DEVELOPMENT OF THE TOLERANT WIND TUNNEL FOR BLUFF BODY TESTING

by

MICHEL HAMEURY

M.A.Sc., ECOLE POLYTECHNIQUE, 1982

A THESIS SUBMITTED IN PARTIAL FULFILMENT OF THE REQUIREMENTS FOR THE DEGREE OF DOCTOR OF PHILOSOPHY

in

THE FACULTY OF GRADUATE STUDIES

Department of Mechanical Engineering

We accept this thesis as conforming to the required standard

THE UNIVERSITY OF BRITISH COLUMBIA

February 1987

MICHEL HAMEURY, 1987
In presenting this thesis in partial fulfilment of the requirements for an advanced degree at the
THE UNIVERSITY OF BRITISH COLUMBIA, I agree that the Library shall make it freely available for
reference and study. I further agree that permission for extensive copying of this thesis for
scholarly purposes may be granted by the Head of my Department or by his or her
representatives. It is understood that copying or publication of this thesis for financial gain shall
not be allowed without my written permission.

Department of Mechanical Engineering

THE UNIVERSITY OF BRITISH COLUMBIA
2075 Wesbrook Place
Vancouver, Canada
V6T 1W5

Date: FEBRUARY 1987
In conventional wind tunnels the solid-wall or open-jet test section imposes on the flow field around the test model new boundary conditions absent in free air. Unless a small model is used, the solid-wall test section generally increases the loadings on the model while the open-jet boundary decreases the loadings compared to the unconfined case.

However, the development of a low wall-interference test section and its successful demonstration would allow the testing of relatively large models without the application of often uncertain correction formulae.

The Tolerant wind tunnel, which makes use of the opposite effects of solid and open boundaries, is a transversely slatted-wall test section designed to produce at an optimal wall open-area ratio (OAR) low-correction data for a wide variety of model shapes and sizes. Initially intended for low-speed airfoil testing, its use is theoretically and experimentally investigated here in connection with bluff body testing.

A simple mathematical model based on two-dimensional potential flow theory and solved with the help of a vortex surface-singularity technique is used to estimate the best wall configuration. The theory predicts an optimum OAR of about 0.45 at which pressure distributions on flat plate and circular cylinder models of blockage ratios up to 33.3 % would differ from the free-air values by not more than 1 %.

On the other hand, experiments performed with flat plate, circular cylinder and circular-cylinder-with-splitter-plate models indicate the existence of an optimum configuration around OAR = 0.6. The experiments also show a maximum allowable blockage in the Tolerant wind tunnel to be equivalent to the blockage created by a 33.3 %-blockage-ratio flat plate model.
# Table of Contents

Abstract ................................................................. ii  
List of Figures .......................................................... vi  
Nomenclature ............................................................ xii  
Acknowledgements ....................................................... xv  

1. INTRODUCTION ....................................................... 1  
   1.1 Generalities ....................................................... 1  
   1.2 An Overview of the Related Literature ......................... 4  

2. THE TOLERANT TEST SECTION ....................................... 7  
   2.1 General Description ............................................. 7  
   2.2 Degrees of Freedom ............................................. 8  

3. MODELLING OF BLUFF BODIES IN THE TOLERANT WIND TUNNEL .... 10  
   3.1 Bluff Bodies .................................................... 10  
      3.1.1 Definitions and Descriptions ................................. 10  
      3.1.2 Wall Effects on Bluff Bodies ................................. 11  
      3.1.3 Bluff Body Models .......................................... 12  
   3.2 Numerical Model of the Tolerant Wind Tunnel .................. 14  
      3.2.1 Wake Source Model in the Tolerant Wind Tunnel .......... 14  
      3.2.2 Mathematical Representation ............................... 18  

4. NUMERICAL RESULTS ................................................ 25  
   4.1 Free Air Results ................................................ 25  
      4.1.1 Computation in the Transform Plane ......................... 25  
      4.1.2 Computation in the Physical Plane ......................... 26  
   4.2 Solid-Wall Confined Flow Results ............................... 27  
      4.2.1 Flat Plate Model ........................................... 27  
      4.2.2 Circular Cylinder Model ................................... 28  
   4.3 Tolerant Wind Tunnel Results ................................... 29  
      4.3.1 Flat Plate Model ........................................... 29
List of Figures

Figure 2.1: Single-slatted-wall tunnel configuration for airfoil testing.

Figure 2.2: Double-slatted-wall tunnel configuration for bluff body testing.

Figure 3.1 (a): Physical and basic transform planes for a flat plate model.

Figure 3.1 (b): Physical and basic transform planes for a circular cylinder model.

Figure 3.2: Theoretical representation of the Tolerant wind tunnel.

Figure 4.1: Pressure distribution over a normal flat plate in unconfined flow: comparison of numerical calculation in transform plane with analytical solution. Given $C_{pb} = -1.38$, $N = 70$

Figure 4.2: Pressure distribution over a circular cylinder in unconfined flow: comparison of numerical calculation in transform plane with analytical solution. Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $N = 70$

Figure 4.3: Variation of source strength with number of panels in transform plane, for flat plate and circular cylinder in unconfined flow.

Figure 4.4: Pressure distribution over a normal flat plate in unconfined flow: comparison of numerical calculation in physical plane with analytical solution. Given $C_{pb} = -1.38$, $N = 60$

Figure 4.5: Pressure distribution over a circular cylinder in unconfined flow: comparison of numerical calculation in physical plane with analytical solution. Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $N = 60$

Figure 4.6 (a): Variation of source strength with number of panels in physical plane, for flat plate and circular cylinder in unconfined flow.

Figure 4.6 (b): Variation of base pressure coefficient with number of panels in physical plane, for flat plate and circular cylinder in unconfined flow.

Figure 4.7: Pressure distribution over a normal flat plate in solid-wall confined flow: comparison of numerical calculation in physical plane with analytical solution. Given $C_{pb} = -1.0$, $h/H = 1/3$, $N(\text{model}) = 80$, $N(\text{wall}) = 20$

Figure 4.8: Corrected pressure distribution over a normal flat plate in solid-wall confined flow: comparison of corrected numerical calculation in physical plane with free-air analytical solution. Given $C_{pb} = -1.0$, $h/H = 1/3$, $CF = 0.6749$

Figure 4.9: Variation of base pressure coefficient with number of panels on solid walls, for a normal flat plate model in confined flow. Given $C_{pb} = -1.0$, $h/H = 1/3$, Wall Length = 12
Figure 4.10: Variation of base pressure coefficient with wall length, for a normal flat plate in confined flow.
Given $C_{pb} = -1.0$, $h/H = 1/3$, N(model) = 80, N(wall) = 20

Figure 4.11: Variation of base pressure coefficient with blockage ratio, for a normal flat plate in confined flow.
Given $C_{pb} = -1.0$, N(model) = 80, N(wall) = 20

Figure 4.12: Variation of blockage-correction factor with blockage ratio, for a normal flat plate in confined flow.
Given $C_{pb} = -1.0$, N(model) = 80, N(wall) = 20

Figure 4.13: Pressure distribution over a circular cylinder in solid-wall confined flow: comparison of numerical calculation in physical plane with free-air analytical solution.
Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, N(model) = 80, N(wall) = 20

Figure 4.14: Corrected pressure distribution over a circular cylinder in solid-wall confined flow: comparison of corrected numerical calculation in physical plane with free-air analytical solution.
Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, CF = 0.7827

Figure 4.15: Variation of base pressure coefficient with number of panels on solid walls, for a circular cylinder model in confined flow.
Given $C_{pb} = -1.0$, $\beta_s = 80^\circ$, $h/H = 1/3$, Wall Length = 12

Figure 4.16: Variation of base pressure coefficient with wall length, for a circular cylinder in confined flow.
Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, N(model) = 80, N(wall) = 20

Figure 4.17: Variation of base pressure coefficient with blockage ratio, for a circular cylinder in confined flow.
Given $C_{pb} = -1.0$, $\beta_s = 80^\circ$, N(model) = 80, N(wall) = 20

Figure 4.18: Variation of blockage-correction factor with blockage ratio, for a circular cylinder in confined flow.
Given $C_{pb} = -1.0$, $\beta_s = 80^\circ$, N(model) = 80, N(wall) = 20

Figure 4.19: Theoretical variation of base pressure coefficient as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.

Figure 4.20: Theoretical variation of blockage correction factor as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.

Figure 4.21: Theoretical variation of standard deviation as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.

Figure 4.22: Theoretical variation of pressure coefficient at $\beta = 30^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$
Figure 4.23: Theoretical variation of pressure coefficient at $\beta = 60^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Figure 4.24: Theoretical variation of pressure coefficient at $\beta = 70^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Figure 4.25: Theoretical variation of base pressure coefficient ($\beta = 80^\circ$) as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Figure 4.26: Theoretical variation of blockage correction factor as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Figure 4.27: Theoretical variation of standard deviation as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Figure 5.1: The closed-circuit "Green" wind tunnel.

Figure 5.2: Pressure tap positions on the floor and in the plenum.

Figure 5.3: Pressure tap positions on models.

Figure 5.4: Tuft positions in the plenum.

Figure 5.5: Modified "Green" wind tunnel for smoke flow visualization.

Figure 6.1 (a) to (m): Pressure distributions over 3 different sizes of flat plate model.
$Re = 10^5$

Figure 6.2 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (dimensionalized plot).

Figure 6.3 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (non-dimensionalized plot).

Figure 6.4: Variation of base pressure coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.5: Variation of front drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.6: Variation of drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.7: Variation of Strouhal number as a function of OAR for 3 sizes of flat plate model positioned at center.
Figure 6.8: Variation of blockage-correction factor as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.9: Variation of standard deviation as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.10 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (dimensionalized plot).

Figure 6.11 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (non-dimensionalized plot).

Figure 6.12: Variation of base pressure coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.13: Variation of front drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.14: Variation of drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.15: Variation of Strouhal number as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.16: Variation of blockage-correction factor as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.17: Variation of standard deviation as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model. \( \text{Re} = 10^5 \)

Figure 6.19: Variation of pressure coefficient at \( \beta = 50^\circ \) as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.20: Variation of pressure coefficient at \( \beta = 100^\circ \) as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.21: Variation of pressure coefficient at \( \beta = 180^\circ \) as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.22: Variation of front drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.23: Variation of rear drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.24: Variation of drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Figure 6.25: Variation of Strouhal number as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.26: Variation of blockage-correction factor as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.27: Variation of standard deviation as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.28: Pressure distributions over 4 sizes of circular cylinder model tested between non-evenly spaced slatted-wall.  
Re = 10^{5}, OAR = 0.453, A0RT = 1.5

Figure 6.29: Pressure distributions over 4 sizes of circular cylinder model tested between non-evenly spaced slatted-wall.  
Re = 10^{5}, OAR = 0.453, A0RT = 3.0

Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. Re = 10^{5}

Figure 6.31: Variation of pressure coefficient at $\beta = 50^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.32: Variation of pressure coefficient at $\beta = 100^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.33: Variation of pressure coefficient at $\beta = 180^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.34: Variation of front drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.35: Variation of rear drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.36: Variation of drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.37: Variation of blockage-correction factor as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.38: Variation of standard deviation as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.39: General flow pattern in the plenums for normal operation.
Figure 6.40 (a) to (l): Plenum pressure distributions for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.41: General flow pattern in the plenums for extreme conditions.

Figure 6.42: Plenum pressure distributions corresponding to testing of normal flat plates at 22 inches upstream of the test section center. OAR = 0.563
### Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>Test section height.</td>
</tr>
<tr>
<td>A0RT</td>
<td>Graded OAR parameter.</td>
</tr>
<tr>
<td>A(_1), A(_2)</td>
<td>Constants.</td>
</tr>
<tr>
<td>a</td>
<td>Radius of the circle in the ( \zeta )-plane.</td>
</tr>
<tr>
<td>a(_i)</td>
<td>Open areas.</td>
</tr>
<tr>
<td>C</td>
<td>Constant.</td>
</tr>
<tr>
<td>C(_i)</td>
<td>Model boundary in the Z-plane.</td>
</tr>
<tr>
<td>C(_d)</td>
<td>Drag coefficient.</td>
</tr>
<tr>
<td>CF</td>
<td>Blockage correction factor.</td>
</tr>
<tr>
<td>C(_b)</td>
<td>Pressure coefficient.</td>
</tr>
<tr>
<td>C(_pb)</td>
<td>Base pressure coefficient.</td>
</tr>
<tr>
<td>C(_pmin)</td>
<td>Minimum pressure coefficient.</td>
</tr>
<tr>
<td>C(_p0)</td>
<td>Empty-test-section pressure coefficient.</td>
</tr>
<tr>
<td>c / H</td>
<td>Normalized chord length of airfoil slats.</td>
</tr>
<tr>
<td>D</td>
<td>Plenum depth.</td>
</tr>
<tr>
<td>f</td>
<td>Vortex-shedding frequency.</td>
</tr>
<tr>
<td>H</td>
<td>Test section width.</td>
</tr>
<tr>
<td>H</td>
<td>Total Head.</td>
</tr>
<tr>
<td>h</td>
<td>Model width.</td>
</tr>
<tr>
<td>K(_1), K(_2)</td>
<td>Calibration constants.</td>
</tr>
<tr>
<td>L</td>
<td>Test section length.</td>
</tr>
<tr>
<td>N</td>
<td>Number of panels.</td>
</tr>
<tr>
<td>n</td>
<td>Number of slats.</td>
</tr>
<tr>
<td>OAR</td>
<td>Open area ratio.</td>
</tr>
</tbody>
</table>
$P_\infty$  Free-stream static pressure.
$Q$  Source strength.
$q$  Dynamic pressure.
$Re$  Reynolds number.
$SD$  Standard deviation.
$St$  Strouhal number.
$S_1, S_2$  Separation positions.
$s$  Boundary surfaces.
$t/H$  Normalized thickness of airfoil slats.
$U$  Velocity of uniform flow.
$V$  Normalized velocity.
$v$  Velocity.
$x, y$  Cartesian coordinates.
$Z$  Physical plane.
$a$  Separation angle in the $\xi$-plane.
$a$  Free stream angle to the x-axis.
$\beta$  Angular position on C in the Z-plane.
$\beta$  Panel angle to the x-axis.
$\beta_\xi$  Separation angles in the Z-plane.
$\gamma$  Circle boundary in the $\xi$-plane.
$\gamma(s)$  Vortex strength per unit length of perimeter.
$\delta$  Source angular positions.
$\epsilon x_1$  Uncertainty in $x_1$.
$\xi$  Transform plane.
$\theta$  Angular position on $\gamma$ in the $\xi$-plane.
$\kappa$  Normalized separation velocity.
$\nu$  Fluid kinematic viscosity.
\( \rho \)  
Fluid density.

\( \psi_U \)  
Stream function due to uniform flow.

\( \psi_V \)  
Stream function due to vortex sheets.

\( \psi_S \)  
Stream function due to source flow.

**Subscripts**

\( c \)  
Corrected.

\( n \)  
Wind tunnel nozzle.

\( t \)  
Wind tunnel test section.

\( r \)  
Reference.
Acknowledgements

The author would like to thank Dr. G. V. Parkinson, not only for his guidance throughout this project, but also for his enjoyable and inspiring lectures.

I wish also to express my sincere appreciation to Dr. I. S. Gartshore for his helpful discussions, constant interest and encouragement without which this work would have been impossible. Thanks to Dr. V. J. Modi who kindly provided most of the test models used in this work.

Special thanks are due to Ed Abell, senior technician in the Mechanical Engineering Workshop, for his fine technical assistance.
1. INTRODUCTION

1.1 GENERALITIES

Wind tunnel testing of scale models is a practice almost as old as designing aircraft. Even today, where fast and cost-effective digital computers combined to efficient algorithms make computational aerodynamics the primary tool for airplane design, the wind tunnel, because of its general reliability and accuracy, remains an essential instrument for obtaining aerodynamic data. It is also one of the few means for verification and validation before the first flight test of a prototype. Moreover, in the case of industrial aerodynamics the construction of a prototype is frequently an impossible task. For example the wind loadings on buildings and bridges which are always unique as a result of their architecture and location, can only be predicted from wind tunnel tests.

Unfortunately, the virtues of this essential tool are tarnished by a vice inherently present in its construction: boundaries. The presence of solid walls in conventional wind tunnels imposes to the flow boundary conditions not existing in real unbounded flow. The basic effects of these boundary conditions are twofold: firstly, they prevent any lateral expansion of the streamtube blocked by the model, and secondly they force the limiting streamlines to be parallel to the walls. The result, known as solid and wake blockage, is to increase the velocity around the model and its wake due to a reduction in area through which the air must flow. Consequently, a model being tested in a solid-wall test section experiences loadings generally higher than the ones measured in an unconstrained flow.

On the other hand, the open-jet test section for which the streamtube is free to expand under blockage effect causes the loadings to be slightly lower than the ones obtained in free-air conditions.

Other types of blockage such as horizontal buoyancy, which implies a variation of static pressure along the test section, and lift interference, which is due to an alteration of stream direction and streamline curvature, are also the direct results of boundary interferences.
The extent to which the boundaries (solid-wall or open-jet) affect the flow field in the test section and therefore the loadings on the test model is primarily a function of the blockage ratio, i.e., the model to tunnel cross-section area ratio. A minimum wall interference condition would therefore require the testing of the smallest possible model. The use of large models, on the other hand, is often needed for similarity purposes to achieve large enough Reynolds number, for greater accuracy or simply because they are easier to work with.

Although the problem of wall constraint was recognized early in the existence of wind tunnels, there still is no final solution to it. There is, however, a multitude of mathematical formulae derived to correct measured aerodynamic characteristics such as drag and lift coefficients. Unfortunately, these formulae are often empirical and their utilization is always limited to certain configurations. Also, the interpretation of large corrections, especially the ones larger than the quantities to be corrected, becomes questionable unless the mathematical model used calculates the flow field with great accuracy (but this would then render the tunnel testing unnecessary!). An alternate way to deal with the problem of wall confinement is to create a test environment in which boundary corrections are kept small and maybe negligible. Basically, two techniques which can be adapted to an existing small wind tunnel can be used: the active and passive wall concepts.

The former consists in dynamically adjusting, through a feedback control system, the boundary conditions at the wall so that the test section streamline-tube is made to approach the free-air pattern. Practically, this is achieved by either deflecting solid flexible walls or using suction and blowing through porous walls. This system, however, requires costly equipment and is therefore not suitable for modest facilities most often found in university laboratories.

The passive method is a less expensive system which uses the principle of ventilated walls. This type of wall makes use of the opposing effects associated with closed and open boundaries which, correctly combined, can simulate free-air conditions resulting in negligible interference for a wide range of blockage ratios.
Boundaries of this type, using longitudinal slots or patterns of holes, have been used successfully in transonic wind tunnel testing to prevent the working section from choking at high Mach number. Their adaptation to low speed testing with larger models has been considered, but the separated flows from the edges of the slots or holes introduce additional empiricism severely limiting the usefulness of the configuration.

Consequently, a new low-correction design of test section for low speed wind tunnel testing has been devised and is under development in the aerodynamics laboratory of the Department of Mechanical Engineering at U.B.C. Designed on the basis of potential flow theory and known as the Tolerant wind tunnel, it was first intended for two-dimensional airfoil testing. Three of the four walls of this novel ventilated test section are solid flat panels, while the fourth one, opposite the suction side of the test airfoil, consists of an array of transverse symmetrical airfoil-shaped slats at zero incidence. These are spaced so that the outer streamline of the test section flow can pass into an outer plenum and return to the test section downstream in such a way that the overall streamline pattern closely approximates the corresponding free-air pattern.

The ever growing area of industrial aerodynamics becomes a natural extension of the use for the Tolerant wind tunnel. Symmetrical bluff bodies could then be tested in a working section modified so that both walls opposite the test body have arrays of airfoil slats in a symmetrical configuration.

The purpose of this work is to rate the possibilities and limitations of the Tolerant wind tunnel when used for bluff body testing. The study of the effect of different wall configurations (i.e. porosity) on the flow surrounding the model is done through numerical modelling, flow visualization and measurements of aerodynamic data such as pressure distribution, drag and vortex shedding frequency.
1.2 AN OVERVIEW OF THE RELATED LITERATURE

The literature concerning both theoretical and experimental wall effects on wind tunnel models is quite abundant. A brief sketch of the available relevant literature is given in this section.

An entire chapter of *Low-Speed Wind Tunnel Testing* [1] describes briefly but clearly the different constraint effects such as solid and wake blockage, and streamline curvature on two and three-dimensional models. It also explains the classical method of images used in connection with fundamental solutions (i.e., vortex, source, and doublet) and thus derives basic formulae for correcting wind tunnel data. A more detailed account of wall interferences can be found in AGARDograph 109 [2]. This NATO publication discusses carefully the problem of solid, wake and lift interferences for airfoils, bodies of revolution, wings and wing body configurations tested in various wind tunnels such as closed rectangular and non-rectangular test sections as well as open and ventilated jet tunnels. Since most of the formulae are derived from linearized theory it is not surprising that they are valid only for blockage ratios less than ten percent.

More relevant to this work is the important paper published by Maskell [3] on the blockage effects on bluff bodies in closed wind tunnels. Maskell used an approximate relation describing the momentum balance in the flow outside the mean structure of the wake and two empirical auxiliary relations to derive expressions for the correction of force and pressure coefficients measured in closed tunnels on bluff models. He then demonstrated the validity of his correction on thin, flat rectangular plates set normal to the flow, for which the blockage ratios ranged from 1.9 % to 4.51 %. He concluded that the theory was sound as long as the correction it calculates remains small.

Gould [4] showed that Maskell’s wake blockage corrections for rectangular plates normal to the flow remain valid whether the plates are mounted on the tunnel axis or adjacent to a wall. He also showed that for the model investigated only small non-linear effects were found even when the corrections approached 100 %. For higher blockage ratios, up to 15 %, Gould empirically derived quadratic expressions which take into consideration the little
non-linearity. Finally, he developed in his paper some blockage correction formulae to use when two models, with non interfering wakes, are present in the working section at the same time.

Works on adaptive walls (active concept) and ventilated test sections (passive concept) for streamlined-model testing are reviewed in a number of AGARD publications [5,6,7,8,9].

The concept of the Tolerant wind tunnel (although not bearing this name), for two-dimensional airfoil testing, was introduced in a paper published by Williams and Parkinson [5]. It briefly summarized a doctoral thesis by Williams [10] and showed that uncorrected lift coefficients and pressure distributions, accurate to within one percent, could be obtained for a wide range of airfoil shapes, sizes, and lift coefficients, using a transversely slotted wall of open-area ratio between 60 and 70 percent. In subsequent papers [11,12] they concluded that although uncorrected lift coefficients are close to unconstrained flow, pitching moment coefficients seemed to require an open-area ratio varying along the longitudinal axis.

The use of slotted-wall wind tunnels for bluff body testing does not seem to have attracted investigators until very recently. Raimondo and Clark [13] experimentally investigated the use of longitudinally-slotted-wall test sections for automotive facilities. Their results showed that accurate model pressure distribution data which does not require blockage correction could be achieved in two test section sizes corresponding to blockage ratios of 16.4 and 21.4 %. In addition, other results [14,15] also showed good agreement for car and truck models of about 15 % blockage ratio, even at extreme yaw angles (less than 20 degrees). Finally, an experimentally-derived blockage correction factor was found to be the same for 3 vehicle configurations, for all yaw angles from 0 to 30 degrees, and was only weakly dependent on the slot open-area ratio (OAR) over the range tested (20 % to 40 %).

Parkinson, in reference [16], introduced the concept of the Tolerant wind tunnel for industrial aerodynamics. He proposed a symmetrical configuration with both walls, opposite the test model, formed by arrays of airfoil slats surrounded by plenum chambers. He rationalized that the low cost and simplicity of a passive system such as this one would be most desirable to improve the capability of small wind tunnels found in university laboratories.
This proposal led to the theoretical and experimental investigation reported in this thesis.

The theoretical flow modelling is merely to provide at least a qualitative, perhaps a quantitative, guide to the choice of a suitable wall configuration, for which the experimental study is the determining factor.

The immediate objective is a low- or negligible-wall-correction test section for two-dimensional bluff body testing. The long term objective is such a test section for general wind engineering testing.
2. THE TOLERANT TEST SECTION

The purpose of this chapter is to explain the principle and describe the physical aspects of the Tolerant test section.

2.1 GENERAL DESCRIPTION

As mentioned by Williams [10], conventional ventilated test sections have slots or holes which lead to undesirable flow separations, thus limiting the applicability of existing theories. The Tolerant test section was therefore introduced first in the configuration of Figure 2.1 as an alternate means for two-dimensional airfoil testing.

In its configuration for two-dimensional bluff body testing, the Tolerant wind tunnel has two solid panels as ceiling and floor while the walls, parallel to the model, consist of arrays of transverse symmetrical airfoil-shaped slats at zero incidence (Figure 2.2). The local angle of attack of these slats should remain small, within their unstalled incidence range, thus preventing any flow separations from them. The slats are spaced so that the outer streamlines of the test section flow can pass into the plenums and return to the test section downstream in such a way that the overall streamline pattern closely approximates the corresponding free-air pattern. The shear layer so formed and its associated turbulent mixing should remain, for most of the wall length, in the plenum separated from the model by the arrays of slats, thus reducing the adverse effects on the test section flow. Only downstream, where the diverted flow re-enters the working section will the test section flow be affected. However, this effect should be minimal since the flow there will already be very turbulent due to the separated wake from the model.

This design is a passive one in that a fixed optimal slatted wall configuration is used for all test models. For most sizes and shapes of test model, an optimized configuration should reduce boundary corrections on the test data to less than 2 %.

The premise that at least one solution exists is based on the fact that closed and open jet boundaries have opposite effects on a test model. Consequently, in an infinitely long test section a correct combination of partly-solid and partly-open boundaries would lead to an
interference-free test section. In a finite working section, however, the existence of a solution is not assured unless the upstream and downstream ends of the test section are far enough from the model to have negligible effects.

An optimal configuration yielding low boundary corrections will be the result of an overall proper geometric arrangement which, because of the number of possible variables, is probably not unique. The next section identifies the possible variables: the degrees of freedom.

2.2 DEGREES OF FREEDOM

In general, any of the test section dimensions, non-dimensionalized with respect to a characteristic length, say the width of the test section H, can be considered as a degree of freedom. This great number of variables makes the problem difficult to handle and must therefore be reduced.

Even if the test model is not necessarily symmetrical, for simplicity, the test section is chosen to be symmetric with respect to the longitudinal center line of the tunnel. Another reason for this choice is the near symmetrical time-averaged shape of the large wake behind a two-dimensional bluff body.

The Tolerant-test-section overall dimensions, such as width H, length L, and height A, will generally be fixed by the existing tunnel it is being adapted to. The plenum depth D, should be as large as possible; however, its size will generally be dictated by practicability and available room. In addition, all airfoil slats are chosen, again for simplicity, to be symmetrical and of the same shape. The degrees of freedom are thus reduced as far as the slats are concerned, to the chord length, c/H, the shape or thickness function, t/c, and the slat angle of attack. In this particular case the shape of the airfoil was chosen to be the NACA 0015 profile with a chord size c/H = 0.0972 (3.5 inches). The angle of incidence is kept at zero, i.e., parallel to the longitudinal axis of the tunnel.

The number of slats and the distribution of open areas (or slats) are also important variables. It seems possible to eliminate one variable by combining the slat size (chord) and the
number of slats in order to form a new variable: the open-area ratio, \( \text{OAR} = 1 - \frac{(N \cdot c/H)/(L/H)}{1} \). However, a given slat size will fix the increment in OAR and therefore limits the available OAR.

Furthermore, since the test section length is finite, the position of the test model can vary along the center line of the tunnel.

Finally, the wind tunnel airspeed can also be varied through the non-dimensional Reynolds number.

In summary, the Tolerant wind tunnel is, in this study, of fixed overall geometry and only the open-area ratio, OAR, is varied for a variety of bluff body shapes of different blockage ratios.

The question is therefore: is there a single OAR which permits the testing of different bluff bodies at high blockage ratios?
3. MODELLING OF BLUFF BODIES IN THE TOLERANT WIND TUNNEL

Because of the great number of possible variables, as shown in the previous chapter, a complete experimental investigation of the Tolerant wind tunnel would be rather tedious and time-consuming. A simple mathematical model representing the most important features of the flow and capable of estimating the effects of different variables or boundary conditions would not only be of great help in the investigation but could also become a useful design tool as well as a means of evaluating some of the residual wall interferences.

This chapter, before describing such a model, will provide the reader with some background information on bluff body flows [17,18], wall effects on them and some of the existing models.

3.1 BLUFF BODIES

3.1.1 DEFINITIONS AND DESCRIPTIONS

The flow past bluff bodies, as opposed to streamlined bodies, is generally characterized by well-separated turbulent wakes originating from the detaching of the flow from the body surface. In addition to the geometry of the body itself, the angle at which the flow encounters the body is also of decisive importance. For instance, a normally streamlined body such as an airfoil behaves as a bluff body when exposed to a flow at an incidence exceeding the stall angle of attack.

The separation-point positions, on either side of a two-dimensional bluff body, from which the boundary layers leave the surface to create the wake, are fixed and independent of the Reynolds number when salient points or sharp edges are responsible for flow separation, as for a flat plate normal to the flow. On the other hand, when boundary layers detach from the surface of a well-rounded body, the separation-point positions will move according to the kinetic-energy level in the boundary layer, surface roughness and Reynolds number.
At small Reynolds numbers the separated shear layers come together downstream creating a "bubble" in which a pair of vortices remains stationary behind the body. Past a critical Reynolds number the shear layers become unstable at some distance downstream, break up and roll up into discrete vortices that move downstream at a velocity somewhat less than that of the main flow. The vortex layers break up closer to the body as the Reynolds number increases. At the back of the solid body, the vortices are shed alternatively from each side with a remarkable regularity resulting downstream in a double row of vortices in which each vortex is opposite the mid-point of the interval between two vortices in the opposite row. A more detailed description of the real wake is given by Roshko [19]. The fluctuating surface-pressure distribution around the body is a direct result of the periodicity of the wake.

Important measurements on bluff bodies usually include the Strouhal number, St, which is the dimensionless frequency at which the vortices are shed, the time-averaged drag coefficient, $C_d$, and the base pressure coefficient, $C_{pb}$. The unsteadiness of the flow is also directly responsible for the creation of an oscillatory force (lift or side force) normal to the wind axis. This force is however difficult to measure and usually requires special equipment.

3.1.2 WALL EFFECTS ON BLUFF BODIES

The qualitative effects of wall confinement on bluff bodies have been experimentally observed for many years. In general, as the flow goes around a confined bluff obstacle, the walls restrict the lateral expansion of the streamtube. Mass continuity will accordingly imply an increase in the velocities around the model. This effect is called solid blockage. A similar phenomenon will arise downstream around the wake where a region of lower total pressure (or energy) displaces the free stream. This is called wake blockage. Another effect, not much investigated yet, is the interaction between the walls and the vortices themselves; some authors [20,21] have demonstrated that base pressure, drag coefficient and Strouhal number can be strongly affected by elements, such as a splitter plate or a nearby plane surface, interfering with the vortex formation.
Consequently, due to wall confinement local velocities and therefore pressure values are greatly modified usually resulting in lower base pressure, higher drag coefficient, and higher vortex-shedding frequency (Strouhal number) than in unconfined flow. In addition, the separation-point positions on well-rounded bodies are, as mentioned before, a function of the Reynolds number and will therefore be affected by wall confinement.

3.1.3 BLUFF BODY MODELS

No mathematical theory has ever been derived to model and predict all aspects of the high-Reynolds number separated flow past bluff bodies. A complete detailed description of such a flow could only be achieved through the yet impossible task of solving the full Navier-Stokes equations. However, as Thwaites [18] notes "... resort may be made to another model, which, while seemingly remote from the physical reality, not only is simple enough for the analysis to be completed, but also gives results which have a clear and valid physical interpretation ". Despite the fact that high levels of turbulence and significant three-dimensional effects are always observed in well separated flows, two-dimensional potential flow theory remains the most widely used method for modelling two-dimensional bluff body flows.

There are two main types of model, both requiring some empirical inputs. The first type, a steady flow model, known as the free-streamline model and often treated in the complex plane, describes the time-averaged flow. In this model the thin separating shear layers are replaced by free streamlines and the irrotational flow external to the wake is evaluated. The base pressure and separation-point positions are determined experimentally and used as empirical inputs. This method which uses the hodograph technique was pioneered by Helmholtz and later improved by Kirchhoff [22]. Although they obtained for the first time a non-zero value for the drag of a two-dimensional flat plate, their drag coefficients seriously underestimated the experimental results. Their assumption of making the separation velocity equal to the free stream velocity is likely to be too low.
This problem was recognized by Roshko [23] who used a new method, the notched hodograph, in which a constant low pressure region behind the body was introduced. His results are in good agreement with experiments.

A simpler model, by Parkinson and Jandali [24], was shown to produce the same good agreement as Roshko's model. It uses a conformal mapping technique in which a circle is mapped onto a slit with the shape of the bluff body surface upstream of the separation points. The wake, bounded by free streamlines, is created by two surface sources symmetrically placed on the downstream part of the circle. This model is used in this work and is described in detail in the next section.

Parkinson and Jandali's wake source model became widely used by many other authors: El-Sherbiny [25] used it for an analytical study of wall effects on the aerodynamics of bluff bodies. Christopher and Wolton [26] adapted the wake-source model for non-symmetric flow. Kiya and Arie [27] have modified it to include the effect of the far-wake displacement. Bearman and Fackrell [28] demonstrated the possibilities of using the wake source model with body shapes that cannot be treated easily by conformal mapping.

The second type of model uses potential flow theory to model the unsteady separated flow behind a bluff body. The method consists of replacing the separating free shear layers by arrays of discrete vortices introduced into the flow field at appropriate time intervals at some points near the separation positions which are usually empirically known. The results, which are of course more expensive to calculate than steady flow, are reported to predict the form of the vortex shedding, the Strouhal number and the time-varying loadings on the model. The accuracy of the predictions is, however, an area where more work is needed. Some examples of this method can be found in references 29, 30, and 31.
3.2 NUMERICAL MODEL OF THE TOLERANT WIND TUNNEL

Because of the unusual configuration of the Tolerant test section and ignoring, for now, the thin boundary layers on solid surfaces, the wake behind the model and the shear layers lying inside the plenums, the entire viscosity effect is restricted to the "production of circulation" at the airfoil slats, thus making irrotationality of the flow a logical assumption. And, since only low wind speed is envisaged, the flow can be considered as being incompressible. Consequently, with the replacement of plenum and wake shear layers by streamlines in order to isolate the irrotational flow from the turbulent region, one can then use potential flow theory to model the regions of interest. The displacement thickness effect of the wake is controlled by the distance between the bounding free streamlines.

The present two-dimensional model combines Parkinson's wake source model, devised for bluff bodies in unconstrained flows, and two arrays of airfoils immersed in an infinite uniform flow. Pairs of upstream and downstream solid walls insure mass conservation between the entrance and exit of the test section. Because of the complexity of the boundaries the solution has to be evaluated numerically; a vortex surface-singularity technique on discretized boundaries lends itself to an efficient solution.

The next three sub-sections give a more detailed description of how the wake source model is used in the Tolerant test section.

3.2.1 WAKE SOURCE MODEL IN THE TOLERANT WIND TUNNEL

Parkinson's wake source model is a semi-empirical model using a conformal mapping technique (Figure 3.1), in which a circle $\gamma$ in the $\zeta$-plane is mapped onto a slit $C$ in the $Z$-plane with the shape of the bluff body surface upstream of the separation points $S_1$ and $S_2$. The wake, bounded by free streamlines, is created by two surface sources of strength $Q$, symmetrically located at angle $\pm \delta$ on the downstream part of the circle. In the analytically-solved model, image sources and sinks, which will not be necessary in this numerical adaptation, must be added to preserve the circle as a streamline in the transform $\zeta$-plane. As
these wake-sources are meant only to reproduce the displacement thickness observed in real flow, the portion of the body inside the wake and the wake flow itself are not modelled. In fact, it is assumed that the body surface exposed to the wake is at the constant base pressure coefficient $C_{pb}$, which also determines the separation velocity $\kappa U$ through Bernoulli's equation:

$$\kappa = \left(1 - C_{pb}\right)^{\frac{1}{2}}$$ \hspace{1cm} (3.1)

The angular position $\delta$ and strength $Q$ of the sources are determined by the requirements of separation positions at $S_1$, $S_2$, zero velocity (stagnation points) at $S_1$, $S_2$ in the $\xi$-plane and a separation velocity equivalent to $\kappa U$ at $S_1$, $S_2$ in the $Z$-plane. The conformal mapping transformation, $Z = f(\xi)$, is chosen so as to make the two stagnation points $S_1$, $S_2$, critical points in the $\xi$-plane in order to insure tangential separation in the physical $Z$-plane.

In the particular case where the model is a flat plate, the separation positions are well defined and the well known Joukowski transformation is chosen to map the vertical slit onto a circle:

$$Z = f(\xi) = \xi - \frac{a^2}{\xi}$$ \hspace{1cm} (3.2)

where $a$, the radius of the circle, is given by

$$a = \frac{1}{2}h$$ \hspace{1cm} (3.3)

and $h$ is the breadth of the plate.

The source positions were found to be

$$\sec \delta = \kappa$$ \hspace{1cm} (3.4)

and the source strength is
The pressure distribution on the front part of the plate in free air is given by

\[ C_p(\theta) = 1 - \frac{\sin^2\theta}{(\cos\delta - \cos\theta)^2} \]  

(3.6)

\[ y = \frac{1}{2}h \sin\theta \]  

(3.7)

For the circular cylinder model the mapping transformation is

\[ Z = f(\xi) = \xi - \cot\alpha - \frac{1}{(\xi - \cot\alpha)} \]  

(3.8)

where the radius of the circle in \( \xi \)-plane is taken to be

\[ a = \csc\alpha \]  

(3.9)

and \( a \) in the \( \xi \)-plane is related to the separation angle \( \beta_s \), assumed known empirically, in the \( Z \)-plane by

\[ a = \frac{1}{2}(\pi - \beta_s) \]  

(3.10)

The diameter of the circular cylinder is given by

\[ h = 4 \csc\beta_s \]  

(3.11)

The source positions are
\[ \cos \delta = \cos \alpha + \frac{\sin^3 \alpha}{\kappa} \]  

(3.12)

and their strength is

\[ Q = 2\pi \csc \alpha (\cos \delta - \cos \alpha) \]  

(3.13)

Finally, the pressure distribution over the circular cylinder in unconfined flow can then be obtained by

\[ C_p(\theta) = 1 - \left( \frac{\sin \theta (1 - 2\cos \alpha \cos \theta + \cos^2 \alpha)}{\cos \delta - \cos \theta} \right)^2 \]  

(3.14)

The angular position \( \beta \) on \( C \), in the \( Z \)-plane, corresponding to \( \theta \) in the \( \xi \)-plane is given by

\[ \sin \beta = \cos \alpha \left( \frac{\sec \alpha - \cos \theta}{\frac{1}{2} (\sec \alpha + \cos \alpha) - \cos \theta} \right) \sin \theta \]  

(3.15)

In the modelling of the Tolerant wind tunnel the bluff model is placed on the centerline of the test section which is represented, as shown in Figure 3.2, by arrays of airfoils of NACA 0015 section between a pair of entrance and exit solid walls. The normalized dimensions of the real test section (see Chapter 5) are used for the computations. The test section length is \( L/H = 2.666 \); the airfoil-slat chord length is \( c/H = 0.09722 \). Also, the length of the inlet and outlet solid walls are 4 times the width (\( H \)) of the test section. The width (or diameter), \( h \), of the model is kept constant equal to unity while the dimension \( H \), the width of the tunnel, is calculated according to the desired blockage ratio (\( h/H \)).

The combination model-walls is then immersed in an infinite uniform flow of unit velocity. Even if the plenums and the plenum flow are not modelled, the plenum shear layers are approximated by streamlines leaving the entrance walls to expand outside the tunnel and finally reattach downstream at the exit walls. These boundary streamlines are allowing the test-section...
airstream to expand to an extent controlled by the open-area ratio (OAR) of the slotted walls, under the effects of solid and wake blockage. However, as mentioned earlier, this only approximates the behaviour of a shear layer since the required boundary condition which is constant pressure along the shear layer, is not satisfied.

Now, because of the presence of the walls, the normalized separation velocity, $\kappa$, and possibly the separation positions are no longer specified and therefore become unknowns. However, El-Sherbiny [25], in an adaptation of the wake source model to solid-wall confined flows, has shown that the calculated base pressure coefficient correlates well with the experimental results if the source position $\delta$, determined from the free-air condition, is kept fixed. This assumption, empirically verified, along with fixed separation positions are adopted for the ventilated-wall wind tunnel model. This is also justified by the fact that the ideal airfoil-slatted boundary condition would produce the unconstrained flow pattern around the test body, and that this would correspond to the free-air positions of the wake sources. Unfortunately, while the model is designed to predict the ideal OAR, it is not expected to correlate well with the experiments, especially far away from the optimal configuration where some assumptions (constant $\delta$, floating pressure value on separated streamlines) are not realistic.

3.2.2 MATHEMATICAL REPRESENTATION

The complex geometry of the wind tunnel boundaries requires the flow field to be solved numerically. The technique used here, based on reference [32], is a vortex-surface-singularity method (also known as boundary element method or simply panel method) in which the solid boundaries, airfoil-slats and model are replaced by vortex sheets. The solid-surface boundary condition of zero normal velocity is satisfied by using the stream function formulation and requiring that the surface of each solid-boundary component (solid walls, airfoil-slats and model) should be a streamline of the flow.

The stream function $\psi_k$ of the $k^{th}$ component is the result of combining 3 fundamental flows: uniform flow $\psi_U$ of unit velocity, line-vortex flows $\psi_v$ from the vortex sheets and source
flows $\psi_s$ from the wake sources. Hence

$$\psi_k = \psi_u + \psi_v + \psi_s$$  \hspace{1cm} (3.16)

for which

$$\psi_u = \text{Im} \left[ U Z e^{-i\alpha} \right] = U (y \cos \alpha - x \sin \alpha)$$  \hspace{1cm} (3.17)

where $U$ is the uniform flow velocity ($U = 1$);

$y, x$ are the boundary coordinates;

$\alpha$ is the flow angle ($\alpha = 0$);

or after simplification

$$\psi_u = y$$  \hspace{1cm} (3.18)

and

$$\psi_v = \frac{1}{2\pi} \int \gamma(s) \ln r(x,y;s) \, ds$$  \hspace{1cm} (3.19)

where $s$ represents all the surfaces in the flow over which the unknown vorticity $\gamma(s)$ is distributed, and $r(x,y;s)$ is the distance from a point on $s$ to $(x,y)$. Also,

$$\psi_s = \frac{Q}{2\pi} \left[ \lambda_1(x,y) + \lambda_2(x,y) \right]$$  \hspace{1cm} (3.20)

where $Q$ is the unknown source strength and $\lambda_j(x,y)$ is the angle of the line joining the point source $i$ to the point $(x,y)$. The source positions ($\delta$) are calculated for the free-air case from equations (3.4) or (3.12) given by Parkinson & Jandali.

Equation (3.16) can then be re-written as
The vorticity distribution \( \gamma(s) \) and the source strength \( Q \) are the principal unknowns from which the velocity distribution and therefore the pressure distribution can be obtained. In the special case where the boundary of the model is a closed contour, the velocity distribution is exactly identical to the vorticity distribution (see Kennedy [32]). However, when the model is an open contour, as in the case here, the velocity distribution must be calculated from the different contributions: uniform flow, vortices and sources (see Appendix 3).

The integral equation (3.21) is exact; no approximation has been made yet. However, solving for \( \gamma(s) \), \( Q \), and \( \psi_k \) can only be done through an approximation. A numerical solution is obtained by discretizing the surfaces \( s \) into \( N \) straight-line panels \( s_j \), on the middle of which, at a control point \( C_i \) of coordinate \( (x_i, y_i) \), the equation (3.21) is applied. The result is a system of \( N \) linear equations:

\[
\psi_k - \frac{1}{2\pi} \int \gamma(s) \ln r(x,y;s) \, ds - \frac{Q}{2\pi} [\lambda_1(x,y) + \lambda_2(x,y)] = \gamma
\]  

(3.21)

where

\[
S_i = \frac{1}{2\pi} \left[ \lambda_1(x_i, y_i) + \lambda_2(x_i, y_i) \right]
\]  

(3.25)

Also,

\[
K_{ij} = \frac{1}{2\pi} \int \ln \left[ r(x_i, y_i; s_j) \right] ds_j
\]  

(3.23)

are called influence coefficients. \( K_{ij} \) is therefore the influence of panel \( j \) on the control point \( C_i \); it is geometry dependent only. Thus, the summation of all \( \gamma_j K_{ij} \), for \( j = 1,\ldots,N \), is the effect of all the vortex panels on the control point \( C_i \). The result of integral (3.23) is given in Appendix 2.
When this discretization is applied to the airfoil-slats, an extra equation per airfoil is required to fix the amount of circulation around the airfoil-slat; thus satisfying what is known as the Kutta condition. Equation (3.21) is then applied at an additional control point \( C_{tp} \) situated at the trailing edge of each airfoil.

\[
\psi_k - \gamma_j K_{tp,j} - Q S_{tp} = R_{tp}
\]  

(3.27)

When the discretization is applied to solid walls, either a full solid wall or the solid sections at the entrance and exit, the value of \( \psi_k \) on these surfaces are specified and equation (3.22) becomes

\[
- \gamma_j K_{ij} - Q S_i = R_i = \gamma_j - \psi_k
\]  

(3.28)

when \( i \) belongs to component \( k \). And since \( \psi_k = \gamma_j \) on the \( k^{th} \) (solid wall) component we have \( R_i = 0 \).

When the discretization is applied to the bluff body, in either the physical or the transform plane, the stream function \( \psi_k \) is arbitrarily set at zero and an extra equation is required to solve for \( Q \). An additional equation can be obtained when equation (3.21) is applied to a control point chosen close to the separation point in order to force the streamline to leave the surface of the body at a fixed given point. But, numerical experiments have shown the results to be very sensitive to the extra-control point position as well as the size of the panels surrounding it.

The method adopted here uses the condition that vorticity \( \gamma(s) \) should vanish at the separation point. This is not a real flow condition but it arises in the potential flow model in
order to have the separating streamlines tangent to the edges of the model. This technique has also the advantage of removing one equation.

Finally, the discretization of the boundary should be such that the sources are placed between two panels in order to reduce the singular influence of the sources at the control point of adjacent panels.

Now, using matrix notation, equation (3.21) can be written for the $k^{th}$ component as

$$
\psi_k \{1\} + \{K_{ij}^k\} \{\gamma_j^k\} + Q \{S_i^k\} = \{R_i^k\}
$$

(3.29)

where

$$
\{K_{ij}^k\} = \begin{bmatrix}
K_{ij} & & \\
& K_{tp,j} & \\
\end{bmatrix}
$$

$$
\{S_i^k\} = \begin{bmatrix}
S_i \\
S_{tp}
\end{bmatrix}
$$

$$
\{R_i^k\} = \begin{bmatrix}
R_i \\
R_{tp}
\end{bmatrix}
$$

include the additional equation calculated at the extra-control point.

Assembling all the unknowns in one vector, we get

$$
\begin{bmatrix}
K_{ij}^* & 1 \\
S_i^* & \\
\end{bmatrix}
\begin{bmatrix}
\gamma_j^* \\
\psi_k \\
Q
\end{bmatrix} = \begin{bmatrix}
R_i^* \\
\end{bmatrix}
$$

in which we still have to apply the zero-velocity condition at each separation point. This is done by removing one column in the matrix $\{K_{ij}^k\}$ and one element in the vectors $\{\gamma_j^k\}$ and $\{R_i^k\}$ corresponding to the term in which $\gamma_j^k = 0$. In this particular case where there is only one component in the uniform flow field (i.e., the bluff body model; flat plate or circular cylinder),
$[K^k_{ij}]$ is a $N \times N$ sub-matrix while $\{S^k_i\}$, $\{R^k_i\}$ and $\{\gamma^k_j\}$ are vector columns of dimension $N$ corresponding to the number of panels (and control points) in the discretization. $\psi_k$ and $Q$ are scalar values.

When there are two components in the flow field, e.g. a test model with $N$ control points and a wall component having $M$ discretized control points on its surface, the global matrix becomes

$$
\begin{bmatrix}
K^k_0 & 1 & 0 & S^k_i \\
K^{k+1}_0 & 0 & 1 & S^{k+1}_i
\end{bmatrix}
\begin{bmatrix}
\gamma^k_j \\
\gamma^{k+1}_j \\
\psi_k \\
\psi^{k+1}_k
\end{bmatrix}
= 
\begin{bmatrix}
R^k_i \\
R^{k+1}_i
\end{bmatrix}
$$

If the wall component is an airfoil-slat with $M$ control points $C_i$, the airfoil-slat has therefore $(M - 1)$ discrete panels and an extra equations is provided by the Kutta condition.

The sub-matrix $[K^k_{ij}]$ has a dimension of $N \times (N+M)$ while $[K^{k+1}_{ij}]$ is a $M \times (N+M)$ sub-matrix. The sub-vectors $\{S^k_i\}$, $\{R^k_i\}$ and $\{\gamma^k_j\}$ have $N$ elements while the sub-vectors $\{S^{k+1}_i\}$, $\{R^{k+1}_i\}$ and $\{\gamma^{k+1}_j\}$ have $M$ elements.

The global matrix is designed to grow automatically to accept up to 15 wall components, i.e., sub-matrices.

A typical discretization of the boundaries comprises 80 panels for the bluff body (flat plate or circular cylinder), 20 panels per airfoil-slats and 10 panels for each of the inlet and outlet solid walls.

In the computer implementation the program makes use of the symmetry to reduce the size of the matrices.

The solution of a model-wall configuration made of a $(100 \times 100)$ matrix takes about 1 CPU-minute on a VAX-11/750 computer while it takes about 2 CPU-hours to solve for a
(400 x 400) matrix. Thus, the solution of many wall configurations (for different OAR) requires several hours of computation.
4. NUMERICAL RESULTS

This chapter presents the results obtained from the mathematical model. It is divided in 3 sections, each of these containing results about the modelling of a flat plate and circular cylinder test body.

The results calculated in unconfined and solid-wall confined flow and presented in the first two sections are used to evaluate and validate the mathematical model. The third section analyses the results obtained in the Tolerant wind tunnel.

4.1 FREE AIR RESULTS

4.1.1 COMPUTATION IN THE TRANSFORM PLANE

Figure 4.1 shows a comparison between analytically and numerically calculated pressure distributions on a normal flat plate in an unconfined airstream. In order to save on computational time, the numerical calculations were performed on only half of the symmetrical domain. The numerical results show little loss of accuracy compared to the analytical method. The base pressure value which is the pressure coefficient at \( x/h = 0.5 \) was not obtained through the inverse transformation like the other pressure values, but through the source strength value. This is a special case which is only valid in unconfined flow.

The use of the inverse conformal mapping transformation to obtain the base pressure leads to a singularity at the separation point which becomes more severe for a circular cylinder, as demonstrated by Figure 4.2. This graph shows the free air pressure distribution over a circular cylinder calculated analytically and numerically in the transform plane. For this case where the given empirical base pressure value is \( C_{pb} = -0.96 \) and separation position is \( \beta_s = 80 \) degrees, the numerical results agree well with the analytical calculations but only on a 45 degree arc starting from the stagnation point. On the remaining part of the cylinder, the two curves diverge as they approach the separation point where the numerical results eventually explode. The reason for this behaviour becomes obvious when one realizes that both the velocity in the
transform plane and the derivative of the mapping transformation vanish, as they should, at the separation point, and that the velocity in the physical plane is obtained by dividing the former by the latter. This singularity does not occur when the problem is solved analytically since the L’Hôpital rule can be used.

The effect of the discretization on the numerical calculation is shown in Figure 4.3 where the source strength value is plotted against the number of panels used to describe the circle-model in the transform plane. Note that the discretization technique is complicated by some constraints such as the source and the separation positions which need to be placed between two panels for more consistent results. The transform-plane discretization leads to good behaviour for the flat plate for which the source strength reaches a value well within 0.08 % of the analytical value \( Q = 1.01819 \) for 70 panels on the circle (that is 34 panels on the front part of the circle corresponding to the front part of the flat plate). However, the discretization method used here gives poor results in the case of the circular cylinder for which the source strength value oscillates irregularly while staying away, by as much as 2.5 %, from the analytical source strength value of \( Q = 0.64842 \), even when the number of panels used is over 150.

4.1.2 COMPUTATION IN THE PHYSICAL PLANE

The same numerical method can also be applied to discretize the actual model in the physical plane. An example of pressure distribution over a flat plate and a circular cylinder model, computed in the physical plane, are shown in Figures 4.4 and 4.5, respectively. These figures show good agreement with the analytical calculations over most of the domain, including the base pressure at the separation point. However, there is in both cases a point where numerical calculations encounter a singularity. This effect comes from the source point placed on the slit to create the displacement thickness observed in the wake of bluff bodies. Fortunately, the singularity affects only a small part of the pressure distribution which can often be omitted without losing any essential information.
Figures 4.6 (a) and (b) show the variation of source strength and base pressure coefficient, respectively, with the number of panels for both the flat plate and the circular cylinder model. Both figures show an identical smooth behaviour with an increase in the number of panels used to describe the model. The numerical solution of the flat plate model, in particular, demonstrates high accuracy by obtaining the source strength value and base pressure coefficient within 0.05% of the analytical value, for a discretization of 60 panels.

The numerical solution of the circular cylinder shows, however, an accuracy within about 1% for a discretization of 100 panels.

In order to avoid a singularity close to a separation point where the pressure value is of critical importance, as well as because the procedure is simpler, the numerical calculation will, from here on, be performed in the physical plane only.

4.2 SOLID-WALL CONFINED FLOW RESULTS

4.2.1 FLAT PLATE MODEL

An example of a computed pressure distribution over a normal flat plate experiencing a blockage ratio of \( h/H = 1/3 \) in a solid-walled wind tunnel is compared in Figure 4.7 to El-Sherbiny's [25] analytical solution. The part of the curve affected by the source singularity is omitted for clarity.

A blockage correction factor, evaluated as described in Appendix 4, was applied to this example and found to be \( CF = 0.6749 \). This factor when divided by the solid wall freestream speed gives the best agreement in a least-square sense with the reference free-air pressure distribution. Figure 4.8 shows this agreement and demonstrates the possibility of using a simple correction factor to evaluate wall effects on a model. Because the CF measures, relative to a set of reference-test data, the overall effect of wall interferences on a model pressure distribution, it can therefore be used for comparing the effects of different wall (including slotted-wall) configurations.
The number of panels used to describe the solid wall can have a significant influence on the pressure distribution and in particular on the base pressure which will be an important characteristic for comparing the effects of different wall configurations (including slotted-wall). Figure 4.9 shows, for example, that for a flat plate model experiencing a blockage ratio \((h/H)\) of 1/3 between solid walls extending upstream and downstream by 6 plate widths, the base pressure coefficient reaches a plateau when the number of panels is greater than 12.

The end (inlet and outlet) effect is also an important parameter which can modify the pressure values. Thus, Figure 4.10 shows that the wall length should be at least 7 times the width of the flat plate to have base pressure values independent of wall length.

The variation of base pressure coefficient with blockage ratio is plotted in Figure 4.11. It compares the numerical calculations to El-Sherbiny’s analytical solution; the agreement is very good.

Finally, Figure 4.12 presents the variation of the blockage correction factor, \(CF\), with blockage ratio. This relation shows a remarkable linearity and thus justifies, for small blockage ratios, the use of linearized correction formulae when available.

4.2.2 CIRCULAR CYLINDER MODEL

This section summarizes the numerically calculated wall interference effects on a circular cylinder model for which the empirical free-air base pressure is \(C_p = -0.96\) with flow separation occurring at an angle of \(\beta_s = 80\) degrees from the front stagnation point.

The pressure distribution over a model experiencing a blockage ratio \((h/H)\) of 1/3 is compared in Figure 4.13 with the free-air analytical solution. It is not compared, as in the previous section, to El-Sherbiny’s solution for confined flow because his results look more like the numerical calculations of Figure 4.2 for which divisions by very small numbers (and eventually zero) was the cause of inaccuracies near the separation point. The curve of Figure 4.13 is more realistic since it shows a fast decrease in pressure followed by a pressure recovery which rises to base pressure at separation. The results show, also, that the pressure recovery
(C_{pb} - C_{pmin}) seems to be independent of blockage ratio, which is in agreement with some experimental observations [25,35].

Figure 4.14 compares the corrected numerical pressure distribution with the free-air analytical solution. The comparison shows good agreement, although overestimating the results especially before the point of minimum pressure. However, from $\beta = 60^\circ$ up to the separation point, the pressure is lower than the free-air data by as much as 16%. Nevertheless, the correction factor remains a simple and useful tool for comparing the effects of different wall configurations.

Figures 4.15 and 4.16 show that the discretization of the wall and wall length, respectively, influence the results in a same manner as in the previous section for the flat plate model. This suggests therefore that wall discretization and wall length are independent of model shape.

The variation of base pressure with blockage ratio again agrees well with El-Sherbiny's solution, as shown in Figure 4.17.

Finally, Figure 4.18 shows the correction factor, CF, plotted against the blockage ratio. Again, the relation is almost perfectly linear.

4.3 TOLERANT WIND TUNNEL RESULTS

4.3.1 FLAT PLATE MODEL

The theoretical effect of different open-area ratios on the surface pressure distribution of various sizes of flat plate model is summarized in Figures 4.19 and 4.20. These calculations were performed for models placed at the center of the test section and an empirical free-air base pressure coefficient of $C_{pb} = -1.38$.

The variation of base pressure coefficient as a function of OAR is plotted in Figure 4.19. Starting with a low OAR value where the solid-wall type of interference effect is felt by the different models in a manner increasing with blockage ratios, the base pressure values increase,
with increasing OAR, at rates varying with the size of the model; the larger the model the faster
the pressure rises with OAR. Eventually, the base pressure coefficient overshoots the given
free-air value \( C_{pb} = -1.38 \) and then reaches a maximum followed, at high OAR, by a
pressure decrease of erratic behaviour. The fact that these curves reach a maximum followed by
a rapid decrease can not be considered as realistic since it is known from experiment that
open-jet boundaries tend to give higher base pressure than the free-air value.

The important point of this graph, however, is the near blockage independency of the
base pressure value shown at \( OAR = 0.49 \) for at least three of the four blockage ratios. For the
three blockage ratios ( 8.3 %, 19.4 % and 25 % ) the base pressure coefficients are
approximately \(-1.40\), lower than the free-air value by less than 2 %, while the fourth model
(33.3 %) at \( C_{pb} = -1.35 \) is higher than the free-air value by about 2 %.

It is interesting to note, also, that Figure 4.19 shows another point where blockage
independency is predicted; at \( OAR = 0.38 \), the three higher blockage ratios have a \( C_{pb} \) of
about \(-1.50\). However, these results, being far from the free-air condition, are not considered
as likely as the values at \( OAR = 0.49 \). Because of the previously mentioned empirical assumption
(source positions are obtained for free-air \( C_{pb} \) and kept constant for different wall
configurations) the mathematical model is thought to be most accurate when the flow field
around the flat plate resembles the free-air conditions for which the base pressure is the given
\( C_{pb} = -1.38 \).

The erratic behaviour encountered at high OAR where the number of slats is less than 5
seems to be due to the individual effect of each slat; in other words the slatted-wall boundary
condition is no longer felt as a homogeneous condition.

Also, most likely because of the free-streamline model used for the plenum shear layer,
the flows calculated at \( OAR = 1.0 \) are far from being the anticipated open-jet results for which
the base pressure coefficients should be higher than the free-air value.

Figure 4.20 shows, through the variation of the blockage correction factor with OAR, the
effect of different slotted-wall configurations on the overall surface pressure distributions. The
fact that the graph of Figure 4.20 closely resembles the one of Figure 4.19 confirms the point that the base pressure variation is a representative measure of the global pressure change on the surface of the model. Note, however, that the best OAR value (the OAR at which models of different size experience the same flow conditions) is shifted to about 0.41 for which the blockage correction factor, CF, is about 0.99 for the three smaller flat plates while the largest model (33.3%) has a CF just above 1.0.

This potential flow model, therefore, predicts that a flat plate model of blockage ratio less than 25.0% tested in the Tolerant wind tunnel with an OAR = 0.41 will experience a residual interference effect equivalent to very low blockage in a solid-walled wind tunnel for which a correction factor of only 1% will be necessary to obtain free-air pressure distribution.

The standard deviation, associated with the evaluation of the blockage correction factor, is plotted in Figure 4.21 as a function of OAR. In general, the values are quite low (less than 0.02); this plot also shows that the error, after correction of the pressure distribution, decreases more or less linearly with increasing OAR to reach a minimum value at OAR = 1.0. This means that even in the case where the blockage correction factor is equal to unity, the pressure distribution is not, in all points of the body surface, equivalent to the free-air pressure distribution; on average, however, it is the best fit. The fact that the minimum (after-correction) error occurs at OAR = 1.0 tends to indicate that the presence of airfoil-slats, although capable of reducing the blockage effect, will also be responsible for distorting the pressure distribution at the surface of the body.

4.3.2 CIRCULAR CYLINDER MODEL

Pressure distributions over circular cylinders are greatly affected by wall interferences. However, because of the nature of this mathematical model for which the separation positions are given empirical values and kept fixed for all configurations, it cannot show any changes in pressure distribution due to variation in separation positions. Again, more so than the flat plate model case, only around the simulated free-air conditions, where the separation points,
\( \beta_s = 80^\circ \), and base pressure value, \( C_{pb} = -0.96 \), are valid, is the model expected to give reliable information.

Pressure values calculated at four different positions (30°, 60°, 70° and 80° from stagnation point) on the circle and plotted in Figures 4.22 to 4.25 describe the slotted-wall effect on different sizes of circular cylinder models.

Qualitatively, the plots of Figures 4.22 to 4.25 resemble the graph of Figure 4.19 showing the flat plate results. The major difference, however, is the relative position of the different blockage-ratio curves which vary with the location where the pressure is calculated. For instance, in Figure 4.22 where the pressure coefficients are calculated at 30° from the stagnation point, the maximum values of each blockage-ratio curve are relatively far apart from each other thus presenting a criss-cross of the curves at around OAR = 0.35. But as the location at which the pressure coefficients are calculated moves towards the separation point, the blockage-ratio curves move downward with respect to the smallest model. This effect results in a continuous shifting of the intersection point towards a more open slotted-wall wind tunnel. Thus at 60° the optimum OAR value is 0.45 while it shifts to 0.49 when the \( C_p \)'s are evaluated at 70°. At separation position (80°) the different blockage-ratio curves have moved downward so much with respect to the small model that no intersection exists anymore. Figure 4.25 shows a base pressure always lower than the free-air value and, therefore, never effectively experiencing the effect of an open-jet boundary. The base pressure values closest to free-air value can then be obtained at OAR corresponding to the maximum of the blockage-ratio curves which is about OAR = 0.68.

Despite this distortion of the cylinder pressure distribution by the slotted-wall, it is remarkable that the best overall fit to the free-air conditions, as shown by the variation of blockage correction factor with OAR in Figure 4.26, is similar to the flat plate results. The optimum OAR is again about 0.42 where the correction to be applied to the pressure distribution is less than 1%. The shape of the model under test, therefore, does not seem to greatly affect the optimum OAR value.
Finally, Figure 4.27 shows the standard deviation to behave much the same as in the flat plate model case. The magnitudes of the error, however, are about twice as high as the values obtained for the flat plate model.
5. EXPERIMENTAL ARRANGEMENT

The main purposes of this experimental programme are to study the real flow in the Tolerant wind tunnel as well as providing comparative data for the theoretical model.

The first section of this chapter, titled Apparatus and Equipment, describes the wind tunnel and the models used in the experiments. It also provides information about the instrumentation used and the data measured during a typical test.

The second section describes the procedure for testing a model while the third one summarizes the error analysis.

The fourth and final section of chapter five describes the flow visualization techniques applied to study the flow in the plenum.

5.1 APPARATUS AND EQUIPMENT

The experiments were performed in a two-dimensional test-section insert designed and built by Williams [10] for an existing low-speed closed circuit wind tunnel (Figure 5.1). This insert is 915 mm wide by 388 mm deep in cross-section, and 2.59 m long. The contraction ratio thus changes from 7 to 11.8. The two-dimensional test model was mounted vertically in the center-plane of the working section between solid ceiling and floor. Both side walls consist of vertical uniformly spaced (except where mentioned) airfoil-shaped wooden slats of section NACA 0015 and chord of 89 mm, at zero incidence. These slatted walls were surrounded by 0.39 by 0.30 by 2.44 m wooden plenums. The side wall of one of the plenums was made of transparent acrylic for better observation of the flow. A full range of open-area ratio (OAR) could be tested by varying the number of slats in the walls.

The tunnel wind speed ranges from 0 to about 40 m/sec and is regulated through a feedback control system. The free stream turbulence level is considered to be better than 0.1 %.

The solid floor had a total of 16 pressure taps positioned on the center line upstream and downstream of the model. The side wall of one plenum was also equipped with 7 pressure taps along the half-height line. Figure 5.2 gives the exact position of all the pressure taps in the
wind tunnel.

Three types of bluff body model were tested; flat plates, circular cylinders and circular cylinder with splitter plate on the wake center line.

The sharp edged flat plates were of three different sizes; 3 (7.6), 7 (17.8) and 12 (30.5) inches (cm) wide, corresponding to blockage ratios of 8.3 %, 19.4 % and 33.3%, respectively. A 45 degree bevel was cut along the rear edges so that the boundary layer on the front face would separate cleanly from the sharp lip. These models were built of steel, aluminum, or acrylic depending on the required section strength, and equipped with pressure taps (between 9 and 15 depending on the size) distributed at the mid-span section on the front and rear faces, as shown in Figure 5.3.

Four sizes of circular cylinders 3 (7.6), 5 (13.7), 9 (22.8), and 12 (30.5) inches (cm) in diameter corresponding to blockage ratios of 8.3 %, 13.8 %, 25.0 %, and 33.3 %, respectively, were also used. They were all built of acrylic and had very smooth surfaces. Each cylinder was equipped with pressure taps located every 10 degrees over a quarter of the circumference at the middle section. In addition, one pressure orifice was inserted at 180 degrees from the first tap with another one directly below at 5 (13.7) inches (cm) from mid span. The cylinders could be rotated in such way that pressure distribution over half of the circumference was measured. Symmetry of the time-averaged flow was assumed and monitored through the extra pressure taps.

The circular cylinder models could also be fitted with a splitter plate of 4 cylinder diameters in length. These plates were made of aluminum sheet of about 1 mm thick; they were secured to the tunnel floor and ceiling with the help of 90 degree angle brackets. The gap between the plate and the cylinder was always carefully sealed.

All the models were mounted on a turn table in the center of the wind tunnel test section. In addition, the flat plate model could also be mounted on a fixed support 22 inches upstream of the center plane.
Because the models were 27 (68.6) inches (cm) long, and therefore extended outside the test section, the holes allowing the model to pierce the floor and ceiling were carefully sealed before each test.

In order to improve the two dimensionality of the flow over the model, large end-plates were tried but difficulties of installation and inconsistent results made their use unreliable. Ceiling and floor boundary layer suction could also be used but was not tried here.

The experiments were carried out at a Reynolds number of $10^5$, based on the width of the flat plates or the diameter of the circular cylinders.

The tunnel wind speed was continuously monitored through a calibrated Pitot-static tube, mounted off-centerline in the nozzle section between the settling-chamber exit and test section entrance, and connected to a Betz manometer. This tube was calibrated against a second Pitot-static tube mounted in the slotted wall empty test section, on the flow centerline, where the test model would be located. Details concerning calibration are given in Appendix 1. Also the total and static pressure ports of the Pitot tube as well as all the pressure taps were hooked up, with plastic tube of 1.6 mm inside diameter and approximately a meter in length, to a 48-port "scanivalve". Individual pressure orifices could then be manually selected and fed to a "Barocel" pressure transducer which transforms the input pressure into an analog electrical signal. The time-averaged surface pressure could then be read, as a voltage, off an averaging digital voltmeter. The time-varying electrical signal was also fed to an spectrum analyser to obtain the vortex-shedding frequencies.

Because the models were touching floor and ceiling through the sealed gap, direct drag force measurements were not attempted, however drag coefficient could be estimated from integration of surface pressure distribution.
5.2 TEST PROCEDURE

A typical series of tests starts, after having modified the existing wind tunnel by adding nozzle, test section and diffuser inserts (see Williams [10]), by a careful calibration of the nozzle Pitot tube used for wind speed monitoring. Once the calibration is completed the bluff model is installed in the test section and the pressure taps are connected to the "scanivalve". Then, a first wall configuration is mounted, usually solid wall corresponding to zero open-area ratio, and the wind speed is adjusted according to the desired Reynolds number of $10^5$. Finally, pressure taps are individually selected and pressure is measured while a spectral plot is also obtained. An averaging-time of about 3 to 5 minutes, depending on the unsteadiness, was allocated to each pressure tap in order to obtain reproducible pressure coefficients and spectra.

The cycle resumes by modifying the wall configuration to obtain a new open-area ratio.

After having tested a full range of open-area ratio, the model is replaced by another one of different size corresponding to a different blockage ratio, and the same measurements are done again for a complete set of wall configurations.

The data are then typed in a computer to calculate pressure and drag coefficients, Strouhal numbers, blockage correction factors and standard deviations.

5.3 ERROR ANALYSIS

Calculations of the uncertainties are described in detail in Appendix 3. The table below summarizes the estimation of maximum uncertainties on the important variables. The error on the surface pressure coefficient $C_P$ is estimated to be maximum at the rear of the model where the measurements are highly oscillatory. The uncertainty will therefore decrease as $C_P$ is measured from rear to front of the model (or from base pressure to stagnation point). Also, the maximum uncertainty will arise at the lowest speed when testing the largest model.

\[
\begin{array}{c|c}
\text{Re} & \pm 4.0 \% \\
\text{q} & \pm 2.0 \% \\
C_P & \pm 2.0 \text{ to } 3.0 \% \\
\end{array}
\]
5.4 FLOW VISUALIZATION

Two flow visualization techniques, tufts and smoke, were used to help acquire some information about the Tolerant wind tunnel flow mechanism. Because of the test section geometry no photography was attempted. The flow patterns were observed and recorded through sketches.

The tufts flow visualization technique uses wool thread approximately half-inch long attached to solid surfaces with masking tape. By aligning themselves with the surface flow, the tufts indicate the local direction of the flow. Rows of tufts were installed on the floor and walls of one of the plenums, as shown in Figure 5.4. In addition, some tufts were attached to each airfoil-shaped slat in order to detect any occurrence of stalled flow on them. All tufts were permanently mounted and continuously observed during each test.

Also, smoke flow visualization was performed in the closed circuit wind tunnel specially modified for the occasion. Because of possible build up of smoke in the tunnel and clogging of the settling-chamber screens, smoke is normally not used in closed circuit wind tunnels. Consequently, UBC's Green wind tunnel was modified in such a way that it effectively became an open-circuit wind tunnel. This was easily accomplished, as shown in Figure 5.5, by closing the first corner with cardboard and opening two inspection side doors: one just before the obstructed corner by which the flow exited; and a second one immediately after the corner which became the air entrance. Even though the entrance and exit were not far apart from each other, no re-ingestion problem was encountered. Decrease in power efficiency and, possibly, flow quality are expected consequences of such a tunnel modification. However, this new tunnel configuration would only be used for flow visualization and usually at very low speed, around 5 m/sec in this case, in order to get a coherent streak of smoke. A smoke generator (CONCEPT GENIE MKV from CONCEPT ENG. Ltd. of England) was used to produce burned-oil
smoke at atmospheric pressure which was fed to a five-gallon capacity tank. A streak of smoke was then obtained by pumping the smoke with a small electric fan into a 5 cm diameter tube terminated by a nozzle of final diameter of 6 mm.

Visualization was done by injecting smoke at different points in the tunnel such as at the base of the model, ahead of the model and through different orifices in the plenum.
6. EXPERIMENTAL RESULTS

This chapter presents and discusses the experimental results obtained in the wind tunnel. The first three sections of the chapter relate directly to the testing of three bluff body models; flat plate and circular cylinder with and without splitter plate. In general, each of these sections shows the effects of different wall configurations on pressure distribution, base pressure coefficient, drag coefficient, Strouhal number and overall blockage correction factor for one model. In addition, the first section shows some floor pressure distributions and discusses the results for a different model position.

The fourth section of this chapter is concerned with the flow inside the plenum; wall pressure distributions and flow visualization form the basis for the discussion.

6.1 FLAT PLATE MODEL

6.1.1 MODEL PRESSURE DISTRIBUTION

Effects of different wall open-area ratios on the time-averaged pressure distribution on a flat plate model are shown in Figures 6.1 (a) to (m). Each of these 13 figures compares, for a given open-area ratio, 3 model pressure distributions corresponding to 3 blockage ratios (8.3 %, 19.4 % and 33.3 %), with an analytical curve for which the free-air base pressure is considered to be \( C_{pb} = -1.13 \) [33].

Figure 6.1 (a), for which the OAR = 0, shows clearly the "squeezing" influence of solid walls on the pressure measurements; the higher the blockage ratio is, the faster the pressure drops down from the stagnation point and the lower is the base pressure. The general shape of the variation, however, stays the same while at the rear of the plate the time-averaged pressure remains relatively constant in spite of highly turbulent flows. Since the rear suction behind the flat plate dominates the drag, its increase in conventional wind tunnels can greatly increase the drag; here, more than double the free-air drag \( (C_d = 1.98) \) at a blockage ratio of 33.3 % \( (C_d = 4.66) \), about 50 % too high for a blockage ratio of 19.4 % and still around 15 % too high.
for a relatively small blockage ratio of 8.3%.

The next figures, 6.1 (b) to (m), show the effect of "opening" the walls. First, one can clearly observe that the wall boundary condition has a large influence on high blockage ratio models while the small model is little affected over the entire range of OAR.

Also, the pressure distribution on the front face of the plate, being little sensitive to wall constraint, remains close to the accepted free-air values while showing some confined flow characteristics at low open-area ratios (OAR less than 0.5). On the other hand, the base pressure coefficient shows a relatively large variation with increasing OAR. In particular, the large-model ($h/H = 33.3\%$) base pressure increases rapidly with OAR to reach the small-model ($h/H = 8.3\%$) value at OAR $= 0.526$ and then keeps on increasing to overshoot the reference base pressure of $C_{pb} = -1.13$. More details on the variation of base pressure with OAR will be given in section 6.1.3.

It is important to note, here, that stalled flows were observed on some slats upstream of the 12-inch ($h/H = 33.3\%$) flat plate model. This is surely an indication of the maximum size flat plate which can be tested without flow separation as required by the criteria for designing the Tolerant wind tunnel.

The pressure distributions and in particular the base pressure coefficients measured at a wall open-area ratio of zero corresponding to an open-jet test section are presented here only as indicative values and should not be considered as reliable since good two-dimensional open-jet flow conditions in the present test section could not be achieved.

6.1.2 FLOOR PRESSURE DISTRIBUTION

Centerline floor pressure distributions were systematically obtained for all flat plate models and wall configurations. However, only a few sets of results are presented here, in Figures 6.2 and 6.3, for they summarize clearly enough the upstream and downstream centerline flow behaviour over the full range of blockage ratios and OAR. Figures 6.2 show, for 3 blockage ratios, the pressure variation along the actual centerline of the tunnel, while Figures 6.3 present
the pressure variation compensated for the empty solid-wall pressure gradient along a
non-dimensionalized axis. The model position is at zero on the abscissa, for either scale, and the
flow is going from right (upstream) to left (downstream).

An important point confirmed by the upstream floor pressure distribution is that in all
cases the nozzle Pitot-static tube, used to monitor the tunnel wind speed, is situated far enough
upstream (1.68 m away from the model) and is never affected by the model pressure field. One
can also note that the "opening" of the walls stimulates early upstream transverse velocities
allowing the perturbation, particularly at high blockage ratio, to reach farther upstream than in
solid-wall confined flow. Moreover, the upstream floor pressure distribution seems to be
independent of OAR in ventilated walls in addition to showing collapsible curves indicating
similar flow for different blockage ratios.

On the downstream part, the time-averaged static floor pressure distribution reaches a
minimum not on the rear face of the flat plate model but at some distance downstream
corresponding to about one width of the model. This is followed by a pressure recovery, which
like the rest of the wake, is sensitive to wall effects as shown in Figure 6.2 (a). The
non-dimensionalized plot of the same data, shown in Figure 6.3 (a), fails to superimpose the
blockage-ratio curves indicating that the non-linear effects of wall interference lead to
non-similar flow.

Partly because of higher base pressures, the pressure recoveries in the Tolerant test
section are more complete than in conventional wind tunnels; the blockage-ratio curves are also
more alike suggesting more similar flows.

In general, the test section seems to be long enough to obtain reasonable pressure
recovery, except perhaps for the large model which appears to "feel" some end effects. The
dramatic air re-entry caused by the sudden end of the plenums, obviously more important at
higher blockage ratio for which more air is deflected into the plenums, is also responsible for an
artificial shortening of the slotted-walls. Consequently, the available test-section length can
become a limiting factor when assessing the maximum permissible blockage ratio.
6.1.3 VARIATION WITH OAR

The next series of plots, Figures 6.4 to 6.8, summarize the sensitivity of some aerodynamic characteristics to walls of varying open-area ratios, for flat plate models of different blockage ratios.

Figure 6.4 shows the variation of base pressure coefficient, averaged over the back of the flat plate, as a function of OAR. Starting with a closed-wall test section, the base pressure coefficients augment steadily with opening of the wall; at a rate increasing with blockage ratio. Two blockage-ratio curves, 8.3 % and 19.4 %, crisscross at OAR = 0.63 where $C_{pb} = -1.20$, while the third curve corresponding to the largest model increases more rapidly to cross the lowest blockage-ratio curve at OAR = 0.53 where $C_{pb} = -1.23$.

Variation of drag coefficients as a function of OAR is presented in Figure 6.6. It is no surprise to see here the same behaviour as in Figure 6.4 since drag coefficients are obtained by integrating the surface pressure distribution in which the contribution from the front face of the plate, Figure 6.5, is little affected by wall confinement. The drag coefficient obtained at OAR = 0.63, for the intersecting curves, is about 1.96 while at OAR = 0.53 the value is $C_d = 2.00$, a difference of about 2 %.

The effect of different wall configurations on the Strouhal number, the dimensionless vortex-shedding frequency, is plotted in Figure 6.7. This graph shows the 8.3 % and 19.4 % blockage-ratio curves decreasing linearly with an increase of OAR and intercept the same Strouhal-number value ($St = 0.142$) at OAR = 0.67. The other blockage-ratio curve (33.3 %), decreasing linearly only for OAR less than 0.6, crosses the Strouhal-number value of 0.141 at a slightly higher OAR of 0.74.

The blockage correction factors calculated by the method explained in appendix 4 are plotted against open-area ratio in Figure 6.8. The general aspect of this graph is similar to the base pressure results showing an intersection point at an OAR of about 0.6, between the 8.3 and 19.4 % blockage-ratio curves, and another one at around 0.49 for the 8.3 and 33.3 % blockage-ratio curves. The values of the blockage correction factor are, at those points, about
0.980 and 0.974, respectively, corresponding to low blockage ratios, perhaps 2 to 3 %, in a conventional wind tunnel.

Figure 6.9 shows the effect of opening the walls on the standard deviation which can be interpreted as a measure of the quality of the overall fit of the Tolerant-wind-tunnel data to the reference pressure distribution. This graph does not show any particular pattern except perhaps that the standard deviation oscillates around a constant value which increases as the blockage ratio increases. Also, there is in general a better fit when the tests are performed in the slotted-wall wind tunnel.

6.1.4 EFFECT OF MODEL POSITION

In order to study the effect of longer slotted walls extending downstream of the model, a series of tests was conducted with the same models but at a new position 22 (55.8) inches (cm) upstream of the center (previous position) of the test section. Unfortunately, this change in position also results in a reduction of slotted-wall surfaces (and therefore open areas) upstream of the model thus making the interpretation of the results more difficult. Nevertheless, the floor pressure distributions of Figures 6.10 and 6.11 tend to indicate no major change in final pressure recovery with the new long after-model walls. The new model position simply allows the rear pressure distribution to remain constant over a longer distance after recovering from the low pressure peak experienced in the separation "bubble", just behind the model. The larger the model is, the shorter is the distance on which the pressure remains constant before being affected by end effects (breather and sudden ending of the plenums).

The floor pressure distributions obtained ahead of the models indicate the presence of disturbed flow at the inlet of the test section. As a result, the Pitot tube used to monitor the tunnel wind speed had to be moved farther upstream in order to insure surrounding undisturbed static pressure. This new position, at a somewhat larger cross-section area of the nozzle, results in a lower indicated wind speed thus degrading the accuracy of the calibration (the higher the wind speed is in the nozzle, the more accurately it can be measured).
Figures 6.12 to 6.17 summarize the effect of varying open-area ratio on certain aerodynamic characteristics when the models are tested at the upstream position. Except for the model of 33.3 % blockage ratio, the results are, within the experimental error, similar to the data obtained when the models were tested at the center of the test section. However, the change in test position became significantly important for the 12-inch flat plate model; over most of the tested range of OAR (0.4 - 0.8), base pressure coefficients were higher and drag coefficients were therefore lower than for the same model tested at mid-section. Also, the Strouhal number values were lowered becoming comparable to the other model sizes at OAR = 0.6.

The fact that the length upstream of the model is reduced by the new position limits the distance over which the flow can move through the slotted-wall into the plenums thus causing larger velocity gradients around the model. Consequently, most of the airfoil-slats upstream of the 33.3 % blockage-ratio model were hit by high incidence flow resulting in well separated flows in the plenums. This is obviously a factor contributing to the differences encountered between the two test positions.

6.2 CIRCULAR CYLINDER MODEL

6.2.1 MODEL PRESSURE DISTRIBUTION

The time-averaged pressure distributions measured on different sizes of circular cylinder positioned at the center of the test section are shown in Figures 6.18 (a) to (m), for various wall configurations. These figures present, systematically, the results for 3 blockage ratios (33.3 %, 25 % and 13.8 %) over the investigated range of OAR as well as low blockage-ratio (8.3 %) data at 4 OAR's: 0, 0.563, 0.635 and 0.708. In addition, each graph shows, for comparison purposes, a reference pressure curve measured by Roshko [20] at a Reynolds number of 14,500 and a blockage ratio of 4.4 % for which the corresponding drag coefficient is \( C_d = 1.15 \). Although different than the true unconfined value, this reference curve approximates, well enough and probably within the experimental error, the desired free-air pressure distribution over a circular
cylinder at a Reynolds number of a hundred thousand. (Indeed, Roshko’s results would need only, after Allen and Vincenti’s formulae (as reported by Roshko [38]), a velocity correction of less than 1.5 % and a drag coefficient correction of about 3 %).

The first plot (Figure 6.18 (a)) shows the results obtained in a conventional test section (OAR = 0); the usual pressure patterns over two-dimensional circular cylinders of different blockage ratios are observed: starting from the stagnation point, the most upstream point on the cylinder, and moving circumferentially towards the back of the model, the pressure decreases rapidly to reach a minimum value around 70 degrees, before undertaking a pressure recovery which is abruptly stopped by flow separation at about 80 degrees. Unfortunately, the exact point of separation which oscillates with the vortex-shedding frequency cannot be accurately determined from the pressure plot. After separation, the pressure stays relatively constant over about 60 degrees (up to $\beta = 120$ degrees) before dropping on the rest of the cylinder at a rate which increases with increasing blockage ratios. Although the minimum pressure and the pressure at separation show large variations (decrease) with increasing blockage ratios, the difference between those two values remains relatively independent of the size of the model.

The use of the Tolerant wind tunnel for the testing of circular cylinders results in a tremendous improvement of the pressure distribution even for a large model with 33.3 % blockage ratios and a low OAR such as 0.344. At this OAR (Figure 6.18 (b)) the 13.8 % blockage-ratio curve follows the reference line up to the minimum pressure point where the present results become slightly higher; the pressure distribution at the back of the cylinder is however too negative. One can also note that the 25 % blockage-ratio curve stays slightly higher than the reference curve on the part of the cylinder preceding the minimum pressure point. Past this point the pressure distribution remains too negative by about 13 %. The largest model shows a slightly too negative pressure before separation while the pressure on the rear part of the cylinder is below Roshko’s curve by about 25 %.
As the open-area ratio increases (Figure 6.18 (b) to (m)) all the curves move upward at a rate which increases with increasing blockage ratio. Unfortunately, a complete collapse of all the blockage ratio curves never occurs. It is however possible, as shown in Figure 6.18 (f) where OAR = 0.526, to obtain for the separated-flow region a good collapse of all curves. It appears that the part of the cylinder preceding the separation point experiences the effects of an open-jet type of boundary while the rear of the cylinder feels the desired effects of unconfined flow.

These results suggest two possible ways for obtaining better collapsible data: a non evenly-spaced slat distribution along the longitudinal axis resulting in a varying-OAR along the wall or, maybe, a simple shift in the angular position of the pressure taps would be enough to correct a possible misalignment of the cylinder.

6.2.2 VARIATION WITH OAR

The effects of various wall open-area ratios on the aerodynamics of circular cylinders are presented in the next 9 figures. Figures 6.19, 6.20 and 6.21 summarize the pressure distributions presented in the previous section by showing the influence of OAR on 3 different parts of the circular cylinder, namely: before the separation point, immediately after separation and directly behind the cylinder. The pressure data measured at 50 degrees from the stagnation point and which are considered to be representative of the pressure variation encountered in the unseparated flow region of the cylinder, are plotted in Figure 6.19. Three main features can be observed. This graph shows, firstly, very little difference between the 8.3 and 13.8 % blockage-ratio curves. In addition, they are only weakly dependent upon OAR over the studied range. Secondly, the two other cylinder-size curves not only are distinctly different from each other through their slope and initial value but also remain higher than $C_p = -0.75$, Roshko's value at $\beta = 50^\circ$, over the considered range of OAR. Lastly, no optimum OAR point, at which the pressure at 50 degrees becomes independent of the blockage ratio, can be determined.
Figure 6.20 reports the effects of OAR upon the pressure coefficient measured at 100 degrees from the stagnation point; this is a sector of the cylinder earlier defined to be the region of constant pressure following the separation of the boundary layer. The importance of this pressure coefficient resides in the fact that it is, in the evaluation of the drag, often considered to be the base pressure coefficient and taken constant over the whole separated flow region. This graph exhibits a well defined criss-cross of all the blockage-ratio curves at a single OAR of about 0.53 where the pressure coefficient corresponds to $C_p = -0.96$. This is a desired behaviour of the Tolerant wind tunnel which is to provide a single OAR for a wide range of blockage ratios.

The pressure values measured at 180 degrees from the stagnation point and plotted in Figure 6.21, characterize the maximum pressure variation on the rear part of the cylinder where the shape of the pressure distribution curve is influenced by wall confinement (as seen in Figures 6.18). An artificial unconfined environment, no matter how it is realized, should be able to eliminate this effect which could be responsible for large drag discrepancies. Although incapable of completely eliminating the rear pressure differences the Tolerant wind tunnel can considerably reduce this problem to a maximum difference, in terms of pressure coefficients, of about 10%. The reason for this residual error is not totally clear but since the presence of periodic vortices is partly responsible for the low pressure region behind the cylinder, it is thought that the interference effect of the slatted-wall, even at large OAR, could alter the dynamics of the vortices thus causing a different-than-free-air pressure pattern in the back of the cylinder.

The cylinder drag characteristics, obtained from integration of the pressure distributions, are plotted in Figures 6.22, 6.23 and 6.24 (note that the drag is defined as positive in the direction of the flow). The graph of Figure 6.22 shows the variation of the front drag coefficients as being almost identical to Figure 6.18, the pressure coefficients at 50 degrees, thus confirming an overall behaviour in the unseparated flow region of the cylinder.
The non-existence of an optimal OAR for the drag coefficient (Figure 6.24) seems mainly due to the frontal behaviour, since the variation of the rear drag coefficient, although not distinctively clear, becomes independent of the blockage ratio at an open-area ratio of about 0.56.

The Strouhal number (Figure 6.25) which characterizes the unsteadiness of the flow due to vortex shedding, becomes also independent of the model size at an OAR of about 0.56 with a value of $St = 0.185$ compared to 0.18 given in the literature [35] for a Reynolds number of $10^5$. Note also that the $8.3\%$ blockage-ratio results remain slightly higher than those for the other cylinder sizes; the difference, however, is less than $3\%$ at the criss-cross point.

Finally, the variation of blockage correction factor and its associated standard deviation with OAR are given in Figures 6.26 and 6.27. It is interesting to note that all the blockage-ratio curves cross at OAR = 0.53 where the blockage correction factor is $CF = 1.0$. Hence, on average at OAR = 0.53, the pressure distribution curves of any blockage ratio (in the range considered) can be approximated by the reference curve. The error or standard deviation, on the other hand, increases with increasing blockage ratio, as shown in Figure 6.27.

6.2.3 EFFECT OF NON-EVENLY SPACED SLATS

It has been shown in the previous sections that a circular cylinder model tested at low OAR often presents a split behaviour characterized by a solid-wall "squeezing" effect in the back of the cylinder while the front part feels the effect of an open-jet boundary. It is therefore logical to assume, if no systematic error due to set-up misalignment is involved, that a non-evenly-spaced-slat slotted-wall with open areas increasing in the direction of the flow could lead to a simple solution.

This section provides merely a starting point to a possible future investigation of this type of slotted-wall by showing the results of four sizes of circular cylinder model tested at a single OAR of 0.453 but with 2 different slat distributions. Both open-area distributions are chosen to increase linearly with the slat number but with different slopes which, as explained in
detail in Appendix 5, are determined through a given factor (AORT). This factor is defined for the first slot open-area as the ratio of the evenly-spaced-slat slot size to the actual (non-evenly-spaced) slot size. A factor of AORT = 2, for example, means that the first slot is half the size of the evenly-spaced case and that the slope would be calculated accordingly to obtain a linear increase of open areas.

The results obtained for values of AORT of 1.5 and 3 are plotted, respectively, in Figures 6.28 and 6.29. These can be compared to Figure 6.18 (d) which shows a similar test in an evenly-spaced slotted-wall (AORT = 1) test section. The resulting pressure distributions are generally lower than the homogeneous-OAR case while bringing the pressure in the unseparated flow region close to or lower than the reference curve. When comparing the new pressure distributions to each other, one can observe noticeable change before the separation point but almost none on the rear part of the cylinder. Consequently, the load distributions of Figure 6.29, where AORT = 3, appear to be more consistent all around.

This simple test, although preliminary, demonstrates the possibility of using graded OAR to obtain consistent boundary conditions.

6.3 EFFECT OF SPLITTER PLATE

The suggestion that interactions between the vortices shed from the cylinder and the slats of the slotted wall could be responsible for altering the surface pressure distribution of large models prompted this investigation. As shown by Roshko [20], the introduction of a long enough (about 4 diameters) splitter plate along the centerline of the wake is sufficient to provoke reattachment of the shear layers on either side of the plate thus resulting in a suppression of the vortex shedding and a considerable increase of the base pressure. The use of this type of model allows us to test a bluff body model without vortices interacting with the slotted walls. In addition to eliminating the unsteadiness of the flow this type of model will also introduce two recirculating regions, on either side of the splitter plate, which effectively become part of the model after-body and therefore have to be reproduced correctly in the Tolerant wind
tunnel in order to get the correct free-air condition.

6.3.1 MODEL PRESSURE DISTRIBUTION

Surface pressure distributions on 4 sizes of circular cylinder with splitter plate obtained with 13 different wall configurations are compared to a reference loading distribution in Figures 6.30 (a) to (m). As in the previous section the reference curve was measured by Roshko [20] in a conventional wind tunnel with a blockage ratio of 4.4 % at a Reynolds number of 14,500; the corresponding drag coefficient is $C_d = 0.72$.

The plain-wall surface loading distributions, plotted in Figure 6.30 (a), show clearly the influence of wall interference. Even at the lowest blockage ratio of 8.3 %, the base pressure coefficients differ from the reference values by as much as 38 % resulting in a drag difference of about 12 %. One can note, however, that for all blockage-ratio curves the pressure distributions after separation (or in the separation bubble) remain relatively constant.

An opening of the walls results once again in an important increase in surface pressure. The best collapse of all the data with the reference curve occurs at an OAR value of about 0.563 or 0.599. But, again, while the base pressure distributions are well reproduced, the front loading distributions corresponding to the blockage ratios of 13.8 % and 25 % are higher than the reference curve. These differences cannot be explained, now, by flow unsteadiness or vortex-wall interactions. It is however easy to postulate that a misaligned model would result in such differences. An error of about 2 degrees would, in a region of high gradient such as the front part of the cylinder, be sufficient to cause the discrepancies observed in Figure 6.30.

On the whole, considering a systematic error in setting up 2 of the models, the OAR value of about 0.563 can be considered as being close to the optimum value. By reproducing the pressure distributions of the circular-cylinder-with-spitter-plate model, which possessed long separation bubbles, the Tolerant wind tunnel tends to demonstrate its capability to simulate unconfined flow conditions around models with after-body. It is assumed here that the base pressure, indeed the whole cylinder pressure distribution, is affected by the shape of the
separation-bubble which in turn is quite sensitive to wall interference. A more complete study should, however, include geometrical measurements such as reattachment length and height of the bubble.

6.3.2 VARIATION WITH OAR

Figures 6.31 to 6.38 summarize the effects of wall-OAR on the aerodynamic characteristics of the circular cylinder equipped with splitter plate.

Effects of different open-area ratios on the unseparated flow region of the model, and reported in Figure 6.31, are well characterized by the variation of pressure coefficient at $\beta = 50$ degrees. Even if the existence of an optimum OAR value is not apparent, the different blockage-ratio curves show the desirable increase in slope with increasing blockage ratios. As reported in the previous section, a misalignment of the model by about 2 degrees is sufficient to shift two of the blockage-ratio curves (13.8 % and 25 %) high enough upward to eliminate the single-OAR criss-cross. Note, however, that the intersection between the 33.3 % and 8.3 % curves and the intersection between the 25 % and 13.8 % curves occur at the same OAR of 0.563.

The next two figures, 6.32 and 6.33, are almost identical and therefore show homogeneity of pressure in the separated-flow region. These plots also show the presence of an OAR value of about 0.563 at which the pressure coefficients are independent of the blockage ratio.

The variations of drag coefficients with OAR are plotted in Figures 6.34, 6.35 and 6.36. As can be anticipated from previous results, the variation of drag on the front part of the models shows inconclusive results while the drag value on the rear part of the cylinder becomes independent of the model size at an OAR of about 0.563. Since the contribution of each part, about 0.275 on the rear and 0.5 on the front, to the total drag value is of the same order of magnitude, the variation of drag as a whole is substantially affected by the (speculative) misalignment of the models. Even if Figure 6.36 shows a criss-cross of 3 blockage-ratio curves at
an OAR of about 0.635, it is believed that a better alignment of the models would bring the 25 %
and 13.8 % blockage-ratio curves down enough to coincide with 0.563 for a $C_d$ of 0.75.

Finally, variation of the blockage correction factor, Figure 6.37, tends to indicate that
pressure values measured in the Tolerant wind tunnel with an OAR of 0.563 would not require
any correction where the maximum value of the standard deviation, as shown in Figure 6.38,
would be as low as 0.04.

6.4 PLENUM FLOW

The use of a proper slatted-wall configuration in the Tolerant wind tunnel introduces
new sets of boundary conditions which make possible the simulation of unconfined flows
around models of relatively large blockage ratios. To accomplish this, the side wall limiting
streamlines are allow to separate as free shear layers and flow downstream, behind arrays of
slats, into plenum boxes in which they take a shape which in turn influences the nature of the
flow around the model. Consequently, a complete assessment of the capabilities and limitations
of the Tolerant wind tunnel does not seem possible without a full understanding of the flow
inside the plenums. This section makes a first attempt to reach such a goal by providing basic
information such as pressure distributions in the cavity and flow patterns as observed from tuft
and smoke flow visualizations. (Chapter 5 shows the pressure tap and tuft positions in the
plenum)

Depending mainly on the blockage, two general pictures of plenum flow have been
observed.

First, in normal conditions corresponding to blockage ratios up to one third (except for
the flat plate) at almost any open-area ratios except unity (open-jet), the observed flow showed
a single elongated recirculation with an eddy center located downstream of the geometric
center of the plenum ( Figure 6.39 ). Strong presence of entrainment along the shear layer was
always evident. Also, the shear layer has been observed to impinge on the downstream end wall
of the plenum at a position oscillating with a frequency correlating with the vortex shedding
frequency. Unfortunately, no smoke flow visualization was done while testing the circular cylinders equipped with splitter plates for which no vortex shedding existed. However, tuft flow visualization showed a much more steady recirculation bubble than a plenum subjected to the unsteadiness originating from a vortex-shedding model. Indeed, smoke flow visualization has revealed a strong interaction between the oscillatory free shear-layer springing from the model and the plenum flow. This interaction was mostly evident at high blockage ratios where the model free shear layer was observed to oscillate far enough sideways to pass through the slatted wall into the plenum provoking large transverse flows through the downstream slats. The plenum free shear layer would then move accordingly.

As shown in Figures 6.40, the mean static pressure distributions measured in this first type of plenum flow are affected by OAR in an increasing manner with increasing blockage ratios. At low blockage values (around 8 \%) the pressure distribution is practically constant and varies little with OAR: it is slightly positive at low OAR and decreases towards zero with increasing OAR. At higher blockage ratios the pressure distribution over the upstream part of the plenum shows a negative pressure which decreases with increasing OAR. This negative pressure region is always followed by a pressure recovery which could bring the pressure value in the downstream end of the plenum significantly higher than zero at high OAR.

The second type of flow pattern observed in the plenum is termed shallow-closed cavity flow (as opposed to open cavity flow in the first case) by Sinha et al. [36]. It is characterized by two separation bubbles, one attached on the upstream face and the other on the downstream face of the plenum (Figure 6.41). This type of flow was mainly encountered in the open-jet (OAR = 1) configuration where the slats were not there to prevent the break up of the free shear-layer; and while testing high blockage-ratio models such as the 12-inch normal flat plate. In this latter case, the large blockage would push the plenum shear-layer far enough sideways to impinge on the plenum side-wall where it would reattach. Also, the testing of the large flat plate at the upstream position showed that a combination of large blockage with little upstream slatted-wall open area can result in a jet-like flow through the slots of the wall to create an even
more obvious shallow-closed-cavity type of flow in the plenum. Moreover, this kind of large flow deflection caused most of the upstream slats to operate at high angles of attack resulting in stalled flows. These facts have definite implications in establishing the limitations of the Tolerant wind tunnel.

Figure 6.42 shows pressure distributions in the plenum for testing of flat plate models at 22 inches upstream of the test-section center in a wall OAR of 0.563. Note the pressure distribution of the 33.3 % blockage-ratio model corresponding to the shallow-closed-cavity flow in the plenum. A large negative pressure in the plenum upstream corner is quickly followed by a rapid pressure recovery which stabilises at a value slightly below zero on the portion of the wall where the shear layer has reattached; this is then followed by yet another small pressure recovery to finally end up at a pressure value slightly above zero. This curve will also move upward as the wall OAR increases.

Figure 6.40 (l) is an example of plenum pressure distributions obtained in an open-jet (OAR = 1) test section configuration in which no airfoil slats were present. It is interesting to note the near collapse of the data in the upstream half of the plenum; this was observed for all models except in the case of the flat plate tested at the upstream position. These types of pressure distribution are typically a combination of pressure distribution on a backstep followed by one due to a forward-facing step [36].

These results therefore indicate the important effect of the slats on the free shear layer. The high vorticity originating from the trailing edges of the slats feeds in turbulence to the free shear layers inside the plenums thus preventing them from breaking. Although this effect is not entirely understood, "it is likely", as suggested by Bearman and Morel [37] in the case of free stream turbulence effect, "to reduce the spanwise coherence of the structures and this may in some way enhance the shear layer growth".
7. CLOSING COMMENTS

The Tolerant wind tunnel was originally devised to produce a low-correction data environment for airfoil testing. The possibility of extending its use for the testing of symmetrical bluff bodies was investigated here, theoretically and experimentally. Also, the fluid mechanics leading to the desired characteristics of blockage-ratio independency was examined in order to establish the capabilities and limitations of the Tolerant test section.

Based on this investigation, this chapter presents the conclusions related to the use and aerodynamics of the Tolerant wind tunnel as well as making some recommendations for future work.

7.1 CONCLUDING REMARKS

Due to the particular nature of the mathematical model, accurate prediction of the base pressure coefficient, the principal unknown, was not anticipated. Numerical results, however, have shown a variation of the base pressure coefficient with OAR similar in trend to the experimental results, at least over a limited range of OAR less than 0.7. Also, the theoretical results do not indicate a definite optimum OAR, but identify a range of OAR between 0.4 and 0.5 suitable for the testing of different blockage-ratio models. The slopes of the blockage-ratio curves, e.g. Figure 4.20, suggest the magnitude of the error occurring in case of wrong OAR choice.

The numerical model also predicts, around optimum OAR, a residual slotted-wall effect equivalent to very low blockage effect in solid-wall wind tunnels. The corresponding blockage correction factor is of the order of 1 %.

Unfortunately, the mathematical modelling of the Tolerant wind tunnel cannot, in terms of maximum allowable blockage ratio, predict the limitation of the test section. However, the theoretical results show an optimum OAR shared by only 3 of the 4 model sizes, thus suggesting a maximum blockage ratio close to 33.3 %. Another possible way to do this would be to compare the calculated airfoil-slat angle of attack with the stall angle of the NACA 0015 airfoil
Similarly, the experimental investigation has shown a convergence, albeit not at a single optimum OAR value, of all the blockage-ratio curves. The range of optimum OAR values lies between 0.55 and 0.65. Because of the large slope of certain blockage-ratio curves, a small systematic experimental error can translate into a significant shift in what is considered the optimum OAR. Therefore, great care should be taken in acquiring the data, especially at large blockage ratios.

The experiments have also shown that a model whose blockage is equivalent to a normal flat plate of 33.3 % blockage ratio would be exceeding the capability of the Tolerant wind tunnel. Although free-air conditions can still be obtained for this high blockage value, it cannot be expected to occur at the same optimum OAR value of smaller models.

It should be pointed out that due to the relative size of its wake the normal flat plate is the "bluffest" model, and that the use of other types of bluff body such as the circular cylinder (with or without splitter plate) will suffer less wake blockage than the flat plate of the same blockage ratio.

The use of tufts in the plenum and on the airfoil-slats makes the Tolerant wind tunnel "self-complaining" about the blockage it has to put up with. This simple flow visualization provides an easy means for detecting the presence of stall flow or the disappearance of the separating shear-layer in the plenum.

When determining the sizes of the Tolerant test section, the depth of the plenum should be great enough to ensure that slat stall angle remains the limiting condition for maximum permissible blockage ratio.

Although power consumption was not measured in the present study, based on measurements made in an earlier study of the Tolerant wind tunnel by Malek [12], it seems likely that the power would be about 5 to 10 % higher for the Tolerant tunnel than for a solid-wall tunnel, largely because of the effects of the plenum flow.
Finally, it is important to note that the wake of a model tested in the Tolerant wind tunnel can be altered in two ways. Firstly, the interaction between the shed vortices and the slotted wall is likely to modify the dynamics of the vortex flow (and perhaps the separation position) behind the model. Secondly, the oscillatory flow re-entry, the flow rushing out of the plenum to re-enter the test section, will increasingly affect the wake flow as the distance downstream of the model increases.

7.2 RECOMMENDATIONS FOR FUTURE WORK

Depending on the goals sought for the theoretical modelling of the Tolerant test section, this model can be improved in many ways; but most improvements are likely to seriously complicate the mathematics of the model and lengthen considerably the computation of the solution. For example, the modelling of the plenum free shear layer by imposing a constant pressure distribution along the separating streamline should bring the solution closer to reality. The problem, however, would become non-linear; that is, the geometry of the shear layer has to be known to be able to impose the required condition and the application of the constant pressure distribution will modify the geometry of the separating streamline. Clearly, the solution requires an iterative procedure.

The adaptation of the mathematical model for the testing of non-symmetric bluff bodies such as an inclined flat plate would be a useful addition. Christopher and Wolton [26] have developed such a model for unconfined conditions. An experimental counterpart would then be required to assess the real flow and evaluate the theory.

In order to determine the maximum allowable bluff body length, the testing of models with long after bodies such as the half Rankine body and long rectangular cylinder should be considered. They will provide fixed "wake" dimensions. In the case of the half Rankine body an equivalent numerical solution would also be easy to obtain.

Should a longer test section (or slotted-wall) become necessary, the opening of the plenum downstream end-wall to atmospheric pressure should delay some of the re-entering
flow responsible for a shortening of the useable test section. The breather would then take care of mass conservation in the tunnel.

Finally, the Tolerant wind tunnel should now be considered for three-dimensional bluff body testing; including boundary-layer wind tunnels with long test sections. For example, the use of 3 slotted panels (walls and ceiling) with an OAR of about 0.6 and a solid-walled floor could be used for the testing of models such as aircraft in take-off or landing configuration, cars, and buildings.
1. Rae Jr., W.H. and Pope, A.
Low-Speed Wind Tunnel Testing.

Subsonic Wind Tunnel Wall Corrections.
AGARDograph 109, 1966

3. Maskell, E.C.
A Theory of the Blockage Effects on Bluff Bodies and Stalled Wings in a Closed
Wind Tunnel.
ARC R. & M. No. 3400, November 1963.

4. Gould, R.W.F.
Wake Blockage Corrections in a Closed Wind Tunnel for One or Two
Wall-Mounted Models Subject to Separated Flows.
NPL AERO REPORT 1290, February 1969.

5. Williams, C.D. and Parkinson, G.V.
A Low-Correction Wall Configuration for Airfoil Testing.

6. AGARD Publication
Numerical Methods and Wind Tunnel Testing.
AGARD-CP-210, October 1976.

7. AGARD Publication
Wind Tunnel Corrections for High Angle of Attack models.

8. AGARD Publication
Wall Interference in Wind Tunnels.
AGARD-CP-335, September 1982.

9. Mokry, M., Chan, Y.Y., Jones, D.J. and Ohman, L.H.
Two-Dimensional Wind Tunnel Wall Interference.
AGARD-AG-281, November 1983

10. Williams, C.D.
A New Slotted-Wall Method for Producing Low Boundary Corrections in
Two-Dimensional Airfoil Testing.
Ph. D. Thesis, Dept. of Mechanical Engineering, The University of British
Columbia, October 1975.
11. Parkinson, G.V., Williams, C.D. and Malek, A.
Development of a low-Correction Wind Tunnel Wall Configuration for Testing
High Lift Airfoils.

12. Malek, A.F.
An Investigation of the Theoretical and Experimental Aerodynamic
Characteristics of a Low-Correction Wind Tunnel Wall Configuration for Airfoil
Testing.
Ph. D. Thesis, Dept. of Mechanical Engineering, The University of British
Columbia, April 1983.

13. Raimondo, S. and Clark, P.J.F.
Slotted Wall Test Section for Automotive Aerodynamic Facilities.
Virginia, U.S.A., Paper AIAA-82-0585-CP.

Slotted-Wall Test Section for Automotive Aerodynamic Tests at Yaw.
SAE International Congress, February 28 to March 4, 1983, Michigan, U.S.A.,
Paper 830302.

15. Elfstrom, G.M., Flay, R.G.J. and Clark, P.J.F.
Slotted Wall Test Section for Car and Truck Aerodynamic Testing.
Proceedings of the ASME Conference on Aerodynamics of Transportation,
Boston, November 14-18, 1983.

16. Parkinson, G.V.
A Tolerant Wind Tunnel for Industrial Aerodynamics.

17. Goldstein, S.
Modern developments in Fluid Dynamics.

18. Thwaites, B.
Incompressible Aerodynamics.

19. Roshko, A.
On the Development of Turbulent Wakes from Vortex Streets.

20. Roshko, A.
On the Drag and Shedding Frequency of Two-Dimensional Bluff bodies.
NACA Technical Note 3169, 1954.

Characteristics of the Flow Around a Bluff Body near a Plane Surface.
22. Lamb, H.
   \textit{Hydrodynamics.}

23. Roshko, A.
   \textit{A New Hodograph for Free-Streamline Theory.}

24. Parkinson, G.V. and Jandali, T.
   \textit{A Wake Source Model for Bluff Body Potential Flow.}

25. El-Sherbiny, S.E.I-S.
   \textit{Effect of Wall Confinement on the Aerodynamics of Bluff Bodies.}

   \textit{A Wake Source Model for Non-Symmetric Flow Past Bluff Bodies in Two-Dimensional Flow.}

27. Kiya, M. and Arie, M.
   \textit{An Inviscid Bluff-Body Wake Model Which Includes the Far-Wake Displacement Effect.}

28. Bearman, P.W. and Fackrell, J.E.
   \textit{Calculation of Two-Dimensional and Axisymmetric Bluff-Body Potential Flow.}

29. Stansby, P.K.
   \textit{A Generalized Discrete-Vortex Method for Sharp-Edged Cylinders.}

30. Inamuro, T., Adachi, T. and Sakata, H.
   \textit{A Numerical Analysis of Unsteady Separated Flow by Vortex Shedding Model.}

31. Kiya, M. and Arie, M.
   \textit{A Contribution to an Inviscid Vortex-Shedding Model for an Inclined Flat Plate in Uniform Flow.}

32. Kennedy, S.F.
   \textit{The Design and Analysis of Airfoil Sections.}

33. Hoerner, S.F.
   \textit{Fluid-Dynamic Drag.}
34. Blevins, R.D.  
*Applied Fluid Dynamics Handbook.*  

35. Farrel, C., Carrasquel, S., Guven, D. and Patel, V.C.  

*Laminar Separating Flow Over Backsteps and Cavities, Part II: Cavities.*  

37. Bearman, P.W. and Morel, T.  
*Effect of Free Stream Turbulence on the Flow Around Bluff Bodies.*  

38. Roshko, A.  
*Experiments on the Flow Past a Circular Cylinder at Very High Reynolds Number.*  
APPENDIX 1

WIND TUNNEL CALIBRATION

The purpose of the empty wind tunnel calibration is to relate the velocity obtained with
a Pitot tube in the nozzle to the velocity in the test region. This method follows closely the
technique used by Williams [10]. The main differences are the use of a smaller Pitot tube (0.25
inch in diameter) in order to reduce its wake interference with the model, and its position of 22
inches upstream of the test-section entrance. This new tube position was chosen to improve
accuracy by producing larger numerical output on both the Betz manometer and the Barocel
pressure transducer.

The method is based on the assumption that there is no appreciable difference in total
head, $H$, between the test section and the nozzle. Thus

$$ H_t = p_{\infty t} + q_t = H_n = p_{\infty n} + q_n \quad (A1.1) $$

where $H$ is the total head, $p_{\infty}$ the static pressure and $q$ the dynamic pressure. The subscripts $t$ and $n$ refer to the test section and nozzle Pitot-tubes, respectively.

The calibration was performed by running the wind tunnel over a range of speeds
covering the nominal test speeds, and measuring $q_t$ and $q_n$. Then, the ratio $q_t / q_n$ leads to a
constant of proportionality, or

$$ q_t = K_2 q_n \quad (A1.2) $$

The pressure coefficient $C_p$ defined as

$$ C_{p_f} = \frac{p_f - p_{\infty f}}{q_t} \quad (A1.3) $$
where \( p_i \) is a surface pressure measured at a point \( i \), can be rewritten using equations (A1.1) and (A1.2) to obtain

\[
C_{p_i} = \frac{p_i - (H_n - q_t)}{K_2 q_n}
\]

or

\[
C_{p_i} = \frac{p_i - (H_n - K_2 q_n)}{K_2 q_n}
\]

or finally

\[
C_{p_i} = 1 + \frac{p_i - H_n}{K_2 q_n}
\]  \hspace{1cm} (A1.4)

Note that all the quantities \( p_i, H_n \) and \( q_n \) are measured in volts and do not need to be converted to pressure units. The value of the calibration constant \( K_1 \) is typically 0.95 for the solid-wall test section and 0.98 for the slotted-wall test section (independent of OAR).

However, for certain applications like the evaluation of the Strouhal number, the conversion from voltage to pressure units becomes necessary and is obtained through a calibration constant \( K_1 \), such that

\[
q_n \text{ (mm H}_2\text{O)} = K_1 \text{ (mm H}_2\text{O/volt)} q_n \text{ (volt)}
\]  \hspace{1cm} (A1.5)

The \( q_n \) values are simultaneously measured in volts with the Barocel pressure transducer and in mm H\(_2\)O on the Betz water micromanometer.
EVALUATION OF THE INFLUENCE COEFFICIENT

The detailed solution of the integral equation leading to the influence coefficient is given in reference [32]. This appendix summarizes the necessary expressions for the evaluation of $K_{ij}$ for the case where a straight panel with constant vorticity $\gamma_j$ is used.

\[
K_{ij} = \frac{1}{4\pi} \{ (b + \Delta) \ln(r_1^2) - (b - \Delta) \ln(r_2^2) + 2a \tan^{-1} \left( \frac{2a \Delta}{a^2 + b^2 - \Delta^2} \right) - 4\Delta \}
\]

where

\[ r_1^2 = a^2 + (b + \Delta)^2 \]
\[ r_2^2 = a^2 + (b - \Delta)^2 \]

$2 \Delta$ is the size of the panel.

and

\[ a = (y_i - y_j) \cos \theta_j - (x_i - x_j) \sin \theta_j \]
\[ b = (y_i - y_j) \sin \theta_j + (x_i - x_j) \cos \theta_j \]

$\theta_j$ is the angle between the panel $j$ and the wind axis (usually horizontal).
DETERMINATION OF VELOCITY FIELD

A velocity vector calculated either at a control point \( C_i \) or any point \((x_i, y_i)\) in the domain is the result of three contributions:

- \( V_U \): Uniform (onset) flow
- \( V_S \): Point sources
- \( V_V \): Vorticity distribution on the boundaries

Each one of these contributions can be divided into two velocity components along the x and y axes, as follows:

**Uniform flow**

From the velocity potential of the uniform flow, the velocity components are found to be

\[
\begin{align*}
V_{ux} &= 1 \\
V_{uy} &= 0
\end{align*}
\]

**Point sources**

The x and y velocity components resulting from two point sources of common strength Q and situated at \((x_{s1}, y_{s1})\) and \((x_{s2}, y_{s2})\) can be written as

\[
\begin{align*}
V_{sx} &= \frac{Q}{2\pi} \left\{ \frac{\cos\lambda_1}{r_{s1}} + \frac{\cos\lambda_2}{r_{s2}} \right\} \\
V_{sy} &= \frac{Q}{2\pi} \left\{ \frac{\sin\lambda_1}{r_{s1}} + \frac{\sin\lambda_2}{r_{s2}} \right\}
\end{align*}
\]

where
\[ r_{s_1} = \left[ (x_i - x_{s_1})^2 + (y_i - y_{s_1})^2 \right]^{\frac{1}{2}} \]
\[ r_{s_2} = \left[ (x_j - x_{s_2})^2 + (y_j - y_{s_2})^2 \right]^{\frac{1}{2}} \]

and \( \lambda_1, \lambda_2 \) are the angles between a horizontal line and \( r_{s_1}, r_{s_2} \) (Note that for symmetry \( \lambda_1 \) is defined positive in the counterclockwise direction while \( \lambda_2 \) is positive in the clockwise direction).

**Vortex sheets**

The normal and tangential velocities induced by a straight panel with distributed vorticity \( \gamma \) are given by

\[ v_n = \frac{\gamma}{2\pi} \ln \frac{r_1}{r_2} \]

\[ v_t = -\frac{\gamma}{2\pi} (\sigma_1 - \sigma_2) \]

where \( \sigma_1, \sigma_2 \) are the angles between the panel and \( r_1, r_2 \). Also

\[ r_1 = \left[ (x_i - (x_j + \Delta))^2 + (y_i - (y_j - \Delta))^2 \right]^{\frac{1}{2}} \]
\[ r_2 = \left[ (x_i - (x_j - \Delta))^2 + (y_i - (y_j + \Delta))^2 \right]^{\frac{1}{2}} \]

The \( x \) and \( y \) velocity components can then be calculated by

\[ V_{y_x} = v_t \cos \beta + v_n \sin \beta \]
\[ V_{y_y} = v_t \sin \beta - v_n \cos \beta \]

where \( \beta \) is the angle between the panel and the \( x \)-axis.
APPENDIX 4

REGRESSION METHOD USED FOR COMPARING TWO PRESSURE COEFFICIENT DATA SETS

This regression method was used by Flay, Elfstrom and Clark [14] for comparing car pressure distributions measured in different wind tunnel configurations to an equivalent reference pressure distribution.

A linear regression analysis, using a least-squares technique, is performed on a reference-test and actual data set, on a velocity basis, to obtain two coefficients: a blockage correction factor, CF, and an associated standard deviation, SD. The blockage correction factor is the value by which the slotted-wall freestream speed must be divided to give the best agreement with the equivalent reference-test pressure distribution. The standard deviation can be regarded as the residual error associated with the corrected pressure distribution.

For pressure-tap number \( i \), the normalized velocity is calculated using

\[
V_i = \frac{v_i}{U} = (1 - C_p)^{1/2}
\]  

(A4.1)

for both pressure data sets. The coefficients CF and SD of the linear regression are obtained through a least-squares fit.

Let \( V_{r_i}, i = 1,...,N \) be the set of normalized reference velocities and \( V_j \) the actual velocity set. The corrected values, \( V_{c_i} \), are obtained through the linear regression

\[
V_{c_i} = A_1 + A_2 V_j
\]  

(A4.2)

for which the coefficients \( A_1 \) and \( A_2 \) are calculated to minimize the sum of the differences between \( V_{c_i} \) and the reference values \( V_{r_i} \).
That is

\[ \sum_{i=1}^{N} (V_{r_i} - (A_1 + A_2 V_i))^2 \] is minimized. \hspace{1cm} (A4.3)

\( A_1 \) is set to zero on the assumption that at the stagnation point the velocity is necessarily zero in both the reference and slotted-wall data sets.

Therefore

\[ \frac{d}{dA_2} \sum_{i=1}^{N} (V_{r_i} - (A_2 V_i))^2 = 0 \] \hspace{1cm} (A4.4)

\( A_2 \) is now called CF (blockage-correction factor) and is used to obtained the corrected pressure coefficients \( C_{P_{Cj}} \) as follows

\[ (1 - C_{P_{Cj}}) = CF^2 (1 - C_{Pj}) \] \hspace{1cm} (A4.5)

The quality of the overall fit of the slotted-wall data to the reference-test data is judged on the value of the standard deviation:

\[ SD = \left[ \frac{1}{N} \sum_{i=1}^{N} (C_{P_{ri}} - C_{P_{Cj}})^2 \right]^{\frac{1}{2}} \] \hspace{1cm} (A4.6)

To summarize, the blockage correction factor CF is a measure of the correlation between a slotted-wall and a reference-test data set; A value of CF = 1 results in no blockage correction. Consequently, the ideal configuration of the Tolerant wind tunnel should produce CF's close to unity. And, the standard deviation SD indicates the error between the corrected and reference pressure distribution; or how well CF would correct a set of slotted-wall data.
This appendix derives the expressions used to obtain varying open areas along the wind axis.

A completely defined slotted-wall configuration requires, in addition to wall length and slat size, three parameters: the OAR which fixes the number of given-size slats to be used, the open-area distribution and finally an initial condition which can, for example, be the size of the first slot.

Let \( a_0, a_1, \ldots, a_n \) be the size of the slots where the subscripts indicate the slot number increasing in the downstream direction. For a slotted-wall containing \( n \) slats, there will be \( (n + 1) \) slots. Also, let \( L \) be the length of the wall and \( c \) the chord of the airfoil-shaped slats. The open area ratio is then defined as

\[
OAR = \frac{1}{L} \left\{ \sum_{i=0}^{n} a_i \right\} = 1 - \frac{n \cdot c}{L} \quad (A5.1)
\]

For a distribution of open areas varying linearly with the slot number, we have

\[
a_j = a_0 + b \cdot i \quad \text{for } i = 1, 2, \ldots, n \quad (A5.2)
\]

where \( b \) is the slope of the linear variation.

The open-area ratio can now be written as

\[
OAR = \frac{1}{L} \left\{ \sum_{i=0}^{n} (a_0 + b \cdot i) \right\} \quad (A5.3)
\]

or
\[
OAR = \frac{1}{L} \left\{ (n+1) a_0 + b \sum_{i=0}^{n} i \right\} \quad (A5.4)
\]

And since

\[
\sum_{i=0}^{n} i = \frac{1}{2} n (n+1) \quad (A5.5)
\]

the expression for OAR becomes

\[
OAR = \frac{1}{L} \left\{ (n+1) (a_0 + \frac{1}{2} n b) \right\} \quad (A5.6)
\]

and the slope \( b \) can then be expressed as

\[
b = \frac{OAR \cdot L - (n+1)a_0}{\frac{1}{2}n(n+1)} \quad (A5.7)
\]

If we require the open areas to increase in the downstream direction, we therefore have

\[
b \geq 0
\]

or

\[
OAR \cdot L - (n+1)a_0 \geq 0 \quad (A5.8)
\]

Thus we have

\[
0 \leq a_0 \leq \frac{OAR \cdot L}{n+1} \quad (A5.9)
\]

or, using equation (A5.1)
where the upper bound corresponds to the evenly-spaced slat distribution.

One can now define a parameter, \( A0RT \), which relates the size of the \( 0^{th} \) slot \( (a_0) \) to the evenly-spaced slot size, such that

\[
A0RT = \frac{L - n \cdot c}{n+1} / a_0
\]

(A5.11)

The value of \( A0RT \) is therefore always greater or equal to 1.

Consequently, for a given (linear) variation of open areas only one parameter need to be given: \( A0RT \). Then, for a given \( A0RT \), the slot size \( a_0 \) and the slope \( b \) of the linear distribution is calculated. For the special case where \( A0RT = 1 \) we have \( b = 0 \) which is the evenly-spaced slat case.

**Slat Position**

The slat position \( x_{S_i} \), measured from the nozzle exit to the leading edge of the slat number \( i \), can then be calculated as follows

\[
x_{s_1} = a_0
\]

(A5.12)

\[
x_{s_i} = a_0 + \sum_{j=1}^{i-1} (a_j + c)
\]

\[
i = 2,3,...,n
\]

(A5.13)

After simplification, we get

\[
x_{s_i} = i \cdot a_0 + (i - 1) c + \frac{1}{2} i (i - 1) b
\]

\[
i = 1,2,...,n
\]

(A5.14)
APPENDIX 6

INSTRUMENTATION

Pressure Transducer
Barocel Pressure Sensor (DATAMETRICS inc.) Type 511J-10
range: 10 mm Hg.
Signal Conditioner (DATAMETRICS inc.) Type 1015
Power Supply (DATAMETRICS inc.) Type 700

Mechanical Pressure Scanner
Scanivalve 48-ports (SCANIVALVE Corp.) Model 48J9-2273

Pressure Lines
Polyethylene tubing (INTRAMETRIC)
Inside diameter: 1.67 mm (0.066 in.)
Outside diameter: 2.42 mm (0.095 in.)
Length: model to Scanivalve ≈ 1 m (3 ft.)
Scanivalve to pressure transducer ≈ 2 m (6.5 ft.)

Manometer
Betz Water Micromanometer (Max-PLANCK-INSTITUT für Strömungsforschung Göttingen)
Smallest division: 0.1 mm H₂O

Averaging Voltmeter
Time Domain Analyser (Solatron, SCHLUMBERGER)
Smallest division: 1 mV
Real Time Analyser

Spectrascope II (SPECTRAL DYNAMICS Corp.) Model SD335

Smallest Division : 0.2 % of range (20,100,200,500 Hz)

Smoke Generator

Concept Genie MK V (CONCEPT ENG. Ltd., England)
This section describes in detail the evaluation of experimental errors on reduced data such as dynamic pressure, velocity, Reynolds number, pressure coefficient and Strouhal number.

The method used here is termed uncertainty analysis and provides relative weighting for the errors. For a dependent variable, \( Y \), related to some independent variables, \( x_1, x_2, \ldots, x_n \), by the function \( Y = f(x_1, x_2, \ldots, x_n) \), the uncertainty of the results is

\[
\epsilon Y = \sqrt{\left( \frac{\partial f}{\partial x_1} \epsilon x_1 \right)^2 + \ldots + \left( \frac{\partial f}{\partial x_n} \epsilon x_n \right)^2} 
\]

where \( \epsilon x_1, \epsilon x_2, \ldots, \epsilon x_n \) are the uncertainties or probable errors of the variables \( x_1, x_2, \ldots, x_n \), respectively.

To use uncertainty analysis it is necessary to obtain the error associated with each independent variable. Such uncertainties are determined sometimes from the precision of an instrument, or from the experience of the experimenter.

The following table summarizes the uncertainties in the basic variables, and forms the basis for the subsequent calculations.

| \( \epsilon K_1 \) | \( \pm 1\% \) |
| \( \epsilon K_2 \) | \( \pm 1\% \) |
| \( \epsilon p_i \) | \( \pm 0.002 \) volts |
| \( \epsilon q_n \) | \( \pm 0.003 \) volts |
| \( \epsilon H_n \) | \( \pm 0.003 \) volts |

Table A7.1: Uncertainties in basic values.
Uncertainty in Pressure Coefficients, $C_p$

Applying equation (A7.1) on the expression used to calculate the pressure coefficients,

$$C_{p_j} = 1 + \frac{p_j - H_n}{K_2 q_n}$$ (A7.2)

the uncertainty in $C_{p_j}$ becomes

$$\epsilon C_{p_j} = \left[ \left( \frac{1}{K_2 q_n} \epsilon p_j \right)^2 + \left( \frac{-1}{K_2 q_n} \epsilon H_n \right)^2 + \left( \frac{p_j - H_n}{K_2^2 q_n} \epsilon K_2 \right)^2 + \left( \frac{p_j - H_n}{K_2 q_n^2} \epsilon q_n \right)^2 \right]^{\frac{1}{2}}$$ (A7.3)

This relation shows that maximum error will occur when $q_n$ is the lowest, when testing the largest model (12 inches in diameter) and when $(p_j - H_n)$ is largest, generally corresponding to the base pressure. The uncertainty at the stagnation point ($C_p = 1$), when $(p_j - H_n) = 0$ and for $K_2 = 0.985$ and $q_n = 0.179$ volts corresponding to a typical test for a 12-inch diameter cylinder, leads to $\epsilon C_p = \pm 0.02$ or about $\pm 2\%$. The same calculation but including a base pressure $p_j = -0.377$ volt and a total head $H_n = 0.308$ volt corresponding to a testing of the same 12-inch cylinder between solid walls gives $\epsilon C_p = \pm 0.08$. For these conditions the base pressure is $C_{pb} = -3.0$ which translates to percentage error of about 3\%.

Uncertainty in Dynamic Pressure, $q$

The relation between $q_n$, the dynamic pressure obtained in the nozzle and expressed in volts, and $q$, the true dynamic pressure of the test section in psi units, is given by

$$q \text{ (psi)} = K_1 \text{ (mm } H_2 O / \text{ volt)} q_n \text{ (volts)} C \text{ (psi / mm } H_2 O)$$ (A7.4)

where $K_1$ is a calibration value and $C$ is used to convert the units. Again using equation (A7.1) to
obtain a measure of the uncertainty, we get

\[ \epsilon q = \left[ (q_C e K_1)^2 + (K_1 C e q_n)^2 \right]^{\frac{1}{2}} \]  (A7.5)

or in terms of percentage

\[ \frac{\epsilon q}{q} = \left[ \left( \frac{e K_1}{K_1} \right)^2 + \left( \frac{e q_n}{q_n} \right)^2 \right]^{\frac{1}{2}} \]  (A7.6)

which gives for the lowest wind speed an error of about ± 2%.

**Uncertainty in Reynolds Number, Re**

The general expression for \( Re \) is

\[ Re = \frac{U h}{\nu} \]  (A7.7)

where the velocity \( U \) is obtained from the dynamic pressure

\[ U = \sqrt{\frac{q}{\frac{1}{2} \rho}} \]  (A7.8)

thus,

\[ Re = \frac{h \sqrt{\frac{q}{\nu}}}{\nu \sqrt{\frac{1}{2} \rho}} \]  (A7.9)

In calculating the error in \( Re \) we neglect the uncertainty in the model size \( h \) but include an error in the kinematic viscosity \( \nu \) and density \( \rho \) due to temperature variations. Applying (A7.1) on (A7.9) we get
and the error in $\nu$ and $\rho$ is estimated for a variation of 10° F about the standard temperature 70° F. Thus, for

$$\rho = 0.07492 \pm 0.0014 \text{ lb/ft}^3$$
$$\nu = 1.64 \pm 0.06 \times 10^{-4} \text{ ft}^2/\text{sec}$$

we obtain an error in $\text{Re}$ of about $\pm 4.0\%$.

**Uncertainty in Strouhal numbers, $St$**

The expression for the Strouhal number is given by

$$St = \frac{f h}{U} \quad \text{(A7.11)}$$

where $f$ is the vortex-shedding frequency. The velocity $U$ is obtained from (A7.8). Hence,

$$St = \frac{f h \sqrt{\frac{1}{2} \rho}}{\sqrt{q}} \quad \text{(A7.12)}$$

and the error in $St$ becomes

$$\frac{e St}{St} = \left[ \left( \frac{ef}{f} \right)^2 + \left( -\frac{e q}{q} \right)^2 + \left( -\frac{e \rho}{\rho} \right)^2 \right]^{\frac{1}{2}} \quad \text{(A7.13)}$$

For an error in frequency estimated to be no more than $\pm 2\%$, the resulting error on the $St$ is $\pm 2.5\%$. 
Uncertainty in Drag Coefficients, $C_d$

Because the drag coefficients are obtained from integration of the surface pressure distribution and therefore depend on the interpolation method used, it is difficult to calculate its error. However, it is estimated, based mainly on the error in $C_p$, to be about 5%.
Figure 2.1: Single-slatted-wall tunnel configuration for airfoil testing.

Figure 2.2: Double-slatted-wall tunnel configuration for bluff body testing.
Figure 3.1 (a): Physical and basic transform planes for a flat plate model.

Figure 3.1 (b): Physical and basic transform planes for a circular cylinder model.
Figure 3.2: Theoretical representation of the Tolerant wind tunnel.
Figure 4.1: Pressure distribution over a normal flat plate in unconfined flow: comparison of numerical calculation in transform plane with analytical solution.
Given $C_{pb} = -1.38$, $N = 70$

Figure 4.2: Pressure distribution over a circular cylinder in unconfined flow: comparison of numerical calculation in transform plane with analytical solution.
Given $C_{pb} = -0.96$, $\beta_5 = 80^\circ$, $N = 70$
Figure 4.3: Variation of source strength with number of panels in transform plane, for flat plate and circular cylinder in unconfined flow.
Figure 4.4: Pressure distribution over a normal flat plate in unconfined flow: comparison of numerical calculation in physical plane with analytical solution. Given $C_{pb} = -1.38$, $N = 60$

Figure 4.5: Pressure distribution over a circular cylinder in unconfined flow: comparison of numerical calculation in physical plane with analytical solution. Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $N = 60$
Plate and circular cylinder in unconfined flow.

Figure 4.6 (6) : Variation of base pressure coefficient with number of panels in physical plane, for flat plate and circular cylinder in unconfined flow.

Figure 4.6 (a) : Variation of source strength with number of panels in physical plane, for flat plate and circular cylinder.

Cp, FLAT PLATE

Cp, CIRCULAR CYLINDER

Q, FLAT PLATE

Q, CIRCULAR CYLINDER
Figure 4.7: Pressure distribution over a normal flat plate in solid-wall confined flow: comparison of numerical calculation in physical plane with analytical solution.

Given $C_{pb} = -1.0$, $h/H = 1/3$

$N$(model) = 80, $N$(wall) = 20

Figure 4.8: Corrected pressure distribution over a normal flat plate in solid-wall confined flow: comparison of corrected numerical calculation in physical plane with free-air analytical solution.

Given $C_{pb} = -1.0$, $h/H = 1/3$

$CF = 0.6749$
Figure 4.9: Variation of base pressure coefficient with number of panels on solid walls, for a normal flat plate model in confined flow.
Given $C_{pb} = -1.0$, $h/H = 1/3$, Wall Length = 12

Figure 4.10: Variation of base pressure coefficient with wall length, for a normal flat plate in confined flow.
Given $C_{pb} = -1.0$, $h/H = 1/3$, $N(\text{model}) = 80$, $N(\text{wall}) = 20$
Figure 4.12: Variation of blockage-correction factor with base pressure.

Figure 4.11: Variation of blockage ratio for a normal flat plate in contained flow.
Figure 4.13: Pressure distribution over a circular cylinder in solid-wall confined flow: comparison of numerical calculation in physical plane with free-air analytical solution.

Given $C_{p_b} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, $N_{\text{(model)}} = 80$, $N_{\text{(wall)}} = 20$

Figure 4.14: Corrected pressure distribution over a circular cylinder in solid-wall confined flow: comparison of corrected numerical calculation in physical plane with free-air analytical solution.

Given $C_{p_b} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, $CF = 0.7827$
Figure 4.15: Variation of base pressure coefficient with number of panels on solid walls, for a circular cylinder model in confined flow.
Given $C_{pb} = -1.0$, $\beta_s = 80^\circ$, $h/H = 1/3$, Wall Length = 12

Figure 4.16: Variation of base pressure coefficient with wall length, for a circular cylinder in confined flow.
Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$, $h/H = 1/3$, $N$(model) = 80, $N$(wall) = 20
Given $C_{pb} = 0.80$, $\theta = 20^\circ$

**Figure 4.16**: Variation of blockage-correction factor with blockage ratio, for a circular cylinder in combined flow.
Figure 4.19: Theoretical variation of base pressure coefficient as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.
Figure 4.20: Theoretical variation of blockage correction factor as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.

Figure 4.21: Theoretical variation of standard deviation as a function of OAR for 4 sizes of flat plate model positioned at the center of the test section.
Theoretical variation of pressure coefficient at $\beta = 30^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Given $C_{pb} = -0.96, \beta_s = 80^\circ$

Theoretical variation of pressure coefficient at $\beta = 60^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Given $C_{pb} = -0.96, \beta_s = 80^\circ$
Given $C_p = 0.96$, $\theta = 80^\circ$.

Pressure Coefficient at 70°

centered at the center of the test section for 4 sizes of circular cylinder model coefficient ($\theta = 80^\circ$) as a function of OAR.

Figure 4.25: Theoretical Variation of pressure coefficient.
Figure 4.26: Theoretical variation of blockage correction factor as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section. Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$

Figure 4.27: Theoretical variation of standard deviation as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section. Given $C_{pb} = -0.96$, $\beta_s = 80^\circ$
Figure 5.1: The closed-circuit "Green" wind tunnel.

Figure 5.2: Pressure tap positions on the floor and in the plenum.
Figure 5.3: Pressure tap positions on models.
Figure 5.4: Tuft positions in the plenum.

Figure 5.5: Modified "Green" wind tunnel for smoke flow visualization.
Figure 6.1 (a) to (m): Pressure distribution over 3 different sizes of flat plate model. 
Re = 10^5
Figure 6.1 (a) to (m): Pressure distributions over 3 different sizes of flat plate model.

Re = 10^5
Figure 6.1 (a) to (m): Pressure distributions over 3 different sizes of flat plate model.

Re = 10^5
Figure 6.1 (a) to (m): Pressure distributions over 3 different sizes of flat plate model.
Re = 10^5
Figure 6.1 (a) to (m): Pressure distributions over 3 different sizes of flat plate model. 
Re = $10^5$
Figure 6.2 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (dimensionalized plot).
Figure 6.2 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (dimensionalized plot).
Figure 6.3 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (non-dimensionalized plot).
Figure 6.3 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at center (non-dimensionalized plot).
Figure 6.4: Variation of base pressure coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.
Figure 6.5: Variation of front drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.6: Variation of drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at center.
Figure 6.7: Variation of Strouhal number as a function of OAR for 3 sizes of flat plate model positioned at center.
Figure 6.8: Variation of blockage-correction factor as a function of OAR for 3 sizes of flat plate model positioned at center.

Figure 6.9: Variation of standard deviation as a function of OAR for 3 sizes of flat plate model positioned at center.
Figure 6.10 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (dimensionalized plot).
Figure 6.10 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (dimensionalized plot).
Figure 6.11 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (non-dimensionalized plot).
Figure 6.11 (a) to (d): Floor static pressure distributions for 3 different sizes of flat plate model positioned at 22 inches upstream of the center (non-dimensionalized plot).
Figure 6.12: Variation of base pressure coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.
Figure 6.13: Variation of front drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.14: Variation of drag coefficient as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.
Figure 6.15: Variation of Strouhal number as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.
Figure 6.16: Variation of blockage-correction factor as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.

Figure 6.17: Variation of standard deviation as a function of OAR for 3 sizes of flat plate model positioned at 22 inches upstream of the center.
Experimental Results

- Blockage Ratio: 8.3 %
- Blockage Ratio: 13.8 %
- Blockage Ratio: 25.0 %
- Blockage Ratio: 33.3 %

- Roshko, Ref. 20

Reynolds nb.: \(1.00 \times 10^9\)

 Nb. of Slots: 99
Open Area Ratio: 0.000

---

**Figure 6.18 (a) to (m):** Pressure distributions over different sizes of circular cylinder model.

\(Re = 10^5\)
Experimental Results

- Blockage Ratio: 13.8 \%
- Blockage Ratio: 25.0 \%
- Blockage Ratio: 33.3 \%
- Roshko, Ref. 20

Reynolds no.: 1.00 \times 10^4
Nb. of Slots: 16
Open Area Ratio: 0.417

Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model.
Re = 10^5

(c)

(d)
Experimental Results

△ Blockage Ratio : 13.8 %
+ Blockage Ratio : 25.0 %
X Blockage Ratio : 33.3 %
— Roshko, Ref. 20

Reynolds nb. : 1.00 x 10^3
Nb. of Slots : 14
Open Area Ratio : 0.490

Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model.
Re = 10^5
Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model. 
Re = 10^5
**Experimental Results**

- Blockage Ratio: 6.3 \% 
- Blockage Ratio: 13.8 \%
- Blockage Ratio: 25.0 \%
- Blockage Ratio: 33.3 \%

Roshko, Ref. 20

Reynolds nb. = 1 \times 10^5
Nb. of Slats = 10
Open Area Ratio = 0.635

---

Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model.

\( \text{Re} = 10^5 \)
Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model. 
Re = $10^5$
Figure 6.18 (a) to (m): Pressure distributions over 4 sizes of circular cylinder model.

Re = 10^5
Figure 6.19: Variation of pressure coefficient at $\beta = 50^\circ$ as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
pressure coefficient at 100

Figure 6.21: Variation of pressure coefficient at $\theta = 180^\circ$

circular cylinder model positioned at the center of the test section.

Pressure Coefficient at 180

Open Area Ratio

Pressure Coefficient at 100

Open Area Ratio

Figure 6.20: Variation of pressure coefficient at $\theta = 90^\circ$

circular cylinder model positioned at the center of the test section.

Pressure Coefficient at 90

Open Area Ratio

Pressure Coefficient at 90

Open Area Ratio
Figure 6.22: Variation of front drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.23: Variation of rear drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Figure 6.24: Variation of drag coefficient as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.25: Variation of Strouhal number as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Figure 6.26: Variation of blockage-correction factor as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.

Figure 6.27: Variation of standard deviation as a function of OAR for 4 sizes of circular cylinder model positioned at the center of the test section.
Figure 6.28: Pressure distributions over 4 sizes of circular cylinder model tested between non-evenly spaced slat-walls.
Re = 10^5, OAR = 0.453, A/0RT = 1.5

Figure 6.29: Pressure distributions over 4 sizes of circular cylinder model tested between non-evenly spaced slat-walls.
Re = 10^5, OAR = 0.453, A/0RT = 3.0
Figure 6.30 (a) to (m): Pressure distributions over different sizes of circular-cylinder-splitter-plate model. $Re = 10^5$. 
Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. Re = 105
Experimental Results

- Blockage Ratio: 8.3 X
- Blockage Ratio: 13.8 X
- Blockage Ratio: 25.0 X
- Blockage Ratio: 33.3 X

--- Roshko, Ref. 20

Reynolds nb.: $1.00 \times 10^3$
Nb. of Slats: 14
Open Area Ratio: 0.490

---

Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. $Re = 10^5$
Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. Re = 10^5
Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. $Re = 10^5$
Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. Re = 10^5
Figure 6.30 (a) to (m): Pressure distributions over 4 sizes of circular-cylinder-splitter-plate model. $Re = 10^5$
Figure 6.31: Variation of pressure coefficient at $\beta = 50^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.32: Variation of pressure coefficient at $\beta = 100^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.33: Variation of pressure coefficient at $\beta = 180^\circ$ as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.34: Variation of front drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.35: Variation of rear drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.36: Variation of drag coefficient as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.37: Variation of blockage-correction factor as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.

Figure 6.38: Variation of standard deviation as a function of OAR for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.39: General flow pattern in the plenums for normal operation.
Figure 6.40 (a) to (i): Plenum pressure distributions for a sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test.
Figure 6.40 (a) to (l): Plenum pressure distributions for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.40 (a) to (l): Plenum pressure distributions for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.40 (a) to (l): Plenum pressure distributions for 4 sizes of circular-cylinder-with-splitter-plate model positioned at the center of the test section.
Figure 6.41: General flow pattern in the plenums for extreme conditions.
Figure 6.42: Plenum pressure distributions corresponding to testing of normal flat plates at 22 inches upstream of the test section center. OAR = 0.563