was calculated using the expression

$$\varepsilon^e = \varepsilon_o + \frac{\Delta L}{50.8} \quad (B.3)$$

where ε_o is the engineering strain right before the removal of the extensometer and ΔL is the subsequent change in the stroke displacement after the removal of the extensometer.

B.2 Tension specimen test results

The average stress versus LVDT displacement curve and failure mode of all single and double plate specimens are provided in Figs B.4 to B.27.

Table B.1 Dynamic and static stresses for tension coupons of the flat bar, 12.7 mm and 7.9 mm thick gusset plates

Material	Extensometer control load drop ΔF_{ED} (MPa)	Stroke control load drop ΔF_{SD} (MPa)	Ratio δ_{corr}	Yield		Ultimate	
				Dynamic stress F_D (MPa)	Corrected static stress F_S (MPa)	Dynamic stress F_D (MPa)	Corrected static stress F_S (MPa)
FB 76 x 4.8 mm							
B2	34	21	0.618	352	331	514	493
B3	-	27	-	352	325	520	493
B4	-	28	-	354	326	518	490
FB102 x 12.7 mm							
A1	43	22	0.512	337	315	542	520
A2	32	19	0.594	335	316	538	519
A3	-	24	-	334	310	533	509
A4	-	25	-	327	302	530	505
FB 102 x 7.9 mm							
C1	41	24	0.585	381	357	590	566
C2	35	24	0.686	372	348	582	558
C7	-	26	-	376	350	581	555
C8	-	23	-	389	366	586	563

Figure B.1 Engineering stress versus engineering strain curves for FB 102 x 12.7 mm tension coupons

Figure B.2 Engineering stress versus engineering strain curves for FB 76 x 4.8 mm tension coupon

Figure B.3 Engineering stress versus engineering strain curves for FB 102 x 7.9 mm tension coupons

Figure B.4 Average stress versus LVDT displacement for double plate specimens with $L/w = 0.5$ and $s/t = 1.0$

Figure B.5 Weld failure for double plate specimens d05 and d05R

Figure B.6 Average stress versus LVDT displacement for double plate specimens with $L/w = 1.0$ and $s/t = 1.0$

Figure B.7 Plate failure for double plate specimens d10 and d10R

Figure B.8 Average stress versus LVDT displacement for double plate specimens with $L/w = 1.0$ and $s/t = 0.63$

Figure B.9 Weld failure for double plate specimens d10a and d10aR

Figure B.10 Average stress versus LVDT displacement for double plate specimens with $L/w = 1.5$ and $s/t = 1.0$

Figure B.11 Plate failure for specimens d15 and d15R

Figure B.12 Average stress versus LVDT displacement for double plate specimens with $L/w = 2.0$ and $s/t = 1.0$

Figure B.13 Plate failure for double plate specimens d20 and d20R

Figure B.14 Average stress versus LVDT displacement for double plate specimens with $L/w = 2.0$ and $s/t = 0.63$

Figure B.15 Plate failure of double plate specimens d20a and d20aR

Figure B.16 Average stress versus LVDT displacement for single plate specimens with $L/w = 0.5$ and $s/t = 1.0$

Figure B.17 Weld failure for single plate specimens s05 and s05R

Figure B.18 Average stress versus LVDT displacement for single plate specimens with $L/w = 1.0$ and $s/t = 1.0$

Figure B.19 Plate failure for single plate specimens s10 and s10R

Figure B.20 Average stress versus LVDT displacement for single plate specimens with $L/w = 1.0$ and $s/t = 0.63$

Figure B.21 Weld failure for single plate specimens s10a and s10aR

Figure B.22 Average stress versus LVDT displacement for single plate specimens with $L/w = 1.5$ and $s/t = 1.0$

Figure B.23 Plate failure for single plate specimens s15 and s15R

Figure B.24 Average stress versus LVDT displacement for single plate specimens with $L/w = 2.0$ and $s/t = 1.0$

Figure B.25 Plate failure for single plate specimens s20 and s20R

Figure B.26 Average stress versus LVDT displacement for single plate specimens with $L/w = 2.0$ and $s/t = 0.63$

Figure B.27 Plate failure for single plate specimens s20a and s20aR

APPENDIX C DETERMINATION OF TRUE STRESS VERSUS TRUE PLASTIC STRAIN RELATIONSHIP

The procedure used by Zhao (2005) was employed to determine the true stress-true plastic strain relationship of the material from the tension coupon tests. It follows an iterative process combined with a numerical simulation to determine the final true stress-true plastic strain relationship after the peak load.

C.1 True stress-true plastic strain values before peak load

Up to the peak load, the true stress versus true plastic strain values are derived from the engineering stress and engineering strain as

$$\sigma^t = \sigma^e (1 + \varepsilon^e), \text{ and} \quad (C.1)$$

$$\varepsilon^p = \varepsilon^t - \frac{\sigma^t}{E}, \text{ with} \quad (C.2)$$

$$\varepsilon^t = \ln(1 + \varepsilon^e) = \ln\left(\frac{A_o}{A}\right) \quad (C.3)$$

where σ^t is the true stress, σ^e is the engineering stress, ε^p is the true plastic strain, ε^t is the true strain, ε^e is the engineering strain, E is the modulus of elasticity, A_o is the undeformed cross-section area and A is the current cross-section area. For all coupons, the static stress from the stroke control tests and extensometer control tests corrected for the equivalent of the stroke control test were used.

C.2 True stress-true plastic strain values after peak load

After the peak load, an iterative numerical method is used to obtain the true stress-true plastic strain relationship of the material. The initial true stress values was estimated using the following expression

$$\sigma^t = \frac{F}{A} \quad (C.4)$$

where F is the current load, and A the current average cross-sectional area defined by

$$A = \frac{A_{\text{mid}} + A_{\text{cor}}}{2}, \text{ with} \quad (C.5)$$

$$A_{\text{mid}} = t_m w_m, \text{ and} \quad (C.6)$$

$$A_{\text{cor}} = t_c w_c. \quad (C.7)$$

A_{mid} and A_{cor} are the cross-sectional areas based on the thickness (t_m) and width (w_m) measured at the mid-point of the sides and the thickness (t_c) and width (w_c) at the corner of coupon, as shown in Fig. C.1. The true plastic strain values were determined using Eqs. (C.2) and (C.3). Due to the non-uniaxial hydrostatic stress state in the coupon after the onset of necking, Eq. (C.4) tends to overestimate the true stress. However, the method proposed by Zhao (2005) where only 75% of the true stress increase is used, was found to provide a good basis to establish the first trial of the iteration to determine the true stress-true plastic strain relationship of the material. The true stress-true plastic strain relationship is then established by the following power law function

$$\varepsilon^p = C \left[\left(\frac{\sigma^t}{\sigma_o^p} \right)^n - 1 \right] + \varepsilon_o^p \quad (C.8)$$

where σ_o^p and ε_o^p are the true stress and true plastic strain at the start of strain hardening, while C and n are parameters to be determined through the iteration process. Using the method outlined by Zhao (2005), the function is forced to pass through the point at peak load, thus the relationship between C and n can be established as

$$\varepsilon_f^p = C \left[\left(\frac{\sigma_f^t}{\sigma_o^t} \right)^n - 1 \right] + \varepsilon_o^p, \text{ and} \quad (\text{C.9})$$

$$C = \frac{\varepsilon_f^p - \varepsilon_o^p}{\left(\frac{\sigma_f^t}{\sigma_o^t} \right)^n - 1} \quad (\text{C.10})$$

where ε_f^p and σ_f^t are the true plastic strain and the true stress at the peak load.

An assumed true stress versus true plastic strain relationship is developed by choosing a value of n for Eq. (C.8) that closely matches the experimental true stress-true plastic strain values calculated using only 75% of the true stress increase after peak load. The true stress is calculated for a given true plastic strain using a rearranged Eq. (C.8)

$$\sigma^t = \sigma_o^t \left(\frac{\varepsilon^p - \varepsilon_o^p}{C} + 1 \right)^{\frac{1}{n}}. \quad (\text{C.11})$$

The true stress-true plastic strain values are then used as the material properties input in a numerical simulation of a cylindrical tension coupon modeled with a bi-quadratic axisymmetric element (CAX8R) with reduced integration, as shown in Fig. C.2. Since Naqvi (2004) has found that there was no significant difference between the load versus transverse deformation curve for rectangular and cylindrical coupons, a cylindrical model was used in the numerical simulation to reduce computational time. The engineering stress versus change in cross-section area ($1-A/A_o$) is plotted for both the test data and the numerical simulation for the first trial value of n . A comparison is made between the test and predicted engineering stresses at the last test data point in order to generate the second trial value of n . If the numerical simulation overestimates the engineering stress at the last data point ($1-A/A_o$)_f, then the second trial value of n is adjusted until the true stress of the generated true stress-true plastic strain curve at the last true plastic strain test

data point is lower than the true stress of the first trial by twice the engineering stress overestimation. The opposite is applied if the engineering stress of the numerical simulation underestimates the stress at the last data point $(1-A/A_o)_f$.

The third trial value of n is obtained through interpolation (extrapolation) using the first two trial values using

$$n_{i+1} = \frac{n_{i-1} - n_i}{(\sigma_f^e - \sigma_i^e) - (\sigma_f^e - \sigma_{i-1}^e)} (\sigma_f^e - \sigma_i^e) + n_i \quad (C.12)$$

where σ_f^e is the engineering stress at the last data point $(1-A/A_o)_f$ from the tests, σ_{i-1}^e is the corresponding predicted engineering stress at the first iteration, and σ_i^e is the corresponding predicted engineering stress at the second iteration. The value of n obtained from Eq. (C.12) is used in generating the final true stress-true plastic strain curve employed in the numerical simulations of the test specimens. Normally, more iterations may have to be carried out before the predicted matches the engineering stress test at the last data point $(1-A/A_o)_f$. Calibrated values of n and C for FB 76 x 4.8, FB 102 x 12.7 and FB 102 x 7.9 are provided in Table C.1, while the engineering stress versus change in cross-section area $(1-A/A_o)$ and true stress versus true plastic strain curves are presented in Figs. C.3 to C.8. The values for σ_f^t , σ_o^t , ε_f^p , ε_o^p , and ε_f^e for all three materials are provided in Table C.2.

C.3 True stress-true plastic strain relationship for the weld

The average true stress-true plastic strain curve from four different all-weld metal coupons of 15.9 mm (5/8") thick butt welded plates tested by Naqvi (2004) was taken as the true stress-true plastic strain relationship for the weld used in the numerical

simulation. Values of n , C , σ_0^t and ε_0^p used in generating the true stress-true plastic strain curve are provided in Table C.3. The true stress-true plastic strain values for each coupon are provided in Table C.4 and plotted in Fig. C.9 along with the used in the numerical simulation of the tension specimens.

Table C.1 C and n values at each iteration used in deriving the true stress-true plastic strain curve for the tension coupons

Trial	n	C	σ_e^f	σ_{i-1}^e (MPa)	σ_i^e (MPa)	$1-(A/A_o)_f$
FB 76 x 4.8 mm						
1	6.00	0.0087	406.3	-	410.7	0.517
2	6.29	0.0076	406.3	410.7	406.6	0.517
3	6.31	0.0075	406.3	406.6	406.9	0.517
FB 102 x 12.7 mm						
1	4.90	0.0115	403.9	-	426.9	0.553
2	5.86	0.0070	403.9	426.9	409.6	0.553
3	6.18	0.0061	403.9	409.6	405.1	0.553
FB 102 x 7.9 mm						
1	5.20	0.0090	430.2	-	462.2	0.553
2	6.75	0.0038	430.2	462.2	438.8	0.553
3	7.32	0.0028	430.2	438.8	433.0	0.553

Table C.2 Constants used in generating the true stress-true plastic strain curve for the tension coupons

Specimen	ϵ_f^p	ϵ_o^p	σ_f^t	σ_o^t	ϵ_f^e
FB 76 x 4.8 mm	0.159	0.023	577.9	361.9	0.724
FB 102 x 12.7 mm	0.133	0.020	579.4	356.5	0.800
FB 102 x 7.9 mm	0.137	0.008	644.7	381.2	0.801

Table C.3 C and n values used in deriving the true stress-true plastic strain relationship for the weld material

Coupon	n	C	ε_o^p	σ_o^t
T5-R1.5	4.95	0.0511	0.03070	496.4
T5-R2	4.92	0.0470	0.03070	507.6
T5-R2.5	4.84	0.0579	0.03070	506.4
T5-R3	4.93	0.0267	0.03070	435.8

Table C.4 True stress-true plastic strain values for the weld material

ε^p	T5-R1.5 σ^t (MPa)	T5-R2 σ^t (MPa)	T5-R2.5 σ^t (MPa)	T5-R3 σ^t (MPa)	Average σ_{avg}^t (MPa)
0.00	496.4	507.6	506.4	435.8	486.6
0.03	496.4	507.6	506.4	435.8	486.5
0.08	563.3	581.2	569.7	531.6	561.5
0.11	596.6	617.1	602.1	572.6	597.1
0.13	619.9	642.1	625.0	599.9	621.7
0.16	639.7	663.3	644.7	622.6	642.6
0.16	640.4	664.1	645.4	623.4	643.3
0.18	654.2	678.7	659.1	638.8	657.7
0.20	666.9	692.3	671.7	652.9	671.0
0.24	689.7	716.6	694.6	678.0	694.7
0.30	719.2	747.9	724.3	709.9	725.3
0.40	759.8	790.8	765.3	753.0	767.2
0.50	793.3	826.2	799.3	788.2	801.8
0.60	822.0	856.4	828.5	818.2	831.3
0.70	847.1	883.0	854.3	844.3	857.2
0.80	869.7	906.8	877.3	867.6	880.3
0.86	882.2	920.0	890.1	880.5	893.2
1.00	908.9	948.1	917.4	908.0	920.6
1.20	942.3	983.3	951.8	942.3	954.9
1.40	971.7	1014.2	982.0	972.3	985.0

Figure C.1 Tension coupons measurement locations

Figure C.2 Finite element mesh for the cylindrical coupon analysis

Figure C.3 Engineering stress versus change in cross-sectional area for FB 76 x 4.8 mm

Figure C.4 True stress versus true plastic strain relationship for FB 76 x 4.8 mm

Figure C.5 Engineering stress versus change in cross-sectional area for FB 102 x 12.7 mm

Figure C.6 True stress versus true plastic strain relationship for FB 102 x 12.7 mm

Figure C.7 Engineering stress versus change in cross-sectional are for FB 102 x 7.9 mm

Figure C.8 True stress versus true plastic strain relationship for FB 102 x 7.9 mm

Figure C.9 True stress versus true plastic strain relationship for the weld material

APPENDIX D ADDITIONAL DATA

Experimental results from tests conducted by the American Welding Society (AWS 1931) and Easterling and Giroux (1993) are provided in this appendix, along with the average values for each test series. Both the original width and the modified width (w_s) are included in the data.

Table D.1 Test results from the AWS 2122 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.85
2	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.91
3	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.85
4	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.59
5	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.63
6	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.61
7	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.80
8	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.77
9	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.82
10	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.67
11	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.70
12	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.71
13	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.66
14	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.66
15	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.63

Table D.2 Test results from the AWS 2133 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.68
2	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.71
3	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.71
4	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.71
5	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.69
6	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.66
7	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.50
8	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.58
9	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.50
10	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.45
11	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.54
12	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.53

Table D.3 Test results from the AWS 2144 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.57
2	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.54
3	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.58
4	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.62
5	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.68
6	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.64
7	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.53
8	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.53
9	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.58
10	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.58
11	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.51
12	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.55
13	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.65
14	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.65

Table D.4 Test results from the AWS 2155 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.54
2	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.54
3	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.55
4	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.50
5	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.52
6	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.50
7	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.66
8	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.61
9	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.45
10	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.52
11	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.55
12	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.56
13	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.43
14	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.52
15	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.58
16	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.63

Table D.5 Test results from the AWS 2166 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _c
1	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.50
2	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.49
3	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.50
4	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.50
5	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.50
6	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.49
7	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.51
8	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.52
9	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.51
10	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.52
11	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.52
12	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.52

Table D.6 Test results for the AWS 2322 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _c
1	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.81
2	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.76
3	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.82
4	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.68
5	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.68
6	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.68
7	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.77
8	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.87
9	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.80
10	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.62
11	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.72
12	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.74
13	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.63
14	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.63
15	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.56

Table D.7 Test results from the AWS 2333 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.76
2	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.77
3	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.81
4	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.79
5	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.79
6	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.78
7	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.59
8	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.58
9	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.56
10	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.57
11	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.60
12	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.61

Table D.8 Test results from the AWS 2344 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.65
2	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.58
3	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.62
4	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	-
5	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.69
6	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.71
7	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.60
8	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.55
9	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.57
10	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.66
11	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.63
12	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.71
13	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.68
14	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.72

Table D.9 Test results from the AWS 2355 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.57
2	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.57
3	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.54
4	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.51
5	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.60
6	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.58
7	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.49
8	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.50
9	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.63
10	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.57
11	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.56
12	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.55
13	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.63
14	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.66

Table D.10 Test results from the AWS 2366 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _c
1	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.54
2	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.50
3	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.60
4	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.62
5	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.61
6	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.60
7	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.53
8	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.54
9	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.52
10	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.60
11	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.57
12	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.65
13	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.64

Table D.11 Test results from the AWS 2143 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.73
2	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.72
3	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.74
4	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.76
5	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.72
6	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.75
7	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.60
8	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.58
9	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.65
10	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.67
11	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.67
12	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.63
13	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.77
14	3.00	0.375	3.00	0.5	3.75	0.75	1.00	0.80	0.73

Table D.12 Test results from the AWS 2153 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.77
2	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.73
3	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.78
4	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.77
5	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.76
6	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.78
7	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.63
8	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.69
9	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.74
10	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.78
11	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.66
12	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.72
13	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.77
14	4.00	0.375	3.00	0.625	3.75	0.6	1.33	1.07	0.85

Table D.13 Test results from the AWS 2163 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.69
2	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.65
3	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.68
4	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.66
5	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.64
6	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.65
7	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.65
8	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.73
9	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.67
10	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.65
11	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.63
12	4.00	0.375	3.00	0.75	3.75	0.5	1.33	1.07	0.66

Table D.14 Test results from the AWS 2343 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.743
2	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.754
3	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.725
4	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.811
5	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.806
6	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.798
7	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.666
8	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.709
9	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.741
10	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.814
11	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.841
12	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.792
13	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.833
14	3.00	0.375	3.00	0.5	3.75	0.75	1	0.8	0.88

Table D.15 Test results from the AWS 2353 series

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
1	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.87
2	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.78
3	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.69
4	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.85
5	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	-
6	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	-
7	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.74
8	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.74
9	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.85
10	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.79
11	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.78
12	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.88
13	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.89
14	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.86

Table D.16 Test results from the AWS 2363 series

Test #	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U
1	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.80
2	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.81
3	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.78
4	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.74
5	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.74
6	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.76
7	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.62
8	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	-
9	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.66
10	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.79
11	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.75
12	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.74
13	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.75
14	4.00	0.375	3.00	0.75	3.75	0.50	1.33	1.07	0.82

Table D.17 Average test results for each AWS series

Series	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _e
2122	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.72
2133	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.61
2144	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.59
2155	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.54
2166	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.51
2322	2.00	0.250	3.00	0.250	3.50	1.00	0.67	0.57	0.72
2333	2.00	0.375	3.00	0.375	3.75	1.00	0.67	0.53	0.69
2344	2.00	0.500	3.00	0.500	4.00	1.00	0.67	0.50	0.64
2355	2.00	0.625	3.00	0.625	4.25	1.00	0.67	0.47	0.57
2366	2.00	0.750	3.00	0.750	4.50	1.00	0.67	0.44	0.58
2143	3.00	0.375	3.00	0.500	3.75	0.75	1.00	0.80	0.69
2153	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.75
2163	4.00	0.375	3.00	0.750	3.75	0.50	1.33	1.07	0.66
2343	3.00	0.375	3.00	0.500	3.75	0.75	1.00	0.80	0.78
2353	4.00	0.375	3.00	0.625	3.75	0.60	1.33	1.07	0.81
2363	4.00	0.375	3.00	0.750	3.75	0.50	1.33	1.07	0.75

Table D.18 Test results of Easterling and Giroux plate specimens

Test	L (in.)	s (in.)	w (in.)	t (in.)	w _s (in.)	s/t	L/w	L/w _s	U _c
1	4.00	0.250	3.00	0.250	3.50	1.00	1.33	1.14	0.94
2	4.00	0.250	3.00	0.250	3.50	1.00	1.33	1.14	0.98
3	4.00	0.250	3.00	0.250	3.50	1.00	1.33	1.14	1.00
4	5.00	0.250	3.00	0.250	3.50	1.00	1.70	1.43	0.98
5	5.00	0.250	3.00	0.250	3.50	1.00	1.70	1.43	0.98
6	5.00	0.250	3.00	0.250	3.50	1.00	1.70	1.43	0.96