Secondary flows and losses in gas turbines.

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SECONDARY FLOWS AND LOSSES IN GAS TURBINES

Volume I of II

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A thesis submitted for the degree of Doctor of Philosophy
of the University of Durham

March 1985

DECLARATION

The work contained in this thesis has not been submitted elsewhere for any other degree or qualification and that unless otherwise referenced it is the author's own work.

ABSTRACT

Early stages of axial flow turbine design require a relatively simple prediction technique for estimating both blade row exit angle and loss profiles produced by secondary flows. Detailed experimental investigation of the flow field in a large scale linear cascade of high turning turbine rotor blades has been made. This gave improved understanding of cascade secondary flow phenomena, and a physical basis for secondary flow angle and loss predictions. Data suitable for comparison with three dimensional flow calculations is presented.

Experimental data was obtained utilizing cobra probes throughout the flow field, and hot wire probes at cascade inlet and exit. Results are presented graphically on various planes through the flow field using both contour and vector plots. The developing passage and leading edge horse-shoe vortices are traced, and their interactions with the cascade inlet boundary layer are clearly visible. At cascade exit two major secondary loss components were identified: a loss core shed from the suction surface formed largely of inlet boundary layer fluid, and an area consisting of new endwall boundary layer fluid swept towards the suction surface. Highly turbulent flows were also evident close to these regions.

Secondary losses were predicted using three discrete loss components: the loss core, a non skewed new endwall boundary layer, and an extra secondary loss related to the classical secondary flow kinetic energy. Experimental data from several sources was compared with secondary loss predictions with some success. Some modifications are clearly desirable to enhance the loss prediction technique, but the relatively simple method gives encouraging results.

ACKNOWLEDGEMENTS

The encouragement of my supervisor Dr. David Gregory-Smith has been much appreciated throughout the period of study spanned by this work.

My grateful thanks are extended to all staff, both academic and technical, of the Engineering Department for their assistance during my period as a research student.

The support of Rolls-Royce Ltd., and the Science and Engineering Research Council are gratefully acknowledged.

Thanks are also due to Chris Wright for her accurate typing of my manuscript.

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NOMENCLATURE

A	Axial co-ordinate
C_L	Lift coefficient
E_{SEC}	Secondary energy loss coefficient
G	Geometric function of a turbomachine
H	Blade height
P	Pressure
R	Gas constant
Re	Reynolds number
S	Spanwise co-ordinate
T	Tangential co-ordinate
V	Velocity
Y	Secondary loss coefficient
a	Annulus area
c	Chord
d	Diameter
h	Hot wire coefficient
i	Incidence angle
k	Hot wire coefficient
l	Length
$\dot{m}$	Mass flow rate
p	Blade pitch
r	Radius
s	Hot wire sensitivity
u	Streamwise hot wire cooling velocity parallel to endwall
v	Streamwise normal hot wire cooling velocity parallel to endwall

w	Streamwise normal hot wire cooling velocity perpendicular to endwall
α	Calibration turret twist angle
β	Calibration turret tilt angle
γ	Flow angle
δ	Boundary layer thickness
δ^*	Displacement thickness
ϵ	Yaw angle
ζ	Total pressure loss coefficient
θ	Probe mounting setting angle
κ	Aspect ratio
λ	Spanwise angle
μ	Viscosity
ξ	Vorticity
ρ	Fluid density
σ	Pressure probe calibration function
τ	Pressure probe calibration function
ϕ	Pressure probe calibration function
χ	Static pressure coefficient
ψ	Pressure probe calibration function
Ψ	Blade tip clearance height
ω	Angular velocity
Ω	Angle of twist between principal normal to a streamline and a Bernoulli Surface

Subscripts

A	Axial
AT	Atmospheric
G	Gross

S	Spanwise
T	Tangential
LOC	Local Cascade Value
XY	Hot wire probe pair in plane of endwall
XZ	Hot wire probe pair perpendicular to plane of endwall
b	Blade surface
e	Experimental value
fil	Filament
h	Hub
n	Normal direction
rel	Relative
sec	Distributed secondary
shed	Shed
st	Streamwise
t	Tip
0	Total
1	Upstream (inlet)
2	Downstream (outlet)

Pressure Probe Tip Tapping Notation

P_B	Bottom tapping pressure (Five-hole probe only)
P_C	Central tapping pressure
P_L	Left tapping pressure
P_R	Right tapping pressure
P_T	Top tapping pressure (Five-hole probe only)

Superscripts

$\bar{}$	Mean value
$'$	Fluctuating value

Underscore

$\_$	Vector value

CHAPTER 1

INTRODUCTION

Secondary flows in a turbo-machine are described as the deviation of the real flow from the primary idealised flow field. High turning blading associated with turbines frequently suffers more severely from secondary flows than the lower turning blading of compressors. Axial flow turbine blading of high turning is the main area investigated within this study.

A major contribution to these secondary flows arises within a blade row from the turning of a flow having an initial velocity gradient, frequently due to the hub or casing boundary layer. Progressive turning increases the strength of the passage vortex as the trailing edge is approached. Additionally the upstream inlet boundary layer interacts with the blade leading edge to produce a leading edge vortex. This wraps itself around the leading edge and sweeps into the flow field of adjacent blade passages, forming suction and pressure side vortex legs. Within the blade passage, the passage vortex sweeps the inlet boundary layer into a low energy loss core on the blade suction surface. High transverse velocities close to the endwalls sweep any developing boundary layer fluid towards the suction surface. These are some of the flow phenomena encountered in rectangular cascade flow; in real machines however the flow is further complicated. Skewed inlet endwall boundary layers, tip leakage flows, radial pressure gradients, profiled endwalls, coolant flows and high turbulence levels are some additional features of turbine secondary flows. It is evident from this qualitative description of secondary flows that they are a complex three-dimensional phenomenon influenced by many factors.

Accurate prediction of turbomachinery flow is a goal aimed at by many workers in fluid mechanics. Considerable progress has been made in this area with three-dimensional calculation techniques of increasing complexity, helped by computer hardware developments. Encouraging results are obtained from this work; however three-dimensional data is voluminous, both to describe machine geometry and to contain the results. Preparation and assimilation of data from these calculation techniques is a time consuming process. These methods consequently are appropriate for use in the design context only when a finalized design is approached.

In preliminary design a need exits for less sophisticated, but none the less reliable, prediction technique suitable for use in iterative type calculations. Predictions of blade row exit angles can be obtained, fairly reliably, using classical secondary flow type methods. Blade profile losses can be estimated using boundary layer techniques or correlations. Secondary loss predictions are often based on correlations, giving an overall value and no indication of the spanwise loss distribution. A selection of secondary loss correlations are available, giving a wide range of loss prediction values for a given blade row. In this variety of correlations universal agreement has not been obtained on the most significant factors influencing the losses, shedding further doubt on their predictive abilities.

A technique proposed by Gregory-Smith (1982) provides a predicted loss profile at blade row exit, based on three independent loss components. These components can be identified in experimental results and consist of the loss core, a new endwall boundary layer loss, and an extra secondary loss. The loss core appears closest to the blade suction surface at exit, with its centre off the endwall. Currently this is assumed to be of triangular shape in the prediction method for ease of calculation. A two-dimensional non-skewed turbulent boundary layer is assumed to grow downstream of the blade throat, and its secondary loss contribution is evaluated from a correlation. The extra secondary loss generated is assumed to be directly proportional to the secondary kinetic energy calculated using a classical method. Although relatively crude in its present form loss predictions obtained using this method suggest that further development would be beneficial.

A review of the literature has shown that many researchers have formulated calculation methods, of varying complexity to predict the secondary velocity distribution at blade row exit. Variations in flow exit angle of a blade row can therefore be evaluated with some degree of confidence. Secondary loss prediction methods for turbines are more difficult to find. Compressor secondary losses can often be estimated from a boundary layer type analysis, however this is not generally possible for the higher turning blading of turbines. Most turbine secondary loss prediction methods are based on correlations of experimental data. Frequently, however, these are obtained only from the rather idealized cascade environment, due to practical difficulties of traversing within working machines. Some workers have proposed complex loss calculation schemes using simplified Navier-Stokes equations with limited success. Several experimental studies have been performed with varying degrees of detail on aspects of turbine secondary flow.

Detailed flow field data from within blading is sparse, with some notable exceptions. Experimental studies on one set of blading can only give quantitative data on the geometry considered, and consequently a major objective of this study was to obtain additional detailed data from a new experimental condition. Turbulence studies within turbines and cascades reported in the open literature are almost non-existent. Another objective of this study was therefore to obtain turbulence data, and gain understanding of the detailed loss generation mechanisms involved within the flow field. This information could be used to refine the secodnary loss prediction method.

A large scale linear cascade of high turning turbine rotor blade profiles was constructed, with provision for detailed probe traversing to investigate the flow field. Traversing was performed using both pressure sensing and hot wire probes, to obtain mean and fluctuating flow field data. A computerized data recording system was developed for both probe types, with detailed data analysis performed on the university main frame machine. Experimental data were plotted on various planes within the cascade to build up a three dimensional image of the flow from a series of two dimensional plots.

Detailed experimental traversing throughout the cascade was performed with the pressure sensing probes and the naturally occurring inlet endwall boundary layer. Hot wire probe traversing was also performed upstream and downstream of the cascade with this inlet condition. These two sets of data complemented one another, high loss areas in the mean flow data were shown to coincide quite closely with high turbulence areas. Pressure probe traversing at cascade inlet and two downstream traverse planes was also performed for thickened and thinned inlet endwall boundary layers. This variation in boundary layer thickness showed only as a quantitative, rather than a significant qualitative difference in the downstream flow field.

Loss predictions were performed for this Durham cascade configuration with each inlet endwall boundary layer simulated. Agreement between the predicted and experimental secondary loss profiles appeared to be reasonable. Predictions were also performed for other cascades and a research turbine nozzle and rotor row. Cascade predictions were qualitatively reasonable; those for the turbine however were not as good. This discrepancy could be due to shortcomings in either the loss prediction method or the limited experimental traversing undertaken.

CHAPTER 2

REVIEW OF PREVIOUS WORK

Secondary flows are a phenomenon that have attracted much interest over many years. As machines of increasing complexity have been constructed greater understanding of these effects have been necessary. Early turbomachine secondary flow work was primarily performed using qualitative data obtained from flow visualization work. Theoretical and empirical methods have been developed since to predict some of these effects.

The secondary flows produced when flow is turned in blade passages may cause Bernoulli surface distortion. In compressors this distortion is often quite small, and endwall flow calculation methods have been developed using boundary layer type techniques. Bernoulli surface distortion in turbines however, with their high flow turning angles, is frequently quite severe. Analysis techniques based on a continuous endwall boundary layer approach are not applicable since the upstream boundary layer is removed and mainstream fluid is swept onto the endwall, by action of both the leading edge and passage vortices. Empirical methods are frequently used to predict these flow patterns, although analytical techniques are becoming available with advances in understanding of the flow phenomena and computing technology.

This literature review concentrates primarily on secondary flow work applicable to turbines.

2.1 THE THEORETICAL APPROACH

Most theoretical approaches have concentrated on the prediction of turbomachine blade row exit angles. Secondary loss prediction is an area where few theoretical advances have been made. Correlations are often used in both turbine and compressor design to predict cautiously the secondary losses.

2.1.1 Secondary Flow Prediction Methods

An early secondary flow analysis is that of Squire and Winter (1951). They derived an expression for streamwise vorticity in the fluid stream leaving a cascade:

$$\xi = 2\,(\gamma_1 - \gamma_2)\,\frac{\partial v_1}{\partial s}$$

The solution for secondary velocity components for a high aspect ratio bend was derived, from this relationship. This compared quite reasonably with experimental data for turning vanes in a wind tunnel corner.

Using a vector analysis Hawthorne (1951) derived an expression for the streamwise vorticity component variation along a streamline

$$\left[\frac{\xi}{V_{st}}\right]_2 - \left[\frac{\xi}{V_{st}}\right]_1 = -2\int_1^2 \frac{\left|\mathrm{grad}\ P_o\right|}{\rho}\,\sin\Omega\ \frac{d(\gamma_2 - \gamma_1)}{(V_{st})^2}$$

He showed that when inlet and exit velocities were equal, with small Bernoulli surface distortion, Squire and Winter's downstream vorticity results could be obtained for a turned endwall type boundary layer.

A simple approach presented by Preston (1954) produced the results of Squire and Winter for a narrow circular arc passage. This analysis used Kelvin's circulation theorem and considered the distortion of vortex filaments passing through the passage. In an impulse cascade of circular arc blades Preston identified three secondary vorticity components, two in the blade wakes and one in the issuing jet. The two wake components are generally referred to as trailing filament and trailing shed vorticity, and that in the mainstream as distributed secondary vorticity (after Hawthorne (1955)). Trailing shed vorticity arises due to variations of bound vorticity around the blade profiles. Stretching of normal vorticity lines, initially in the upstream boundary layer, produces the trailing filament vorticity component. Distributed secondary vorticity between the blade wakes is produced by turning the normal vorticity lines of the inlet endwall boundary layer within the blading. Using a similar analysis Preston also derived Hawthorne's (1951) more general result for vorticity variation along the streamline.

Results from an impulse turbine cascade were presented by Hawthorne and Armstrong (1955). These showed that the trailing shed vorticity, derived from the measured change in blade circulation, added to the calculated trailing filament vorticity, gave reasonable agreement with the measured strength of the vortex sheet shed in the blade wake.

An analysis of axial flow turbomachines, assuming axial symmetry was presented by Smith (1955). This suggested that in a first order analysis the secondary flows could be obtained by knowing the number of blades in a row, and the axially symmetric flow solution. He concluded that secondary flows were proportional to blade circulation, and inversely proportional to the number of blades in a blade row. In jet engine practice, with typical blade numbers per row, he suggested that secondary flows were small enough to be neglected.

An attempt to evaluate secondary flow boundary layer data was made by Johnston (1960). He typified this boundary layer as one where skewing of the near wall velocity vectors occured towards the centre of main flow curvature. A triangular polar plot correlated the available data, showing variations of cross flow and streamwise flow velocity variations.

A general form of the vorticity equation for compressible viscous flows was presented by Lakshminarayana and Horlock (1973) for a bulk viscosity of zero. The streamwise vorticity component was derived by taking the dot product of their general equation with the unit velocity vector along the streamline. A similar analysis gave the normal vorticity when the dot product was taken with the unit velocity vector along the streamline normal. It was shown for a inviscid uniform density flow that the streamwise secondary vorticity equation of Hawthorne (1951) could be obtained. With similar flow restrictions the duct streamwise secondary vorticity equation of Squire and Winter (1951) was also derived. The general form of the vorticity equation in a rotating frame was also presented. Components of the absolute vorticity in the relative streamwise and normal directions were derived for the rotating case. For the more restricted case of inviscid uniform density flow a Squire and Winter (1951) type equation was derived for the rotating co-ordinates.

$$(\xi_{st})_2 - (\xi_{st})_1 = 2\,\xi_n\,(\gamma_{1rel} - \gamma_{2rel})$$

An anomaly in the trailing shed vorticity formulation of Hawthorne (1955) was found by Came and Marsh (1974). The relationships proposed by Hawthorne predicted that flow at exit from two many bladed cascades, giving no net fluid deflection, had a streamwise vorticity component not present in the original inlet flow. Came and Marsh proposed a streamwise vorticity relation giving no streamwise component for the same two cascades. Their analysis technique, similar to that given by Preston (1954), applied Kelvins circulation theorem and Helmholtz law to flow through a many bladed cascade. In a real cascade, with non zero blade pitch they proposed:

$$\xi_{sec} = \xi_{st} \frac{\cos \gamma_1}{\cos \gamma_2} + \xi_n \frac{1}{\cos \gamma_1 \cos \gamma_2} \left[\frac{1}{2} (\sin 2\gamma_2 - \sin 2\gamma_1) - \frac{V_{A1}}{p} \oint \frac{d\ell}{V_b} \right]$$

$$\xi_{fil} = -\xi_n \frac{V_{A1}}{\cos \gamma_1} \oint \frac{d\ell}{V_b}$$

$$\xi_{shed} = - \xi_n \frac{p}{2 \cos \gamma_1} \left[\sin 2\gamma_2 - \sin 2\gamma_1 \right] - \xi_{st}\, p \cos \gamma_1 - p\, V_{LOC2} \cos \gamma_2 \frac{d\gamma_2}{ds}$$

Marsh (1974) derived a revised distributed secondary vorticity relation for cascade flow with an axial velocity ratio, the other two components were unchanged.

$$\xi_{sec} = \xi_{st} \frac{\bar{V}_{A2}}{\bar{V}_{A1}} \frac{\cos \gamma_1}{\cos \gamma_2} + \xi_n \frac{1}{\cos \gamma_1 \cos \gamma_2}$$

$$\left[\frac{1}{2} \left[\sin 2\gamma_2 - \frac{\bar{V}_{A2}}{\bar{V}_{A1}} \sin 2\gamma_1 \right] + (\gamma_2 - \gamma_1) \right.$$

$$\left. \left[\frac{\left[\frac{\bar{V}_{A2}}{\bar{V}_{A1}}\right] \tan \gamma_2 - \tan \gamma_1}{\tan \gamma_2 - \tan \gamma_1} \right] \right]$$

With no axial velocity ratio this expression reduces to that given by Came and Marsh (1974). Marsh showed, using Gregory-Smith's (1970) guide vane data, that axial velocity ratio effects increased the hub vorticity by 18% ($(\bar{V}_{A2}/\bar{V}_{A1}) = 1.32$) and decreased it at the tip by 10% ($(\bar{V}_{A2}/\bar{V}_{A1}) = 0.845$) when compared with the Came and Marsh (1974) distributed secondary vorticity relationship. Marsh suggested that the design of these guide vanes magnified the axial velocity effects and that for most blade rows the effect on secondary vorticity would be less. In another analysis Marsh (1975) showed that ξ_{sec} was also influenced by compressibility. For accelerating flows in a turbine cascade (with $\gamma_2 = 60^0$) however Marsh showed that compressibility had little influence for quite a range of inlet angles ($-40^0 < \gamma_1 < 40^0$). For an impulse cascade ($-\gamma_2 = \gamma_1$) the predicted secondary vorticities increased substantially.

In axial flow turbomachines the blade rows form annuli around the shaft axis. An annular cascade secondary flow formulation was proposed by Hawthorne and Novak (1969) assuming no axial variation in secondary velocity components. Glynn and Marsh (1980) suggested an alternative formulation with no secondary velocity variation in the mean exit flow direction. This later formulation for a cascade of both large radius and hub tip ratio, i.e. a rectangular cascade, gave results consistent with the conventional secondary flow analysis. The Hawthorne and Novak relation was shown not to be consistent. It was suggested by Glynn and Marsh that departure of the flow from free-vortex design, in an annular cascade, influenced the secondary flow effects significantly.

A method of calculating cascade secondary flow was proposed by Glynn (1982) including the effects of Bernoulli surface distortion. This work clearly showed the roll up of Bernoulli surfaces at the suction surface endwall corner within the cascade. Outlet angle predictions however were not significantly improved when compared with those neglecting Bernoulli surface roll up. Glynn suggested that this may not be so for cascades of larger turning; the cascade used in his comparisons had a turning angle of -62.4^{0}. In contrast to these results Horlock and Lakshminarayana (1973) showed a significant improvement in outlet angle prediction for a compressor cascade when Bernoulli surface rotation and displacement effects were included in the analysis. They found that Bernoulli surface rotation reduced the strength of the secondary vorticity.

2.1.2 Secondary Loss Prediction Methods

Most loss predictions are based on correlations of experimental data obtained from both cascades and turbines.

An early loss correlation was proposed by Ainley and Mathieson (1951) developing the earlier work of Carter (1948). This expressed the secondary loss as:

$$Y = G \frac{\cos^2 \gamma_2}{\cos^3 \bar{\gamma}} \left[\frac{C_L}{p/c} \right]^2$$

With the function of machine geometry given graphically as:

$$G = f\left[\left[\frac{a_2}{a_1}\right]^2 \frac{d_t}{d_n + d_t}\right]$$

An analysis by Ehrich and Detra (1954) traced Bernoulli surfaces in a cascade, assuming a linear build up of secondary velocities. They compared their analysis results with experimental data, and noted the similar patterns at blade row exit. They proposed an energy loss coefficient based on the kinetic energy of secondary flow non-dimensionalized on inlet flow kinetic energy.

$$E_{sec} = \frac{0.1178\ (\gamma_1 - \gamma_2)^2}{\kappa\left[1 - \frac{0.2}{\kappa}\right]^3}$$

For typical aspect ratios and turning angles however they concluded that their secondary loss coefficient was small.

Lakshminarayana and Horlock (1963) reviewed secondary flows and loss predictions. According to Hawthorne (1955ii) a secondary loss coefficient could be obtained from:

$$Y = (\gamma_1 - \gamma_2)^2 \left[\frac{p}{C}\right]^2 \frac{2C}{H} \cos^2 \gamma_2\ G\left[\frac{\delta_1}{p \cos \gamma_2}\right]$$

with

$$G\left[\frac{\delta_1}{p \cos \gamma_2}\right]$$

given as a function graphically in the paper. Lakshminarayana and Horlock suggested that losses arising from secondary velocities were low, but losses due to separation, at the suction surface endwall corner particularly, could be high. They considered, in addition to cascade effects, secondary flows due to blade overtip leakage which are aggravated in turbines where the blade suction surface leads the pressure surface. An empirical secondary loss coefficient for turbines was given in three components as:

$$Y = \frac{0.04\, C_L^{\,2}}{\kappa}$$ due to cascade secondary flow after Vavra (1960)

$$+\ 0.0423 \left[1 - \frac{C_{Lt}}{C_{Lh}} \right] \frac{C_{Lh}^{\,2}}{\kappa}$$ mainstream secondary flow after Von Karman (1941)

$$+\ 0.5\, C_L^{\,2}\, \frac{\Psi}{p}\, \frac{1}{\kappa}$$ clearance flows after Ainley (1951)

Belik (1968) derived a Poisson equation from the streamwise vorticity variation along a streamline equation of Hawthorne (1951). This he solved in series form for the secondary flow stream function, with the upstream boundary layer velocity profile represented by a Fourier series. Approximations to a Coles boundary layer profile were obtained using a trigonometric function and the distribution of secondary kinetic energy evaluated. Close to the endwalls however the vorticity was limited, by reducing the number of Fourier terms included in computation, to compensate partially for the high velocities in this region given by the inviscid analysis. A series solution for the secondary kinetic energy, normalized by the inlet kinetic energy, was suggested. Belik proposed the simple relation that all secondary kinetic energy was lost to the mainstream flow, and showed as secondary loss in downstream planes. Analysis of a reaction turbine blade cascade (76^0 turning angle) showed very low secondary loss values, and in summary Belik suggested this to be general. He also stated that secondary kinetic energy would be quite high in impulse turbine and compressor blades. No modelling of the suction surface endwall corner separation region was undertaken, although he suggested this resulted in significant losses.

Bernoulli surface tracing was reported by Belik (in discussion of Horlock (1969)) as a development of his earlier work. The successive roll up of the inlet boundary layer fluid through the blade passage was clear. He also computed the path lines of low energy fluid on the endwall and compared this favourably with some endwall flow visualization photographs.

A major review of secondary loss correlation methods for turbines was made by Dunham (1970). He reviewed the available correlation methods, compared them with experimental data, and produced a new secondary loss correlation:

$$Y = \frac{C}{H}\;\frac{\cos\gamma_2}{\cos(\gamma_1+i)}\left[\frac{C_L}{p/c}\right]^2\frac{\cos^2\gamma_2}{\cos^3\bar{\gamma}}\left[0.0055+0.078\sqrt{\frac{\delta_1}{c}}\;\right]$$

Dunham also highlighted the additional complications in real turbines of blade end clearances, moving endwalls and radial pressure gradients.

An analysis of 25 turbine results gave Dunham and Came (1970) an improved secondary loss correlation to that of Ainley and Mathieson (1951). They proposed:

$$Y = 0.0334\;\frac{c}{H}\;\frac{\cos\gamma_2}{\cos(\gamma_1+i)}\left[\frac{C_L}{p/c}\right]^2\frac{\cos^2\gamma_2}{\cos^3\bar{\gamma}}$$

Dunham and Came suggested the need for loss correlation methods would continue despite the advances in computer hardware and software.

Sophisticated computer models require comprehensive data input, and at the preliminary design stage this is neither available nor practical.

A turbine endwall boundary layer calculation method was proposed by Dring (1971). This used the triangular cross flow model proposed by Johnston (1960) in a momentum integral equation approach. In incompressible flow an endwall boundary layer total pressure defect term was proposed. This assumed that the cross flow velocity head component of the total pressure was lost. Dring claimed, although admitting scatter, to have predicted losses within 30% of the measured values.

Belik (1972) proposed that the total losses, in three dimensional flow, past blades in cascade could be considered as the sum of four discrete components. These he defined as:

i) two dimensional profile losses

ii) endwall shearing stress losses

iii) secondary losses due to dissipation of all secondary kinetic energy

iv) separation losses at the suction surface endwall corner.

He emphasised that this summation approach was simplified since no inter-relation of terms was assumed. He evaluated the secondary losses from his earlier energy loss relationship (Belik (1968)), and a separation loss relationship (Belik (1970)). This separation loss was greater than that calculated due to the secondary kinetic energy, and caused by transport of low energy fluid towards the suction surface endwall corner. Results of these secondary loss calculations compared favourably with experimental data from a reaction blade ($\gamma_1 - \gamma_2 = 78^0$), and an impulse blade ($\gamma_1 - \gamma_2 = 132^0$).

In a review paper Horlock and Lashminarayana (1973) presented much data on the theoretical prediction of secondary flows, considering the small shear large disturbance approach most deeply. This is the approach most frequently applied to turbomachinery blade rows. They also reviewed the secondary loss prediction methods available, and presented Dunham's (1970) correlation as a tentative method open to discussion. Experimental investigations of some other workers were also discussed. These suggested that Reynolds number and 'moderate' Mach number variations had negligible influence on the secondary flow losses.

Came (1973) performed a series of total pressure traverses at exit from a turbine blade cascade with varying inlet boundary layer thickness and flow incidence angle. His experiments indicated that the secondary losses, after deducting the inlet boundary layer loss, at cascade exit increased with inlet boundary layer thickness. He applied Dunhams (1970) correlation to his results and suggested this had been unduly influenced by one experimenter's point. A new secondary loss correlation was proposed:

$$Y = \left[\frac{C_L}{p/c}\right]^2 \frac{\cos^3 \gamma_2}{\cos^3 \bar{\gamma}} \frac{1}{\cos(\gamma_1 + i)} \left[0.25\ \bar{\bar{\zeta}}_1 + 0.009\ \frac{c}{H}\right]$$

Came also suggested that cascade loss correlations should be applied cautiously to turbine secondary loss prediction. An underestimate of losses was probable, owing to the influence of skewed endwall boundary layers, and the inflow of the loss profile from previous row.

In an experimental programme to investigate effects of cascade endwall profiling Morris and Hoare (1975) identified some errors in the Came (1973) correlation. They proposed the following revised secondary loss correlation:

$$Y = \left[\frac{C_L}{p/c}\right]^2 \frac{\cos^2 \gamma_2}{\cos^3 \bar{\gamma}} \frac{c}{H} \frac{\cos \gamma_2}{\cos(\gamma_1 + i)} \left[0.294\left[\frac{\delta_1^*}{c}\right] + 0.011\right]$$

Booth (1975) presented a three dimensional compressible endwall boundary layer analysis along similar lines to Dring (1971). His aerodynamic loss model included contributions from the annulus wall boundary layer and the passage vortex. The boundary layer losses were evaluated by integrating a calculated energy thickness in the exit plane. Passage vortex losses were estimated by assuming that the transverse kinetic energy component was lost, and the streamwise component preserved. Booth experimented on a turbine stator and showed the mass flow in the passage vortex to be nearly proportional to inlet boundary layer thickness. An investigation on the effect of aspect ratio showed that the secondary losses were inversely proportional to blade height, and little influenced by chord. Booth suggested that correlations of secondary energy losses of the following form:

$$Y = \frac{\delta_1}{H} \times G \text{ (Blade Geometry)}$$

could be found but presented no specific data.

Good agreement between a viscous flow calculation and annular cascade results were presented by Waterman and Tall (1976). These were obtained after considerable computational effort. Three cascades were tested, two of which had endwall profiling. The empirical factor in the Prandtl mixing length model was adjusted to give results comparable with experimental data for the cascade with non-profiled endwalls. This factor then remained constant for the other endwall configurations.

A component type secondary loss prediction technique was presented by Gregory-Smith (1982). Three secondary loss components were identified due to the loss core, the new endwall boundary layer and an extra secondary loss. The inlet boundary layer was all assumed to be swept onto the suction surface at blade exit forming a triangular loss core. This distribution gave a discontinuity in the resulting loss profile but was used for simplicity in analytical treatment. The new endwall boundary layer loss was estimated from a two dimensional non-skewed method given by Duncan et al (1960). Secondary kinetic energy of the passage vortex was evaluated by solving for the secondary flow stream function of Glynn and Marsh (1980). The extra secondary loss was assumed equal to the secondary kinetic energy. This model, although rather idealistic in assuming no interaction of loss components, has produced encouraging results on cascades and in simple machine configurations.

2.2 EXPERIMENTAL INVESTIGATIONS

Many experimental investigations have been undertaken to improve understanding of secondary flow problems in turbomachines. This review is concerned primarily with the problems occurring in turbines, although much effort has also been expended on the secondary flows within compressor blades. Most of the available data concerns the mean flow fields of cascades and machines. Fluctuating flow data available in the open literature is quite sparse.

2.2.1 Mean Flow Field Investigations

Armstrong (1955) conducted a series of experiments on an impulse turbine cascade, essentially to verify the theoretical secondary flow methods of both Squire and Winter (1951) and Hawthorne (1955). He showed that for high upstream vorticity the small perturbation theory did not predict the secondary velocities accurately. With small inlet vorticities arising in a thickened boundary layer however the theory appeared to give good agreement with experiment. Armstrong suggested that improved correlations with his high inlet vorticity measurements could be obtained by Bernoulli surface tracing. Total pressure contour plots were presented from two planes downstream of the trailing edge for the natural endwall boundary layer. These plots showed two high loss regions close to the suction surface endwall corner. Armstrong's data showed these to be contra-rotating regions of the flow field, which moved further from the endwall with increased distance from the trailing edge plane.

A review of secondary flow visulization work at N.A.C.A. was presented by Herzig and Hansen (1955). Smoke and wall flow visualization work was presented from both rectangular and annular cascades. Overturning close to the endwall was clearly evident in many of the photographs. A qualitative appreciation of the passage vortex at low speed was obtained by moving the smoke injection source and noting its effects. Similar conclusions were made from the wall flow visualizations obtained at higher Mach number. In the annular cascades considered, radial pressure gradients caused migration of high loss flow along the blade surfaces towards the blade root. Flow fences were fitted at mid height, and the radial loss migration towards the root was reduced, although losses associated with tip region phenomena appeared at the fence location. Blade tip clearance effects were investigated in rectangular cascade using a moving endwall. Turbine secondary losses were aggravated by this relative movement due to reduced static pressure in the blade pressure surface tip region, and an increasing tendancy to flow separation close to the trailing edge.

A rectangular cascade of turbine blades was investigated by Turner (1957). He presented contours of total pressure at the cascade exit plane with an upstream boundary layer present and with it removed. These two contour plots exhibited similar qualitative Bernoulli surface distortion but with a much smaller loss core evident for the removed boundary layer case. Turner concluded that the cascade endwall boundary layer flow was not influenced by the upstream boundary layer. Removal of the upstream boundary layer probably increased the inlet vorticity significantly, since in reality the subsequent new inlet endwall boundary layer would be very thin. At cascade exit consequently this was evident from the more intense Bernoulli surface roll up with the effectively very thin inlet boundary layer.

Endwall motion relative to turbine blades and tip clearance effects were considered by Klein (1966) in both an annular and a rectangular cascade. Results of downstream total pressure loss traverses from the annular cascade showed the peak loss value migrating further from the endwall with increasing inlet boundary layer skew. Local to the endwall the loss values also increased with inlet skew, and Klein suggested that this was due to adverse flow incidence. For zero tip clearance he found that the secondary losses increased with inlet boundary layer skewing. At large blade tip clearances however inlet boundary layer skew had little influence on the blade row exit loss patterns. Klein presumed that the clearance vortex was of sufficient strength to dominate the effects of both inlet skew and relative motion between blade tip and wall. At typical tip clearance values occurring in a turbine Klein suggested some reduction in secondary losses could be achieved due to the relative motion. In his rectangular cascade experiments Klein found no influence on the local total pressure loss coefficient at cascade exit from variations of the inlet boundary layer displacement thickness. Blade tip clearance variation effects could not be determined from his experimental data obtained using the rectangular cascade.

The endwall boundary layer in an annular turbine cascade was investigated by Sjolander (1975). Extensive surface flow visualization studies were performed using both oil dot and oil film techniques. Blade pressure surface results showed the flow to be essentially two dimensional, however complex flow patterns were evident on both the blade suction and endwall surfaces. On the suction surface, towards the trailing edge, increasing flow away from the endwalls was evident. These were stronger at the blade tip owing to the radial pressure gradient. Endwall flow patterns on the hub showed strong cross flows towards the suction surface, which blended well with the blade surface observations. Sjolander identified two endwall separation lines occurring around each leading edge, sweeping across the passage onto the adjacent blade suction surface. These separation lines also extended upstream, implying reverse flow, curling around the leading edge and onto the blade suction surface. A reattachment point was suggested on a line through the leading edges and between the separation lines, and was identified from the endwall flow visualization by diverging streaklines. Traversing with a three-hole probe was also performed both within the blade row and at passage exit. Exit plane data exhibited blade wakes, two closely adjacent loss cores near the suction surface endwall corner of each blade and a thin hub boundary layer. Radial traversing within the passage was limited to thirteen points on the endwall, forming three pseudo-stream lines at about 10%, 50%, and 90% of the inter-blade gap. Results from these traverses showed strong cross flows in similar areas to the flow visualization at the wall, decreasing with radial distance into the mainstream. The limited data also suggested concentration of high loss fluid on the suction surface under the influence of the strong cross flows.

Morris and Hoare (1975) investigated secondary losses in a rectangular cascade with endwall profiling. A reduction of 25% in overall secondary loss was obtained, with an aspect ratio of 0.5, for a symmetric cubic profile of one endwall within the blading. Losses were reduced close to the flat wall, and Morris and Hoare attributed this to blade pressure distribution changes due to endwall profiling. Non-axisymmetric inter-blade profiling increased losses near the profiled wall considerably, but reduced them again close to the plane wall. Blade wake distortion was also evident with this endwall configuration. They suggested that care was needed in endwall profile design to ensure that overall losses were reduced, and the influence of profiling on blade surface velocity distributions was taken into account.

Effects of inlet boundary layer skew to a rectangular cascade were investigated by Carrick (1975). He used a variable speed moving belt to generate this skew upstream of the blades. As with the work of Klein (1966), Carrick found that the secondary losses increased with inlet skew, and his results appeared much more sensitive to this than those of Klein. The passage traversing performed by Carrick showed that the secondary flow intensities were also increased with inlet boundary layer skew. He found that except close to the suction surface endwall corner static pressure within the endwall boundary layer was almost constant. A boundary layer analysis was proposed for the endwall flows, for thin inlet boundary layers and low skew values, since at higher skew the suction surface endwall corner loss area increased. An inviscid three-dimensional procedure gave good qualitative comparisons with the experimental results for high inlet skew and thicker boundary layers. Quantitative agreement between experimental results and computer predictions could probably be obtained, Carrick claimed, if satisfactory viscosity and turbulence models were available.

Detailed measurements and flow visualization within a large scale rectangular turbine cascade were made by Langston et al (1977). A separation saddle point, corresponding to the reattachment point of Sjolander (1975), was identified just downstream of the leading edge plane and about 30% of passage width from the pressure surface. At increased positive incidence this saddle point moved across the passage towards the suction surface. Attachment and separation lines were also identified passing through this saddle point. The separation lines extended towards the across passage suction surface, and around the leading edge onto the adjacent passage suction surface. One attachment line passed onto the pressure surface and the other into the upstream flow field. As with Sjolander's cascade the pressure surface flow field was essentially two dimensional. The suction surface exhibited a separation line running from the endwall close to the leading edge towards midspan at the trailing edge. Langston et al suggested that saddle points had been visible in the flow visualization data of earlier workers. Development of the area mass averaged total pressure losses through the cascade showed a slow increase, from the inlet boundary layer value, up to about 60% chord. Langston et al suggested that this was due primarily to shear effects of the three dimensional endwall separation and its associated attachment lines. Downstream of the throat (60% chord) the losses increased more rapidly.

This was attributed to suction surface diffusion allowing the passage vortex size to increase, and interactions of the pressure surface attachment line, from the adjacent blade, with the suction surface boundary layer. Downstream of the trailing edge a sharp increase in loss was measured. This rapid increase corresponded to the separation loss at the trailing edge and mixing of the two fluid streams. Endwall static pressure data showed the maximum pressure to occur coincident with the saddle point. Successive axial traverse plane data showed the inlet boundary layer flow swept onto the blade suction surface and a new thin boundary layer forming on the endwall. Secondary velocity vectors suggested a vortex like motion in the region of the loss core, the centre of which did not correspond to the minimum measured traverse plane static pressure. Langston et al explained that this discrepancy was due to the method of obtaining the secondary velocity vectors. Regions of three dimensional flow were evident from the vector plot, these tied in well with the flow visualization results discussed earlier.

Comprehensive experimental testing of a turbine nozzle and rotor blade in rectangular cascade form was reported by Marchal and Sieverding (1977). Their experimental programme included both surface and flow field flow visualizations, and inter-blade pitot probe traverses. Blade and endwall oil film flow visualizations showed similar phenomena to those of Sjolander (1975) and Langston et al (1977). Results from the pressure surface of the rotor blade however showed strong flows towards the endwall near the leading edge. The developing passage vortex motion was traced using light sheet illumination of injected smoke at low velocity. The vortex around the leading edge developing into two legs (termed suction side and pressure side) was clearly defined by this method. Similarly in a plane just upstream of the trailing edge the passage vortex was shown, with the counter rotating suction side leg of the leading vortex still in evidence towards its midspan side. Development of the losses and secondary velocity vectors were presented on successive axial traverse planes of the nozzle cascade. The formation and spanwise migration of the suction surface endwall corner loss region, initially formed from the inlet boundary layer fluid, were clearly visible. Area mass averaged traverse plane loss data showed considerable loss increases downstream of the throat, which did not appear influenced by the upstream inlet boundary layer thickness. Marchal and Sieverding suggested that this rapid loss increase was due chiefly to passage vortex and blade boundary layer interaction. These results showed similar trends to those of Langston et al (1977).

Coolant flows are frequently employed in gas turbine practice. Goldman and McLallin (1977), in an experimental programme, primarily to obtain heat transfer data, found that secondary losses could be reduced by injecting onto the endwall coolant of higher total pressure than at turbine guide vane inlet. This flow seemed to inhibit formation of the passage vortex and the radial inflow at the trailing edge of their annular cascade.

A comparison of duct and cascade flows was made by Barber and Langston (1979). They concluded that significant aerodynamic differences occured between ducts and cascades of the same turning angle, primarily due to suppression of the leading edge vortex. Additional data from the Langston et al (1977) cascade was also presented, with a fully developed inlet boundary layer. This showed complex flow patterns on both blade pressure and suction surfaces, and close to the pressure surface leading edge endwall corner some reverse flow. Blade surface static pressure data on the pressure surface with this revised inlet bounary layer was no longer invariant with span, confirming the surface flow visualizations.

Additional work on the annular Sjolander (1975) cascade was performed by Bindon (1979) who investigated the effects of variable hub inlet boundary layer skew. His endwall flow visualizations showed significant differences for zero inlet skew with the upstream slot, needed to allow hub rotation, open and covered. Increasing the inlet boundary layer skew however increased the cascade endwall cross flow angles, and also their spanwise extent. The limited loss data presented by Bindon suggested that losses at the suction surface endwall corner extended much further out from the endwall with increased inlet skew.

Hunter (1979) performed detailed traverses of the flow field in a large scale research turbine. Exit traverses of the nozzle guide vanes showed the blade wakes to be inclined, due to the spanwise variation in swirl angle. The area of loss at the hub was found to be 60% greater than that at the blade tip. This difference was attributed to the radial inflow of low kinetic energy fluid under the influence of the spanwise pressure gradient from the outer parts of the annulus. Blade surface flow visualizations confirmed this, showing tip to hub migration. This effect could be reduced by tripping the previously laminar blade surface boundary layers. Thickening the inlet boundary layer had little influence on the blade wakes, but enlarged the loss areas at both hub and tip, spreading them over the endwalls. The cores of high loss fluid were essentially the same size at hub and tip for this thickened boundary layer. Extreme magnitudes of over and under turning were also reduced, owing to the reduction in inlet vorticity associated with the more rounded velocity profiles. A stationary pressure probe survey at rotor exit, effectively circumferentially meaning rotor induced variations, showed the upstream nozzle guide vane circumferential flow variations clearly. A hot wire probe phase-lock sampled at rotor exit provided rotor relative data. At the rotor root intense secondary velocities were recorded, due to the high blade turning angle and relative skewing of the inlet boundary layer. This effect was also evident in the relative dynamic head contours, which showed negligible build up of boundary layer fluid on the hub but rather its spanwise migration away from the endwall. Similar rotor exit data was obtained for both thickened and natural stage inlet boundary layers. Hunter suggested that the unsteady flow field entering the rotor coupled with its boundary layer skew was the primary reason for these flow changes compared to the stator. In the stator, flow was essentially that to be found in rectangular cascades with additional effects from the spanwise pressure gradient.

Additional data for the Langston et al (1977) cascade was obtained by Graziani et al (1980) for a thinner inlet boundary layer, in what was primarily a heat transfer study. Their endwall flow visualizations showed the saddle point had moved slightly further downstream of the leading edge plane and towards the pressure surface, for this thinner inlet boundary layer. Also downstream of the suction side leg of the leading edge vortex significantly reduced endwall cross flow angles were evident. Endwall static pressure distribution data for the thinned inlet boundary layer compared much more favourably with a potential flow solution than did that from the original Langston et al paper. Blade surface static pressure contours for the thin inlet boundary layer case were much less distorted than for the thick case. In summary the three dimensional secondary flow effects appeared to be much less marked for the thinned inlet boundary layer case based on surface data. No flow field data however was presented and consequently a quantiative difference within the air flow was impossible to determine.

An additional transverse plane of the Langston et al (1977) cascade experiment was presented by Langston (1980). In this plane, at approximately 70% axial chord from the leading edge, Langston suggested that the centre of passage vortex rotation, determined from a vector plot, coincided with both the minimum static pressure and maximum total pressure loss. In their earlier paper however Langston et al (1977) showed that these points were not coincidental in their experimental data at 82% axial chord. From these earlier results they suggested the method of obtaining the vector plot, which determined their vortex centre, was the most likely cause of the mismatch.

In a flow visualization study Gaugler and Russell (1980), showed the development of the leading edge and passage vortices in a turbine nozzle guide vane cascade. Using neutral density helium bubbles they presented photographs showing fluid originally in the inlet bounary layer rolling up into the passage vortex core. Superimposing individual bubble traces they showed that the actual path of the passage vortex centre followed more closely the free-stream direction than their endwall oil dot streaklines would suggest. Bubbles closes to the vortex core appeared to be subjected to intense rotations, whilst those towards the outside adopted a more relaxed path.

Denton and Usui (1981) injected small quantities of ethylene gas into a low speed turbine to study flow mixing. An initial test run was made in the annulus upstream of the blade rows, which showed very little diffusion of the injected gas implying low turbulence. Gas injected close to the nozzle guide vane pressure surfaces, at blade row inlet close to the endwall, emerged with peak concentrations close to the suction surface off the endwall. The hub region showed considerably more tangential diffusion than that at the tip; however greater radial diffusion had occurred at the blade tips. Tracer gas concentrations were found over the endwalls with peaks in the blade wakes, when gas injection took place at about half inlet boundary layer thickness from the endwall at nozzle row inlet and in similar tangential positions to the previous tests. Rotor results were obviously influenced by rotation of the machine, and considerable tangential smearing of the concentration profiles occurred. This was due to the differing transit times of tracer gas passing around the blade pressure and suction surfaces. At rotor hub an increased radial spread of concentrations compared to the nozzle were evident, and this was attributed to the stronger passage vortex existing at the rotor root. The tangential spread of concentration contours at the rotor tip was presumed due chiefly to overtip leakage flows. Mixing occurring within the whole turbine stage showed little radial diffusion of tracer gas concentrations when injected at midspan. Closer to the endwalls however, increased mixing occurred. At the hub, peak concentrations were on the endwall suggesting that some fluid, initially on the endwall upstream, entered the rotor off the endwall and left it on the endwall. At the casing skewing of the concentration profiles was evident, consistent with distortion due to rotor blade row secondary flows.

The influences of exit flow Mach number and endwall cooling on a rectangular nozzle cascade were experimentally investigated by Sieverding and Wilputte (1981). The secondary loss development through the cascade showed little change with increasing Mach number, however downstream of the trailing edges these losses increased with Mach number. Pitchwise mass averaged loss data downstream of the cascade showed that a significant proportion of the additional secondary losses arose from the new developing endwall boundary layer. Injecting coolant flows through endwall holes reduced the pitchwise mass averaged secondary losses, and significantly changed the secondary flow angle distributions. Some apparent secondary loss reduction could be attributed to the higher energy coolant flow. Coolant flows also reduced the secondary velocities. Consequently interference between blade suction surface boundary layer and the transported inlet boundary layer was also reduced. The data presented in this paper showed that endwall coolant injection could have a significant influence on development of secondary losses, and should be considered when evaluating secondary flow phenomena.

A reconstructed Langston et al (1977) cascade has been tested by Moore (1983) who studied flow trajectories and mixing using ethylene gas injection and sampling probes. At a plane slightly downstream of the blade leading edges, ethylene was injected into the suction and pressure side legs of the leading edge vortex. The pressure side leg passed from the adjacent blade leading edge across the endwall towards the trailing edge. This showed little diffusion in a plane 40% chord downstream of the trailing edges, with the maximum concentrations occuring at the loss core peak. The fluid in the suction side leg of the vortex also appeared at the peak of the loss core in this plane, having undergone more diffusion than that of the pressure side leg. Sampling upstream of the trailing edges suggested that fluid in the pressure side leg of the leading edge vortex emerged in the centre of the passage vortex. This fluid consequently did not become as diffused as that in the suction side leg, which was convected around the vortex core by its rotational movement. The low loss area in the flow field downstream of the cascade was found to originate from fluid outside the inlet boundary layer, but within the half passage closest to the suction surface. This was traced by successive ethylene injection and sampling. Entropy flux contours were also plotted, derived by assuming a perfect gas with small relative changes in total pressure and total temperature. Highest entropy values were found within the main loss core, but in the developing endwall boundary layer values of about half the maximum were obtained.

Effects of hub inlet boundary layer skew on an annular cascade of high turning turbine nozzle guide vanes were investigated by Boletis et al (1983). Inlet skewing was achieved by rotating the hub upstream of the blading in a similar manner to Bindon (1979), however much more detailed flowfield measurements were undertaken throughout the cascade. Inlet skew was found to emphasize characteristics of the passage vortex. The cascade radial pressure gradients were counteracted since radial outflow of high loss fluid, originating in the suction surface endwall corner, occurred downstream of the blade row. Concentrations of low momentum fluid, initially largely in the upstream boundary layer, into the suction surface endwall corner were increased with the inlet skew to the cascade. Development of pitchwise averaged streamwise flow angle in the outer portions of the annulus was, as expected, found to be uninfluenced by inlet skew variation. Significant differences in flow angle were noted downstream of the blade row nearer the hub for increased skew. These were attributed to a corner vortex counteracting the effects of the passage vortex. The influence of the corner vortex also increased with inlet skew.

As an example of a new flow visualization technique Langston and Boyle (1983) show the endwall flow patterns of a turbine nozzle cascade. By using different coloured inks and a slowly evaporating solvent (oil of wintergreen) the area downstream of the endwall separation line was clearly defined. Upstream of the separation flow was swept on to the blade suction surfaces, and subsequently reappeared on the endwall close to the blade.

2.2.2 Fluctuating Flowfield Data

The endwall boundary layer in a turbine nozzle cascade was investigated by Senoo (1958). Instantaneous hot wire anemometer traces were presented comparing mainstream and endwall boundary layer fluctuations. Upstream of the nozzle throat the endwall boundary layer remained laminar regardless of the state of the incoming boundary layer. Downstream of the throat however the endwall boundary layer was turbulent. Senoo viewed the apparent reverse transition of the turbulent inlet boundary layer to the laminar endwall boundary layer upstream of the throat as unusual. Several investigators (in the preceeding section 2.2.1) suggest that the old inlet boundary layer is rolled up off the endwall. A new endwall boundary layer therefore grows from the endwall separation line of the presure side leg of the leading edge vortex. Senoo probably measured this new developing boundary layer and found it to be laminar.

A compressor cascade was studied by Kiock (1973) who obtained turbulence intensity data downstream of the blading in plane, annular and rotating cascades. He found the overall degree of turbulence to be the same downstream of a plane and rotating cascade. In his rotating blading the downstream turbulence level varied with blade boundary layer condition. for separated boundary layers the downstream turbulence levels increased. Kiock measured an increase in efficiency of later blade rows in his compressor rig when the high turbulence shed from upstream blade rows reduced flow separations on the later blades. Due to the rapid decay of turbulence Kiock found that velocity fluctuations of one rotor blade row did not influence the next.

Traversing of cascade wakes was performed by Raj and Lakshminarayana (1973), and as with Kiock (1973) these experiments were performed on compressor blade profiles. The blade wakes were found to be asymmetric, due primarily to the blade loading. Maximum turbulence intensity values were measured on the blade wake centre lines close to the blades. These peak intensity values decreased with increasing distance from the trailing edges, and also influences of the wake broadened. Raj and Lakshminarayana found that the streamwise turbulence intensity decreased more rapidly than the component normal to the wake. This finding was predicted by their theoretical considerations. The maximum streamwise normal Reynolds stress components were found to be of opposite sign close to either side of the wake centre line. These high Reynolds stress values decayed rapidly with increasing distance from the trailing edges.

Additional wake data from the rotating blade was obtained by Raj and Lakshminarayana (1976). They found that for rotating blades the rate of wake decay was greater than that for either isolated aerofoils or cascades. All wake turbulence parameters were higher in the rotor than for the cascade.

Two spanwise hot-wire probe traverses just upstream of the cascade trailing edge plane were reported by Langston et al (1977), within the new endwall boundary layer. Turbulence intensities increased as the endwall was approached and appeared to be slightly higher on the traverse nearer the suction surface. No turbulence data was presented from within the cascade bulk flow field.

A large scale turbine nozzle passage was investigated by Bailey (1980) using Laser-Doppler velocimetery. The curved converging nozzle guide vane passage was attached both upstream and downstream to the windtunnel ducting. In this configuration both the leading edge vortex and wake effects were supressed. Three spanwise traverses were performed on four planes through the passage, and consequently data was quite sparse in both the streamwise and cross-stream directions. Development of the passage vortex was traced on each of the four equipotential planes. It was shown to be centred at midpassage, about 10% span from the endwall, in the measurement plane closest to the trailing edge. This conclusion was shown to agree with the guide vane results of Marchal and Sieverding (1977), but not with the rotor blade results of Langston et al (1977). Bailey reported large regions of his passage vortex to have turbulence intensities similar to the free stream values. He suggested that this was an unexpected finding, since the passage vortex was composed chiefly of turbulent inlet boundary layer fluid. Turbulent stresses close to the suction surface and near the endwall however were higher than those measured near the pressure surface. From the results presented it seemed that the shear stress components, in the endwall plane, near the passage exit increased adjacent to the endwall. Comparatively therefore large regions of the passage vortex were of low turbulence, but adjacent to the suction surface endwall corner the turbulence intensities were increased.

Turbulent flows in a strongly curved square duct were investigated by Humphrey et al (1981) using a Laser-Doppler anemometer. These experimental investigations were more detailed than those of Bailey (1980), but without bulk fluid acceleration. Mean and turbulent flow quantities were presented. Despite the complex mean flow field and the Reynolds stress distributions the secondary cross stream flows were driven chiefly by centrifugal force, and the cross passage pressure gradient. At bend inlet the streamwise turbulence intensities were found to be maximum adjacent to the wetted surfaces (about 12%) and a minimum in the passage centre (about 6%). This regular pattern was distorted half way around the bend, and here intensity values had increased to 15% at about 10% radial gap in from the bend outer radius on the plane of symmetry. A closed 6% turbulence intensity contour was situated at about 15% radial gap from the inner radius wall. Adjacent to this wall however the turbulence levels increased and the contours became closely spaced. On the bend exit plane the streamwise turbulence contours suggested inner wall values (15%) to be slightly greater than those at the outer wall (13%). Again minimum turbulence was measured at the duct mid point (6%), although a trough of low value contours extended from here to the inner radius endwall corner. These contour patterns suggested that convection of Reynolds stresses by cross stream secondary flows become increasingly important as the distance travelled around the bend increased. Radial turbulence intensity contours at bend inlet and exit planes were also presented. At inlet maximum radial turbulence intensity values were recorded at 25% span on the outer wall (10%) and at 35% span on the inner wall (9%). These highest areas were linked by a ridge of high values bounded by the 7% contour running between them. At exit significantly higher values (14%) were measured between 70% and 80% of duct radial gap, from the inner curved wall. Values close to the flat endwall however were higher than those on the duct symmetry plane (10-11%). Streamwise radial shear stress contours at inlet to the bend were negative at the inner and positive at the outer curved walls. A negative contribution to the generation of turbulent kinetic energy was recorded adjacent to the flat wall from the inner radius for about 60% of the flat wall. Here negative shear stress values corresponded to negative velocity gradients. On the bend exit plane positive values of the streamwise radial shear stress occurred towards the outer radius and flat endwall. Negative values appeared close to the inner wall, extending radially outwards to about 20% radial gap onto the flat wall.

Rotating compressor blade wakes and annulus wall boundary layer mixing were studied by Davino and Lakshminarayana (1982). Obviously some differences in wake structure will exist between turbines and compressors, however little turbine wake data is openly available. Close to the rotor an asymmetric turbulent wake structure was measured, which tended towards a symmetric structure with increasing distance from the trailing edge plane. Complex turbulence structure was measured at the blade tips. Similar levels of complexity could be expected in turbine experiments at rotor tip due to tip leakage phenomena.

The wake of an aerofoil in two dimensional flow was studied by Hah and Lakshminarayana (1982) at varying positive incidence. Peak turbulence intensity data for the three fluctuating velocity components decreased with increasing incidence, however the distance from wake centreline to the free stream values also increased. The streamwise through wake shear stress values measured suggested that maximum values were not changed by increasing incidence, again however the through wake variations were broadened.

Developing laminar and turbulent flow fields in a non-accelerating square curved duct were investigated by Taylor et al (1982). These experiments supplemented the earlier fully developed turbulent entry flow experiments of Humphrey et al (1981). At inlet Taylor et al measured low turbulence intensities except within the wall boundary layers. After 90^0 of turning anisotropic turbulence was measured. Higher streamwise intensities were found close to the suction surface and higher radial intensities close to the pressure surface. Generally large values of the streamwise radial shear stress were measured near the pressure surface and side walls, indicating large momentum transport in areas of high streamwise velocity gradient.

The foregoing review spans about thirty years of secondary flow work relevant to axial flow turbines. Classical secondary flow methods are used to predict pitchwise averaged blade row exit angles with some confidence. Reliable estimation of blade row secondary losses, using simple correlation methods is difficult. The available correlations do not all agree on the most significant flow and blading parameters, giving the designer reduced confidence. More complex three-dimensional techniques are becoming available suitable for predicting secondary losses. These methods are unsuited to iterative use early in the design process owing to their complexity. The need for a simple and yet reliable secondary loss prediction method is therefore evident. Flow field investigations in both cascades and simple machines have given increased understanding of the mechanisms involved in turbine secondary flow. Much of the reported experimental work has concentrated on the mean flow field, with few studies reporting the complex turbulence phenomena. Additional detailed experimental data from a turbine blade configuration, giving both mean and turbulent flow information, would be desirable to improve understanding of this complex flow field.

CHAPTER 3

EXPERIMENTAL APPARATUS AND TECHNIQUE

3.1 THE LOW SPEED WIND TUNNEL TEST FACILITY

3.1.1 The Air Supply

Air for the wind tunnel at the beginning of the experimental programme was obtained from a research cross flow fan (Tuckey 1983). This fan produced total pressure fluctuations in the working section of about 20%, which arose from the unsteady forced vortex situated close to the casing tongue. The data acquisition system was developed to cope with these fluctuations and produced repeatable results.

A preliminary manual probe calibration was made using this fan and limited traversing was undertaken. Catastrophic failure of the fan occured, owing to metal fatigue of the blades at their junction with the end discs (Durham 1981).

A double entry centrifugal fan was obtained as a replacement (Keith Blackman Series 28) for the old machine with similar output flange dimensions. Use was therefore made of most of the existing wind tunnel ducting.

This fan was more stable in its operation than the old one, although the existing data acquisition system was retained. Higher wind tunnel velocities were also obtainable giving a more representative cascade blade Reynolds number.

3.1.2 The Low Speed Wind Tunnel

Failure of the cross flow fan damaged some of the fan exit ducting, which was not used for the replacement centrifugal fan. The straight length upstream of the settling chamber was thus shortened. The configuration of the wind tunnel was otherwise the same for both fans. At exit from the fan diffusion to a large setting chamber took place through honeycomb and gauze screens. Air was then accelerated through a contraction into the parallel working section (Figure 3.1). Situated after the working section was a transition piece, admitting air to the cascade at the desired incidence angle. Traverses of the working section, after fitting the new fan, showed both total pressure and angle variations. These variations were significantly reduced by fitting honeycomb at exit from the setting chamber contraction.

3.2 THE CASCADE

Cascade Design Data

Flow inlet angle	42.75^{o}
Blade exit angle	-67.5^{o}
Blade chord	216 mm
Axial chord	175 mm
Span	457 mm
Blade pitch	191 mm
Zweifel loading coefficient	1.07
Cascade Re	4×10^{5}

3.2.1 Construction

Seven blades were cast in epoxy resin using a mould cast from an aluminium master blade using a technique similar to that of Gregory-Smith and Marsh (1971). Pressure tappings were made in both surfaces of two blades to give an instrumented passage and periodicity checks in the adjacent passageways (Figure 3.2). Small bore plastic tubing was cast under the surface of these blades running in the spanwise direction. Holes drilled from the blade surface into these tubes, on varying spanwise planes, gave static pressure tappings. The unused tappings were covered by thin adhesive tape. Surface static pressures were recorded manually from an inclined multi-tube methylated spirit manometer.

Although seven blades were cast and fitted to the cascade only six, forming five passages, were utilized for these experiments. The remaining passage allowed for a change of incidence at a later date, if required.

The blades were held into a framework of timber and plywood, at the required stagger angle by studs and locating dowels. Nearly all of one endwall was constructed of 'Perspex', allowing for flow visualization work, and also a valuable visual check on traverse probe tip position. The opposite endwall, made from plywood, was slotted for insertion and traversing of the probes (Figure 3.3). 'T' shaped slot fillers were inserted into the unused slots, thus presenting a smooth internal endwall surface to the flow. The traversing slot in use was sealed over with masking tape, except around the probe stem, thus preventing bulk outflow or inflow of air. This tape required adjustment with each new tangential probe position. Traverses were completed in the spanwise direction, away from the 'Perspex' endwall, before indexing tangentially, and this resealing process was an acceptable solution. Plates mounted onto the flanges of the cascade (Figure 3.1) allowed for three different tangential positions of the traverse gear assembly. This economised on the length of tangential traverse slider required.

Three bosses were fitted into the endwall of the transition piece, allowing spanwise traverses to be made upstream of slot 1 (Figure 3.1). Data from these upstream traverse bosses was used to evaluate the effectiveness of boundary layer thickness adjustments prior to traverse plane measurements. When commissioning the cascade and its associated instrumentation data from these bosses was used to check the cascade inflow, and consequently highlighted the need for honeycomb upstream of the working section.

3.2.2 Sign Convention

The three axes defined for the cascade make a right handed set of the tangential, axial and spanwise directions (Figure 3.4). Positive component velocities ran parallel to their respective axes, in the direction of increasing positive co-ordinate. Positive yaw and spanwise flow angles were obtained when both of their respective velocity components were positive.

Setting of probes in yaw rotated the probe tip axes about the probe stem. The angular setting of the 'twist' (θ) calibration mounting (Section 3.4.1) was of the opposite sense to the positive yaw (ϵ) convention. To obtain cascade yaw angles the following relationship was used:-

$$\epsilon = 90 - \theta + \alpha$$
$$= \tan^{-1} (V_T/V_A)$$

It is possible to define two spanwise angles, one relative to the yaw angle direction, termed 'streamwise spanwise angle' (β), and the other relative to the axial direction, termed 'spanwise angle'. In this investigation the spanwise angle is most frequently used, and is evaluated from the calibration turret 'tilt' angle using the following relationship:-

$$\lambda = \tan^{-1} \left[\frac{\tan \beta}{\cos \epsilon} \right]$$
$$= \tan^{-1} (V_S/V_A)$$

3.2.3 Endwall Boundary Layer Control

Three cascade endwall boundary layers were investigated in this experimental programme, termed natural, thickened, and thinned. (Appendix IV)

The natural boundary layer case was investigated most thoroughly. Traversing was undertaken using pressure probes in all ten slots, and hot wire probes were used at inlet and exit. This natural boundary layer grew along the tunnel endwalls from the settling chamber to the cascade inlet giving a thickness of 116 mm.

Boundary layer thickening was achieved by disturbing the flow upstream of the cascade. This disturbance was made from aluminium sheet and attached to the working section endwalls (Figures 3.1 and 3.5). The cascade endwall boundary layer thickness was increased to 164 mm.

To thin the boundary layer, spacers were inserted into the transition piece/working section flange, with the top and bottom surfaces blanked off (Figure 3.1). This produced a boundary layer thickness of 52 mm. The bleed slot was rather too close to the blades for the new boundary layer to become settled. Slot 1 traverse data for the thinned boundary layer consequently shows this effect (see Section 6.1). A bleed off of air further upstream would have been more desirable, but would have delayed the experimental programme further.

3.3 THE EXPERIMENTAL APPARATUS

3.3.1 Data Acquisition System

As large quantities of data were to be recorded an automated acquisition system was considered necessary. This data acquisition system was controlled by a 'Cifer 2684' minicomputer. Hardware and software for the Data Acquisition Unit (D.A.U.) were provided by the University Microprocessor Centre. (Durham 1982). The unit was addressed by 'Fortran' subroutine calls from the 'Cifer' program running to control both the experimental activity and record the data.

Up to eight analogue D.C. voltages, between 0 and 10 volts positive were fed into the D.A.U. from various transducers (Figure 3.6). Instructions from the 'Cifer' to the D.A.U. microprocessor selected a multiplexer channel and subsequently allowed its analogue to digital conversion. Experimental data from each selected channel was then read by the 'Cifer', appearing as an integer in the 'Fortran' program between 0 and 4095 (0 representing zero volts with a linear variation to 4095 representing 10 volts.) Calibration factors for each transducer gave physical meaning to the data, for further use and subsequent storage onto floppy disc data files. A box containing ten relays was also controlled by the D.A.U. and Cifer. These were used to switch solenoid valves to select pressure combinations from the five-hole and three-hole probes, and route them to a single differential pressure transducer.

The 'Cifer' wrote experimental data direct to its internal floppy disc. This data was transferred to the University mainframe machine for subsequent analysis and presentation.

3.3.2 Traverse Probes

Pressure sensing and hot wire probe supporting stems were designed so that with rotation the probe tip measuring volume remained fixed in cascade co-ordinate space. Each probe stem was clamped into a dowelled carrying boss for attachment to the traversing gear (Section 3.3.3).

Pressure probes were of the swan neck type, with tip sensing tubes of 0.5 mm bore (Figure 3.7). A probe tip identification notation was adopted for subsequent use throughout the experimental programme. At the end of the probe stem each protruding hypodermic tube was finished to a different length, thus giving a unique sensing tube identification.

The twin hot wire sensing elements used (DISA 55P53 and 55P54) have their sensing volumes at orientations differing by 90^0 to the probe support (DISA 55H24) datum location. A probe stem was therefore manufactured to accept both types of hot wire probe sensing tip with an indexing slot (Figure 3.8). This ensured that the measuring volume for both hot wire probes remained on the stem centreline, and thus invariant with its rotation.

3.3.3 Probe Traversing Gear

The probe traversing gear was designed to accept both pressure sensing and hot wire probes. Slider assemblies and a calibrated rotary mounting were purchased from outside suppliers ("Unislide" and "Ealing Beck" respectively). Robust brackets were designed and manufactured to hold the sliders and rotary mounting with co-planar axes (Figure 3.9). Dowel pins located the individual components so that dismantling and reassembly was straight forward. The slider assemblies had no mechanism to take up wear in the slideways. Nylon packing pieces were therefore made to overcome this problem. A split outrigger bearing was also fitted on the spanwise slider providing additional support to the probe stem.

Potentiometers were connected to the spanwise and tangential sliders linear movements using anti-backlash pinions and racks. These were used as voltage dividers giving a voltage output related to slider positions. Hysteresis effects were further reduced by traversing each slider in one direction at each axial traversing slot. Calibration of these slider potentiometers was performed using the surface plate and a vernier height gauge. This calibration process avoided the need for precision potentiometers and slider screw threads. The output voltages were however found to be linear over the traversing ranges required.

To each probe stem was clamped a boss having a machined register diameter and dowel pin for precise location in the rotary mounting. The angular position of the probe, after initial setting during the calibration, was therefore repeatable for subsequent removal and insertion.

The probe traverse slots were cut parallel to the cascade front face using a vertical milling machine. Alignment of the traversing mechanism was achieved by trial and error, by adjusting the whole assembly on the 'T' slotted members. Clearance on both sides of the 9.35 mm diameter probe stem in the 10 mm slot for the required traverse length was taken to indicate satisfactory alignment.

3.3.4 Pressure Probe Apparatus

Multi-tube pressure sensing probes were traversed in the cascade flow field, and a pitot-static tube upstream was used for reference. Data was recorded from both of these probes, and the traverse position transducers by the data acquisition system (Figure 3.10).

Probe positional data was obtained from the traverse gear potentiometers (section 3.3.3). The upstream reference probe signal, for conditional sampling purposes (section 3.6.3) was after amplification fed directly to the D.A.U. Connections were also made to the solenoid valve switch box from this upstream probe. Pressure combinations from the multi-tubed traverse probes were also routed to these solenoid valves. Control signals from the Cifer triggered the relay box, thus allowing various pressure combinations to be connected to the single traverse probe differential pressure transducer. Output from this transducer, after amplification and addition of an offset voltage was connected to the D.A.U. Addition of an offset voltage was necessary to ensure that the voltage at the D.A.U. was always positive, since some probe pressure combinations could produce negative pressure differential values.

The value of this offset voltage was updated at each traverse probe position, by connecting both legs of the differential pressure transducer to atmosphere through a solenoid valve pair.

3.3.5 Hot Wire Probe Apparatus

Twin sensor hot wire probes were traversed in the cascade flow field. Data was recorded from hot-wire anemometry equipment, the upstream reference pitot static probe and the positional transducers (Figure 3.11).

Traverse probe position was recorded in the same manner as for the pressure probes (Section 3.3.4).

Signals from each of the hot wire sensors were fed directly to the constant temperature anemometers. Output from these was linearized, then conditioned using amplifiers and offset voltages. Gain and offset settings on these signal conditioners were adjusted at intervals as required. Adjustments were made when signal output from the probes, as viewed on a dual beam oscilloscope, became either "clipped" or of low amplitude. Revised conditioner setting values were also checked on the oscilloscope, and if satisfactory entered into the 'Cifer' as additional data in the gain adjustment sequence. Each channel was fed to a mean and root mean square (rms) unit giving data on the mean and fluctuating velocity components. Instantaneous signals from the conditioners were also fed to a turbulence processor, giving a signal propo^r^tional to the turbulent velocity correlation. Output from the processor had an offset voltage applied, since its sign could vary.

The upstream pitot-static probe was only used to record the upstream dynamic head, to ensure that the tunnel conditions did not drift from the required operating Reynolds number. No offset voltage was necessary since the dynamic head was always positive.

3.4 PRESSURE PROBE CALIBRATION

3.4.1 Calibration Apparatus

A compound rotary mounting was made from two rotary mounts and a pair of angle brackets (Figure 3.12). This assembly was mounted onto the cascade working section for the calibration process (Figure 3.1). Calibration of the five-hole probe involved setting both twist (α) and tilt (β) mountings to give data at various flow angles. Three-hole probe calibration required rotation of the α mounting only, with the β mounting fixed to ensure the probe stem remained perpendicular to the flow.

To avoid nulling the pressure probes in yaw at each experimental point extra effort was required in the probe calibration procedure to give the required data definition. Surfaces of five-hole probe calibration, and lines of three-hole probe calibration were produced for this purpose.

A preliminary calibration was performed, recording the various probe tip differential pressures manually, for both pressure probes. This data was obtained using the old fan, with no cascade fitted at exit from the wind tunnel working section. The wind tunnel mainstream air velocity was then varied between 20 and 36 m/sec, representing approximate bulk cascade entry and exit velocities. No Reynolds number effects were observed on either the five-hole or three-hole probe calibrations obtained.

Full calibration data for both pressure sensing probes was subsequently obtained using the new fan, and a modified version of the experimental data acquisition program. This calibration data was therefore obtained using the same conditional sampling techniques as used in the experimental investigation. Probe calibration setting angle data was entered manually, and recorded on floppy disc, with data logged probe tip differential pressures for analysis on the university mainframe computer.

Zero inclination on the β mounting was checked using an inclinometer on the turret top. This was adjusted using shim material under the wind tunnel mounting bracket so that it was parallel with the working section top and bottom surfaces. This assumed that the bulk flow ran parallel with the tunnel walls. A manual traverse with the five-hole probe showed this to be so in the area traversed by the probe tip, under motion from the β mounting. Probes were aligned with the flow direction in the other plane visually with the wind tunnel centreline, scribed onto the working section bottom. At this setting the 'twist' mounting was zeroed and the probe clamped into its holding boss. This method was used rather than nulling in α since probe tip absolute accuracy was felt to be doubtful.

3.4.2 Five-Hole Probe Calibration Procedure

The calibration process was based on that proposed by Schaub et al (1964) who calibrated a five-hole probe in an incompressible air stream from a nozzle. Schaub et al suggested that for an accurately made probe tip the calibration effort could be greatly reduced using the symmetric properties of the calibrating flow field and probe tip. A full calibration was performed for this probe, thus allowing for asymmetry of the probe tip, and for possible probe stem interference effects.

The probe calibration was obtained by setting the β mounting and incrementing the α mounting so that a carpet of points was obtained between -30^0 and $+30^0$ in α and -25^0 and $+25^0$ in β. More points were obtained at small turrent setting angles, since it was felt that most cascade traversing would produce flow angles onto the probe within this region.

The four calibration functions identified by Schaub et al (1964) were evaluated at each calibration condition (Appendix I). Dis-continuities within three of these functions arose when pressure at the central sensing hole equalled that at the 'left' or 'right' sensing tube (Figure 3.7). This problem was overcome by obtaining positive and negative α calibrations for each of the four functions.

All four calibration functions were treated similarly to ease computation. The positive and negative α calibrations were extended by 5^{o} into the opposite sign calibration to provide an overlap.

Each of the eight resulting sets of data from the calibration process was of a form suitable for representation as a surface, with α and β being the controlling variables.

Initially, with the manually collected calibration data obtained using the old fan, standard polynomial surface subroutines were fitted to a relatively sparse set of data points. Increasing both the α and β degrees of these polynomials improved the quality of the fit, determined from least squares residual data. A third order polynomial in α and β fitted the data adequately, and this was used to obtain preliminary traverse data prior to the fan failure.

Calibration data obtained using the data acquisition system was far more comprehensive than the earlier set. Polynomials were again fitted to the data, however increasing their order did not appear to influence the least squares residuals significantly. An improvement in quality of fit was obtained when bi-cubic spline surfaces were used from the mainframe computer subroutine library. Assessment of the quality of a surface fitted to the experimental data is difficult. Sections through the resulting surface were taken at various α and β values with both the experimental points and the fitted surface plotted. Adjustments were made to the number and positions of interior spline knots before a satisfactory fit was obtained, as determined by both least squares residual and visual criteria.

Data arrays were generated from the satisfactory fit for each of the ϕ and ψ functions, at one degree intervals of α and β for interpolation purposes in subsequent analysis of experimental data (Appendix I). The bi-cubic spline coefficients of σ and τ were also stored for their subsequent evaluation within the data analysis programs.

All the probe calibration angle data was held in the mounting turret co-ordinate angles, cascade angles were derived later in the analysis programs

3.4.3 Three-Hole Probe Calibration Procedure

Calibration of the three-hole probe, being a simple case of the five-hole probe, followed similar lines to those previously discussed. The calibration fixture was set with $\beta = 0$ and the three-hole probe traversed only in α to obtain data. Only three of the four functions identified for the five-hole probe were required, ϕ, σ and τ (Appendix I). Polynomials in α were found to fit the data adequately. The quality of fit was again assessed both by least squares residuals and plotting the resulting calibration lines. The problem of discontinuities at some flow angles in the calibration coefficients was overcome in the same manner as that previously described for the five-hole probe.

Arrays of ϕ were generated at one degree intervals for both the positive and negative α values, for interpolation in the experimental data analysis programs. Polynomial coefficients were also stored for evaluating the σ and τ values.

3.5 HOT WIRE PROBE CALIBRATION

3.5.1 Calibration Apparatus

Air was passed from a nozzle over the hot wire probe, which was calibrated against a reference pitot tube.

Compressed air was piped from a receiver to a combined filter and water trap, which removed particulate matter from the calibration flow (Figure 3.13). A pressure regulating value was fitted, which maintained pressure up to the gate valve. This gave essentially constant air velocity at the nozzle, for a given gate valve setting, while the supply pressure varied during the calibration. A flexible hose was used so that the nozzle, jet regulating controls and hot wires were all within easy reach. The straight pipe length gave a fully developed velocity profile prior to the nozzle contraction.

The jet from the nozzle was traversed with a small total pressure probe upstream of a reference probe. Care was taken to avoid wake interactions between these two probes for the horizontal and vertical centreline traverses. The traversing probe was 1 diameter and the reference probe 3 diameters downstream of the nozzle. Jet velocity for these traverses was maintained at 40 m/sec, being slightly greater than the maximum encountered in the cascade from the pressure probe results. The velocity profile obtained was flat for a large portion of the core flow (Figure 3.14).

3.5.2 Calibration Procedure

All the hot wire probe instrumentation was mounted on a trolley enabling it to be easily moved from the wind tunnel to the calibration facility. The probes were calibrated in the holder which was then used for the cascade traversing.

The probe to be calibrated was mounted in its holder and clamped into a retort stand. A total reference probe was mounted just off jet centreline, 1.5 nozzle diameters downstream, and the hot wire to be calibrated diametrically opposite and 1 diameter from the nozzle face (Figure 3.13). Care was taken to ensure that the hot wire pair under calibration were not influenced by prong wakes and did not shed their wakes onto each other.

The calibration jet was set to the approximate mean velocity encountered in the cascade plane to be traversed. Adjustments were made to the anemometer settings to optimize their responses to a square wave input for each wire. After optimization the controls were taped over to prevent accidental adjustments occuring.

At the start of the calibration process atmospheric conditions were recorded. This enabled manometer differential pressure readings for even increments of velocity to be evaluated. The jet was set to the required dynamic head, using the pitot tube and manometer, and the mean output voltages from each of the hot wire anemometers was recorded manually. The response of hot wire anemometers to increasing flow velocity is non-linear (Figure 3.15) and was consequently linearized to enable meaningful r.m.s. values to be obtained.

At breaks during cascade traversing the hot wire probes were recalibrated, using the calibration jet, in an attempt to minimize effects of sensitivity changes due to sensor fouling.

3.5.3 Linearization of Hot Wire Probe Calibrations

The role of the linearizer is to give an output directly proportional to the flow velocity. This enables flow velocities, both mean and fluctuating, to be evaluated much more easily. A least squares third order polynomial was fitted to the hot wire calibrations obtained, suitable for the "Prosser 6150" linearizer. The coefficients of this polynomial were then set on the linearizer. Check calibrations on the linearizers were then performed using the air jet and two hot wire probes to ensure it was set up correctly, (Figure 3.15). This check calibration also gave the constant of proportionality, the hot wire sensitivity, for each wire calibrated. This sensitivity was entered into the experimental data recording program.

3.6 EXPERIMENTAL METHOD

The experimental method was written into the data acquisition program which ran on the Cifer minicomputer. Prior to an experimental run the probe traversing gear was set up on the cascade, and aligned with the appropriate traverse slot. The instrumentation was allowed to warm up thoroughly before data collection began.

3.6.1 Initialization Sequence

A common initialization sequence set the computer up with appropriate calibration factors for the probe type in use. Information for identifying the experimental run was entered firstly, followed by the traverse probe type. The computer then prompted for input of appropriate calibration factors and instrument settings.

The probe traversing gear was then referenced to the tangential and spanwise datum. The tangential datum line was scribed onto the side of the cascade. A straight edge was aligned with this datum line and the probe stem brought to it twice, so that two readings were obtained one from each side of the probe stem. The tangential slider datum was assumed to be the mean of these two readings. Referencing of the probe to the spanwise datum was carried out using a shim of a known thickness and noting when contact occured at the perspex endwall. The centre of the measuring volume for each probe type was assumed to occur at half the tip width in the spanwise direction. Hot wire probes were far too fragile for this approach, and the shorting probe was used for this purpose. Careful measurement of this and the hot wire probe types used allowed the position of the measuring volume centre to the established within close limits.

All the experimental work took place at a Reynolds number of 4×10^5, based on axial chord and bulk exit velocity. The wind tunnel working section dynamic head required to achieve this varied from day to day. Atmospheric conditions were recorded using a mercury column barometer and a thermometer. In obtaining the barometric pressure allowance was made for the density variation of mercury with temperature (Kaye and Laby 1973). It was however assumed that the mercury column, in a glass sided case and reasonably draught free, was all at the recorded temperature. Air viscosity was evaluated from a polynomial representation (Watson 1972). At a given set of atmospheric conditions the required wind tunnel working section dynamic head could thus be determined, giving the required cascade Reynolds number (Appendix II).

Time averaging was used to evaluate the dynamic head at the upstream reference probe. Fine adjustment of the fan speed enabled setting the inlet dynamic head to within 1% of the desired figure. If pressure sensing probes were in use for traversing, the upstream total to atmospheric pressure differential was recorded for use within the conditional sampling procedure.

3.6.2 Probe Position Data

A grid of data points through the cascade was determined in advance to give a distribution of traverse data suitable for further analysis and presentation. The probe was positioned on a required tangential co-ordinate by trial and error, using the calibration of the tangential traverse potentiometer. Spanwise co-ordinates were then selected, in the same way, traversing progressively away from the perspex endwall. The actual position of the probe tip was displayed onto the 'Cifer' screen, repeatedly updated until the desired position was obtained.

3.6.3 Pressure Probe Data Recording

The pressure probes were set approximately to the mean two dimensional flow angle at midspan on the traverse plane under consideration. Flow yaw angles onto the probes were then generally within the limits set at the probe calibration. Traverse slots close to the leading edges were an exception to this, since close to the wall flow angles very different to those at midspan were measured. These flow angles differed sufficiently from the probe setting angles to give data off the calibration curves. Resetting the probe in yaw enabled these points to be measured successfully. For spanwise angles greater than the maximum allowed for in the five-hole probe calibration, extrapolations off the calibration surface were necessary to give results.

After the probe was positioned correctly the traverse transducer zero pressure differential was selected, and recorded after a two second settling time delay. An updated zero correction for each traverse probe setting was thus obtained. Unfortunately facilities were not available to automatically check and update the zero on the upstream reference probe. Manual checking of this revealed very low zero differential drift with time in any experiment. Absolute values of the reference signal did not contribute directly to the recorded traverse probe data, but were used only to trigger data capture.

All recorded traverse probe pressure differential data values were conditionally sampled, referred to the initially time meaned upstream reference signal. This technique was particularly useful for the old fan, where wind tunnel working section fluctuations in total pressure were severe, and repeatable data was obtained. These very low frequency (1-2 Hz) working section total pressure fluctuations at the reference probe showed a strong correlation with dynamic head measurements within the cascade. An x-y plotter was connected to the two pressure transducer outputs and several correlogrammes were obtained showing this relationship. Improved data recording rates were achieved when using this conditional sampling technique compared to the traditional long time period averaging. This data recording technique was retained when the new fan was installed, although the total pressure fluctuations were significantly reduced.

For a selected probe differential pressure conditionally sampled data was obtained by reading pairs of data points. One of these from the upstream reference probe, and the other from the selected traverse probe tip tappings (Figure 3.16). These data pairs were read as rapidly as possible using the D.A.U. A comparison between the reference signal and that initially time averaged was then made using integer arithmetic for added speed. If outside the tolerance band of 1.5% on the initial reference signal value the data pair was rejected, and the cycle repeated (Figure 3.17). When more than ten pairs of acceptable data points were recorded running mean, variance, and standard deviation values were evaluated. This data was then used to evaluate the variation allowable on the mean value for a 99% confidence level (Kreyszig, 1972). If this variation, the confidence interval, was less than a ±1% tolerance of the mean value, then the current mean value was recorded as the probe pressure differential selected. A check counter was also used to prevent possible excessive looping, and if this occurred the achieved tolerance, evaluated from the confidence interval, was recorded. Generally probe pressure differential data was within 1% of the mean value with 99% confidence.

When all the required probe tip pressure differentials were recorded, for a specific probe position, the complete set of data was written to disc file.

3.6.4 Hot Wire Probe Data Recording

Two separate hot wire probe traverses, with different probe types, were required on each traverse plane to obtain data on perpendicular planes in the cascade. One traverse obtained velocity data from a plane parallel to the cascade endwalls, the other from the plane through the probe stem, in the local yaw angle direction.

The probe mounting was set to the flow yaw angle, as determined in the five-hole probe traversing, at a given probe position. This data was manually entered point by point from a computer listing. The user was then questioned on its accuracy, after manually setting the probe mounting to the required angle.

The zero offset value was read into the computer from the upstream reference pitot tube. An updated zero value was obtained for each traverse point prior to recording the upstream dynamic head. This was possible for the hot wire probe traverses as other pressure differential data was not required. After selecting the dynamic head on the solenoid switch box a two second settling time was allowed prior to sampling twenty values, at one second intervals. These values were meaned, to give an estimated time average of the upstream dynamic head.

The recorded dynamic head at any traverse point was compared with that required for constant Reynolds number. If the error was greater than 1% the fan speed was reset and the whole process repeated.

During the dynamic head sampling period the analogue circuitry of the hot wire anemometers had stabilized. This allowed meaningful data to be read by the D.A.U. of mean, r.m.s. and turbulence correlation values.

With a full set of data at the required Reynolds number the recorded data was checked to ensure that none came within 5% of the analogue to digital converter limits (0 and 10 volts). If this condition occurred then the offset and gain controls on the signal conditioner were adjusted to give acceptable voltage levels. A dual beam oscilloscope monitored the instantaneous outputs from the signal conditioner of each hot wire. "Clipping" of these signals due to amplifier saturation was thus avoided. Conditioner offset and gain settings were adjusted to give a small measured mean value, and consequently maximized the fluctuating signal. After adjusting these settings the stored values were ammended by manual alterations on the 'Cifer', and the data recording process repeated.

When an acceptable set of data was obtained it was written to disc, and the process repeated for subsequent traverse points.

3.6.5 Experimental Data Transfer

Experimental data was stored on 5.25" floppy discs within the 'Cifer' computer. Initially this was the only medium available for data storage from the 'Cifer'.

A program became available which read data from the 'Cifer' onto the university's mainframe computer when the two machines were linked by a line. This performed well when the mainframe machine had few users, however it became unacceptably slow at peak times, especially during term time. An 8" disc drive was obtained and data was transferred from the 5.25" discs to a suitably formatted 8" disc. This could then be submitted as a batch job and directly read by the mainframe machine.

A small mainframe pre-processing program was written to remove line de-limiters put in on the 'Cifer'. This was necessary as maximum acceptable record lengths differed considerably from 'Cifer' to mainframe, and prevented loss of data. Raw experimental data in the mainframe files was then suitable for analysis.

3.7 DATA ANALYSIS

All probe data analysis was performed on the university's mainframe computer. Pressure data was converted from raw voltages to millimetres of water gauge values and corrected for Reynolds number variation (Appendix II) within the 'Cifer' computer. Hot wire data however was stored in voltage form from the analogue to digital converter, in addition to the signal conditioner gain and offset settings.

The analysis programs on the mainframe computer converted these inputs into data suitable for presentation and comprehension. Data was analysed point by point for storage in arrays prior to plotting and pitchwise averaging.

3.7.1 Five-Hole Probe Data

As discussed previously two fitted surfaces were obtained for each calibration function (Section 3.4.2). The first step in the analysis procedure was to determine on which surface the experimental data point was located (Figure 3.18). The two surfaces represented positive and negative values of α, the calibration turrent twist. Values of ϕ and ψ plotted for α = 0 show the variation of β, derived from both the positive and negative calibration surfaces, to be approximately linear (Figure 3.19). A linear function, fitted to this data by eye, was found to be satisfactory for calibration surface selection, and consequently retained for the whole experimental programme. If the wrong surface was erroneously selected the calibration overlap of 5^0 prevented any experimental data points from being undefined.

With the required α calibration surface selected, linear interpolation in the appropriate calibration tables of ϕ and ψ followed. Initially ϕ and ψ for $\alpha = 0^0$ and $\beta = 0^0$ were assumed. The α value was incremented in 1^0 steps until the experimental ϕ was just exceeded. The β value was then similarly incremented, at the fixed α value until this was just exceeded by the experimental ψ value. This process was repeated until the experimental ϕ and ψ (ϕ_e and ψ_e) were spanned in the table by calibration values of ϕ and ψ forming a cell (Figure 3.20). The intersection of straight lines drawn through the ϕ_e and ψ_e values on the cell boundary was assumed to give the experimental data values of α and β (α_e and β_e). Evaluation of σ and τ followed using α_e and β_e in the appropriate spline fit.

Extrapolated data arose when the experimental point occurred outside the probe calibration. Linear extrapolation was used to determine values of α_e and β_e, one value of which was linearly interpolated from data on the calibration carpet boundary. Values of σ and τ were evaluated in a similar manner.

Angles were then defined in cascade co-ordinates, rather than those used for the calibration process (Section 3.2.2). Total velocity magnitude was then evaluated using σ (Appendix I), and the component velocities derived from this and the flow angles. Values of total pressure loss and static pressure coefficients were also evaluated.

3.7.2 Three-Hole Probe Data

The three-hole probe analysis technique adopted was a simple case of the five-hole probe. After selecting the appropriate calibration line all interpolations were linear to determine the experimental α value (α_e). Values of σ and τ were calculated from the polynomial coefficients at α_e. Linear extrapolation was again used if the experimental data was not on the calibration line.

3.7.3 Hot Wire Probe Data

Data from the experimental rig was stored in voltage form. These voltages were converted to velocity values by using the hot wire sensitivities measured at calibration and corrected for Reynolds number differences (Appendix II). Analysis for the two pairs of hot wires (XY and XZ) was performed in parallel due to some cross coupling of the equations (Appendix II).

Ten hot wire signals were recorded from each experimental data point (i.e. five from each traverse of XY and XZ wire pairs). Redundancy in the solution equations was obtained as only eight velocity values were evaluated. This redundancy was represented in two derived values of $\bar{u}$ and $\overline{u'^2}$ (i.e. $\bar{u}_{xy}$, $\bar{u}_{xz}$ and $\overline{u'^2}_{xy}$ and $\overline{u'^2}_{xz}$). Values of the $\bar{u}$ velocities were generally within 5% of each other. Larger differences were evaluated in the $\overline{u'^2}$ signals but these were usually within 15% of each other. The differences between these calculated $\bar{u}$ and $\overline{u'^2}$ values from the two wire pairs was assumed to be due to errors in probe positioning, probe sensor contamination and experimental errors in both data recording and manipulation. The resulting mean of both $\bar{u}$ and $\overline{u'^2}$ was therefore used for evaluating flow angles and for presentation.

Analysis of the hot wire data from the two wire pairs was explicit. An analysis proposed by Gregory-Smith (1982ii), retaining higher order terms, involved some iteration but generally converged rapidly. Results from this revised analysis showed some inconsistencies at slot 8, due to experimental data scatter, and consequently results for this traverse slot are derived from the first order analysis. Data presented at slot 1 was analysed using the revised analysis after Gregory-Smith.

3.8 DATA PRESENTATION

Any study of three dimensional phenomena requires effective data presentation, to aid interpretation and to communicate the work to others. The classical problem is that the conventional two dimensional piece of paper cannot truly convey the three dimensional subject. Holographic techniques can overcome this problem but at present are impractical for thesis presentation.

Processed experimental data is stored in a large three dimensional array within the mainframe computer. Presentation of this data, on section lines parallel to the array axes, produced three types of plot:

1. Plots on constant axial co-ordinate planes.
2. Plots on constant spanwise co-ordinate planes.
3. Plots on pseudo stream surface planes.

Data is presented in the form of contour and vector plots. Pitchwise and area mass averaged data is also presented.

3.8.1 Contour Plots

A contour plot is a series of lines linking points on a plane having the same function values. This is a common technique used by cartographers to represent hills and valleys on maps.

The available contouring programs all plotted contours on regular data grids. Curved contours drawn by some of these programs overlapped occasionally, and these were rejected, since real data would very rarely give overlapping contours. A contouring program using linear interpolation between data values, also on a regular rectangular grid, was chosen (Cederquist, 1976). The source coding for this program was available and modified to generate contour co-ordinates on a trapeziodal grid.

Co-ordinates for all required contours were generated cell by cell on the required plane (Figure 3.21). The cell central value was assumed to be the mean of the corner nodes. Linear interpolation along each of the eight lines making up the cell defined the path of a specific contour through it. This process was repeated for each cell to generate contour co-ordinates for any plot.

Plotting of the contours was performed using external "Fortran" subroutines. By performing two 'contouring' operations thick and thin contour lines could be plotted giving increased clarity. Thick contour lines can in principle be produced by plotting several contours of similar values. This process in areas of small contoured variable gradient gives discrete contours and confuses rather than clarifies. A simple thick line plotting algorithm was used to overcome this problem (Figure 3.22).

3.8.2 Vector Plots

The plotting of arrows representing vector quantities as an aid to flow visualization is a straight forward operation. A simple scaling scheme was used to avoid excessive arrow overlap, or very short arrows for each plot. A vector scaling cell was constructed around each data point (Figure 3.23). The required arrow length scaling factor for each cell was determined by where the vector direction crossed the cell boundary closest to the cell centre. This process was repeated for the whole plot plane. The overall scaling factor used was the minimum cell scale value calculated. Problems in this scaling process were encountered when plotting on spanwise planes, owing to the high passage curvature. These were avoided by scaling vectors from the downstream flow field only. However as a result some overlap has occurred on these plots close to midspan.

3.8.3 Pitchwise Mass Averaged Data

Mass flow weighted pitchwise averaged data is generally used throughout this study unless otherwise stated. Due to the low velocities involved the flow field can be regarded as incompressible. The pitchwise mass averaged total pressure loss coefficient consequently can be defined as:

$$\bar{\zeta} = \frac{\int V_A \, \zeta \, dT}{\int V_A \, dT}$$

Defining the local yaw angle as:-

$$\epsilon = \tan^{-1}\left[\frac{V_T}{V_A}\right]$$

then the pitchwise mass averaged yaw angle is given by:-

$$\bar{\epsilon} = \tan^{-1}\left[\frac{\bar{V}_T}{\bar{V}_A}\right]$$

$$= \tan^{-1} \left[\frac{\int v_A v_T \, dT}{(\int v_A \, dT)^2} \right]$$

All the above integrals were evaluated in the tangential direction across the blade pitch, on a series of constant spanwise co-ordinate planes, using the trapezium rule. Each trapezium spanned interpolated values midway between experimental data points.

3.8.4 Area Mass Averaged Data

An area mass weighted total pressure loss coefficient for each traverse plane was defined as:-

$$\overline{\overline{\zeta}} = \frac{\rho \int_2 \int_1 v_A \, \zeta \, dT \, ds}{\dot{m}}$$

with the total mass flow defined as:-

$$\dot{m} = \rho \int_2 \int_1 v_A \, dT \, ds$$

Integral 1 was evaluated as for the pitchwise mass averaging, and integral 2 in the spanwise direction.

Evaluating the mass flow rate on each traverse plane provided a check on both the experimental technique, and the numerical integration procedure.

CHAPTER 4

NATURAL INLET BOUNDARY LAYER PRESSURE PROBE RESULTS

4.1 INLET FLOW FIELD

Probe traverse data from traverse slot 1 defines the inlet flow field in considerable detail with the experimental data points shown in Figure 4.1. Chronologically this was the second traverse plane to be investigated (only slot 10 preceeding it) and the five-hole probe data was taken very close to the end wall (2 mm). Subsequent contour plots however show the effect of the predominantly spanwise total pressure gradient on the five-hole probe results to be small when compared with the three-hole probe data. Some extrapolation from the probe calibrations is evident in regions of high positive yaw angle occuring upstream of the blade leading edges.

In a collateral boundary layer the Bernoulli surfaces (surfaces of constant total pressure) are parallel with the endwall. Experimental contours of total pressure loss coefficient (comparable with Bernoulli surfaces) are approximately parallel with the cascade endwall (Figure 4.2). The two slight depressions in the contours (at T = −80 and T = 100 approximately) are probably due to the presence of the leading edge vortex. This phenomenon occurs upstream of the blade leading edges and draws high energy free stream fluid towards the endwall. Contours of streamwise spanwise angle (Figure 4.3) show this to be a possible explanation with increased negative angles (i.e. flow towards the endwall) in comparable positions. The -2^{0} contour dividing into two closed regions suggests that the leading edge vortex is, at the slot 1 location, beginning to divide into the adjacent passages. Higher negative streamwise spanwise angle values, very close to the wall, are a result of the spanwise total pressure gradient and spacing of the sensing holes in the five-hole probe. The cascade air inlet angle at slot 1 can be defined as the pitchwise mass averaged yaw angle value at midspan, which has a value of approximately 44^0.

As the leading edge vortex divides around the blade leading edges, entrained fluid passing close to the end wall heading for the suction surface (the suction side leg of the leading edge vortex after Marchal and Sieverding (1977)) is subjected to increasing yaw angle. This is evident from both the yaw angle contour plot close to the wall (Figure 4.4) and the secondary velocity vector plot (Figure 4.5). Yaw angle data from the three-hole probe suggests angles of greater than 90^0 (i.e. negative axial velocity) close to the endwall at $T \approx -60$ and $T \approx 130$ mm. Although these results are from extrapolated data (Figure 4.1) and therefore possibly unreliable, they do reflect a trend and fit in with the physical explanation of the flow. Fluid in this leg of the vortex is rotating in the opposite sense to the passage vortex which subsequently forms. The pressure side leg of the leading edge vortex close to the endwall is subject to decreasing yaw angle and is thus rotating in the same sense as the subsequent passage vortex (Figure 4.5).

Variation in static pressure coefficient shows the flow tending towards the stagnation value of -1 approximately in line with the blade leading edges (Figure 4.6). Towards the passage centre values greater than zero show an increase in velocity compared to that at the reference probe upstream. Contours of total velocity (Figure 4.7) show the combined effects of static pressure and total pressure loss coefficients. The former have predominantly cross passage gradients and the latter spanwise gradients.

4.2 FLOW DEVELOPMENT THROUGH THE CASCADE

Results from the pressure probe traversing within the cascade blade passage are presented in this section (i.e. data from slots 2 to 7), on planes of constant axial co-ordinate. The development of the cascade flow losses and the passage vortex are traced.

A section through an idealized passage vortex is shown in Figure 4.8. At the centre of rotation both the passage cross flow and the spanwise flow angles approximate to zero. The flow angles are not exactly zero because of the migration of the vortex centre through the blade passage.

4.2.1 Traverse Slot 2

The experimental data points (Figure 4.9) show that for both the five-hole and three-hole probes difficulty in interpreting the raw pressure data was experienced. This was chiefly due to the very low local dynamic head, coupled with the high spanwise flow angles existing in the pressure surface endwall corner. To complete the data set adjacent spanwise values on a given tangential traverse were manually substituted.

Total pressure loss coefficient contours (Figure 4.10) show a skewing of the 0.05 contour compared with slot 1 data (Figure 4.2), this is more noticable on the suction surface as a spanwise migration from the endwall. The pressure surface exhibits the opposite trend although much reduced. Data close to the endwall from the five-hole probe suggests two regions of high total pressure loss coefficient. These regions correspond to low energy upstream boundary layer fluid entrained in the two legs of the leading edge vortex. The peak value close to the pressure surface, based on only one extrapolated five-hole probe data point, is suspect. Data from the three-hole probe however gives confidence in this identification of the pressure side leg of the leading edge vortex. Contours above 0.6 for the three-hole probe are based on four reliable data points before encountering the problems of extrapolation referred to above. Adjacent to the suction surface however both five-hole and three-hole probe data can be considered reliable and provide additional evidence of the suction side leg of the leading edge vortex to that of section 4.1. The 0.7 contour on the three-hole probe data shows low energy fluid, originally in the upstream boundary layer, being swept off the endwall by the action of this vortex. The curling under of the 0.5 contour shows higher energy fluid being drawn around by the vortex and indicates its direction of rotation. Entrainment of high velocity fluid in the suction side leg of the vortex is also shown by the curved total velocity contours from the three-hole probe data close to the suction surface (Figure 4.11). The low velocity region in the pressure surface endwall corner referred to above and the high pitchwise velocity gradient are also shown clearly in Figure 4.11.

Visualization of the developing passage vortex is obtained from the secondary velocity vector plot (Figure 4.12) however the passage vortex centre does not tie in very well with the vortex centre as perceived from the plotted vectors. This is partly due to the relatively weak nature of the passage vortex at this traverse slot, and its interactions with the pressure side leg of the leading edge vortex. Marchal and Sieverding (1977) suggest that close to the leading edge the passage vortex exists with a strong eccentric core owing to these interactions. The suction side leg of the leading edge vortex does not show up particularly well on the secondary velocity vector plot, although the suction surface exhibits spanwise flow towards the endwall due to the negative spanwise angles (Figure 4.13) generated by this phenomenon. An increasing yaw angle

(Figure 4.14) close to the suction surface endwall corner, compatible with a vortex rotating in the opposite sense to the passage vortex, is apparent from the five-hole probe data. The three-hole probe data in confirming this shows the effect more clearly. A location of the suction side leg vortex centre produced using the same criteria as the passage vortex is also shown (Figure 4.12) although very close to the end of the zero spanwise angle contour. The line of zero cross flow is derived from five-hole probe midspan data, and three-hole probe near wall data.

High negative yaw angles are apparent at T ≈ 80 mm close to the endwall, showing the very high turning associated with the pressure side leg of the leading edge vortex. Also existing in the same region are negative spanwise flow angles, indicating flow towards the endwall, these being based on two data points and a manually extrapolated point. This small area of negative spanwise flow angle is probably due to endwall flow separation around the pressure side leg of the leading edge vortex.

Static pressure coefficient contours (Figure 4.15) reflect the high pitchwise velocity gradient (compare Figure 4.11) and tie in with linearly interpolated wall static coefficient data obtained from suction surface pressure tappings. The blade surface and probe static pressure coefficients are in moderate agreement.

4.2.2 Traverse Slot 3

Data outside the probe calibration range is again evident close the the blade pressure surface, and at a series of points 10 mm from the endwall for the five-hole probe (Figure 4.16). Painstaking manipulation of the three-hole probe setting angle during the traversing produced a set of data within the probe calibration. However in the low dynamic head regions, errors can be expected to be significantly higher than elsewhere.

The plot of total pressure loss coefficient contours (Figure 4.17) from S ≈ 40 mm towards midspan shows strong similarities with the data of slot 2 although there is a general increase in Bernoulli surface skewing. A low loss area is evident close to the pressure surface resulting from extrapolated five-hole probe data, and since it does not form part of a trend can be regarded as erroneous. The high loss area associated with the pressure side leg of the leading edge vortex is now defined more clearly as is the curving around of the 0.5 contour. Data from the three-hole probe shows low loss fluid being drawn in by this swirling vortex motion. Comparison of Figure 4.17 and 4.10 shows migration of this high loss area towards the suction surface and away from the end wall. The loss peak close to the suction surface, evident from the three-hole probe data of slot 2 (Figure 4.10), is no longer visible. This suggests that it is either very close to the suction surface, and thus outside the traversed area, or flattened against the endwall, as implied by the three-hole probe results. Blade surface flow visualization work by many other workers (notably Marchal and Sieverding (1977), Langston et al (1977) and Sjolander (1975)) imply that the suction surface leg of the leading edge vortex is swept onto the blade surface close to the endwall and subsequently migrates in the spanwise direction.

Total velocity contours (Figure 4.18) show low kinetic energy fluid in the loss peak close to the pressure surface as a spanwise migration of the contours. Pitchwise velocity gradients have increased since slot 2 due to the continuing suction surface acceleration. The gathering strength of the passage vortex is shown by the secondary velocity vector plot (Figure 4.19). The location of the vortex centre is now closer to the perceived centre than for slot 2. At the suction surface endwall corner a reducing positive spanwise flow angle is evident (Figure 4.20). The three-hole probe yaw angle contours (Figure 4.21) show a flow towards the pressure surface compatible with rotation of the suction side leg of the leading edge vortex.

Static pressure coefficient (Figure 4.22) derived from both blade surface tappings and probe data show much improved agreement compared with slot 2 suction surface data (Figure 4.15). Pressure surface values however show some scatter due to the resolution errors involved in measuring small pressure differences on a manometer.

4.2.3 Traverse Slot 4

Extrapolation problems associated with low dynamic heads and high spanwise flow angles are again evident close to the pressure surface (Figure 4.23). No significant tangential incursions into the experimental data grid are made by the extrapolated data points.

Distortion of the Bernoulli surfaces is apparent now even at midspan (Figure 4.24). An area of low loss fluid now exists at the pressure surface endwall corner, having been drawn in from the mainstream flow by the strengthening passage vortex. Differences existing between the five-hole and three-hole probe data in this area are chiefly due to the high spanwise flow angles, causing extrapolations from the five-hole probe calibration. Tangential migration of the loss core is much in evidence,when compared with the slot 3 data (Figure 4.17), combined with a smaller spanwise shift. Data from the three-hole probe shows clearly low loss fluid, originally in the free stream, continuing to be drawn towards the suction surface under the loss core, owing to the influence of the passage vortex. No evidence of the suction side leg of the leading edge vortex appears presumably due to its proximity with the suction surface.

Acceleration of the fluid close to the pressure surface over the whole span is taking place (Figure 4.25) coupled with continuing growth in the tangential velocity gradient. Loss core fluid is now of a similar kinetic energy to that surrounding it. Since the loss core does not appear evident from the total velocity contours, it has therefore undergone a bulk acceleration from the slot 3 traverse location.

Vortex centre migration towards the suction surface is apparent from the secondary velocity vector plot (Figure 4.26). A spanwise migration towards the endwall is also visible. Evidence of the suction side leg of the leading edge vortex is not available from either the spanwise angle or yaw angle contours (Figure 4.27 and 4.28).

A disturbed region of static pressure coefficient contours exists in the region of the loss core due to its acceleration from slot 3 (Figure 4.29) and associated increase in kinetic energy. Agreement between the pressure surface static pressure tappings and probe data is very good. The high static pressure gradient close to the suction surface makes tie-up between the blade surface and probe data difficult, however agreement appears reasonable close to the endwall.

4.2.4 Traverse Slot 5

Pressure surface extrapolation problems have decreased since slot 4, data at the pressure surface endwall corner now falling onto the calibration surface (Figure 4.30). Two extrapolated points at the endwall near the suction surface are due to the high yaw angles encountered in this region. Both three-hole and five-hole probes have a manually interpolated point due to ommisions within the probe traversing. One extrapolated point is in evidence for the three-hole probe, owing to the high spanwise yaw angle gradient, coupled with an inappropriate probe setting angle.

Continuing loss core migration towards the suction surface is evident from the total pressure loss coefficient contours (Figure 4.31). Closure of the 0.1 contour near the pressure surface would appear probable from the five-hole probe results, but for the extrapolation problems referred to above. The three-hole probe data at the pressure surface endwall corner suggests that fluid of mainstream total pressure has now been drawn around by the passage vortex.

The total velocity contours show continuing acceleration across the whole passage (Figure 4.32). In the suction surface endwall corner region a reduced tangential velocity gradient exists, owing to the interaction of high endwall tangential velocities with the suction surface. This area shows as a region of increasing total pressure loss coefficient, (Figure 4.31) based on very few three-hole probe data points.

The passage vortex centre appears from the vector plot of secondary velocities (Figure 4.33) to be in good agreement with the perceived centre. High spanwise velocities close to the blade surfaces, and high overturning close to the endwall are reflected in the spanwise and yaw angle contour plots (Figures 4.34 and 4.35).

Roll up of static pressure coefficient contours at the suction surface endwall corner enables easier tie-up to be made between the blade surface static data and the traverse probe data (Figure 4.36). A reducing static pressure gradient close to the suction surface is evident compared with slot 4 data (Figure 4.29) due to both decreasing local velocity gradients and increasing total pressure loss coefficients.

4.2.5 Traverse Slot 6

Very few extrapolation problems are in evidence although the increasing suction surface spanwise velocities have caused some (Figure 4.37).

The developing trends in total pressure loss coefficients shown by the slot 5 data (Figure 4.31) are continuing at the slot 6 location (Figure 4.38). Mainstream total pressure fluid is now evident over the whole pressure surface. Near midspan the 0.05 contour, close to the suction surface, has moved towards the endwall, suggesting a migration of loss away from the midspan plane, when compared with slot 5. Two areas of loss are in evidence, one slightly away from the passage surfaces, the other close to the blade suction surface. The loss peak, slightly away from the passage walls, has developed under the influence of the passage vortex from upstream boundary layer fluid. The other loss peak probably results from loss generation within the separation of the suction side leg of the leading edge vortex on the blade surface. This loss generation occurs upstream of the traverse plane but subsequently is convected into the probe measurement zone by the action of the passage vortex. Examination of the data from slots 3, 4, and 5 (Figure 4.17, 4.24 and 4.31) also shows this phenomenon, although with clarity reducing further upstream. Langston et al (1977), whose results also show the existence of two loss peaks close to the suction surface, suggest the peak adjacent to the suction surface is due to interaction between the passage vortex and the blade boundary layer. The path of this loss area close to the blade plotted on a pseudo-stream surface corresponds closely to the suction surface separation line found by other workers from flow visualization studies (Section 4.6.1). A development of the high loss region existing in the suction surface endwall corner, initially observed at slot 5, is evident. This could be due either to strong tangential velocities within the passage vortex convecting the developing endwall boundary layer fluid, or a separation loss arising from a corner vortex.

Total velocity data from the three-hole probe shows a peak in kinetic energy at a lower spanwise location than the loss core (Figure 4.39). The five-hole probe data does not verify this but shows the loss core area to be partially composed of higher velocity fluid than the surrounding areas. Both probes show a net acceleration of the loss core when compared with slot 5 data (Figure 4.32). A reducing tangential velocity gradient is evident due to suction surface diffusion and continuing acceleration on the pressure surface. Relatively low total velocity is evident at the suction surface endwall corner, corresponding to the region of developing loss (compare Figure 4.31 and 4.38).

The secondary velocity vector plot (Figure 4.40) shows the passage vortex to have similar characteristics to that existing at slot 5. Location of perceived vortex centre now ties in very well with the inersection of the zero spanwise and zero cross flow angle contours. Contours of spanwise flow angle show the pressure surface negative spanwise flows decreasing, and those on the suction surface increasing in the positive direction when compared with slot 5 (Figure 4.41 and 4.34). High overturning on the endwall is evident from the yaw contours (Figure 4.42). The fall off in overturning near the endwall however being more intense towards the suction surface than for slot 5 data (Figure 4.35).

Agreement between static pressure coefficient data on the blade pressure surface and that from the probe appears to be good (Figure 4.43). Suction surface data however is much more difficult to correlate owing to the high pressure gradients existing close to the suction surface endwall corner. Away from this region the correlation between probe and surface tapping data appears reasonable.

4.2.6 Traverse Slot 7

Extrapolation of flow data from the probe calibration is now limited to a few points close to the suction surface, owing to the increasing spanwise flows (Figure 4.44).

The total pressure loss coefficient contours (Figure 4.45) show broad similarity with those from slot 6 (Figure 4.38). Migration of the 0.05 contour towards the pressure surface is apparent from the five-hole probe data, although scatter does appear towards the endwall. The three-hole probe results suggest that this contour makes a closed loop at the endwall, implying that it also meets the pressure surface. Spanwise migration of the loss core is apparent, together with considerable broadening of the area within the 0.5 contour. Three high loss areas now appear within the 0.5 contour, two of which are those previously identified, and the furthest one from the endwall probably resulting from additional loss generation upstream being fed into the traversing zone by the passage vortex. This additional loss arises from the complex flow phenomenon occuring around the suction side leg of the leading edge vortex separation. Continuing loss development at the suction surface endwall corner is evident, with an enlargement of the area bounded by the 0.5 contour.

Contours of total velocity (Figure 4.46) show the suction surface endwall corner loss region to be of low velocity, although somewhat decelerated when compared with slot 6 (Figure 4.39). Continuing pressure surface acceleration, coupled with suction surface diffusion has reduced the cross passage variation considerably since the slot 6 traverse plane.

A fully developed passage vortex is evident from the secondary velocity vector plot (Figure 4.47). As for slot 6 the two methods of evaluating the passage vortex centre tie in closely. At the suction surface endwall corner a decrease in the tangential velocity is apparent, corresponding to the area of higher total pressure loss coefficient. About 80 mm from the endwall, adjacent to the suction surface, the vectors seem to be moving around an obstacle. This phenomenon occurs at the same position as one of the total pressure loss peaks.

High spanwise flow angles, which caused the extrapolated data points referred to above, are clearly shown by the contour plot (Figure 4.48). Close to the endwall very high overturning angles are evident, owing to the action of the passage vortex (Figure 4.49) its effects can be seen to reduce as the blades are approached from the decreasing negative yaw angles.

A peak of static pressure coefficient is almost coincident with the passage vortex rotation centre (Figure 4.47 and 4.50). This peak represents a locally low value of static pressure. Owing to the higher cross passage velocity gradients upstream of slot 7 the correlation between maximum static pressure coefficient and the passage vortex centre is considerably reduced on the earlier traverse planes. Matching between wall and probe data appears to be good, however interpretation of the pressure field is eased considerably by the reduction in tangential pressure gradient since slot 6.

4.2.7 Loss Development and Vortex Migration Within The Blade Passage

A summary of loss core development is obtained by tracing successive positions of a loss contour through the passage (Figure 4.51). An arbitrary boundary to the loss region was selected as being the 0.5 total pressure loss coefficient contour on each traverse plane. The loss core formation due to the influence of the passage vortex is clear from the tangential migration and subsequent rolling up of the 0.5 contour. Blade passage convergence also influences the development of the loss core, however its effects are small when compared to those of the passage vortex. It is evident from the lower diagram that the locus of the loss core peak progresses around the passage vortex centre locus within the blade passage.

4.3 FLOW DEVELOPMENT DOWNSTREAM OF THE CASCADE

4.3.1 Traverse Slot 8

The traverse data points show some extrapolated data (Figure 4.52) in regions of high spanwise flow angle, associated with flow close to the blade suction surface. One manually interpolated data point was required due to an omission in the probe traversing.

A striking feature of the total pressure loss coefficient contours (Figure 4.53) are the high values associated with the blade wakes. Qualitative wake to wake repeatability appears to be good for both the five-hole and three-hole probe data. Slight quantitative discrepancies can be expected owing to the 15 mm tangential intervals between spanwise traverses being relatively large compared to the wake thickness (see also section 4.3.3). The area within the 0.5 contour, associated with the loss core, has both grown and undergone spanwise migration when compared to slot 7 data (Figure 4.45). Migration of the loss core is due to the action of the passage vortex. The high loss region, bounded by the 0.5 contour and formerly associated with the suction surface endwall corner, has undergone a shift in the negative tangential direction. Peak loss values in this area on the endwall however have remained on the wake centre-line. This suggests that the tangential shift is due to high overturning angles associated with the passage vortex now not bounded by the blade suction surface.

Total velocity defect within the blade wakes, associated with the blade boundary layers, is evident away from the endwall (Figure 4.54). Fluid within the loss core however has a similar total velocity to the mainstream, thus suggesting that it is an area of static pressure defect. Low velocity fluid is also associated with the loss region close to the endwall on the wake centre-lines, because it originated from swept up developing endwall boundary layer fluid.

Secondary velocity vectors indicate that downstream of the blade trailing edges the passage vortex remains a dominant feature of the flow field (Figure 4.55). A predominantly spanwise migration of the vortex centre is evident when compared with the slot 7 data (Figure 4.47). The loss area originating from the suction surface endwall corner shows itself as an area of much reduced secondary tangential velocity adjacent to the wall, approximately aligned with the blade wakes. Within this downstream development of the suction surface endwall corner loss region it is possible to define a small counter vortex. Its centre is defined from zero passage cross flow, derived from a combination of three-hole and five-hole probe data, and the zero spanwise flow angle contour very close to the endwall. The size and strength of this counter vortex, being much weaker than the dominant passage vortex, makes its precise definition difficult. Close to the endwall, at $T \simeq -210$mm, the secondary tangential velocity component is positive, and at $T \simeq -225$ negative providing further evidence that the five-hole probe identified a counter vortex in this area. Owing to the different relative positions of the spanwise traverses within the blade wakes, repeatability in the other wake is not as clear. Fluid shearing is also evident, owing to the interactions of the opposing spanwise velocity components on each side of the blade wakes. This phenomenon corresponds to the trailing shed and trailing filament vorticity of classical secondary flow theory. Close spacing of the spanwise angle contours (Figure 4.56), within the blade wake region, clearly shows the opposing spanwise flow directions, originating in fluid from each side of the blades. Yaw angle contours show underturning and overturning within the wakes (Figure 4.57). This was due to cross passage total velocity variation (Figure 4.46 and 4.54), and also the spacing of the yaw sensing holes in the probe tip responding to the high total pressure gradient.

Contours of static pressure coefficient show a region of low static pressure, bounded by the 3.0 contour, lying close to the area associated with the high total pressure loss coefficient (Figure 4.58). The point of maximum static pressure coefficient, i.e. minimum local static pressure, coincides closely with the passage vortex centre. Values on the blade wake centre-lines show a reducing trend towards the downstream stagnation value, this being difficult to define in a region of high total pressure loss coefficient gradient, and relatively high velocities downstream of the trailing edge.

4.3.2 Traverse Slot 9

The number of extrapolated data points has diminished since the previous traverse plane owing to reduction in the high spanwise flow angles (Figure 4.59). Two interpolated data points are evident due to omissions in the traversing.

Contours of the total pressure loss coefficient (Figure 4.60) show the same features as slot 8 (Figure 4.53). Continuing spanwise migration of the loss core, enlargement of the area enclosed by the 0.5 contour and broadening of the blade wakes are apparent. Skewing of the endwall loss region, in the negative tangential direction, is more distinct than at slot 8 owing to the spreading action of the unbounded passage vortex. The peak loss value within this area is still on the endwall close to the wake centre-line. However, three-hole probe results suggest that it is moving in the positive tangential direction towards the pressure surface giving continued evidence of a vortex counter to the passage vortex. Wake thickness variations are again due chiefly to the relatively coarse traversing increments used, although diffusion of the wakes since traverse slot 8 has reduced these differences slightly. The effects of interpolation within the contouring routine are evident giving apparent asymmetry of the two wakes.

Total velocity defect within the blade wakes does not appear as severe as at slot 8 owing to the mixing of mainstream fluid with the wakes (Figure 4.61). Only part of the loss core can be distinguished from the total velocity countours, indicating that it is of similar total velocity to the surrounding fluid.

Continuing predominantly spanwise migration of the former passage vortex centre is evident from the secondary velocity vector plot (Figure 4.62), when compared with the slot 8 data (Figure 4.55). The regions close to the endwall, corresponding to the small counter vortices apparent at slot 8, continue to be visible. Definition of their rotational centres however is not possible, owing to the spanwise flow angle data finishing 5 mm from the endwall (Figure 4.63), and the corresponding zero cross flow angle contours also existing very close to the endwall. It would appear that the measured spanwise and yaw angles (Figure 4.63 and 4.64) suggest the continued existence of these counter vortices. Reductions in spanwise flow angle maxima and minima are also evident from the contour plot, this being also reflected in reduced spanwise velocity components within the former passage vortex.

In the region of the loss core high static pressure coefficient values are apparent (Figure 4.65), with the peak value coinciding quite closely with the centre of the former passage vortex. The wakes identifiable in the slot 8 data (Figure 4.58) are not readily definable here, owing to the increased distance downstream from the blade trailing edge of this traverse plane. Rapid reduction in static pressure gradients away from the blade trailing edges is to be expected with the cascade exhausting to uniform atmospheric static pressure.

4.3.3 Traverse Slot 10

The experimental traverse points (Figure 4.66) show that no extrapolation from the probe calibrations were necessary in obtaining the flow data. This was the first traverse slot to be investigated and the five-hole probe was used very close to the wall. Significant Bernoulli surface distortion (Figure 4.67), resulting in varying spanwise total pressure gradients over much of the endwall are evident. The effect of this on the spanwise angle calibration of the five-hole probe is not easily quantifiable (compare with Slot 1 section 4.1) in the near wall region.

Continuing development of the total pressure loss coefficient field is evident (Figure 4.67). Diffusion has resulted in considerable wake broadening, and reduction in the corresponding peaks of total pressure loss coefficient. Repeatability of the wake regions appears improved, when compared with the slot 8 and 9 data (Figure 4.53 and 4.60). This is largely due to diffusion giving greater definition within the selected traverse increments. Passage vortex action is evident in the combination of spanwise and tangential migration of the 0.5 contour enclosing the loss cores. The high loss fluid near the endwall has undergone significant skewing, owing to the action of high overturning fluid close to the endwall, since the previous traverse plane. The peak loss values within these regions continue to be aligned with the blade wakes, based on the five-hole probe data. No evidence of an endwall boundary layer existing between the wakes is shown by the total pressure loss coefficient contours. The high tangential velocities, close to the wall, sweep the developing boundary layer into the endwall loss region. Spreading of these loss areas in the negative tangential direction and their increasing areas is due to this effect.

Definition of the wakes near midspan was felt to be dubious owing to the relatively large tangential traverse increments, consequently a midspan traverse with small increments in the wake was performed. Results from this show good wake definition when comparing the two total pressure loss coefficient data sets , however some definition is lost from the yaw angle results (Figure 4.68).

Decreasing wake total velocity defect, owing to their diffusion with the mainstream flow, is evident (Figure 4.69) when compared with data from slots 8 and 9 (Figure 4.54 and 4.61). Some of the loss core is again identifiable as an area of total velocity defect. The endwall loss area is quite well defined by a region of low total velocity, as it is chiefly composed of swept up endwall boundary layer fluid.

Secondary velocity vectors derived from the slot 10 traverse data (Figure 4.70) appear qualitatively similar to those existing at slots 8 and 9 (Figures 4.55 and 4.62). Considerable reduction in the range of spanwise flow angle, and the cross wake yaw angle variation are also apparent, owing to the reducing strength of the former passage vortex (Figures 4.71 and 4.72). These phenomena both show clearly on the secondary velocity vector plot. With extra vectors close to the endwall the increasingly high negative tangential velocities above the endwall loss region show clearly. This area also shows as reduced negative yaw angle close to the wall, with increasing overturning at larger spanwise co-ordinates.

The contours of static pressure coefficient show the fluid to be approaching the uniform atmospheric cascade exhaust condition (Figure 4.73). The peak value, corresponding to low static pressures, is close to the passage vortex centre.

4.3.4 Loss Development and Vortex Migration Downstream of the Blade Passage

Loss development downstream of the cascade is summarised in Figure 4.74, which shows the 0.5 total pressure loss coefficient contours for traverse slots 7, 8, 9 and 10.

Spanwise migration and growth of the loss core is evident, owing to the influence of the now unbounded passage vortex. Overturning of the fluid near the wall leads to a skewing of the loss region, formerly at the suction surface endwall corner. However at the wall no significant tangential translation occurs.

Continuing migration of the passage vortex centre occurs downstream of the blade trailing edges (Figure 4.75) almost coinciding with the point of minimum static pressure, as discussed previously. The locus of peak total pressure loss coefficient, corresponding to the upstream boundary layer fluid, is always outside that of the vortex centre. Tangential co-ordinates on the locus plot are referenced from the suction surface within the passage, and the apparent wake centre-line downstream of the blades. Consequently downstream of the trailing edges the tangential co-ordinate is somewhat uncertain.

4.4 MASS MEANED FLOW FIELD DATA

4.4.1 Mass Meaned Flow Field Data Upstream of the Cascade

The pitchwise mass meaned yaw angle upstream at midspan represents the two dimentional cascade inlet flow angle of 44.2^0 (Figure 4.76), being comparable with the design inlet flow angle of 42.75^0. This discrepancy is due to a combination of difficulties in fabricating the wooden cascade working section transition piece to a high angular accuracy, and problems associated with aligning the pressure probes with the zero yaw orientation. Agreement between five-hole and three-hole probe yaw angle data is good, suggesting that both probes are aligned to the same reference direction. Increasing yaw angle close to the endwall, corresponding to a decreasing overturning angle, is due to the leading edge vortex phenomenon (Section 4.1).

Upstream of the cascade the pitchwise mass meaned total pressure loss coefficient profile has similarities with that occuring for a flat plate boundary layer. The experimental profile (Figure 4.76) compares favourably with that derived for a power law profile having the same displacement thickness (δ^*) and area mass averaged total pressure loss coefficient ($\bar{\bar{\zeta}}$). Discrepancies are due to the pitchwise variation in flow parameters, three dimensional flows associated with the leading edge vortex, and the assumption that the data can be represented by a power law profile.

4.4.2 Mass Meaned Flow Field Data Within the Blade Passage

Flow angle data is presented here as overturning angle (relative to midspan) rather than yaw angle, enabling easier comparisons to be made within the curving blade passage.

The developing overturning angle profiles within the blade passage are presented in Figure 4.77. Only minor changes between Slot 1 (Figure 4.76) and Slot 2 are apparent, with a slight increase in overturning at about 15 mm from the wall for slot 2. Overturning at slot 2 is evident at spanwise locations from 7 mm to 150 mm, the underturning at the wall resulting from the leading edge vortex and its associated three dimensional flow.

High overturning close to the wall is apparent from the slot 3 data, owing to the gathering intensity of the passage vortex. Disagreement between the five-hole and three-hole probe data from the wall to 10 mm span is evident. The five-hole probe shows increasing overturning at the wall, but the three-hole probe shows a reduction followed by a rising trend. A region of developing underturning is perceptible between 60 mm and 140 mm from the endwall.

Dramatic overturning is evident from both slot 4 and slot 5 data. Increased underturning intensity is noticeable when compared with slot 3 data, due to the gathering strength of the passage vortex.

Decreasing overturning at the wall is a feature of slot 6 data, this being chiefly due to low turning fluid collecting in the suction surface endwall corner. Low turning fluid at the pressure surface has low axial velocity and consequently on a mass weighting basis has a much smaller effect on the pitch averaged data. Further from the wall a sharp reduction in overturning is followed by a distinct underturning peak.

Migration of the underturning peak in the spanwise direction is a feature of the slot 7 data, which otherwise shows the same trends as slot 6.

Development of the pitchwise mass meaned total pressure loss coefficient (Figure 4.78) shows the transition from a flat plate type boundary layer to loss core profiles occuring within the blade row.

Data from slot 2 and slot 3 show only detailed differences in shape, but an overall increase in magnitude when compared with slot 1 data. (Figure 4.76)

Formation of a loss peak is evident from slot 4 data, the slight rise close to the wall being due to the shape of the developing loss core under the influence of the passage vortex (Figure 4.24). Data from successive slots towards the trailing edge shows the loss peak broadening slightly with its magnitude remaining essentially constant. Spanwise migration of the peak loss is also a feature towards the cascade exit plane. Rising loss values close to the wall are due to the developing loss region in the suction surface endwall corner as the trailing edge is approached.

4.4.3 Mass Meaned Flow Field Data Downstream of the Cascade

Overturning angle data downstream of the cascade (Figure 4.79) shows both spanwise migration and increasing peak underturning away from the trailing edge plane. The effect of relatively low turning fluid in the suction surface endwall corner loss region is clear, causing a decrease in overturning close to the wall. Passage vortex influence causes this region to spread into the mainstream flow. This is evident from the increased underturning at the wall for slot 10 compared to slots 8 and 9.

Migration and spreading of the peak in mass meaned total pressure loss coefficient is evident (Figure 4.80), owing to the influence of the passage vortex. Spreading of the suction surface endwall corner loss region in the spanwise direction produces a double loss peak on both the slot 9 and slot 10 data. The two dimensional nature of the flow towards midspan reveals a total pressure loss coefficient equivalent to the blade profile loss. Slight differences in the profile loss are due chiefly to the relatively large tangential traversing increments used within the blade wakes.

4.4.4 Development of the Area Mass Averaged Total Pressure Loss Coefficient

A continuing growth in area mass averaged total pressure loss coefficient can be expected as air flows through the cascade blading and subsequently away downstream to atmosphere (Figure 4.81). The loss present at traverse slot 1 represents the total pressure defect within the upstream boundary layer, loss generation within the cascade being additional to this value. Travelling through the cascade the fluid experiences a gradual increase in loss from slot 2 to slot 5, however no increase in loss is apparent from the data between slots 5 and 6. Losses at slot 7 show a considerable increase over those at slots 5 and 6, since losses previously very close to the suction surface have been convected into the probe traverse range by the passage vortex. Sharp increases in loss are apparent across the blade trailing edges owing to the inclusion of the blade profile loss.

As both the inlet boundary layer and blade profile losses are not functions of the secondary flow, it is possible to deduct these values from the area mass averaged total pressure loss coefficient and obtain a notional "secondary loss". Values of this for the Durham cascade are plotted together with data derived from Langston et al (1977) and Marchal and Sieverding (1977) for comparison in Figure 4.82. The cases plotted show similar secondary loss development characteristics. Data from the Durham cascade however shows more rapid growth early in the blading compared to the others, followed by a period (from -60% to -30% axial chord) of reduced secondary loss generation. The high velocities associated with the passage vortex secondary flow phenomenon are not included as a loss, since they contribute to the local total pressure as measured by both five-hole and three-hole probes.

4.4.5 Cascade Mass Flow Rate

A useful check on both experimental method and integration techniques used in evaluating the pitchwise mass meaned data was obtained by calculating the mass flow rate through each traverse plane. Results are presented normalized using the mass flow calculated at slot 1 (natural inlet boundary layer) with data from Langston et al (1977) for comparison (Figure 4.83). Scatter on the natural inlet boundary layer data from Durham seems generally smaller than that on the Langston data.

4.5 DATA PRESENTED ON CONSTANT SPAN PLANES

Planes of constant span allow presentation of yaw angle and in plane velocities in an easily assimilable form using velocity vector plots. The path of the leading edge vortex travelling towards the suction surface can also be seen.

A typical distribution of data points (Figure 4.84) shows the axial location of each traverse plane and its tangential probe range in the cascade frame of reference. Extrapolated and interpolated data points can be identified from the experimental data point plots in sections 4.1, 4.2 and 4.3, thus avoiding a considerable number of very similar additional plots. Due to the regular spanwise traverse increments on each traverse plane, a large number of plots of each measured parameter were generated without the need for interpolation. A selection from these is presented emphasising various aspects of the flow field.

4.5.1 Total Pressure Loss Coefficient Data

The three-hole probe was traversed to within 1 mm of the cascade endwall. The path of high loss fluid, initially in the inlet boundary layer, towards the blade suction surface is evident (Figure 4.85). Fluid close to the blade pressure surface has a decreasing total pressure loss coefficient up to about 30% axial chord from the trailing edge. This is due to the passage vortex sweeping mainstream fluid towards the endwall, with consequent displacement of developing endwall boundary layer fluid towards the suction surface. A slight rise towards the pressure surface trailing edge, with the 0.05 contour meeting the blade again is evident here, and also noticeable in the slot 7 three-hole probe data (Figure 4.45). Interpolation within the contour plotting routine draws contours from the slot 8 wake data up the pressure surface to meet the data at traverse plane 7. This is not a real effect, since the losses associated with wake mixing are unlikely to occur very far upstream of the trailing edge. Flow visualization pictures on the endwall from other workers (for example Marchal and Sieverding (1977)) show no evidence of this upstream wake mixing. A continuing rise in total pressure loss coefficient along the suction surface is evident, except for an area near the leading edge associated with the suction side leg of the leading edge vortex. This continuing rise is due to high loss fluid within the pressure side leg of the leading edge vortex meeting the blade surface, and the subsequent development of a high loss region in the suction surface endwall corner. High gradients of total pressure loss coefficient within the blade wakes are apparent. Their large width is due to the build up of endwall boudary layer fluid as discussed in Section 4.3

Data 5 mm from the endwall shows qualitatively the same information as that previously discussed, with the advantage that five-hole (Figure 4.86) and three-hole (Figure 4.87) probe data can be compared directly. The continuing presence of a loss peak close to the pressure side of the leading edge is due to the extrapolation and interpolation necessary to complete a set of data for slot 2. These manipulations of the data within traverse plane 2 gave acceptable results (Figure 4.10), and should be recalled when interpreting this data. Pressure surface data exhibits only small differences when compared with data 1 mm from the endwall. A reduction in the maximum loss value on the suction surface is apparent, owing to the high spanwise gradients associated with the suction surface endwall corner loss region. Good repeatability between five-hole and three-hole probe data has been achieved on this plane.

Agreement between five-hole and three-hole probe pressure surface data is fair 20 mm from the endwall, although the position and magnitude of the peak loss values differ (Figure 4.88 and 4.89). Contour plotting interpolation problems within the wake region are much reduced when compared with the data 1 mm from the endwall (Figure 4.85), owing to the diminished wake effects at this spanwise position (Figure 4.53). Peak loss values towards mid-passage are due to the positive spanwise migration, out of the plot plane, of high loss fluid entrained in the passage vortex. Suction surface data shows an increasing loss coefficient towards a peak at about mid chord, the five-hole and three-hole probe results differing in its magnitude but not its position.

At 40 mm from the endwall the reducing loss towards the pressure surface trailing edge, due to passage vortex entrainment of mainstream fluid continues to be evident (Figure 4.90). The high loss area on the suction sufrace now close to the trailing edge, is composed of fluid within the loss core. An examination of the intersection of the 0.5 contour with the 40 mm constant span line reveals this at slot 7 (Figure 4.45). Similar pressure surface information is evident 60 mm from the endwall (Figure 4.91), with the peak loss on the suction surface approaching the trailing edge. Intersections of the 0.5 contour with the 60 mm constant span line on slot 8, 9 and 10 traverse plane data (Figure 4.53. 4.60 and 4.67) show this high loss area to be associated with the loss core. Reduction in its tangential width with increasing axial co-ordinate (Figure 4.91) indicates continuing spanwise migration out of the plot plane.

Loss reduction on the pressure surface 80 mm from the wall, owing to passage vortex action, is much diminished (Figure 4.92). Suction surface losses are now concentrated in the last 40% of axial chord before the trailing edge. Continual broadening of the wake, to traverse slot 10 represents the spanwise migration of the loss core out of the plot plane.

Losses within the blade passage diminish approaching the midspan plane (Figure 4.93, 4.94 and 4.95). Reducing wake thicknesses are also evident because the loss core is excluded from the plotting plane as it moves from the perspex endwall. The midspan plane (Figure 4.95) shows clearly the sharp increase in loss after the trailing edge plane, owing to the mixing of blade wake and mainstream fluid. Again contouring interpolations from the trailing edge along the pressure surface are evident.

4.5.2 Total Velocity Data

Contours of total velocity close to the endwall within the passage show a series of points of inflection (Figure 4.96). The locus of these points approximating to the endwall separation line found by other workers in their flow visualization studies (e.g. Marchal and Sieverding (1977)). Areas of velocity defect downstream of the trailing edge composed of the blade wake, and the suction surface endwall loss region are evident.

Agreement between five-hole and three-hole probe data 20 mm from the endwall appears to be good (Figure 4.97 and 4.98). Points of inflection on the contours are clearly visible on the three-hole probe data, but less distinctly for the five-hole probe data. A wake velocity defect reduction is also apparent when compared with data 1 mm from the wall (Figure 4.96).

Data 50 mm and 220 mm from the wall (Figures 4.99 and 4.100) show only differences in detail from that at 20 mm. Points of inflection on the passage contours are no longer evident. Distinct suction surface diffusion, beginning at about 40% axial chord from the trailing edge, is a feature of the flow further from the endwall.

A two-dimensional inviscid incompressible flow calculation was performed for comparision with the midspan flowfield data (Figure 4.101). Total velocity contours were scaled from the calculated Mach number distribution. Flow within the cascade ties up quite well qualitatively, although the predicted maximum pressure surface velocities are not reached until well into the downstream flow field. Agreement between prediction and experimental data is good in the upstream flow field, differences around the leading edge stagnation point arise as a result of computational difficulties necessitating special leading edge treatment. The flow field velocities far downstream of the trailing edge agree well, since an inviscid method cannot produce wakes of velocity defect fluid without an empirical loss generation mechanism.

4.5.3 Velocity Vector Plots

Vectors representing axial and tangential velocity components plotted on constant span planes give a convenient visualization of yaw angle and in plane velocity magnitudes.

In the absence of cascade surface flow visualization data velocity vectors 1 mm from the endwall are the closest available approximation to the endwall flows (Figure 4.102). Discontinuities in surface oil film flow visualization patterns of other workers (for example Marchal and Sieverding (1977) and Sjolander (1975)) trace the path of leading edge vortex separation along the endwall. Traverse data, with its discrete positioning away from the endwall, gave only a speculative approximation to this separation line. The similarity of this speculative separation line and flow visualization studies of others is encouraging. Data from slot 1 close to the blade leading edges suggests negative axial velocity. Although based on extrapolated probe data points they appear both qualitatively correct and repeatable. Results from Sjolander's (1975) oil dot flow visualization clearly show flow from the pressure to suction surfaces around the leading edge, with negative axial velocities. Close to the pressure surface, near the leading edge (data corresponding to slot 2), in plane velocities appear very low. This phenomenon was identified by Langston et al (1977) as a separation saddle point. Significant tangential velocities are evident at the slot 3 and slot 4 locations with very low axial velocities (some axial velocities from reliable probe data being negative). This flow sweeps developing endwall boundary layer fluid towards the suction surface. Flow away from the suction surface (under turning) is evident at the slot 6 and 7 locations, probably owing to a vortex rotating in the opposite sense to the passage vortex.

Planes 2, 3, 4, 5, and 7.4 mm from the wall (Figures 4.103 to 4.107) show similar features to those displayed 1 mm from the endwall. Flow at the slot 6 and 7 locations close to the suction surface show reducing underturning on planes 2 mm and 3 mm from the endwall, and increasing overturning corresponding to the 5 mm and 7.4 mm planes. Supportive evidence therefore is given to the existence of a suction surface endwall corner vortex, rotating in the opposite sense to the passage vortex. This counter vortex probably originates as the suction side leg of the leading edge vortex. Light sheet flow visualizations at at very low speed by Marchal and Sieverding (1977) show the existence of a counter vortex, close to the trailing edge plane. Their counter vortex however was centred between the passage vortex and midspan, about one third pitch from the suction surface. Close to the leading edge, on the pressure surface side, axial velocity increases with increasing distance from the endwall. High overturning fluid, towards the trailing edge, occupies less of the passage width with increasing spanwise co-ordinate. This reduction in overturning is due to the reducing tangential velocity components within the passage vortex, between its centre and the endwall. Reducing overturning 15 mm from the endwall (Figure 4.108) is due to the same effect. On planes 40 and 60 mm from the endwall however underturning is in evidence, since these planes cut the passage vortex between its centre and the midspan plane (Figure 4.109 and 4.110).

Increasing the spanwise co-ordinate of the viewing plane shows the vectors rapidly tending towards two-dimensional flow at midspan, data at

100 mm and 220 mm from the wall serve to illustrate this (Figures 4.111 and 4.112).

4.5.4 Flow Angle Data

As previously discussed (section 4.2) intersection of the zero cross flow and zero spanwise flow angle contours defines the passage vortex centre on a traverse plane. The zero spanwise angle contour, 20 mm from the wall, shows the approximate path of the vortex centre for much of the blade passage (Figure 4.113). Close proximity of contours around zero spanwise flow angle indicate the high cross passage spanwise velocity gradients associated with passing through the passage vortex. Action of the passage vortex, causing increasing overturning (increasing negative yaw angles) is evident on the pressure surface side of the zero spanwise angle contour (Figure 4.114). This can also be seen in the slot 5 secondary velocity vector plot (Figure 4.33), where 20 mm from the endwall, between the vortex centre and pressure surface, overturning is evident.

Spanwise flow angles at midspan are very low owing to the two-dimensional nature of the flow (Figure 4.115). The yaw angle distribution at mid span is given (Figure 4.116) for comparison with that existing 20 mm from the wall (Figure 4.114) and shows less cross passage variation. Cascade exit yaw angle at midspan appears reduced, owing to the high overturning present 20 mm from the wall (see also pitch averaged data Figures 4.77 and 4.79).

4.5.5 Static Pressure Coefficient Data

As a solid perspex window forms the cascade endwall, for eventual flow visualization purposes, no endwall static pressure tappings were available. The three-hole probe data 1 mm from the wall is assumed to give approximate endwall data, as for the velocity vector plots of section

4.5.3. Data thus obtained is comparable to the endwall data of other workers (Figure 4.117). The point of maximum static pressure coefficient occurs upstream of the trailing edge, corresponding to the slot 7 location (Figure 4.50) being surrounded by closed contours of increasing local static pressure. Similar findings were reported by Langston et al (1977) and Graziani et al (1980). Graziani et al (1980) present endwall data for two inlet boundary layer thicknesses (δ/H = 0.014 and 0.119) and show that by increasing the boundary layer thickness the point of maximum static pressure coefficient (using the Durham notation) moves away from the suction surface. The natural inlet boundary layer for the Durham cascade (δ/H = 0.254) is comparatively thicker, and continues this trend towards the pressure surface for approximately the same cascade turning angle. Sjolander (1975) using a lower turning annular nozzle guide vane cascade, (δ/H = 0.075), showed the point of maximum static pressure coefficient to be at the suction surface, about 40% axial chord from the trailing edge plane. Contours running from the suction to pressure surfaces (Figure 4.117) show a ridge of higher static pressure coefficient close to the path taken by the speculative leading edge vortex separation line (Figure 4.102). Endwall static pressure coefficient data of Langston et al (1977) also shows this phenomenon, and in their case supported by results of flow visualization.

The relationship between probe and blade surface tapping data can easily be examined on planes parallel to the cascade endwall. Contours of static pressure coefficient from the probe data are extended to the blade surface results on the following plots. The correlation between the two data sets is good, giving added confidence in the pressure probe data. Migration upstream of the peak static pressure coefficient is evident 20 mm from the endwall when compared with the near wall data (Figure 4.117, 4.118 and 4.119). Good agreement between the blade surface tapping values, and both three-hole and five-hole probe data was obtained on this spanwise plane. High static pressure gradients existing close to mid chord near the suction surface are evident. Increasing values of static pressure coefficient on the suction surface reflect the growing surface velocities, with the contour pattern in the passage still showing evidence of the leading edge vortex path.

Data 30 mm from the endwall shows similar trends to those discussed above (Figure 4.120), with the 4.0 contour now being attached to the suction surface at about 50% axial chord.

Considerable reduction of the area existing within the 4.0 contour has occured 60 mm from the wall (Figure 4.121), when compared with data 30 mm away. It now exists only on the blade suction surface tapping data. There is little change evident in contour patterns between the 60 mm and midspan planes except the continuing rise in pressure gradient close to the suction surface (Figure 4.122).

Agreement between the predicted inviscid incompressible two-dimensional static pressure coefficient distribution (Figure 4.123) and that measured at midspan appears to be good. Towards the trailing edge plane however, as the blade wakes and their associated phenomena are encountered the level of agreement reduces. Blade surface pressure tapping results also tie in quite closely with those predicted. (Compare Figure 4.122 and 4.123.)

4.6 DATA PRESENTED ON PSEUDO-STREAM SURFACE PLANES

In this section data from curved planes, approximating to spanwise projections of the midspan streamlines, is presented viewed in the tangential co-ordinate direction. A view in the spanwise direction on the two extreme planes illustrates their curved shape and defines their identification notation (Figures 4.124 and 4.125). Values downstream of the trailing edges were chosen to produce a satisfactory pseudo-streamsurface from the available data positions (Figure 4.84). The two sets of data points used in generating the plots close to the blade surfaces show the extrapolations and manual interpolations necessary to complete the probe data set. (Figures 4.126 and 4.127). Plots on other planes were formed from similar sets of data with less extrapolations, since the two shown are the worse cases.

Contour plotting was carried out using five spanwise strips individually in each plane, consequently solving the problems associated with a varying number of spanwise data points through the cascade. Small mis-matching of the contours therefore sometimes occurs at the junctions of these strips, with axial co-ordinates corresponding to the locations of traverse slots 2, 4, 6 and 9. As a contour plot is only a visual portrayal of data, it was felt that the time required to overcome these minor plotting problems could not be justified.

4.6.1 Total Pressure Loss Coefficient Data

Close to the pressure surface the action of the passage vortex is evident as it draws down mainstream fluid of low total pressure loss coefficient towards the endwall (Figure 4.128). Flow upstream of the leading edge shows the inlet boundary layer as previously discussed (Section 4.1). Upstream of the trailing edge interpolations within the contouring routine suggest the migration of wake characteristics, this is not a real flow effect (see section 4.5.1). Downstream of the trailing edge the loss core from the adjacent suction surface and its associated endwall loss region appears. This region seems to be quite small owing to a combination of its high skewing in the tangential direction and the traverse data positions downstream of the trailing edge chosen for the plotting plane.

Data on the adjacent pseudo-streamsurface (Figure 4.129) shows similar characteristics. The high loss at the wall just downstream of the leading edge is due to extrapolated data (Figure 4.9) and not loss generation in the tangential direction since the previous plane (Figure 4.128). Significant curving of the 0.1 contour close to the endwall, at about mid chord, shows the loss area associated with the upstream boundary layer beginning its migration in the spanwise direction. Fluid with negligible total pressure loss coefficient is evident within 5 mm of the endwall for the remaining 50% of axial chord. Downstream of the trailing edge the loss area from the suction surface endwall corner, defined by the 0.5 contour, has grown considerably, now appearing to be part of the wake at the slot 8 and 9 locations (Figures 4.53 and 4.60). Redistribution of high loss fluid within the wake is apparent, owing to this plot's proximity to the issuing passage jet.

Attachment of contours to the endwall region downstream of the leading edge, shows continuing flow of high loss inlet boundary layer fluid towards the suction surface (Figure 4.130). The blade wake is now only evident from the slot 10 data, owing to its increasing width resulting from mixing. Migration of the suction surface endwall corner loss region, originating in the adjacent passage, away from the endwall is also evident downstream of the blading.

The developing flow of high loss fluid from the pressure to suction surfaces is clear (Figures 4.131 to 4.134), this being accompanied by increasing total pressure loss coefficient fluid close to the endwall. The highly skewed loss area, originating from the adjacent passage suction surface endwall corner, (Figures 4.53, 4.60 and 4.67) appears to move further downstream of the trailing edge. Very little change in the 0.05 contour, about 100 mm from the endwall, is apparent from these plots. This suggests that little spanwise migration towards the endwall of mainstream fluid takes place around mid-passage.

Increasing spanwise migration of the high loss region, and its associated rising maximum value is evident as the suction surface is approached (Figures 4.135 to 4.139). Low total pressure losses, close to the endwall, are an indication of mainstream fluid entrained by the passage vortex. Close to the suction surface (Figure 4.139) however the developing loss region at the blade surface endwall junction is apparent. Bindon (1979) for his zero inlet skew case shows qualitatively similar data (17.5% gap from the suction surface) but with no region of lower loss close to the endwall. As discussed earlier (Section 4.2) losses generated close to the suction surface are fed into the traversed flow field by action of the passage vortex and suction surface separation line. The spanwise path taken by this higher loss fluid is evident from the plot (Figure 4.139).

A history of the loss core migration in the spanwise direction is shown clearly by successive tracings of the 0.5 total pressure loss coefficient contour (Figure 4.140).

4.6.2 Total Velocity Data

Contours of total velocity close to the pressure surface (Figure 4.141) show diffusion to about 75% axial chord from the trailing edge, followed by acceleration, which is essentially constant across the blade span. Adjacent to the suction surface acceleration to about 40% chord from the trailing edge is evident (Figure 4.142) followed by diffusing flow towards the trailing edge. Considerable spanwise variation is evident due to the three-dimensional nature of the flow close to the suction surface, induced chiefly by the passage vortex.

4.6.3 Velocity Vector Plots

Velocity vectors are plotted of axial and spanwise velocities, and consequently the changing tangential velocity component, due to high passage curvature, is perpendicular to the plotting plane and does not show. The considerable apparent decel eration towards the trailing edges on the following plots is due to an increasing tangential velocity component (Figures 4.141 and 4.142 show high total velocities at the trailing edge). If vectors representing the resolved axial and tangential velocity components and the spanwise velocities were plotted the visualization of true spanwise flow angles would have been impaired. The vectors plotted are those as seen by an observer looking in the tangential direction towards the suction surface.

Adjacent to the pressure surface high spanwise velocities towards the endwall are evident (Figures 4.143) close to the leading edge. The action of the passage vortex in entraining mainstream flow is clearly shown. Data on this pseudo-stream surface, in the absence of flow visualizations, is the closest to blade surface flow patterns available, and can therefore be compared with the flow visualization studies of other workers. Both Langston et al (1977) and Sjolander (1975), having performed pressure surface visualizations, state that they are essentially two dimensional and do not present them. Barber and Langston (1979), for the Langston et al (1977) cascade with a 'fully developed' inlet flow, show strong flows towards the endwall just downstream of the leading edge on the pressure surface, with some reverse flow present between the endwall separation point and the leading edge. Graziani et al (1980) present pressure surface flow visualization data for their thin boundary layer case showing only slight spanwise shifting of the limiting streamlines. A considerable area, from the two-dimensional stagnation line to about 30% axial chord from the trailing edge, was however left blank. Marchal and Sieverding (1977) show considerable flow towards the endwall close to the leading edge in their rotor blade oil film pressure surface flow visualization. Qualitative agreement between the Durham data, close to the pressure surface, and the flow visualization studies of other workers appears good.

Spanwise velocities towards the endwall reduce in magnitude with the plot plane nearing mid-passage and the passage vortex centre (Figures 4.144 to 4.148). The vectors show flow rising over an area close to the endwall, corresponding to the path of the passage vortex endwall separation line. Approaching the suction surface increases the definition of flow deflection over this area (Figures 4.149 and 4.150). Increasing spanwise velocity components away from the endwall are evident on approaching the suction surface owing to the passage vortex. (Figures 4.151 to 4.153). Data close to the suction surface (Figure 4.154) shows good qualitative agreement with surface flow visualization of many other workers (for example Sjolander (1975), Marchal and Sieverding (1977), Barber and Langston (1979) and Graziani et al (1980)). A speculative suction surface separation line, based on the centres of developing losses measured adjacent to the wall (Section 4.2.5) is shown, and comparisons with available flow visualization data suggest this to be reasonable. Strong spanwise flows away from the endwall, beginning at about 50% axial chord are a characteristic feature following the speculative suction surface separation line. Both Barber and

Langston (1979) and Graziani et al (1980) show that with increasing inlet boundary layer thickness the intensity of spanwise flows also increases.

4.6.4 Flow Angle Data

No contour plots of spanwise angle are presented in this section, since these can be visualized from the vector plots (Section 4.6.3). Part of the zero cross flow angle contour is shown on those of yaw close to the suction surface (Figure 4.155), flow overturning occurs on the endwall side of this line owing to the passage vortex. Flow visualizations of suction surface flow by other workers (Section 4.6.3) show a separation line running up the blade surface from about 50% axial chord. This separation line can be estimated from both the extra loss generation on the suction surface (Section 4.2.5) and the zero cross flow condition (Section 4.2). On approaching the suction surface zero crossflow lines move away from the endwall (Figure 4.156). Following this trend it is anticipated that subsequent flow visualization studies on the Durham cascade will reveal a blade suction surface separation line, occuring on the midspan side of the zero cross flow line nearest the blade. The separation line suggested from the two methods coincides quite closely (Figures 4.154 and 4.156).

4.6.5 Static Pressure Coefficient Data

Adjacent to the pressure surface, contours of static pressure coefficient show only small changes with span (Figure 4.157). High negative values, tending to the stagnation value of -1, close to the leading edge indicate the low total velocity of the flow. Data on this plane is comparable to the blade surface tapping data (Section 4.7.2).

Only small changes in the contour pattern at approximately mid-passage are evident, (Figure 4.158) the most significant being the general increase in static pressure coefficient, resulting from the higher total velocities. At the trailing edge a spanwise gradient exists, with higher static pressure coefficient values, i.e. lower local static pressure, at the endwall. At about 75% axial chord from the trailing edge, close to the endwall, a considerable loop in the zero contour is evident almost enclosing a region of higher static pressure coefficient. The velocity vector plot (Figure 4.148) clearly shows flow into this region from the surrounding higher pressure area close to the endwall.

A region of high static pressure coefficient away from the endwall close to the trailing edge is seen to form as the suction surface is approached (Figure 4.159). This area becomes elongated, moving upstream and towards the endwall (Figure 4.160), nearer to the suction surface, and corresponds to a region of high total velocity within the developing loss core (Figure 4.137). At its inception this high static coefficient value is coincident with the maximum total pressure loss coefficient within the loss core (compare Figure 4.159 and 4.135 also Figure 4.160 and 4.137). Adjacent to the wall however strong spanwise flows associated with the passage vortex have moved the maximum loss core values slightly away from the point of maximum static pressure coefficient (Figures 4.161 and 4.139). Contours close to the suction surface can be compared with static pressure coefficient data, obtained from surface tappings (Section 4.7.2).

4.7 BLADE SURFACE PRESSURE DISTRIBUTION DATA

Blade surface static pressure distribution data was obtained from sets of surface static pressure tappings cast into two cascade blades. Each set of tappings in the traversed cascade passage consisted of eleven pressure surface and fifteen suction surface points. As a check on periodicity four points on the adjacent pressure surface and five on the suction surface were provided. (Figure 3.2)

4.7.1 Data on Constant Span Planes

Data from the traversed blade passage and the adjacent blade tappings shows good agreement (Figure 4.162). Within this section therefore only data from the traversed passage is presented, thus avoiding the presentation of excessive numbers of data points.

Considerable variation in the blade static pressure coefficient with span is evident, owing to the three-dimensional nature of the flow, particularly on the suction surface (Figure 4.163). A two-dimensional prediction is also shown for comparison with the midspan data. Qualitative agreement on the pressure surface with the prediction appears to be good, although the experimental midspan data shows consistently higher negative local static pressure coefficient. Suction surface agreement at midspan is again qualitative with a consistently lower local static pressure coefficient close to the trailing edge. Flow on the pressure surface shows only small changes from the near wall planes to midspan, implying a largely two-dimensional flow pattern around the blade profile.

Close to the endwall the suction surface static pressure coefficient distribution shows only a very gradual rise to a peak about 40% axial chord from the trailing edge. Planes further from the endwall show increasing peak values moving towards the leading edge. Static pressure coefficient distribution near the trailing edges rises with increasing span to a maximum at 41 mm from the endwall (9% span, 0.35 δ), and then falls towards midspan. This reduction is associated with the loss core. Blade static pressure coefficient data of Langston et al (1977) exhibits similar trends, however his data at 12.5% span (1.05 δ) from the wall shows a peak value of similar magnitude to that occuring at midspan, but much closer to the trailing edge (30% instead of 55% axial chord). The Durham data does not show this phenomenon, possibly because spanwise increments of pressure tapping spacing are rather too large. However probe data close to the wall (Figure 4.161) suggests a peak value of approximately 4.3 about 30 mm (6.5% span, 0.26 δ) from the wall and 30% axial chord from the trailing edge.

4.7.2 Data on Blade Surfaces

Blade surface static pressure tappings are at spanwise increments between both endwalls, and in this section are normalized to lie between the perspex endwall and midspan. This provides a check on flow symmetry about the midspan plane. At these normalized points (Figures 4.164 and 4.165) small discontinuities in the contours occur owing to slight asymmetry about the midspan plane. Data from both the traversed passage and adjacent blade surfaces is presented by plotting contours using the mean value at geometrically similar points.

Blade pressure surface data shows a predominantly two dimensional static pressure coefficient distribution (Figure 4.166). Values tend towards the stagnation condition (-1) close to the leading edge. The two closed contours upstream of the -0.9 contour arose from a spurious data point. Qualitative agreement between blade surface and probe data is good (Figure 4.166 and 4.157). Apparent downstream migration of the wall contours at midspan is due to the static pressure gradient between the wall and the probe data (Figure 4.122).

A much more complex static pressure coefficient distribution is evident on the suction surface (Figure 4.167) due to the passage secondary flows. Data close to the suction surface (Figure 4.161) exhibits qualitatively similar characteristics, differences arising due to the high cross passage pressure gradients (shown clearly by the close contour spacing in Figure 4.122). The high static pressure coefficient region associated with the loss core (Figure 4.161), encircled by the 4.0 contour, is not apparent from the suction surface data. This suggests that the point of minimum static pressure is not attached to a solid boundary and is maintained by the passage vortex action. Contours of blade surface static pressure coefficient are presented in Graziani et al (1980) showing similar characteristics to those discussed here.

Matching of probe and wall values of static pressure coefficient is also discussed on each of the passage traverse planes (Section 4.2) and on spanwise planes (Section 4.5.5).

CHAPTER 5

NATURAL INLET BOUNDARY LAYER

HOT WIRE PROBE RESULTS

5.1 UPSTREAM MEAN FLOW DATA

A sparser grid of experimental points was employed for the hot wire probe data (Figure 5.1) than that used for the five-hole probe. Hot wire probe traversing time was thus economised involving only minor additional errors in interpreting the results. These additional interpretation errors are small, owing to the predominantly tangential variation of flow field velocities away from the near wall region, evident from the pressure probe data (Section 4.1).

Total velocity results derived from both hot wire and pressure probe data are directly comparable. (Figures 5.2 and 4.7) Qualitatively the two sets of results appear very similar, both showing the upstream influence of the blade leading edges, and their interactions with the upstream endwall boundary layer. Lower velocities are shown over the whole of the traverse plane by the hot wire probe results when compared with the five-hole probe data. Differences are chiefly due to problems of hot wire sensor contamination and assumptions made in analysing their results. Owing to these difficulties it seems likely that the hot wire probe data will be less reliable than that obtained from the pressure sensing probes. The hot wire data was re-analysed using the wire sensitivities as measured after the traversing. However this gave only minor changes to the total velocity distributions and magnitudes. Contours of total velocity were obtained using the arithmetic mean of the two $\bar{u}$ values measured at each traverse point. Typically these two values were within about five percent of each other.

Qualitative agreement between the yaw angles derived from both hot wire and pressure probe data is good (Figure 5.3 and 4.4). The hot wire data however shows a consistently lower yaw angle than that measured by the five-hole probe. As the same hot wire probe was used for all the yaw traversing on this plane the discrepancy could be due to a misalignment during the calibration, although it would be difficult to account for about ten degrees difference in this manner. Repaired probe geometry was sometimes doubtful, with the prongs being at different angular settings from the standard stem datum. These geometric changes are possibly a contributory factor in the measured yaw angle differences. Replotting the yaw angle data, using the hot wire sensitivities obtained from recalibration after traversing in the analysis, exhibited negligible qualitative differences, but produced a general yaw angle increase of about 2^0. Close to the wall however the revised sensitivities suggested two wildly uncharacteristic yaw angle values which were assumed to arise from computational difficulties. The pitchwise mass meaned yaw angle distribution of the slot 1 data shows good qualitative agreement obtained between the three sets of results and the possible midspan differences (Figure 5.4). Revising the sensitivity clearly affected the quantitative values of the results slightly.

Agreement between streamwise spanwise flow angles measured by the hot wire and five-hole probes is poor (Figures 5.5 and 4.3). The hot wire probe results show a region of increased negative streamwise spanwise angle towards the blade pressure side of each leading edge, in common with the pressure probe data. Angle magnitudes appear to have an offset of about 5^0, and a gain of approximately 2 when compared with the pressure probe data. Revision of the hot-wire sensitivities to their values when recalibrated after traversing changed the streamwise spanwise angle data considerably (Figure 5.6). Unlike the hot wire yaw angle, where positioning to the five-hole probe yaw angle ensures the wires are sensing similar cooling velocities, the streamwise spanwise angle results are derived from differing wire cooling velocities. Consequently differential changes in sensitivities between the two wires, caused by fouling, produces considerable changes in the evaluated streamwise spanwise angles. Considerable differences in the sensitivities of some XZ wires occurred, about 25% for one wire with negligible change in the other, during the traversing of this slot due to probe fouling.

Cascade inlet mass flow rate evaluated from the hot wire probe data appears in close agreement with the pressure probe data (Figure 4.83).

5.2 UPSTREAM FLUCTUATING FLOW DATA

Turbulent kinetic energy contours show an increasing trend on approaching the endwall (Figure 5.7). An increasing turbulent kinetic energy is evident, despite experimental scattering, towards mid-passage, reducing slightly in front of the blade leading edges. This phenomenon probably is due to action of the leading edge vortex, upstream of the blades, drawing in mainstream fluid of lower turbulent kinetic energy. Contours of turbulent kinetic energy obtained using the hot wire sensitivities obtained after traversing show only minor differences when compared to those discussed above.

Differences in the turbulence intensity contours, for each of the three local wire co-ordinate directions, show the upstream turbulence to be non-isotropic (Figures 5.8, 5.9, and 5.10). As discussed previously (Section 3.7.3) mean values of $\overline{u'^2}$ were used in presentation, the differences between the two values ($\overline{u'^2}_{XY}$ and $\overline{u'^2}_{XZ}$) were assumed due to differences in XY and XZ probe positions, probe fouling and experimental errors. Pitchwise mass meaned turbulence intensity data also clearly shows this effect (Figure 5.11). Close to midspan both the streamwise and perpendicular to the endwall pitchwise mass meaned turbulence intensity components agree closely, the component parallel to the endwall however shows considerably higher values. Turbulence intensity levels in the mainstream of 2% to 5% are quite high, being due chiefly to the honeycomb flow straightener at entrace to the wind tunnel working section. Two of the turbulence intensities exhibit a slight peak away from the endwall, with a much larger peak occuring for the third, perpendicular to the wall component. Accurate evaluation of the integrals occuring within the pitchwise mass meaning procedure was difficult, because of the relatively coarse spanwise data definition used here.

Pitchwise mass meaned turbulence data, evaluated using the end of run wire sensitivities shows a slight general increase but little qualitative difference to that referred to above (Figure 5.12). It is evident from the two sets of pitchwise mass meaned data that revising the hot wire sensitivities influenced the turbulence data only slightly.

Hinze (1959) shows the relative turbulence intensity data of Klebanoff (1954) from the two dimensional boundary layer along a smooth wall with zero pressure gradient. The pitchwise mass meaned turbulence intensity data from the Durham cascade inlet flowfield approximates to these conditions. The published data is for a much lower mainstream turbulence intensity (of about 1%), with a boundary layer thickness Reynolds number of about half that existing at the cascade inlet. Both sets of data show rising relative turbulence intensities approaching the wall. However at about 15% of the boundary layer thickness Klebanoff shows, as one would expect, a falling off of the turbulence component perpendicular to the endwall, where the Durham data does not. The turbulence intensity levels of the Durham data are higher throughout the boundary layer than those of Klebanoff. Data for a rough endwall, after Corrsin and Kistler (1954), is also given by Hinze (1959), for conditions similar to those of Klebanoff but with a higher wall shear stress. Although this data shows higher turbulence levels than Klebanoff the Durham data is higher still, but qualitatively similar.

The pitchwise mass meaned distribution of turbulent kinetic energy exhibits low levels close to midspan (Figure 5.11). A peak is evident at about 60 mm from the endwall, broader than those for the individual turbulence intensity peaks due to summing of their individual effects. An increasing trend in kinetic energy of turbulence is apparent 5 mm from the endwall. Distribution of turbulent kinetic energy, normalized using the wall friction velocity, within a two dimensional boundary layer is also given by Hinze (1959), after Klebanoff (1954). This shows a continually increasing turbulent kinetic energy through the boundary layer, with a rapid rise close to the wall and a slight change in slope at 30% of the boundary layer thickness. The peak detected in the Durham data, occurs at about 45% of the boundary layer thickness, being much more obvious than the change of slope shown by Klebanoff.

At cascade inlet the measured normalized turbulent shear stresses are small (Figures 5.13 and 5.14). measurement errors are probably large compared with the low shear stress levels, leading to the scattering of the zero contour of Figure 5.14. The shear stresses appear to be increasing as the wall is approached.

5.3 DOWNSTREAM MEAN FLOW DATA

Hot wire probe data downstream of the cascade, at traverse slot 8 was obtained on the same data grid as the five-hole probe (Figure 5.15). Since differences in data for slot 1 (Sections 5.1 and 5.2) using both as new and run end sensitivities were quantitative, and not qualitative, slot 8 data was analysed using the as new wire sensitivities obtained at initial calibration. Problems within the second order analysis after Gregory-Smith (1982ii) for the mean velocity components were experienced, owing to experimental scattering causing convergence to the incorrect solutions. Data presented for traverse slot 8 was therefore derived from the first order analysis, thus avoiding these difficulties. Evaluation of the mean streamwise velocity component, in the first order analysis, generally gave two possible roots of similar values, the mean of which was used for the plots. In some instances however experimental scatter on the data gave one imaginary root, in such cases the other value was used. At one point (-0.8 tangential, 75 spanwise), two imaginary roots were obtained, linear interpolation between points on the same spanwise traverse was used in obtaining the data for the plots.

Again mean total velocity data from the hot wire probe (Figure 5.16) and the five-hole probe (Figure 4.54) are directly comparable. Both sets of data show at midpassage, towards midspan, similar velocity magnitudes with greater wake velocity defects being shown by the hot wire probe data. Wake to wake repeatability of the hot wire probe data is clearly worse than that obtained from the five-hole probe. Close to the endwall, near the wake centrelines, agreement between the two hot wire wakes appears to be good, and also shows qualitative similarities with the five-hole probe data. The width of the two hot wire wake regions and their respective velocity gradients, appears to differ considerably near midspan. These differences can be attributed chiefly to the relative positions of probe traverses within the blade wakes. Similar differences are also visible in the five-hole probe data close to midspan. On the suction surface side of the blade wake (at about -165 mm tangential co-ordinate) the hot wire data shows a region of velocity defect not seen in the pressure probe traverse data. This region of relatively low total velocity corresponds to a break in traversing and subsequent restarting after cleaning the probe tip in acetone and recalibrating (Figure 5.15). Probe sensor contamination and subsequent cleaning clearly influenced probe performance even after the recalibration. Data from the suction surface side of the adjacent wake, although incomplete, does not show this trend, but rather an increasing velocity towards midpassage.

Yaw angle data derived from the hot wire probes (Figure 5.17) shows fair agreement with that obtained from the five-hole probe (Figure 4.57). A discontinutity in the yaw angle contours, close to midpassage, is due to a change of probe owing to breakage (see Figure 5.15). This probe, which had been previously repaired, had an angular offset to the prongs of some five degrees, with respect to the stem datum. Taking this factor into account, the yaw angle data between the wakes, from both probe types, is seen to be in fair agreement. Data within the blade wakes shows differences which can be attributed to having few tangential traverses in a region of high yaw angle gradient, which also emphasizes any slight disparity in probe position between the two data sets. Pitchwise mass meaned hot wire yaw angle data shows reasonable agreement with the pressure probe results (Figure 5.18), differences between the two sets of data arise from the factors previously discussed. The necessary change of probe increased the negative yaw angles in a region of relatively high axial velocity and consequently produced an influence on pitchwise mass meaned values.

Spanwise flow angle data derived from the hot wire probe traverses is directly comparable with that obtained from the five-hole probe (Figure 5.19 and 4.56). The hot wire probe data shows spanwise bands of contours corresponding to probe traversing sessions (Figure 5.15). This could be partly due to variations in contact resistance within the probe mount, since the probe sensing tip was removed after each traversing session although this seems unlikely. Fouling of the probes during use was a problem which influenced the sensor sensitivities. The band of contours at about −165 mm tangential co-ordinate was obtained after the probe was cleaned in acetone. Fouling obviously reoccurred since at the next spanwise traverse the sensitivities had changed, judging from the contour map. Agreement with the suction surface side of the wake regions from the five-hole probe data is reasonable. Hot wire probe wake to wake repeatability is also encouraging in this region.

Cascade exit plane mass flow is significantly less than that evaluated from the pressure probe data (Figure 4.83), however the generally lower measured total velocity (and by implication axial velocity) would account for this.

5.4 DOWNSTREAM FLUCTUATING FLOW DATA

The distribution of turbulent kinetic energy downstream of the cascade is evident from the contours (Figure 5.20), which show higher values close to areas associated with the total pressure loss core. Values within the wake areas show an increase when compared to the midspan mainstream flow, however differences are evident between the two wakes. These wake discrepancies can be accounted for in a similar way to that discussed for the mean flow data (Section 5.3). Repeatability between regions away from influences of the blade wakes appears to be fair in the adjacent passages. The core on the suction surface side of the blade wakes, consisting of high turbulent kinetic energy fluid, exists in the total pressure gradient associated with the loss core (Figure 4.53). These peak turbulent kinetic energy values are practically coincident with both the passage vortex centre (Section 4.3.1 and Figure 4.55) and the minimum local static pressure (Figure 4.58). Higher turbulent kinetic energy fluid has been concentrated in a region of low static pressure by the action of the passage vortex. Fluid of relatively low turbulent kinetic energy, originally in the meanstream flow, has been drawn close to the endwall. Downstream over most of the traverse plane, the turbulent kinetic energy exists at a higher level than that upstream. Normalizing the data with respect to the bulk exit dynamic head divides the contour values plotted by the square of the velocity ratio of the cascade (about 3.5). It is evident that, normalized using inlet or exit dynamic head, a considerable increase in the turbulent kinetic energy of the flowfield has occurred, indicating a growing energy deficit from the mean flow. Data from this study shows low turbulent kinetic energy occuring in midpassage close to the blade exit plane. Taylor et al (1982) for a strongly curved duct, with no bulk fluid acceleration or leading edge phenomena, show maximum turbulent kinetic energy occuring near to the duct centre 2.5 hydraulic diameters downstream of the bend. A direct comparison between the Durham data and that of Taylor et al is difficult, owing to the many differences in the flow regimes; however the two studies appear to be in disagreement.

Turbulence intensity data for each of the three measured components show remarkable similarities (Figure 5.21, 5.22 and 5.23). At cascade exit therefore the turbulence appears to be approximately isotropic. Each of the respective contour plots shows low free stream intensities close to midspan, increasing within the blade wakes. Areas of highest intensity occur at the passage vortex centre location, with a ridge of high values connecting to the blade wake, corresponding to high secondary velocity areas. Poor wake repeatability, as with the mean flow data, again is a feature of the midspan flow field. Mean $\overline{u'^2}$ data is plotted here, again differences are assumed due to probe positioning, sensor contamination, and experimental data scatter. Contours are plotted normalized using the upstream velocity, for data based on the local bulk exit velocity values should be divided by the cascade velocity ratio (about 1.87). Davino and Lakshminarayana (1982), for a compressor rotor, show the wake effect on the three turbulence intensities exits for about 10% of the blade pitch in the tangential direction. Assuming that the Durham data is of a similar nature, then with traversing at 15 mm increments in the tangential direction over a blade pitch of 191 mm, failure to obtain repeatable wake data was very likely.

Bailey (1980) investigating a turbine nozzle cascade at low speed using laser Doppler techniques measured mean and turbulent velocities on a series of equi-potential planes. In his experiment the leading edge vortices were suppressed since only one blade passage was used, bounded upstream by the wind tunnel walls. On each plane three spanwise traverses were made obtaining the data. The plane closest to the trailing edge extended from about 28% axial chord on the suction surface to 8% axial chord on the pressure surface. Data from this plane showed very low turbulence intensities and Bailey concluded that large regions of the passage vortex were non-turbulent. Durham cascade data shows, for the turbine rotor blade considered, high turbulence fluid exists within most of the total pressure loss core. The Durham cascade measurements also included the blade wakes but it seems likely that the qualitative nature of the loss core flow would not have changed significantly due to their inclusion.

Taylor et al (1982) in their curved duct found anisotropic turbulence at exit. This was characterised by high streamwise turbulence intensities close to the suction surface and high cross passage intensities at the pressure surface. In their study Taylor et al used a duct with no nett fluid acceleration, and obviously no leading edge vortex phenomenon as in the rotor cascade tested at Durham.

Pitchwise mass meaned turbulence intensity data shows the high degree of isotropy present in the flow at cascade exit (Figure 5.24). Peak values in each of the curves coincide at about 50 mm from the endwall. These peaks occur closer to the wall than for the pitchwise mass meaned total pressure loss coefficient peak at slot 8 (Figure 4.80). Owing to the largely isotropic nature of the turbulence at cascade exit the pitchwise mass meaned turbulent kinetic energy follows a similar form to that shown by the turbulence intensity values. (Figure 5.25) The pitchwise mass meaned turbulent kinetic energy distribution is of similar form to the pitchwise mass meaned total pressure loss coefficient data at slot 8. The turbulent kinetic energy also represents an energy loss from the mean flow field and modelling of turbulence, although highly complex, would lead to predictions of blade row exit losses.

Turbulent shear stress data, from the streamwise and normal (tangential) turbulence correlation, shows high values within one of the blade wakes (Figure 5.26). Repeatability of the wake regions is again poor, owing to the relatively large tangential traversing increments used with the shear stress variation of one wake missing completely. Both Davino and Lakshminarayana (1982) for a compressor rotor and Hah and Lakshminarayana (1982) for an isolated aerofoil show this shearing stress within the blade wakes to be very localized, consequently at Durham these values were not fully observed. The high values of turbulent shear stress in the blade wake coupled with the high local velocity gradients, normal to the streamwise direction, indicate work done by the mean flow fluid against the shear stress. This phenomenon represents a loss of energy from the mean flow, but a gain by the turbulent flow. Evidence of a shear stress reversal across the loss core area is shown by the migration of the zero contour away from the wake region. Similar features are shown by data from the missed wake loss region, although the stress reversal is not apparent owing to the limited tangential traversing range. Taylor et al (1982) in their curved duct, using Laser Doppler techniques, found high streamwise-normal shearing stresses close to the pressure surface and the duct sidewalls on successive streamwise planes. The results presented here do not show particularly high shearing stresses close to the downstream projection of the blade pressure surfaces.

Improved wake to wake repeatability is shown by the streamwise-spanwise turbulent shear stress (Figure 5.27) when compared with the other shear stress data. Measured values within the blade wake areas do not show significant changes when compared with the remainder of the traverse plane. The mean flow data shows opposing spanwise flows on either side of the blade wakes. A reversal in shear stress again is shown across the loss core area. The zero shear stress contour, at about mid passage, follows the zero spanwise angle contour quite closely (Figure 4.56) and consequently passes close to the passage vortex centre. Higher magnitude shearing stress values close to the suction surface side of the blade wakes, within the loss core areas, reflect the higher secondary velocities occuring within this region. Taylor et al (1982) show data for this shearing stress in their curved duct, with high values occuring within the mainstream flow, the Durham data however does not show this.

CHAPTER 6

VARIABLE INLET BOUNDARY LAYER PRESSURE PROBE RESULTS

The cascade inlet boundary layer thickness was varied by using an upstream fence, or an air bleed. Thickening of the boundary layer was achieved by using a fence of triangular castellations positioned in the wind tunnel working section. Spacers inserted between the cascade transition piece and wind tunnel working section allowed a bleed off of fluid from within the boundary layer thus thinning it.

6.1 INLET FLOW FIELD

Identical meshes of five-hole probe data points were used for both the thickened and thinned boundary layers (Figure 6.1), the meshes being slightly different to that used for the natural case (Figure 4.1). Data was taken from 5 mm off the wall to midspan using the five-hole probe, with the three-hole probe traverse points located as before (Figure 6.1 shows the thickened boundary layer experimental grid).

Total pressure loss coefficient contours show the change in boundary layer thickness clearly (Figure 6.2 and 6.3), both exhibiting the same characteristics as those for the natural inlet boundary layer (Figure 4.2). Slight depressions of the contours, corresponding to the upstream influence of the leading edge vortex, are apparent; this phenomenon being more noticeable for the thinned boundary layer data. The contours exhibit a slightly increasing trend in boundary layer thickness with increasing tangential co-ordinate for the thinned case, owing to continuing development of velocity profile downstream of the bleed slot. Thickening of the inlet boundary layer appears to have influenced the data more than thinning, when compared with the natural inlet velocity profile data.

As for the natural boundary layer case (Figure 4.3) regions of high streamwise spanwise flow angle are again in evidence each side of the 44° yaw angle contour (Figures 6.4 and 6.5). The action of the leading edge vortex in drawing down mainstream fluid towards the endwall is shown for all three boundary layer cases. The influence of the leading edge vortex on spanwise flows appears to extend further into the mainstream with increasing boundary layer thickness, as expected.

Yaw angle data for thickened and thinned inlet boundary layer cases, are both qualitatively and quantitatively similar to that presented for the natural case (Figure 4.4), and are not presented here. Similarly both the secondary velocity vectors and the static pressure coefficient data shows little variation with inlet boundary layer thickness.

Contours of total velocity show that the increasing inlet boundary layer thickness reduces velocities further into the mainstream (Figure 6.6, 6.7 and 4.7). The upstream effect of the leading edge is also evident from the reducing total velocities as stagnation conditions are approached.

6.2 FLOW DEVELOPMENT DOWNSTREAM OF THE CASCADE

6.2.1 Traverse Slot 8

Thickened and thinned inlet boundary layer experimental data at slot 8 was collected on the same grid as that used for the natural boundary layer case (Figure 4.52). Slightly more extrapolations from the five-hole probe calibration were necessary with the thickened inlet boundary layer, owing to the higher spanwise flow angles present at the cascade exit close to the trailing edges.

The total pressure loss coefficient contour plots all show the same characteristics, a double peaked loss core, a suction surface endwall corner loss region and blade wakes (Figure 6.8, 6.9, and 4.53). Increasing the inlet boundary layer thickness enlarges the loss core area, owing chiefly to the increased mass flow in the upstream boundary layer. (Figure 6.10). The loss core from both the natural and thinned inlet boundary layers exhibit strong similarities with each other, probably because their inlet total pressure loss coefficient profiles were similar (Section 6.1). Losses occuring at the suction surface endwall corner seem little influenced by inlet boundary layer thickness, owing to their composition from the developing endwall boundary layer. Thickened and thinned inlet boundary layer data both show higher peak losses existing within the blade wakes than for the natural boundary layer case. In an area of high tangential total pressure gradient, such as the blade wakes, slight differences in probe positioning can lead to apparent mismatching of the data, highlighted by the contouring process. Loss core centres for the two modified boundary layers, being coincident with each other, are both nearer the passage centre when compared with the natural inlet conditions. (Figure 4.75).

As only small quantitative differences are discernable in the total velocity data for the three inlet boundary layer conditions at slot 8 the data is not presented here (Figure 4.54 shows the data for the natural inlet boundary layer).

Secondary velocity vector plots for both the thickened and thinned inlet boundary layers show the same features observed for the natural inlet conditions (Figure 6.11, 6.12 and 4.55). Spanwise flows for the thickened inlet boundary layer appear stronger than for the other cases. Passage vortex centres for the thick and thin inlet boundary layer data are situated closer to the passage centre than for the data corresponding to natural inlet conditions (Figure 4.75). It is possible to define a counter vortex close to the endwall near each blade wake for both thickened and thinned inlet boundary layer data. These were defined from zero crossflow, derived from both three and five-hole probe data, and zero spanwise flow angle contour intersections as for the natural inlet boundary layer case (Section 4.3.1). These vortices are much weaker than the dominant passage vortex.

Reducing static pressure coefficient values are evident in areas associated with the blade trailing edge (Figures 6.13 and 6.14). Slightly higher values are shown by the thickened boundary layer data when compared with the natural case (Figure 4.58). Thinned boundary layer data shows large quantitative differences with the other sets of data, due chiefly to the difficulty in defining a satisfactory inlet static pressure to the cascade with the reference probe being some distance upstream of the bleed slot.

6.2.2 <u>Traverse Slot 10</u>

Thickened and thinned inlet boundary layer experimental data at slot 10 was taken on identical grids, modified from that used for the natural boundary layer (Figure 6.15 shows the thickened inlet boundary layer experimental data points). No extrapolations from the probe calibration were required to complete either data set.

Towards midspan the total pressure loss coefficient data appears similar for each inlet boundary layer condition, due to the approximately two dimensional blade wakes (Figure 6.16, 6.17 and 4.67). The loss region, originating from the suction surface endwall corner, undergoes tangential skewing of similar intensity for all three inlet boundary layers. Data from the thickened inlet boundary layer shows merging of the loss core with the suction surface endwall corner region, as defined by the 0.5 contour

(Figure 6.18). Loss core shape, size and maximum value for the thinned and natural inlet boundary layer again appear to be very similar. Considerable growth in the loss core however is evident for the thickened boundary layer data when compared with the other inlet conditions, primarily due to the higher mass flow in the inlet boundary layer. The loss core centre at slot 10 for the thickened inlet boundary layer lies further from the endwall than that for the natural and thinned inlet conditions (Figure 4.75). With the loss core centre peak becoming increasingly diffused further from the trailing edge plane it is perhaps unwise to draw specific conclusions from this data derived from slot 10 traverse information. Loss core centre position information at slot 8 suggests the two induced inlet conditions have coincident centres, a fact at variance with the above.

As evident for the natural boundary layer, the loss core areas show up as regions of low total velocity for the varied inlet boundary layers. Since the total velocity contour plots are similar for all three inlet conditions the varied boundary layer data is not presented (Figure 4.69 shows natural inlet boundary layer data).

Considerable variations in spanwise velocity components are apparent from the secondary velocity vector plots, for all three inlet boundary layer cases investigated, when compared with the data upstream at slot 8 (Figure 6.19, 6.20 and 4.70). The secondary tangential velocity components for the thinned inlet boundary layer appear to be less than those for the thickened inlet boundary layer. Quantitative comparisons of this type with the natural boundary layer data are not easily made, owing to the different traversing positions used. The suction surface endwall corner loss region appears more sharply defined for the natural inlet conditions owing to the concentration of traverse information in this area. As seen at slot 8, both the modified boundary layer vortex centres lie towards the centre of the projected passage, when compared with the natural inlet condition (Figure 4.75).

Static pressure coefficient data at the slot 10 traversing plane for all three inlet conditions show a peak value associated with the passage vortex centre (Figure 6.21, 6.22 and 4.73). As the cascade exhausts directly to atmosphere static pressure downstream rapidly tends to atmospheric, giving reduced definition of features observable at the slot 8 location. The thinned inlet boundary layer data shows considerably higher values owing to the difficulties of defining the inlet static pressure.

6.3 MASS MEANED FLOW FIELD DATA

6.3.1 Mass Meaned Flow Field Data Upstream of the Cascade

Changing the cascade inlet boundary layer thickness produces no observable effect on the pitchwise mass meaned inlet flow angle at midspan of 44.2^{0} (Figure 6.23). Overturning (reducing positive yaw angle) from about 30 to 120 mm span appears to increase with boundary layer thickness. Closer to the endwall however agreement between the three inlet boundary layers, as measured by the five-hole probe improves. A shifting of the three-hole probe zero appears to have occurred, possibly due to slight accidental bending of the probe tip, since the natural inlet boundary layer traversing was performed. The varied inlet condition data however appears to be self-consistent.

Variation of cascade inlet boundary layer thickness is clear from the pitchwise mass meaned total pressure loss coefficient data (Figure 6.24). Agreement between the five-hole and three-hole probe data appears to be good for all three inlet boundary layer conditions. Considerable boundary layer thickening was achieved using the triangular castellations positioned in the wind tunnel working section. A power law profile, of the same displacement thickness and area mass averaged total pressure loss coefficient, shows reasonable agreement with the thickened experimental profile. Thinning of the boundary layer however appears to have been less effective. Poor agreement between the experimental data and its power law equivalent is evident, probably due to the proximity of the bleed slot to the slot 1 traverse plane.

6.3.2 Mass Meaned Flow Field Data Downstream of the Cascade

Mass meaned data from the downstream traverse slots shows the effect of changing inlet boundary layer thickness to be insignificant on the midspan yaw angle (Figures 6.25 and 6.26). Data from both downstream traverse slots show the maximum yaw angle (minimum underturning) occurring with the natural inlet boundary layer. Hawthorne (1955ii) showed theoretically that increasing the inlet boundary layer thickness reduced the maximum underturning magnitude, placing it at a spanwise position corresponding to the inlet boundary layer thickness. These downstream results suggest that the peak yaw angle position (minimum underturning) moves closer to the endwall with reducing inlet boundary layer thickness. Greater variation in the inlet boundary layer thickness is shown by the upstream data (Figure 6.24) than in spanwise movement of the yaw angle maximum downstream. Close to the wall the effect of the suction surface endwall corner loss region is evident, from the increasing yaw angles (reducing overturning), this phenomenon being more significant on the slot 10 data (Figure 6.26). Classical outlet angle prediction methods do not show this increasing yaw angle. This near wall data shows little variation with inlet boundary layer thickness suggesting that it is a passage endwall phenomenon.

Total pressure loss coefficient data from the downstream traverse slots shows a general increase in loss with increasing boundary layer thickness (Figures 6.27 and 6.28). Data from traverse slot 8 shows the magnitude of the loss peak associated with the loss core increasing, and also its spanwise migration from the endwall with boundary layer thickening. Traversing increments of the natural boundary layer data, at traverse slot 10 make both the position and peak value of the loss core uncertain, however the same trends as at slot 8 are shown. Decreasing peak loss values, and their migration from the endwall are evident with the flow advancing downstream of the blade trailing edges. Suction surface endwall corner loss values, being very close to the wall at slot 8, have spread more into the flow field by the slot 10 location. The natural boundary layer data exhibiting a distinct second loss peak at traverse slot 10. Scattering of the midspan pitchwise mass meaned total pressure loss coefficient, equivalent to the blade profile loss, at slot 8 is probably due to the high gradients and few tangential traverse points within the blade wakes. Diffusion of the wakes reduces the midspan scattering on the slot 10 data.

6.3.3 Development of the Area Mass Averaged Total Pressure Loss Coefficient

At inlet to the cascade the area mass averaged total pressure loss coefficient shows clearly the increasing boundary layer thickness (Figure 4.81). Downstream of the cascade the thickened and thinned boundary layer data appear similar to that from the natural case, when the variations in inlet loss value are considered. The net secondary loss data confirms this observation (Figure 4.82). Uncertainty in the slot 8 profile loss data suggets a loss value for the thickened and thinned inlet boundary layers to be higher than that for the natural case. At slot 10 secondary loss data agreement between thickened and natural inlet boundary layers is good, however the thinned boundary layer loss value is larger.

6.3.4 Cascade Mass Flow Rate

Results from the variable inlet boundary layer data are similar to those obtained for the natural inlet conditions (Figure 4.83). The errors appearing slightly larger for the thinned inlet boundary layer, probably owing to the upstream air bleed giving additional uncertainties to the inlet reference condition.

6.4 BLADE SURFACE PRESSURE DISTRIBUTION DATA

Owing to the bleed of air from the upstream boundary layer, at entry to the cascade, the inlet static pressure becomes difficult to define precisely for the thinned inlet boundary layer condition. Static pressure coefficient consequently also becomes difficult to define, however the qualitative nature of the thinned inlet boundary layer data is unaffected.

6.4.1 Data on Constant Span Planes

As in section 4.7.1 data from the traverse passage only is presented, preventing excessive numbers of data points on the diagrams. Pressure surface data, for the thickened inlet boundary layer, shows significant differences upstream of the mid chord point close to the wall compared with the midspan distribution (Figure 6.29). These variations being due to the high spanwise total pressure gradient at inlet. The thinned inlet boundary layer data by comparison shows much smaller differences (Figure 6.30). Suction surface data for all three inlet conditions (Figures 6.29, 6.30 and 4.163) suggest that midspan conditions are approached much closer to the endwall with reducing inlet boundary layer thickness. Data from all three cases show the maximum trailing edge suction surface static pressure coefficient occuring 61 mm from the endwall (in each case the next spanwise tapping, 81 mm from the endwall, showing a reduction). The actual peak, if occuring between the fixed location tapping, holes cannot be measured with the cascade in its current form.

6.4.2 Data on Blade Surfaces

These plots are presented using data as defined in section 4.7.2. Pressure surface data for all three inlet conditions is very similar (Figures 6.31, 6.32, and 4.166). Close to the leading edges the −0.9 contour reflects the changing inlet boundary layer thickness. Varying the cascade inlet boundary layer results in only minor changes to the suction surface static pressure coefficient (Figures 6.33 and 6.34). These changes are most apparent close to the endwall and at about mid chord, seen from the increasing contour curvature towards the trailing edge with the thickening of the upstream boundary layer. Graziani et al (1980) presented blade surface static pressure data for the Langston et al (1977) cascade, for two inlet boundary layer thicknesses, showing similar characteristics to the Durham data. The secondary flow effects, on the suction surface, for their thinned boundary layer case (δ/H = 0.014) were much less evident than their natural case (δ/H = 0.119).

CHAPTER 7

LOSS PREDICTION METHOD AND RESULTS

7.1 LOSS PREDICTION METHOD

The secondary loss prediction technique used here is that developed by Gregory-Smith (1982) for annular blade rows. Secondary losses are here defined as the overall loss at exit from a blade row after deducting the resulting profile loss. This method is intended primarily for preliminary design calculations where simple reliable secondary loss predictions are required. Viscous fully three-dimensional calculation techniques, even if now feasible, are hardly justified for this application on the grounds of expense. As the relative costs of computer processing power reduces, coupled with theoretical developments, this may become a less severe limitation.

Secondary flow angles are predicted reasonably reliably by classical secondary flow theories originally proposed by Hawthorne (1955). Numerical solution of the equation for secondary flow stream function, proposed by Glynn and Marsh (1980), gives secondary velocities in the cascade exit plane, and consequently the calculated blade row exit flow angle. The secondary flow theory assumes that no distortion of the Bernoulli surfaces occurs within the blade row, which for the Durham cascade is a poor assumption. Glynn (1982) has attempted to overcome this limitation.

Gregory-Smith's technique assumes that the pitchwise averaged secondary loss distribution at exit from a blade row is composed of three discrete components, these being identified as:

(i) The Loss Core

(ii) The Extra Secondary Loss

(iii) The New Endwall Boundary Layer

No interaction between these three components are assumed, and consequently their sum is assumed to give the secondary loss distribution.

7.1.1 The Loss Core

The experimental results presented in Chapter 4 show that the developing loss core within the blade row forms from fluid originally in the upstream boundary layer, this phenomenon has also been observed by others (for example Langston et al (1977) and Marchal and Sieverding (1977)). It is therefore assumed that the loss core forms entirely from upstream boundary layer fluid, having the same mass flow and total pressure defect. To simplify analysis the shape of the loss core is assumed to be triangular, with the point of minimum total pressure (equal to the upstream static pressure) a specified distance away from the endwall (Figure 7.1). Pitchwise mass averaging of this assumed loss core gives the spanwise distribution of total pressure loss coefficient. A discontinuity in the spanwise loss profile is inevitable because of this assumed triangular loss core shape, and consequently a revised loss core form would be desirable.

7.1.2 The Extra Secondary Loss

Strong secondary velocities are evident in the experimental results of Chapter 4; an idealized model of these velocities is given by the classical theoretical approach. To allow for the interaction of these velocities with the blade boundary layers, an extra secondary loss is proposed. This extra loss is assumed to be proportional to the kinetic energy of the calculated secondary flow, and as a first step, the constant of proportionality is assumed to be unity. Intuitively this assumption appears to be too severe, as one can visualize the tangential secondary velocity components being accepted by the following blade row as variation in incidence angle. The radial (spanwise) velocities however are more likely to be dissipated, thus contributing to the total pressure defect as seen by the following blade row. It should be noted that since the secondary kinetic energy evaluated is a steady state phenomenon the commonly used pressure probes are sensitive to it. Pitchwise averaged total pressure loss coefficient distributions consequently do not include its effects, since the secondary velocities are included in the measured local dynamic head values.

7.1.3 The New Endwall Boundary Layer

Action of the passage vortex, within the blade row, sweeps the inlet boundary layer onto the suction surface. A new boundary layer begins to form downstream of the pressure side leg of the leading edge vortex separation line on the endwall. The results of Chapter 4 would suggest that this too is swept towards the suction surface. Towards the trailing edge plane however some evidence of endwall boundary layer growth is apparent. This new boundary layer is highly skewed, under the action of overturning flow, close to the endwall. A two-dimensional boundary layer calculation is performed (Duncan et al 1960) with the effects of skew and cross passage variation neglected for simplicity. This new boundary layer is assumed to grow from the passage throat and be turbulent from its inception. The resulting power law profile data is used to evaluate a total pressure loss coefficent.

7.2 DURHAM CASCADE PREDICTION RESULTS

Loss prediction data for the Durham cascade is evaluated at the slot 10 location. The increased blade wake width at slot 10, compared to slot 8, gives more data points at midspan from which to estimate the blade profile loss.

Inlet boundary layer thickness and power law index values were evaluated to give the same mass meaned total pressure loss coefficient and displacement thickness as measured experimentally. The accuracy of the power law profile in reflecting the measured boundary layer profile varied considerably.

7.2.1 Natural Inlet Boundary Layer

Blade row exit flow angles were predicted for the Durham cascade using both power law and measured inlet boundary layer data (Figure 7.2). Very high overturning angles are predicted close to the endwall by both inlet boundary layer representations. Pitchwise mass averaged data from the cascade however shows a decrease in overturning, owing to flow separations, close to the suction surface endwall corner. These separations are not currently modelled within the angle prediction method. Experimental data from slot 10 exhibits a sharp underturning peak about 90 mm away from the wall, although the prediction also shows an underturning maximum, it is not as clearly defined. The assumption of no Bernoulli surface distortion, which in the light of experimental evidence is clearly not justified in this instance, probably is a significant smoothing factor on the predicted data. This assumption allows the fluid with high total pressure defect to remain at the wall and not concentrate into a 'loss core'. Predictions from both the power law and pitchwise mass averaged inlet boundary layer are very similar, indicating fair agreement between the two inlet velocity profile representations. (Figure 4.76)

The spanwise distribution of secondary loss was derived from the experimental pitchwise mass averaged total pressure loss coefficient distribution at slot 10 less the estimated blade profile loss. Agreement beween the predicted and experimental data distributions appears to be reasonable (Figure 7.3). Close to the endwall however the predicted losses are considerably greater than those measured, owing to the high secondary kinetic energy and the developing new endwall boundary layer assumptions. Calculation of the secondary kinetic energy follows from secondary flow theory, and the predicted high wall overturning angles imply strong local tangential secondary velocity components. These velocity components are partly responsible for the increased predicted losses at the wall. Secondary kinetic energy distributions derived from both experimental data and power law representations of the inlet velocity profile are quite similar, indicating reasonable agreement between the two inlet profiles. The new boundary layer growing from the throat, being still fairly thin at the measuring plane, also helps to concentrate the predicted loss at the endwall. High secondary kinetic energy values from about 20 mm to the loss peak consistently put the predicted loss values above those measured. The location of the loss peak was chosen to be coincident with the slot 10 peak thus giving assured agreement in its positioning. Agreement between the predicted and measured loss peak distributions suggests that redistribution of the inlet boundary layer models the loss peak quite closely. A revised loss core distribution, although involving some additional calculation, would be desirable to remove the discontinuity in the predicted loss peak.

A comparison of the predicted loss values for the Durham cascade is given in Table 7(i). It is evident that the predicted total secondary loss is rather too high, and this is consistent with Figure 7.3. The secondary kinetic energy is seen to be the most significant factor in this over estimate of the losses, amounting to about 85% of the total secondary loss measured.

7.2.2 Thickened Inlet Boundary Layer

Exit angle predictions for the thickened inlet boundary layer show similar features to those obtained for the natural inlet boundary layer (Figure 7.4). Again close to the wall high overturning is predicted. For the underturning peak however improved predictions are obtained when the pitch averaged total pressure profile from slot 1 is used at inlet, rather than the equivalent power law representation. The two boundary layer profiles used (Figure 6.24) with their differing vorticity profiles give this difference. A continually decreasing total pressure loss coefficient gradient as given by the power law distribution is not shown by the slot 1 experimental data.

Distributions of the secondary loss are dominated around the loss peak by the high predicted values of secondary kinetic energy (Figure 7.5). As with the natural inlet boundary layer prediction secondary loss values close to the wall are rather over estimated, owing to the concentrating effect of the new boundary layer coupled with the strong wall secondary velocities. Increased secondary kinetic energy values are predicted to about 55 mm span when the power law inlet velocity profile is used in the secondary flow calculations.

The evaluated secondary kinetic energy for the power law profile is greater than the overall measured secondary loss (Table 7(i)). When the slot 1 pitch averaged data is used, within the prediction program, the secondary kinetic energy amounts to some 85% of the overall secondary loss. It thus appears also for this case that the assumption of the extra secondary loss being equal to the secondary kinetic energy is too severe.

7.2.3 Thinned Inlet Boundary Layer

Peak underturning values, for both power law and measured boundary layer profiles, are predicted closer to the wall than those measured (Figure 7.6). Significant differences in the maximum predicted underturning values arise due to the poor power law representation of the inlet total pressure loss profile, coupled with its distinct discontinuity at the boundary layer thickness. Some differences are also probably a result of the bleed slot being rather too close to the blades, and consequently not allowing boundary layer equilibrium to be established at the slot 1 measuring plane. As for the two other inlet boundary layer conditions high overturning is predicted close to the wall.

Secondary kinetic energy, evaluated from the power law inlet boundary layer representation, exhibits values above that of the measured secondary loss up to about 60 mm from the wall (Figure 7.7). The predicted secondary loss envelope, based on the boundary layer data from slot 1, follows the measured values quite closely, away from the influence of the new boundary layer and the high secondary velocities close to the wall. Modelling of the loss peak does not appear very satisfactory when compared with the other boundary layer cases considered previously, probably due to the poor power law representation of the upstream flow used in its evaluation.

It again appears that the secondary kinetic energy loss assumption is too severe, with it accounting for some 80% of the total secondary loss. (Table 7(I)).

7.3 CARRICK'S CASCADE PREDICTION RESULTS

Testing of a cascade of impulse turbine blading with variation of inlet boundary layer skew was undertaken by Carrick (1975). His work presents no data on the flow angle at exit from the cascade, consequently no comparisons between predicted values and his experiment are possible. Carrick suggested that the blade profile loss derived from his zero inlet skew experiments was probably the most accurate, since with inlet skew on one endwall the midspan plane of the cascade was probably not two-dimensional. Plots of his data have a secondary total pressure loss coefficient of about 2% at midspan when the stated profile loss is used. Pitchwise mass averaged boundary layer data was used for both inlet conditions in the prediction.

7.3.1 Zero Inlet Skew Low Reynolds Number (ZS/LR)

Agreement between the predicted values and experimental data appears to be best around the loss core peak (Figure 7.8). Again this suggests that the redistribution of the inlet boundary layer is a reasonable assumption, although clearly its form in the exit plane could be improved. The second experimental loss peak, near the wall, hardly shows on the predicted loss distribution. Calculated secondary kinetic energy however shows a point of inflection almost coincident with the measured loss peak near the wall. Owing to both the high predicted overturning, implying high tangential components of secondary kinetic energy, and the new assumed endwall boundary layer close to the wall the secondary losses are over predicted in this region. The predicted loss distribution near the wall is significantly higher than that measured, and between 3% and 10% span below that measured. Overall the predicted loss is over estimated by about 34% (Table 7(ii)).

7.3.2 High Inlet Skew Low Reynolds Number (HS/LR)

The distribution of predicted loss within the loss core peak is not in close agreement with that measured by Carrick (Figure 7.9). Repositioning the estimated loss peak, currently coincident with the measured peak, however would improve the qualitative agreement obtained. Prediction of the loss peak however is only based on the total pressure defect within the inlet boundary layer, and consequently takes no account of upstream skew, which for this inlet condition could be a factor influencing the distribution. A slight peak in the prediction close to the wall is evident, derived from the secondary kinetic energy calculation. The magnitude and position of this peak however does not correlate very well with the experimental data. Again very close to the wall high losses are predicted. The loss of all secondary kinetic energy assumption, in this case, is clearly too severe, since its predicted value is greater than the measured secondary loss (Table 7(ii)). Secondary losses for this cascade with inlet skew are overpredicted by about 45%.

7.4 SJOLANDER'S CASCADE PREDICTION RESULTS

Sjolander (1975) tested an annular cascade of turbine nozzle guide vanes with fairly low turning, using a three-hole probe and extensive flow visualizations. No pitchwise averaged total pressure or flow angle data however were presented. The inlet boundary layer for the prediction was taken to be the upstream profile presented. An area averaging of total pressure loss coefficient data was therefore undertaken, using a contour plot of total pressure loss coefficient. This data was obtained about 15% of an axial chord downstream of the trailing edge plane. The pitchwise averaged data in this comparison was therefore derived on an area weighting basis, and not mass meaned. Data from the low velocity regions associated with the blade wakes are probably over estimated, having a significant effect on the profile loss estimate and consequently the secondary loss. Digitizing a contour plot to obtain any data is an exercise prone to errors due to approximations in the contouring process and in recording co-ordinates of a contour's path. Data from this prediction is therefore compared with dubiously manipulated experimental data.

Four radial experimental traverses, presenting cross flow angle data, show similar qualitative features to the Durham cascade results. Flow is at approximately the two-dimensional cascade exit angle near midspan, and on approaching the wall a region of underturning, followed by overturning is seen. Adjacent to the wall evidence of a significant reduction in overturning is apparent, due probably to separation in the suction surface endwall corner. Flow exit angle prediction data does not show this but rather a continuing increase in overturning as the wall is approached. Comparisons of a more quantitative nature however between the prediction and the experimental data from a relatively coarse grid spacing are difficult.

Agreement between the predicted secondary loss distribution, and that derived from the data appears most satisfactory around the loss core peak (Figure 7.10). Close to the wall, owing to the effects of the calculated endwall boundary layer and the high secondary kinetic energy, the predicted losses are significantly higher than those derived from the experimental results. Very low secondary kinetic energy values between the new boundary layer edge and the loss peak lead to an under-estimation of secondary losses in this area. Overall the predicted loss is some 40% too high (Table 7(ii)) when compared with the derived experimental data. The predicted loss distribution suggests that this over-estimation arises chiefly due to the high loss values predicted near the wall.

7.5 TEST TURBINE PREDICTIONS

The test programme on this single stage turbine was conducted chiefly to obtain overall performance data (Rolls-Royce (1981)). No detailed blade row inlet boundary layer data was therefore available. The blade profile used in the Durham cascade was that of the rotor mid height section suitably scaled.

7.5.1 Nozzle Guide Vanes

The inlet boundary layer to the nozzle guide vanes was assumed to be of seventh power law form, merging with pitot data at 9% annulus height. This assumption is clearly questionable, but since the effect of significant annulus area reduction upstream of the blades was not measured, it was felt to be justified.

The exit flow angle prediction (Figure 7.11) follows the experimental data quite closely from about 25% annulus height to the blade tip. At the root however the prediction differs considerably, probably due to the influence of the strong hub curvature.

The experimental secondary loss distribution is based on the mean exit local dynamic head, using the midspan value as a basis makes very little difference (Figure 7.12). The predicted secondary kinetic energy component of the loss is shown. Losses predicted close to the annulus walls will be increased by the new developing endwall boundary layer, which will be quite thin at the measuring plane. The loss core formed from the assumed inlet boundary layer is not visible when normalized using the exit dynamic head. It appears that the secondary kinetic energy loss assumption is reasonable at the hub, but under-estimates the losses at the tip. Comparisons between area mass averaged values are not possible since the experimental data is not available.

7.5.2 Rotor Blades

The velocity distribution at inlet to the turbine rotor was taken to be the nozzle guide vane exit conditions transformed to the rotor relative frame of reference. Again no boundary layer data was available and consequently the near wall velocity profile data was estimated from the annulus distributions. This unskewed representation of the inlet boundary layers is clearly open to discussion, but it was felt to be reasonable when only qualitative comparisons with experimental data are made.

The experimental exit angle data shows two slight underturning maxima at about 35% and 65% annulus heights, and two minima at 15% and 85% (Figure 7.13). As in the other cases considered, maximum overturning is predicted adjacent to the endwalls. Peak underturning areas are also predicted, but do not coincide with the experimental data. The position and magnitude of these areas being strongly dependent on the inlet endwall boundary layers to the blade row. Similar qualitative experimental exit flow angle data is shown by the Durham cascade at slot 10 (Figure 7.2) when compared to the test turbine rotor exit flow.

In transforming from the rotating to stationary reference frame at rotor exit the magnitudes of the secondary velocities remain unaffected. Results are presented in the stationary reference frame since total pressure loss data was obtained with fixed probes. Clearly the predicted secondary kinetic energy losses are too high (Figure 7.14) when compared with the overall rotor loss pattern less an estimated profile loss. Predicted losses near the wall are again increased by the developing boundary layer. In the prediction method for the rotor no account is currently taken of skewing the new hub boundary layer from rotor relative to stationary co-ordinate systems. This and the rotor inlet boundary layer assumption renders the loss peak calculation very doubtful. Qualitatively the rotor loss prediction appears to be rather poor, however the flow field is highly three dimensional with skewed endwall boundary layers at both inlet and outlet. No area mass averaged data from the test turbine in available, making comparisons impossible.

7.6 OTHER LOSS PREDICITONS

Gregory-Smith (1982) has used this loss prediction technique on other blade row examples. In a rectangular cascade, investigated by Marchal and Sieverding (1977) the overall loss was overpredicted by 16% (Table 7(ii)). As in the cases cited above too much predicted loss was concentrated close to the wall.

Predictions by Gregory-Smith (1982) on work by Hunter (1979) show good agreement with his nozzle guide vane flow outlet angle measurements for both thick and thin inlet boundary layers. Loss predictions for these blades are good for the thick inlet boundary layer, with an under-estimate of 10% at the hub as the worse case (Table 7(iii)). The estimates for the thin inlet boundary layer are not so good with an under prediction at the hub and over prediction at the outer wall (Table 7(iii).

TABLE 7.i: SUMMARY OF TOTAL SECONDARY LOSS PREDICTIONS

DURHAM CASCADE

Boundary Layer	Natural		Thick		Thin	
Slot 10 Observed Loss	0.2847		0.3808		0.2696	
Estimated Profile Loss	0.1050		0.1100		0.1000	
	------		------		------	
Secondary Loss	0.1797		0.2708		0.1696	
Predictions Boundary Layer Data Type P : Slot 1 Profile PL : Power Law						
	PL	P	PL	P	PL	P
Loss Core	0.0845	0.0845	0.1752	0.1752	0.0532	0.0532
Secondary Kinetic Energy	0.1567	0.1519	0.3192	0.2307	0.4010	0.1412
New Boundary Layer	0.0163	0.0163	0.0163	0.0163	0.0163	0.0163
	------	------	------	------	------	------
Total Predicted	0.2575	0.2527	0.5107	0.4222	0.4705	0.2107
Observed Loss/ Predicted Loss	0.70	0.71	0.53	0.64	0.36	0.81

SECONDARY LOSS ESTIMATES USING CORRELATIONS

	Secondary Loss Estimate (Normalized on inlet dynamic head)		
Boundary Layer	Natural	Thickened	Thinned
Correlation			
Dunham (1970)	0.353	0.494	0.323
Dunham and Came (1970)	0.493	0.493	0.493
Came (1973)	0.464	0.817	0.341
Morris and Hoare (1975)	0.405	0.724	0.356

TABLE 7.ii: SUMMARY OF TOTAL SECONDARY LOSS PREDICTIONS

Source of Data	Carrick (1975) ZS/LR	Carrick (1975) HS/LR	Sjolander (1975)	Marchal & Sieverding (1977)
Observed Loss	0.1210	0.1450	0.1252	
Profile Loss	0.0539	0.0539	0.0650	
	------	------	------	
Secondary Loss	0.0671	0.0911	0.0602	0.011
Predictions				*
Loss Core	0.0482	0.0604	0.0238	0.0050
Secondary Kinetic Energy	0.0475	0.0976	0.0548	0.0046
New Boundary Layer	0.0052	0.0052	0.0218	0.0035
	------	------	------	------
Total Predicted	0.1009	0.1632	0.1004	0.0131
Observed Loss/ Predicted Loss	0.67	0.56	0.60	0.84

* Predictions by Gregory-Smith (1982)

SECONDARY LOSS ESTIMATES USING CORRELATIONS

Secondary Loss Estimate (Normalized on inlet dynamic head)

Data Source	Carrick (1975)		Sjolander
Correlation	ZS/LR	HS/LR	
Dunham (1970)	0.252	0.233	0.216
Dunham and Came(1970)	0.149	0.149	0.316
Came (1973)	0.122	0.142	0.104
Morris and Hoare (1975)	0.113	0.152	0.156

TABLE 7.iii: SUMMARY OF TOTAL SECONDARY LOSS PREDICTIONS

HUNTER'S DATA

Thick Inlet Boundary Layer

Position	Hub	Tip	Combined
Observed Secondary Loss	0.163	0.149	0.155
Predictions*			
Loss Core	0.058	0.070	0.065
Secondary Kinetic Energy	0.027	0.034	0.031
New Boundary Layer	0.063	0.039	0.049
	-----	-----	-----
Total Predicted	0.148	0.143	0.145
Observed Loss/ Predicted Loss	1.10	1.04	1.07

Thin Inlet Boundary Layer

Position	Hub	Tip	Combined
Observed Secondary Loss	0.157	0.089	0.120
Predictions*			
Loss Core	0.029	0.036	0.033
Secondary Kinetic Energy	0.031	0.034	0.033
New Boundary Layer	0.057	0.031	0.043
	-----	-----	-----
Total Predicted	0.117	0.101	0.109
Observed Loss/ Predicted Loss	1.34	0.88	1.10

* Predictions by Gregory-Smith 1982

CHAPTER 8

DISCUSSION

8.1 MEAN FLOW DATA

The detailed traverse results of this study were obtained from a large scale high turning cascade of turbine rotor blade profiles with a fairly low aspect ratio. Mean flow field data were obtained throughout the cascade for a naturally occuring inlet endwall boundary layer. Fluctuating flow data on blade inlet and exit traverse planes were obtained for this inlet boundary layer condition, using twin sensor hot wire probes. Cascade inlet and exit traverse plane data were also recorded for artificially thickened and thinned inlet boundary layers.

Flow upstream of the blade row had similarities with that of a flat plate boundary layer. Influence of the blade row was clearly evident in the static pressure field upstream, which approached the stagnation condition in line with the leading edges. Detailed examination of the upstream yaw and spanwise angle distributions suggested that the leading edge vortex discussed by other workers was present.

Quantitative traverse results from within the blading exhibited many phenomena identified by other workers, frequently however only in their qualitative flow visualization studies. Development of the passage vortex, apparently from the pressure side leg of the leading edge vortex, was traced successively on each of the axial probe traversing planes. The passage vortex centre was defined as the intersection of the zero cross flow and zero spanwise flow angle contour lines on each traverse plane, rather than subjectively from secondary velocity vector plots. The centre of the developing total pressure loss core through the blade passage was not coincident with that of the passage vortex as frequently presumed.

Loci of the passage vortex centre and minimum static pressure also did not coincide well within the blade row, however as the trailing edge was approached their coincidence improved. Downstream of the blade row the former passage vortex centre and point of minimum static pressure were practically coincidental. These differences in position of the loss core peak, passage vortex centre, and minimum static pressure were probably due to interaction of the vortex flows and strong predominantly cross passage static pressure gradients. The suction side leg of the leading edge vortex was lost from traverse plane view after slot two onto the blade suction surface. Flow visualization studies of others suggest that the separation line of this vortex leg travels up the blade suction surface. High loss fluid within this leg, after some growth and spanwise migration, reappears on the suction surface at traverse slot six. The separation line of this vortex leg feeds shearing losses generated close to the blade surface into the mainstream flow. A developing region of loss was traced, beginning with the three-hole probe results of slot four, towards the trailing edge in the suction surface endwall corner. This area grew in size on successive traverse planes from developing boundary layer fluid swept off the cascade endwall by the passage vortex. Downstream of the trailing edge plane this loss region was skewed violently by the strong overturning flow adjacent to the endwall.

Blade wakes were a prominent feature of the downstream flow field, although their definition would have been improved with more closely spaced spanwise traverses. A measure of downstream loss core size was obtained by considering the area enclosed by the 0.5 total pressure loss coefficient contour. Increases in the area enclosed by this contour with increasing inlet boundary layer thickness was noted, suggesting a relationship between the two. Qualitatively however the flow field was little changed with inlet boundary layer thickness variation. The loss prediction method currently assumes that all inlet boundary layer fluid is included in the idealized loss core, which on the basis of these experimental results seems a reasonable first assumption. Evidence from the downstream traverse plane suggests that the new endwall boundary layer is little influenced by the inlet boundary layer thickness. Flow visualization results of other workers give evidence of an endwall separation line across the passage, and downstream of this a new endwall boundary layer forms. Variations in the upstream boundary layer will probably influence the strength of the leading edge vortex and consequently its path across the passage. The initial conditions of the new endwall boundary layer will vary, but experimental data from this study suggests little change is evident in this boundary layer downstream of the blading. Secondary velocity vector plots, coupled with spanwise angle contour plots, from downstream traverse planes, show increasing spanwise flows with increased inlet boundary layer thickness. Classical theory would also predict this from an increase in inlet vorticity. Data from three downstream traverse planes were obtained for the natural inlet boundary layer. These traverses showed the loss core continuing to migrate in a spanwise direction downstream of the blade row, driven by the spanwise velocity components.

Loss development through the cascade, obtained on an area mass averaged basis, showed a gradual rise to traverse slot 6. Data from slot 7 suggested an increasing loss generation rate, perhaps due to losses being fed into the traversing probe range from near the suction surface. Other workers have also noted an increased rate of loss generation in similar areas, about 25% axial chord from the trailing edge.

Various aspects of the flow field were highlighted by plots produced on constant spanwise planes and pseudo-stream surfaces. No flow visualization data were obtained for the Durham cascade, and consequently surface flow patterns were inferred from these plots using flow data obtained near the passage boundaries. Speculative separation lines were thus estimated and compared favourably with those of other workers obtained from flow visualizations. Recently flow visualizations have been performed on the Durham cascade (Walsh 1984) and agreement with the previous speculations is encouraging.

8.2 TURBULENCE DATA

Very little data on fluctuating flow within turbine components is available in open literature, and consequently hot wire probe traversing was performed on the Durham cascade. Hot wire probe data were obtained at slots 1 and 8 with the natural inlet boundary layer. Mean flow field velocity and angle data were also obtained from the hot wire probes, and was directly comparable with that of the pressure sensing probes. Generally agreement between the two sets of data was good, however the hot wire probes suffered from sensor contamination which influenced their accuracy. An additional problem with the hot wire probes was that two sets of independent traverses were required to complete a data set. Point to point repeatability in probe positioning therefore influenced the data analysis, especially the cross coupled terms. This problem was most severe in the fluctuating flow data analysis, however it also influenced the evaluated mean streamwise velocity components in the endwall plane, and these components were used to evaluate other mean flow data.

Upstream fluctuating flow field data were similar to that obtained from the flat plate boundary layer traverses of other workers. The turbulence levels obtained at cascade inlet however were high by comparison, probably due to the flow straightening honeycomb at the beginning of the wind tunnel working section. Turbulence levels within an operating turbomachine are likely to be high, owing to complex flow mixing phenomena arising from previous blade rows, and consequently no efforts were made to reduce the levels occuring at the cascade inlet. On approaching the endwall the measured perpendicular turbulence component continued to increase. Intuition however suggests this to be unlikely, and this is confirmed by the smooth flat plate boundary layer data of other workers. This apparent discrepancy could be due to the leading edge vortex and its associated flows close to the endwall, which are absent on a smooth effectively infinite flat plate.

No hot wire probe traversing was undertaken within the blade passage in this study, however Fulton (1983) has traversed at the slot 7 location. He found data broadly similar to that measured at slot 8, but with higher turbulence levels close to the loss core centre.

Downstream of the trailing edges high turbulent kinetic energy levels were recorded, in areas of high total pressure loss coefficient associated with the loss cores and blade wakes. Peak turbulent kinetic energy occurred on the midspan side of the loss core, in an area of total pressure gradient close to the former passage vortex centre. The three turbulence intensity components were very similar to each other, and also qualitatively similar to the turbulent kinetic energy distribution. The approximately isotropic nature of the turbulence was a surprising finding, since workers investigating duct flows have not found similar results.

Comparisons of a more qualitative nature are difficult owing to the lack of readily available cascade data.

8.3 LOSS PREDICTION

The loss prediction technique sums three discrete components to obtain an overall predicted pitchwise averaged secondary loss profile at blade row exit. These three components were identifiable from the cascade traverse results. Development of the loss core, from inlet boundary layer fluid of low total pressure, was traced on successive traverse planes. A triangular loss core shape is currently assumed for the predicted loss core for computational convenience, with the peak value coincident with that measured experimentally. Clearly further development is needed on its positioning before a practical loss prediction can be obtained. An improved assumed loss core shape would also be desirable to remove the sharp loss peak predicted. Downstream of the endwall separation line from the pressure side leg of the leading edge vortex, a new highly skewed boundary layer grows. Low total pressure fluid from this was swept towards the suction surface endwall corner, a characteristic not currently modelled in the loss prediction method. A simple two dimensional non-skewed turbulent boundary layer is assumed to grow from the passage throat. Total pressure losses derived from this new endwall boundary layer are assumed to contribute to the overall predicted blade row secondary loss distribution close to the endwall. An extra secondary loss is predicted by assuming proportionality with the secondary kinetic energy, evaluated using a classical method. This constant of proportionality is currently assumed to be unity. Results presented in this study (Chapter 7) show that frequently this component can be a large proportion of the predicted secondary losses, and this suggests that a revision of this constant would be desirable. An alternative approach could be to consider the tangential secondary velocities producing an incidence variation onto the following blade row, and the radial (spanwise) components being proportional to the extra secondary loss. Secondary velocities however do not contribute directly to a total pressure loss, since a directionally sensitive pressure sensing probe would record their contribution to the local dynamic head. Hot wire probe traverses downstream of the cascade showed high turbulence levels close to areas of total pressure defect. These fluctuating velocities are not recorded by conventional, long time constant, pressure sensing probes and contribute directly to a local loss of mean flow energy relative to the upstream flow field. Further traversing within the cascade would trace development of these high turbulence zones and give additional data for an extra secondary loss prediction method.

Predictions of secondary loss presented in Chapter 7, for cascades, show reasonable agreement with experimental results obtained. Turbine predictions however are not so encouraging. This can be attributed to shortcomings in the prediction method, which currently neglects such things as overtip leakage, and practical experimental problems in operating machines. In a working turbine access for probe traversing is frequently limited by physical constraints and consequently resolution of the experimental data may not be as good as desirable. All the predictions suggest larger total pressure losses close to the endwall than those measured. These are predicted from the relatively localized new endwall boundary layer and high local tangential secondary velocities. Experimentally very close to the wall the total pressure losses are more difficult to measure owing to probe interference effects. The mismatch between predicted and experimental losses close to the endwall therefore is probably due to a combination of errors in both methods.

The loss prediction method currently gives encouraging results when the relatively crude assumptions within the derivation of each loss component are considered. Experimental results from an isolated turbine rotor blade cascade show a complex three dimensional flow field, in which the three components of the secondary loss prediction method are evident. Continued development of the two dimensional loss prediction method will probably lead to improved predictions of this complex three dimensional phenomenon.

CHAPTER 9

CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

9.1 CONCLUSIONS

The complex three dimensional flows within a high turning turbine rotor blade cascade have been experimentally investigated in detail. Mean data were obtained from upstream, through the blading and into the downstream flow field for the naturally occurring cascade inlet endwall boundary layer. Artificially thickened and thinned inlet boundary layers were also investigated, at cascade inlet and exit only, with pressure sensing probes. Fluctuating flow field data however were only obtained on the upstream and one downstream traverse plane with the naturally occurring inlet endwall boundary layer.

The two main flow field features were the passage vortex and loss core. The passage vortex swept fluid of low total pressure, initially in the inlet boundary layer, across the passage to form a loss core close to the blade suction surface. This loss core then migrated away from the endwall as the trailing edge was approached.

A leading edge vortex formed upstream of each blade and divided into two legs, termed pressure and suction side. The pressure side leg developed into the passage vortex, and its separation line on the endwall was inferred from three-hole probe data. The passage vortex centre convected itself across the passage from blade pressure to suction surfaces, its centre did not correspond to the maximum total pressure loss coefficient. The suction side leg was lost from traverse plane view onto the blade suction surface. Towards the trailing edge it appeared again, feeding high loss fluid generated close to the blade surface into the mainstream flow.

A new endwall boundary layer forms downstream of the pressure side leg of the leading edge vortex endwall separation line. High tangential velocity components within the passage vortex sweep this low total pressure fluid towards the suction surface, where a high loss region forms in its corner with the endwall.

Downstream of the cascade blade wakes were also identified. Regrettably however their definition was poor, since only a few spanwise traverses were taken in these areas.

The inlet boundary layer to the cascade was artificially thickened and thinned. Secondary losses measured at blade row exit were only slightly influenced by these changes.

Fluctuating flow field measurements, using twin sensor hot wire probes, identified areas of very high turbulence level close to the loss cores and within the blade wakes. Wake definition would again have improved if more data points were obtained from these regions.

Experimental data obtained in this study provides a detailed insight into the complex nature of cascade secondary flows. In an operational turbine however the flow patterns will be further complicated by overtip leakage, radial pressure gradients, coolant flow and many other factors. These experimental results will provide additional test data for flow calculation techniques, however unless these are highly detailed many features may well not be evaluated.

The loss prediction method used in this study produces reasonable results from relatively simple independant calculations of three secondary loss components. Identification of these components was possible within the experimental findings and suggests that continued development of the method will prove fruitful.

9.2 FURTHER EXPERIMENTAL WORK

The cascade is a much simplified turbo-machine component, well suited to large scale detailed experimental investigation, although not subject to all turbine flow effects. Several changes of varying complexity could be made to the Durham cascade to investigate some of these additional real turbine effects.

Discussions of the results downstream of the blade row suggest that more traverse points within the blade wake region would be desirable. This information would be relatively straight forward to obtain and give much more detailed wake data.

Hot wire probe traversing of the cascade flow field within the blading would give considerable turbulence data for use in the loss prediction method. The current wind tunnel configuration however lacks any air filtration system. Fouling of the hot wire sensors influences both their sensitivities and frequency response characteristics, and air filtration would reduce these problems. Variations in the blade row inlet turbulence levels would also be desirable to investigate its influence on the turbulence structure within the blade row. Increased upstream turbulence could be obtained by installing a mesh of bars. Reductions in inlet turbulence however would be more difficult to achieve, without major modifications to both the working section and settling chamber. The current relatively high turbulence levels probably arise from the flow straightening honeycomb at working section inlet.

Additional variations to inlet boundary layer thickness could be made. Thicker upstream endwall boundary layers could be obtained by installing upstream fences of differing geometry and layout. To thin the endwall boundary layer and allow it to reach equilibrium conditions a bleed slot further upstream will be required. The bleed slot used in this study conveniently utilized the cascade transition piece to working section joint flange, and was too close to the cascade to allow equilibrium to be established.

The experimental traversing showed a build up of loss in the suction surface endwall corner. A fillet radius in the corner would influence loss distribution in this area. In real turbines blade root fillets are used to reduce stress concentrations, ease manufacturing and influence the flow field. Wall profiling of a more general nature is frequently employed in machines, and some variations on the Durham cascade would prove a useful model to assess its effects on secondary flows.

A turbine frequently operates at flows away from the design condition. One factor of off design flows is variation of flow incidence onto blading. Currently the Durham cascade is mounted onto a wooden transition piece, to change incidence a new transition piece would be required. The seven blades in the cascade however would allow a considerable incidence variation to be achieved.

Many workers have found that secondary losses are influenced by aspect ratio. A movable cascade endwall could be constructed, with an associated upstream plate giving an inlet endwall boundary layer, to investigate this effect. Two similar endwalls may need to be constructed to avoid asymmetry of the cascade flow. The traversing effort could then be reduced by investigating only half the blade height, after having measured symmetric flows about midspan. In a real turbine however symmetry of the flow about passage mid height is unlikely to be achieved.

The influence of inlet boundary layer skew on both rectangular and annular cascades has been studied by several workers. It is reported that secondary losses increase due to this effect. Each blade row in a turbomachine receives highly skewed flow from the discharge of the previous row. A moving endwall upstream of the cascade could simulate this inlet boundary layer skew. On a rectangular cascade, as at Durham, this could be achieved with a moving belt.

Overtip leakage in both turbines and compressors contributes to losses generated within the blade rows. These leakage flows could be simulated by cantilevering the blades from one cascade wall and creating an effective tip clearance at the opposite wall. Successively trimming the blade lengths would vary the tip clearance practically irreversibly. This limitation could be overcome by traversing the blades through the supporting wall, however added constructional complexities would arise with this technique. Blade motion relative to the endwall with variable tip clearance would be another avenue for study. The rectangular cascade lacks spanwise pressure gradients associated with annular blading, and consequently tip clearance effects measured may not model those more generally present.

A new blade profile would provide additional detailed data for inclusion in the loss model, and also provide another test case for the loss prediction method.

9.3 SECONDARY LOSS PREDICTION

A revision in the assumed loss core shape within the prediction method is clearly desirable, to remove the sharp discontinuity currently forecast. The loss core peak position is currently selected as that measured in the associated experimental results. In a true design prediction this information will frequently not be known. Bernoulli surface distortion occurring within the blade cascade is currently assumed to be small, and therefore neglected. Experimental results from this study and others have shown that for high turning turbine blading this is an unrealistic assumption. Incorporation of a Bernoulli surface tracing method into the loss prediction technique would give both a revised loss core distribution, and predict its peak position.

Experimental results show that the new endwall boundary layer, forming downstream of the pressure side leg of the leading edge vortex endwall separation line, is highly skewed. At present a simple two dimensional turbulent boundary layer is assumed to form downstream of the passage throat. The total pressure defect associated with this new endwall boundary layer is assumed to contribute directly to the cascade secondary losses. An estimate of endwall boundary layer skewing could be obtained knowing the spanwise tangential secondary velocity distribution in the blade row exit plane. The loss generated by such a skewed turbulent boundary layer could then be estimated from a suitable correlation.

Extra secondary losses are generated within the blade row, additional to the cascade inlet and new endwall boundary layer losses. This extra secondary loss is currently assumed to be directly proportional to the secondary kinetic energy, predicted using a classical method. Tangential secondary velocity components can be visualized to produce an incidence variation onto the following blade row, consequently providing a useful overall contribution. Radial components however could be assumed to contribute to the extra secondary loss generated. A simple approach would be to consider them directly proportional to these extra secondary losses, with revision as required to this initial assumption. At exit from the blading however the secondary velocities, a mean flow phenomenon, contribute to the total pressure field and are not strictly a loss at this point.

Fluctuating flow field results from this study, downstream of the blading, suggests very high turbulence levels close to high loss regions. As previously discussed additional hot wire probe traversing within the blading will show where this turbulent energy originates from the mean flow field. A considerable challenge would then be presented, to produce a realtively simple method of predicting this turbulent energy, using information available at the preliminary design stage.

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APPENDIX I

PRESSURE SENSING PROBES

I.i RECORDED DATA

At each probe position in the cascade the following differential pressure data was recorded:

1. Pressure transducer zero value
2. $P_L - P_R$
3. $P_C - P_L$
4. $P_C - P_R$
5. $P_T - P_B$
6. $P_{01} - P_C$
7. $P_{01} - P_1$

Each of the above pressure differentials were selected on the solenoid valve switch box, by an instruction from the 'Cifer'. Data for the three-hole probe skipped selection of $P_T - P_B$.

A conditioning differential pressure $(P_{01} - P_{AT})$ was also recorded but not used in the analysis.

The probe setting angle was recorded, as the rotary mounting setting, for use in defining the yaw angle relative to the cascade axis system.

I.ii DATA ANALYSIS

I.ii.i Calibration Functions

Four calibration functions identified by Schaub et al (1964) were defined as follows for each half of the α range:

Function	Positive α	Negative α
ϕ	$\dfrac{P_L - P_R}{P_C - P_R}$	$\dfrac{P_L - P_R}{P_C - P_L}$
ψ	$\dfrac{P_T - P_B}{P_C - P_R}$	$\dfrac{P_T - P_B}{P_C - P_L}$
σ	$\dfrac{P_C - P_R}{P_{01} - P_1}$	$\dfrac{P_C - P_L}{P_{01} - P_1}$
τ	$\dfrac{P_{01} - P_C}{P_C - P_R}$	$\dfrac{P_{01} - P_C}{P_C - P_L}$

Notes:

1. Figure 3.7 defines pressure probe tip notation.
2. ψ is not defined for the three-hole probe.

Clearly ϕ varies chiefly with $P_L - P_R$ (i.e. predominantly with α) and ψ with $P_T - P_B$ (i.e. predominantly with β).

Consequently we can define eight functions to be determined by probe calibration. Four for positive α

$\phi = f_1 (\alpha, \beta)$

$\psi = f_2 (\alpha, \beta)$

$\sigma = f_3 (\alpha, \beta)$

$\tau = f_4 (\alpha, \beta)$

and four for negative α

$\phi = f_5 (\alpha, \beta)$

$\psi = f_6 (\alpha, \beta)$

$\sigma = f_7 (\alpha, \beta)$

$\tau = f_8 (\alpha, \beta)$

I.ii.ii Evaluation of α_e and B_e

At each experimental data point values of ϕ_e and ψ_e were determined for positive and negative α. A simple straight line algorithm then determined if α was positive or negative.

Tabulated values of ϕ and ψ were held at 1^0 increments of α and β in four two dimensional data arrays, thus allowing for positive or negative α. Linear interpolation using both ϕ and ψ tables located ϕ_e and ψ_e, and consequently defined α_e and β_e, the experimental flow angles onto the probe sensing tip.

Values of σ_e and ψ_e were then evaluated from the bi-cubic spline coefficients, or the polynomial coefficients for the three-hole probe.

I.ii.iii Cascade Flow Angles

Conversion from calibration angles (α and β) to cascade flow angles (ε and λ) was performed using the following:-

$$\varepsilon = 90 - \theta + \alpha$$

$$\lambda = \tan^{-1}\left[\frac{\tan\beta}{\cos\varepsilon}\right]$$

I.ii.iv Total Velocity

At calibration assuming positive α

$$\sigma = \frac{P_C - P_R}{P_{01} - P_1}$$

In the cascade we evaluate σ_e knowing α_e and β_e:

$$\sigma_e = \frac{P_C - P_R}{P_{OLOC} - P_{LOC}}$$

$$= \frac{P_C - P_R}{1/2\, \rho\, V^2_{LOC}}$$

thus

$$V_{LOC} = \sqrt{\frac{2}{\rho} \frac{(P_C - P_R)}{\sigma_e}}$$

I.ii.v Component Velocitieis

Trigonometric relationships were used to evaluate the velocity components from the total velocity and flow angles at an experimental point:-

$$V_S = V_{LOC} \sin \beta$$

$$V_T = V_{LOC} \cos \beta \sin \varepsilon$$

$$V_A = V_{LOC} \cos \beta \cos \varepsilon$$

I.ii.vi Total Pressure Loss Coefficient

Assuming positive α then at calibration:-

$$\tau = \frac{P_{01} - P_C}{P_C - P_R}$$

but in the cascade we evaluate τ_e knowing α_e and β_e:-

$$\tau_e = \frac{P_{OLOC} - P_C}{P_C - P_R}$$

$$= \frac{(P_{01} - P_C) - (P_{01} - P_{OLOC})}{P_C - P_R}$$

Total pressure drop from upstream probe to local cascade probe is:-

$$P_{01} - P_{OLOC} = (P_{01} - P_C) - \tau_e(P_C - P_R)$$

The total pressure loss coefficient at any cascade traverse point becomes:-

$$\zeta = \frac{P_{01} - P_{0LOC}}{1/2\ \rho V_1^2} = \frac{(P_{01} - P_C) - \tau_e (P_C - P_R)}{(P_{01} - P_1)}$$

I.ii.vii Static Pressure Coefficient

Since $P = P_0 - 1/2\ \rho V^2$

then:-

$$P_1 - P_{LOC} = (P_{01} - P_{0LOC}) - 1/2\ \rho (V_1^2 - V_{LOC}^2)$$

the static pressure coefficient can be defined as:-

$$\chi = \frac{P_1 - P_{LOC}}{1/2\ \rho V_1^2} = \zeta - \frac{V_1^2 - V_{LOC}^2}{V_1^2}$$

APPENDIX II

CONSTANT REYNOLDS NUMBER CASCADE OPERATION

Reynolds number is defined as:-

$$Re = \frac{\rho V \ell}{\mu}$$

then for constant Re on two days (suffices M and N)

$$\frac{\rho_M V_M \ell_M}{\mu_M} = \frac{\rho_N V_N \ell_N}{\mu_N}$$

As:

1. the length term is constant ($\ell_M = \ell_N$)

2. $V = \sqrt{\dfrac{2 (P_0 - P)}{\rho}}$

3. $\rho = \dfrac{P_{AT}}{RT}$

then:

$$\frac{P_{ATM}}{RT_M} \sqrt{\frac{2 (P_{OM} - P_M) RT_M}{P_{ATM}}} \cdot \frac{1}{\mu_M}$$

$$= \frac{P_{ATN}}{RT_N} \sqrt{\frac{2 (P_{ON} - P_N) RT_N}{P_{ATN}}} \cdot \frac{1}{\mu_N}$$

$$\frac{P_{ATM} (P_{OM} - P_M)}{\mu_M^2 T_M} = \frac{P_{ATN} (P_{ON} - P_N)}{\mu_N^2 T_N}$$

Thus

$$(P_{OM} - P_M) = (P_{ON} - P_N) \frac{P_{ATN}}{P_{ATM}} \left[\frac{\mu_M}{\mu_N}\right]^2 \frac{T_M}{T_N}$$

Defining day N as the standard to which all pressure differentials should be corrected then the above defines the correction factor. This factor was used to evaluate the required wind tunnel working section velocity, to give constant Reynolds number on day M. The achieved and required dynamic head on day M were used to evaluate a pressure correction factor which was then applied to all measured differential pressures thus correcting them to standard day conditions. Hot wire probe data was corrected by the square root of this factor, since this was directly related to velocity and not pressure.

APPENDIX III

HOT WIRE PROBES

III.i RECORDED DATA

Two sets of hot wire probe traverses were performed, one with XY wires and the other with XZ wires. The sensing elements of the XY wires were parallel to the cascade endwall, and those of the XZ wires parallel to the local streamwise direction (i.e. five-hole probe yaw angle). The probe setting angle was recorded at each traversing point.

III.i.i Sensitivity Data

Sensitivity of wire 1 [S_{XY} (+45) or S_{XZ} (+45)]

Sensitivity of wire 2 [S_{XY} (-45) or S_{XZ} (-45)]

III.i.ii Signal Conditioner Settings

Offset voltages for wires 1 and 2 [off_1 and off_2]

Amplifier gain settings [g_1 and g_2]

III.i.iii Turbulence Processor Settings

Amplifier gain settings [tpg1 and tpg2]

III.i.iv Hot Wire Signals Recorded

Denoting $\bar{E}_1$ the mean signal of wire 1 and e_1 as the instantaneous component of the same signal (such that $\bar{e}_1=0$). The following data was recorded:-

1. $(\bar{E}_1 - off_1)*g_1$
2. $(\sqrt{\bar{E}_1^{\,2} + \bar{e}_1^2} - off_1)*g_1$
3. $(\bar{E}_2 - off_2)*g_2$

4. $\left(\sqrt{\bar{E}_2^{\,2} + \overline{e_2^2}^{\,2}} - off_2\right) * g_2$

5. $\overline{e_1 e_2} * g_1 * g_2 * t_{pg1} * t_{pg2}$

From the above signal values recorded the following data was derived:

$\bar{E}_1$, $\overline{e_1^2}$, $\bar{E}_2$, $\overline{e_2^2}$, $\overline{e_1 e_2}$

III.ii HOT WIRE PROBE ANALYSIS

III.ii.i Derivation of Analysis Equations

For a hot wire in the x-y plane we can identify three mutually perpendicular instantaneous component velocities two of which, u and v are in the x-y plane. (Figure AIII.1)

1. Normal to the wire velocity component in x-y plane

   $V_L = u \cos \alpha + v \sin \alpha$

2. Along the wire velocity component in x-y plane

   $V_M = -u \sin \alpha + v \cos \alpha$

3. Normal to the wire velocity component perpendicular to x-y plane

   $V_N = w$

Define the effective wire cooling velocities as:-

$$V_{eff}^2 = V_L^2 + k^2 V_M^2 + h^2 V_N^2$$

(NOTE - for ideal wires $k = 0$ and $h = 1$ however here $k = 0.13$ and $h = 1.05$ after Durao and Whitelaw (1974)).

$$V_{eff}^2 = \frac{E^2}{S^2} = (u \cos \alpha + v \sin \alpha)^2$$

$$+ k^2(-u \sin \alpha + v \cos \alpha)^2 + h^2 w^2$$

$$= (\cos^2 \alpha + k^2 \sin^2 \alpha) u^2$$

$$+ (\sin^2 \alpha + k^2 \cos^2 \alpha) v^2$$

$$+ \sin 2\alpha(1-k^2)uv + h^2 w^2$$

each instantaneous velocity can be considered to consist of a mean and fluctuating component. Thus:-

$$u = \bar{u} + u'$$

$$v = \bar{v} + v'$$

$$w = \bar{w} + w'$$

Consequently

$$\frac{E^2}{S^2} = (\cos^2 \alpha + k^2 \sin^2 \alpha)(\bar{u} + u')^2 +$$

$$(\sin^2 \alpha + k^2 \cos^2 \alpha)(\bar{v} + v')^2 +$$

$$\sin 2\alpha\,(1 - k^2)(\bar{u} + u')(\bar{v} + v') +$$

$$h^2 (\bar{w} + w')^2 \qquad (1)$$

Defining

$$\nu = \frac{\sin^2 \alpha + k^2 \cos^2 \alpha}{\cos^2 \alpha + k^2 \sin^2 \alpha}$$

$$\mu = \frac{\sin 2\alpha\,(1-k^2)}{\cos^2 \alpha + k^2 \sin^2 \alpha}$$

$$\phi = \frac{h^2}{\cos^2 \alpha + k^2 \sin^2 \alpha}$$

substituting into (1) and expanding gives:-

$$\frac{E^2}{S^2} = \bar{u}^2 \left(\cos^2 \alpha + k^2 \sin^2 \alpha\right) \left[1 + \frac{2u'}{\bar{u}} + \frac{u'^2}{\bar{u}^2} + \nu \left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{2\bar{v}v'}{\bar{u}^2} + \frac{v'^2}{\bar{u}^2} \right] + \mu \left[\frac{\bar{u}\bar{v}}{\bar{u}^2} + \frac{\bar{v}u'}{\bar{u}^2} + \frac{\bar{u}v'}{\bar{u}^2} + \frac{v'u'}{\bar{u}^2} \right] + \phi \left[\frac{\bar{w}^2}{\bar{u}^2} + \frac{2\bar{w}w'}{\bar{u}^2} + \frac{w'^2}{\bar{u}^2} \right] \right] \qquad (2)$$

Evaluating the square root of the above expression using the Binomial theorem and assuming:-

1. With wire prongs aligned to the flow then $\bar{u} > \bar{v}$ so $\bar{u}^2 >> \bar{v}^2$. Also $\bar{u} > \bar{w}$ so $\bar{u}^2 >> \bar{w}^2$.

2. Third and higher order correlations of the fluctuating terms are very small compared to the second order correlations.

Then

$$\frac{E}{S} = \bar{u}\sqrt{\left(\cos^2 \alpha + k^2 \sin^2 \alpha\right)} \left[1 + \frac{u'}{\bar{u}} + \left[\frac{\nu}{2} - \frac{\mu^2}{8} \right] \left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{2\bar{v}v'}{\bar{u}^2} + \frac{v'^2}{\bar{u}^2} \right] + \frac{\mu}{2} \left[\frac{\bar{v}}{\bar{u}} + \frac{v'}{\bar{u}} \right] + \frac{\phi}{2} \left[\frac{\bar{w}^2}{\bar{u}^2} + \frac{2\bar{w}w'}{\bar{u}^2} + \frac{w'^2}{\bar{u}^2} \right] \right] \qquad (3)$$

Since $E = \bar{E} + e$

then $\bar{E} = \overline{\bar{E} + e} = \bar{E}$ since by definition $\bar{e} = 0$

thus:

$$\frac{\bar{E}}{S} = \bar{u}\sqrt{(\cos^2\alpha + k^2\sin^2\alpha)}\left[1 + \left[\frac{\nu}{2} - \frac{\mu^2}{8}\right]\left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{\overline{v'^2}}{\bar{u}^2}\right] + \frac{\mu}{2}\frac{\bar{v}}{\bar{u}} + \frac{\phi}{2}\left[\frac{\bar{w}^2}{\bar{u}^2} + \frac{\overline{w'^2}}{\bar{u}^2}\right]\right] \tag{4}$$

Evaluating the mean of (2) gives:-

$$\frac{\overline{E^2}}{S^2} = \bar{u}^2\ (\cos^2\alpha + k^2\sin^2\alpha)\left[1 + \frac{\overline{u'^2}}{\bar{u}^2} + \nu\left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{\overline{v'^2}}{\bar{u}^2}\right] + \mu\left[\frac{\overline{uv}}{\bar{u}^2} + \frac{\overline{u'v'}}{\bar{u}^2}\right] + \phi\left[\frac{\bar{w}^2}{\bar{u}^2} + \frac{\overline{w'^2}}{\bar{u}^2}\right]\right] \tag{5}$$

Squaring (4) and making the same assumptions used when expanding (2) above gives

$$\frac{\bar{E}^2}{S^2} = \bar{u}^2\ (\cos^2\alpha + k^2\sin^2\alpha)$$

$$\left[1 + \nu\left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{\overline{v'^2}}{\bar{u}^2}\right] - \frac{\mu^2}{4}\frac{\overline{v'^2}}{\bar{u}^2} + \mu\frac{\bar{v}}{\bar{u}} + \phi\left[\frac{(\bar{w}^2 + \overline{w'^2})}{\bar{u}^2}\right]\right] \tag{6}$$

As

$$E = \bar{E} + e$$

$$E^2 = \bar{E}^2 + 2\bar{E}e + e^2$$

$$\overline{E^2} = \bar{E}^2 + \overline{e^2}$$

$$\frac{\overline{e^2}}{S^2} = \frac{\overline{E^2} - \bar{E}^2}{S^2} \text{ (i.e. equation (5) - equation (6))}$$

$$= \bar{u}^2 (\cos^2 \alpha + k^2 \sin^2 \alpha) \left[\frac{\overline{u'^2}}{\bar{u}^2} + \frac{\mu^2}{4} \frac{\overline{v'^2}}{\bar{u}^2} + \mu \frac{\overline{v'u'}}{\bar{u}^2} \right] \quad (7)$$

For a pair of crossed hot wires with $\alpha = \pm 45^0$

$\nu = 1$ for $\alpha = \pm 45^0$

$\mu = \dfrac{2\ (1-k^2)}{1 + k^2}$ for $\alpha = + 45^0$

$\mu = \dfrac{-2\ (1-k^2)}{1 + k^2}$ for $\alpha = - 45^0$

$\phi = \dfrac{2\ h^2}{1 + k^2}$ for $\alpha = \pm 45^0$

After substitutions and some manipulation the following equations can be derived for XY wire pairs at $\pm 45^0$

$$\frac{\bar{E}_{XY(+45)}}{S_{XY(+45)}} + \frac{\bar{E}_{XY(-45)}}{S_{XY(-45)}} = \bar{u}\sqrt{2\ (1+k^2)} \left[1 + \frac{2\ k^2}{(1+k^2)^2} \left[\frac{\bar{v}^2}{\bar{u}^2} + \frac{\overline{v'^2}}{\bar{u}^2} \right] + \frac{h^2}{1+k^2} \left[\frac{\bar{w}^2}{\bar{u}^2} + \frac{\overline{w'^2}}{\bar{u}^2} \right] \right] \quad (XY1)$$

$$\frac{\bar{E}_{XY(+45)}}{S_{XY(+45)}} - \frac{\bar{E}_{XY(-45)}}{S_{XY(-45)}} = \bar{v}\sqrt{2(1+k^2)}\ \frac{(1-k^2)}{(1+k^2)} \quad (XY2)$$

$$\frac{\overline{e^2}_{XY(+45)}}{S^2_{XY(+45)}} - \frac{\overline{e^2}_{XY(-45)}}{S^2_{XY(-45)}} = 2(1-k^2)\ \overline{v'u'} \tag{XY3}$$

$$\frac{\overline{e^2}_{XY(+45)}}{S^2_{XY(+45)}} + \frac{\overline{e^2}_{XY(-45)}}{S^2_{XY(-45)}} + 2\,\frac{\overline{e_{XY(+45)}\cdot e_{XY(-45)}}}{S_{XY(+45)}\cdot S_{XY(-45)}}$$

$$= 2(1+k^2)\ \overline{u'^2_{xy}} \tag{XY4}$$

$$\frac{\overline{e^2}_{XY(+45)}}{S^2_{XY(+45)}} + \frac{\overline{e^2}_{XY(-45)}}{S^2_{XY(-45)}} - 2\,\frac{\overline{e_{XY(+45)}\cdot e_{XY(-45)}}}{S_{XY(+45)}\cdot S_{XY(-45)}}$$

$$= \frac{2\,(1-k^2)^2}{1+k^2}\ \overline{v'^2} \tag{XY5}$$

Similarly for an XZ wire pair:-

$$\frac{\overline{E}_{XZ(+45)}}{S_{XZ(+45)}} + \frac{\overline{E}_{XZ(-45)}}{S_{XZ(-45)}} = \overline{u}\sqrt{2(1+k^2)}\left[1 + \right.$$

$$\left.\frac{2k^2}{(1+k^2)^2}\left[\frac{\overline{w}^2}{\overline{u}^2} + \frac{\overline{w'^2}}{\overline{u}^2}\right] + \frac{h^2}{1+k^2}\left[\frac{\overline{v}^2}{\overline{u}^2} + \frac{\overline{v'^2}}{\overline{u}^2}\right]\right] \tag{XZ1}$$

$$\frac{\overline{E}_{XZ(+45)}}{S_{XZ(+45)}} - \frac{\overline{E}_{XZ(-45)}}{S_{XZ(-45)}} = \overline{w}\sqrt{2(1+k^2)}\ \frac{(1-k^2)}{(1+k^2)} \tag{XZ2}$$

$$\frac{\overline{e^2}_{XZ(+45)}}{S^2_{XZ(+45)}} - \frac{\overline{e^2}_{XZ(-45)}}{S^2_{XZ(-45)}} = 2(1-k^2)\ \overline{w'u'} \tag{XZ3}$$

$$\frac{\overline{e^2}_{XZ(+45)}}{S^2_{XZ(+45)}} + \frac{\overline{e^2}_{XZ(-45)}}{S^2_{XZ(-45)}} + \frac{2\ \overline{e_{XZ(+45)} \cdot e_{XZ(-45)}}}{S_{XZ(+45)} \cdot S_{XZ(-45)}}$$

$$= 2(1+k^2)\ \overline{u'^2_{XZ}}$$

(XZ4)

$$\frac{\overline{e^2}_{XZ(+45)}}{S^2_{XZ(+45)}} + \frac{\overline{e^2}_{XZ(-45)}}{S^2_{XZ(-45)}} - \frac{2\ \overline{e_{XZ(+45)} \cdot e_{XZ(-45)}}}{S_{XZ(+45)} \cdot S_{XZ(-45)}}$$

$$= 2\ \frac{(1-k^2)^2}{1+k^2}\ \overline{w'^2}$$

(XZ5)

III.ii.ii ANALYSIS OF EXPERIMENTAL DATA

Data from two traverses, one with an XY and the other with a XZ probe, were analysed in parallel.

XY wires

Solve for $\bar{v}$ from XY2

Solve for $\overline{v'^2}$ from XY5

Solve for $\overline{v'u'}$ from XY3

Solve for $\overline{u'^2}_{XY}$ from XY4

XZ wires

Solve for $\bar{w}$ from XZ2

Solve for $\overline{w'^2}$ from XZ5

Solve for $\overline{w'u'}$ from XZ3

Solve for $\overline{u'^2}_{XZ}$ from XZ4

Solution for u:

Equation XY1 can be rewritten as quadratic:-

$$\overline{u}^2_{XY}\sqrt{2(1+k^2)} - \overline{u}_{XY}\left[\frac{\overline{E}_{XY(+45)}}{S_{XY(+45)}} + \frac{\overline{E}_{XY(-45)}}{S_{XY(-45)}}\right] +$$

$$\sqrt{2(1+k^2)}\left[\frac{2\,k^2}{(1+k^2)^2}\left(\overline{v}^2 + \overline{v'^2}\right) + \frac{h^2\left(\overline{w}^2 + \overline{w'^2}\right)}{1+k^2}\right] = 0$$

this is soluble for $\overline{u}_{XY}$ using the $\overline{w}$ and $\overline{w'^2}$ data from the XZ wires evaluated above.

Similarly $\overline{u}_{XZ}$ can be evaluated from equation XZ1 using $\overline{v}$ and $\overline{v'^2}$ data from the XY wires.

Differences in $\overline{u}_{XY}$ and $\overline{u}_{XZ}$, $\overline{u'^2}_{XY}$ and $\overline{u'^2}_{XZ}$ were assumed to be a function of probe positioning and sensor contaminaton. The mean value of $\overline{u}$ and $\overline{u'^2}$ was used in the data analysis.

Mean flow velocity data was evaluated from the following relationships. (Fig AIII.2)

$$V_A = \overline{u}\cos\theta - \overline{v}\sin\theta$$

$$V_T = \overline{u}\sin\theta + \overline{v}\cos\theta$$

$$V_S = \overline{w}$$

where θ is the hot wire probe setting angle (i.e. measured five-hole probe yaw angle).

Mean flow angle data was then derived from the following:-

$$\varepsilon = \tan^{-1}\left[\frac{V_T}{V_A}\right]$$

$$\lambda = \tan^{-1}\left[\frac{V_S}{V_A}\right]$$

APPENDIX IV

DURHAM CASCADE INLET BOUNDARY LAYER DATA

Boundary Layer Condition	δ^* (mm)	$\overline{Y}$	δ_a (mm)	n	δ_b (mm)	θ_a (mm)	θ_b	$\frac{\delta^*}{\theta_a}$	$\frac{\delta^*}{\theta_b}$
Natural	12.14	0.0845	116	0.117	102	9.8	9.9	1.23	1.23
Thickened	27.88	0.1752	164	0.205	150	19.8	19.7	1.41	1.42
Thinned	9.67	0.0532	52	0.230	82	6.7	7.6	1.44	1.27

δ^* — Inlet boundary layer displacement thickness evaluated by integrating mass meaned slot 1 data.

$\overline{Y}$ — Mass meaned total pressure loss coefficient upstream (at slot 1)

δ_a — Inlet boundary layer thickness (Power law form of index 'n') to give experimental δ^* and Y

δ_b — Estimated inlet boundary layer thickness from upstream velocity profile at slot 1

θ_a — Inlet boundary layer momentum thickness evaluated using $\dfrac{\delta_a\, n}{(2n+1)(n+1)}$

θ_b — Inlet boundary layer momentum thickness evaluated by integrating mass meaned slot 1 data