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**STEADY AERODYNAMIC PERFORMANCE OF A VERY
HIGHLY-LOADED LOW-PRESSURE TURBINE AIRFOIL**

by

Wu Dai

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

Master's of Applied Science

In Aerospace Engineering

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Dedicated to

my family

for their love, sacrifice, support, tolerance and encouragement.

Abstract

The current requirements for lowering the weight of the low-pressure turbines of modern aircraft engines have resulted in high-loaded blades with strong adverse pressure gradients over the aft suction surfaces. The stronger adverse pressure gradients increase the risk of boundary layer separation and the accompanying higher losses. In some cases the separated boundary layer fails to reattach to the surface resulting in very high profile losses. This thesis investigates the aerodynamic performance of a very highly-loaded low-pressure turbine airfoil, PAKD-A.

Measurements were made in a low speed linear cascade wind tunnel under steady flow conditions. The first investigation is to examine the influences of the typical levels of Reynolds number and freestream turbulence intensity on the blade loadings and the profile losses at the midspan of the blade. The second investigation is to examine the extent of the two-dimensional flow in the cascade by means of flow field measurements at inlet and outlet of the cascade across the full spanwise extent of the blade. Comparisons are made with the results for a more lightly-loaded airfoil, PAK-B, that was designed for the same inlet and outlet flow conditions.

It was found that PAKD-A has higher profile losses than PAK-B as expected. Both airfoils were found to have separation bubbles on the suction surface. The significant difference between them is that the PAKD-A separation bubble appears to “burst” and fails to reattach at a particular Reynolds number, while the length of the PAK-B separation bubble continues to increase with decreasing Reynolds number. Flow field measurements showed that all the measurements performed at midspan of cascade in the current study are essentially two-dimensional.

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List of Symbols

A = Area

$$\text{AVR} = \text{Axial velocity ratio} = \frac{\int_0^1 (V_e \cos \alpha_e)_{ms} d\left(\frac{y}{s}\right)}{\int_0^1 (V_i \cos \alpha_i)_{ms} d\left(\frac{y}{s}\right)}$$

$$\text{AVDR} = \text{Axial velocity density ratio} = \frac{\int_0^1 (\rho_e V_e \cos \alpha_e)_{ms} d\left(\frac{y}{s}\right)}{\int_0^1 (\rho_i V_i \cos \alpha_i)_{ms} d\left(\frac{y}{s}\right)}$$

B = True chord length

B_x = Blade axial chord

$$C_d = \text{Dissipation coefficient} = \frac{T\dot{S}_a}{\rho V_e^3}$$

$$C_{po} = \text{Total pressure coefficient} = \frac{P_{oref} - P_o}{P_{oref} - P_{sref}}$$

$$C_{pl} = \text{Static pressure distribution coefficient} = \frac{P_{oi} - P_{sl}}{P_{oi} - P_{si}}$$

$$C_{Pn} = \text{Three-hole probe port pressure coefficient} = \frac{P_n - P_{sref}}{P_{oref} - P_{sref}}$$

$$C_{ps} = \text{Static pressure coefficient} = \frac{P_s - P_{sref}}{P_{oref} - P_{sref}}$$

C_q	= Dynamic pressure coefficient = $\frac{P_o - P_s}{P_{oref} - P_{sref}}$
C_v	= Velocity ratio = $\frac{V}{V_{ref}}$
E	= Voltage
$f_s(\Delta x)$	= Spatial correlation coefficient function = $\frac{\overline{v'(x)v'(x + \Delta x)}}{v'^2}$
$f_t(\Delta t)$	= Autocorrection coefficient function = $\frac{\overline{v'(t)v'(t + \Delta t)}}{v'^2}$
H	= Blade span; Shape factor
i	= Incidence angle = $\alpha_i - \beta_i$
K	= Three-hole probe calibration coefficient
$\dot{m}$	= Mass flow rate per unit span
O	= Cascade throat
P_o	= Total pressure
$\overline{P_o}$	= Mass-averaged total pressure
P_{C1}	= Wind tunnel contraction inlet static pressure
P_{C2}	= Wind tunnel contraction outlet static pressure
P_n	= Three-hole probe port pressure (n=1, 2, 3)
P_s	= Static pressure
P_{sl}	= Surface static pressure
ΔP	= Transducer pressure difference
Q	= Dynamic pressure

Re	= Cascade Reynolds number = $\frac{\rho V_i B_x}{\mu}$
Re _w	= Probe Reynolds number = $\frac{\rho V_i w}{\mu}$
Re _{δ*}	= Boundary layer displacement thickness Reynolds number = $\frac{\rho V_e \delta^*}{\mu}$
Re _θ	= Boundary layer momentum thickness Reynolds number = $\frac{\rho V_e \theta}{\mu}$
t	= Time; Blade thickness
t ₀	= Starting time
T	= Static temperature
tet	= Blade trailing edge thickness
Δt	= Time difference
S	= Blade spacing; Entropy
V	= Mean velocity
v	= Instantaneous velocity
v'	= Root-mean-square (RMS) of fluctuating velocity
w	= Three-hole probe width
x	= Axial location; Streamwise location
y	= Pitchwise location
Y	= Total pressure loss coefficient = $\frac{\overline{P_{oi}} - \overline{P_{oe}}}{\overline{P_{oe}} - \overline{P_{se}}}$
Y _m	= Mixed-out profile loss coefficient = $\frac{\overline{P_{oi}} - \overline{P_{om}}}{\overline{P_{om}} - \overline{P_{sm}}}$

Y_p	= Profile loss coefficient (Figure 5.23, 5.24)
Z	= Zweifel coefficient = $2\left(\frac{S}{B_x}\right)\cos^2\alpha_e\left(\tan\alpha_e + \frac{V_{xi}}{V_{xe}}\tan\alpha_i\right)$
α	= Flow angle
β	= Blade mental angle
γ	= Blade stagger angle
δ	= Boundary layer thickness; Deviation angle;
δ^*	= Boundary layer displacement thickness = $\int_0^\delta 1 - \left(\frac{V}{V_e}\right) dy$
θ	= Boundary layer momentum thickness = $\int_0^\delta \frac{V}{V_e} \left(1 - \frac{V}{V_e}\right) dy$
Λ	= Calculated integral length scale = $\int_0^\infty f_s(\Delta x) d\Delta x$
ν	= Kinematic viscosity
ρ	= Air density
σ	= Standard deviation

Subscripts

a	= Per unit surface area
e	= Cascade outlet; Edge of boundary layer
i	= Cascade inlet
m	= Mixed-out
max	= Maximum
MD	= Maximum displacement

mim	= Minimum
ms	= Midspan
s	= Separation
t	= Transition onset
T	= Transition completion
ref	= Reference condition
R	= Reattachment
x	= Axial component
y	= Pitchwise component

Superscript

'	= Fluctuation
—	= Average

Abbreviations

AMDC	= Ainley/Mathieson/Dunham/Came loss system
AVR	= Axial velocity ratio
AVDR	= Axial velocity density ratio
FSTI	= Freestream turbulence intensity
LPT	= Low-pressure turbine
LSTL	= Low-Speed Turbomachinery Laboratory
K-H	= Kelvin-Helmholtz
T-S	= Tollmien-Schlichting

Chapter 1

Introduction

The low-pressure turbine plays an important role in a high bypass ratio commercial aircraft turbofan engine. In a high bypass ratio engine, the fan which is driven by the low-pressure turbine generates up to eighty percent of total thrust of the engine. Typically, the low-pressure compressor, electrical generator, oil pumps and accessory gearbox are also driven by the low-pressure turbine. To achieve high efficiency for the low-pressure turbine is one of the goals of the engine design and optimization. Typically, a one percent increment of the low-pressure turbine efficiency contributes a 0.7-0.9 percent increment in overall engine efficiency (Wisler, 1998). During the past 50 years, low-pressure turbine efficiency has increased 10 percent, but further improvements in efficiency are hard to achieve (Howell, et al, 2001).

For an aircraft engine, the weight is an important aspect for the designer and customer because a lighter engine can contribute the lower fuel consumption or higher aircraft loading for the same fuel consumption. For some engines the low-pressure turbine contributes one third of the total weight (Howell, et al, 2001). Reducing the weight of the low-pressure turbine is one of the targets for current

aircraft engine design and research. But to achieve the required engine thrust by reducing the number of low-pressure turbine blades, each blade has to turn more air mass flow. This will result in higher aerodynamic loading or tangential force for each low-pressure turbine blade. The high loading of the blades results in stronger adverse pressure gradients on the suction side of the airfoils and increases the risk of boundary layer separation, especially at low Reynolds number conditions. Flow separation can be tolerated if the separated flow reattaches on the surface before the blade trailing edge. The separation bubble produced by the reattachment of the separated flow generates relatively little additional profile losses. For low Reynolds number conditions, if the separation flow cannot reattach on the blade surface, the massive flow separation on the blade surface causes the very high profile losses and consequently poor efficiency.

The objective of the present project is to measure the aerodynamic behaviour of a modern very highly-loaded low-pressure turbine blade under steady conditions in a low speed cascade wind tunnel. The blade was designed by Pratt & Whitney, East Hartford, CT, and is designated PAKD-A, where -A indicates that the blade is aft-loaded. PAKD-A is designed for the same flow turning as the PAK-B blade which has been investigated at both steady and unsteady flow conditions by Mahallati (2003) and McAulliffe (2003). PAKD-A has a 25% higher space-to-chord ratio than PAK-B and is then much more highly loaded.

In first part of the project the static pressure distributions and boundary layer behaviors on the blade surfaces were measured at two freestream turbulence

intensities (FSTI=1.5% and 4.0%) with a wide variation of Reynolds number from 30000 to 150000. The Reynolds number is based on the inlet velocity at the cascade and the axial chord length of the blade. Profile loss measurements were also conducted under the same measurement conditions (FSTI and Reynolds number) as the loading measurements. All measurements in the first parts of the thesis were two-dimensional which were conducted at midspan of the airfoil and were intended to be essentially two-dimensional. The second part of thesis investigated the extent of the two-dimensional flow in the cascade by means of three-dimensional flow measurements at inlet and outlet of the cascade across the full spanwise extent of the blade. Typically, the low-pressure turbine has a high aspect ratio (5:1) (Howell, *et al.*, 2001). This means that a low-pressure turbine passage is mainly dominated by two-dimensional flow and the secondary flow influences relative small regions near the endwalls. For the high-lift PAKD-A blade which was tested in the cascade, the aspect ratio was 2.36 and the secondary flow development depends on the inlet boundary layer condition. Thus it was necessary to conduct the three-dimensional measurements at inlet and outlet to verify that essentially two-dimensional measurements had been obtained at the middle span of the blade.

In Chapter 2, the previous published literature on low-pressure turbines is reviewed. Especially, the recent research on high-lift low-pressure turbine blades is summarized. In Chapter 3, the experimental apparatus and measurement setup which were employed in the current study are described in detail. Chapter 4 presents the aerodynamic parameters that are used to describe the flow of the linear

cascade. The data reduction procedures for these parameters are given as well in this chapter. Chapter 5 presents and discusses the midspan experimental results for the PAKD-A airfoil, including the blade loading and profile loss measurement. Comparisons with the previous PAK-B measurements are also given in this chapter. Chapter 6 describes and discusses the results of the three-dimensional measurements and compares with the corresponding PAK-B three-dimensional results. Finally, conclusions and recommendations for further work are given in Chapter 7.

Chapter 2

Literature Review

2.0 Introduction

The goal of the present project was to measure the aerodynamic behavior of a modern very high-lift low-pressure turbine blade, PAKD-A and compare it with that of the baseline PAK-B blade. The purpose of this chapter is to review the previous literature related to high-lift low-pressure turbines.

In this chapter, Sections 2.1 present a review of separation and transition in boundary layers and separation bubbles. Section 2.2 reviews the secondary flow pattern in turbine cascades. Section 2.3 summarizes the recent experimental studies related to high lift low-pressure turbines. Additional discussion of specific results from the literature will be given in connection with the presentation of the results in Chapters 4 to 6.

2.1 Boundary Layer Transition

The aerodynamic characteristics of a turbine blade are closely related to the boundary layer development along its surfaces. In low-pressure turbines, a substantial portion of the airfoil surfaces may be covered by transitional flow (Mayle, 1991). Transition can be affected by different effects, such as pressure gradient, free-stream turbulence intensity, surface roughness, surface curvature, surface vibration, wall temperature, acoustic waves, flow unsteadiness, etc. These effects can influence the structure of the transition and the development of the boundary layer. A generally accepted classification proposed by Mayle (1991) shows that there are four important modes of transition in a boundary layer: natural transition, bypass transition, separated-flow transition and reverse transition. The first three transition modes are considered as primary in turbomachinery flow. The following sections discuss these three primary transition modes.

2.1.1 Natural Transition

The natural transition occurs in very low freestream turbulence intensity and begins with weak instability, as shown in Figure 2.1 (White, 1991), a schematic of natural boundary layer transition with zero pressure gradient. Under these conditions, the laminar boundary layer becomes unstable with respect to small disturbances and linear oscillations begin to grow when a critical momentum thickness Reynolds number is reached. These oscillations are known as Tollmien-Schlichting (T-S) instability waves. The two-dimensional T-S oscillations are

amplified within the boundary layer as they convect downstream at about 30% of the freestream velocity. The amplified T-S waves develop into three-dimensional waves and roll up into loop vortices. Eventually the rolled-up vortices break down and develop into turbulent spots. The turbulent spots grow in the longitudinal and lateral direction and convect downstream to coalesce eventually to form fully a turbulent boundary layer.

Figure 2.1: Schematic of natural transition process (White, 1991).

Since the freestream turbulence intensity is usually high in turbomachinery, natural transition is not common in these machines. Instead, transition usually occurs by either the bypass mode or the separated-flow mode.

2.1.2 Bypass Transition

Bypass transition is caused by high levels of freestream turbulence intensity or large surface roughness. These large disturbances can lead to a complete bypass of the Tollmien-Schlichting mode instability. Mayle (1991) suggested that the T-S waves' formation and possibly the vortex loops of natural transition can be bypassed. The turbulent spots appear as the boundary layer is disturbed by high freestream turbulence or high surface roughness and the spots then convect downstream within the boundary layer to develop the fully turbulent flow. This transition process significantly reduces the length of the unstable laminar flow and promotes earlier transition. Bypass transition is the normal transition mode for attached flows in the real engine environment. Rough blade surfaces also result in instability wave generation, largely bypassing the T-S wave formation and resulting in early turbulent spot development. Walker (1993) found that T-S waves could also be observed in the bypass transition mode, but that their streamwise extent is very short. Johnson and Dris (2000) propose a theory for the formation of turbulent spots during bypass transition.

2.1.3 Separated-Flow Transition

Laminar boundary layer separation commonly occurs in low-pressure turbines. Reynolds number, adverse pressure gradient and off-design flow angle can affect the laminar boundary layer separation. If the laminar boundary layer does not possess sufficient momentum to overcome the pressure rise downstream, the flow at the surface may be brought to rest and the boundary layer will be separated from the surface. This process is shown in Figure 2.2 (modified from Zhang, 2002). Once the boundary layer separates from the surface, at X_s , a free shear layer is formed. The region under the separated shear layer is relatively stagnant and sometimes referred to as the “dead-air” region, as described by Ward (1963) and Tani (1964). The free shear layer detached from the surface can not support a transverse pressure gradient and a pressure “plateau” region is formed, which is evident in the static pressure distribution. The shear flow detached from the surface is inherently unstable and highly susceptible to two inviscid instabilities, Kelvin-Helmholtz (K-H) and Tollmien-Schlichting (T-S) instabilities. The growth of disturbances in the shear layer leads to the onset of separated-flow transition, at X_t . It has been suggested that the end of the transition is located at the maximum bubble displacement, $X_t = X_{max}$. With the separated-flow transition process, the momentum mixing with freestream fluid may allow the detached shear layer to reattach on the surface, X_R . If the detached free shear layer does not possess or gain sufficient momentum to reattach to the surface, the airfoil is stalled. The fully separated boundary layer would cause high profile losses.

Figure 2.2: Schematic of separation bubble (modified from Zhang, 2002).

Separated-flow transition can occur in the free shear layer after a laminar boundary layer has separated. It is commonly accepted that detached laminar shear layer is inherently unstable and promotes the growth of disturbances, leading to early laminar-to-turbulent transition (Malkiel and Mayle, 1996). The laminar-to-turbulent transition in the separated-flow transition is assumed to be a result of the

superposition of the effects of different types of instabilities: Kelvin-Helmholtz (K-H) instability and Tollmien-Schlichting (T-S) instability. The first type of instability (K-H) can be associated with the separated laminar shear layer and near-wall low speed flow, which can be regarded as an asymmetric mixing layer, which is unstable due to the inflection point in the velocity profile (Morkovin, 1993). It occurs and develops at a specific location downstream of the separation point and is traceable to the presence of a maximum of mean velocity within the shear layer. The second type of instability (T-S) occurs at relatively large Reynolds numbers and mild adverse pressure gradients and is convected by the flow as it is amplified and interacts with the inflectional instability (Mayle, 1991).

Separated-flow transition occurs commonly in the compressors and low-pressure turbines of gas turbine engines. For compressors, the blades typically operate in a strong adverse pressure gradients environment, which can lead to laminar boundary separation. For low-pressure turbines, the blades generally operate at low Reynolds number and large regions of laminar flow cover the blade surfaces, which can result in laminar separation.

Critical studies of transitional phenomena in compressors and turbines were presented in the review papers of Mayle (1991) and Walker (1993). In their studies, they have given an overview of the recent developments, prediction methods and their application to compressor and turbine blade design.

Hatman and Wang (1999) formulated an improved approach for analyzing the separated-flow transition process in separated shear layers and proposed a

methodology for predicting the separated-flow transition and associated separated flow parameters. In their study, three main separated-flow transition modes were introduced, defined as transitional separation mode, laminar separation/ short bubble mode, and laminar separation/long bubble mode.

The transitional separation mode occurs as the laminar boundary layer separates at relatively high Reynolds number and low adverse pressure gradient. The onset of the transition can take place upstream of the boundary layer separation point. The earlier transition is similar to the natural transition in the attached boundary layers. The later transition is affected by the addition of the K-H instability and near-wall fluid ejection, which shortens the transition length. The location of the end of transition is close to the reattachment point.

The laminar separation/short bubble mode occurs at moderate Reynolds numbers and mild adverse pressure gradients. The onset of transition is induced downstream of the separation point by an inflectional instability at the location of maximum bubble displacement.

The laminar separation/short bubble mode is characterized by quick transition completion due to the interaction between the separated shear layer and the reverse-flow vortex. Transition completion may occur at or before reattachment or shortly downstream of reattachment. Both T-S instabilities and K-H instabilities have been observed for this short bubble mode. T-S type instabilities are initiated upstream of the separation point and are still present within the detached shear layer. The K-H instability sets in after the laminar shear layer detaches. The laminar

separation/long bubble mode is likely to occur as the laminar boundary layer separation takes place at low Reynolds numbers and strong adverse pressure gradients. The onset of transition occurs downstream of the laminar flow separation point. Due to the locally low Reynolds number, mixing is reduced and the separated shear boundary layer cannot reattach immediately. The transition completion is delayed. As mixing progresses between the main flow and the separated shear layer, the detached boundary layer becomes fully turbulent and eventually reattaches to the surface much further downstream. In some cases, the separated boundary layer may fail to reattach due to very strong adverse pressure gradients, resulting the bubble “bursting” and airfoil stall.

Yaras (2001, 2002) examined the separated-flow transition process in the laminar separation/short bubble mode of the separated shear layer. In his study, he suggested that in addition to the effects of Reynolds number and pressure gradient examined by Hatman and Wang (1999), freestream turbulence intensity also plays an important role in affecting the onset and length of transition in the separated-flow transition process. He found that with increasing the freestream turbulence intensity, transition onset takes places upstream of the maximum displacement location because the instability was increased by the elevated freestrem turbulence. Yaras proposed correlations for the locations of transition onset and flow reattachment based on the freestream turbulence and pressure gradient. According to his correlations, the transition characteristics can be predicted well in the cases he examined.

2.2 Secondary Flow

The present study is concerned mainly with the two-dimensional flow behavior at the midspan of the blades. To confirm that the flow was in fact two-dimensional near the midspan, it was necessary to measure the extent of the three-dimensional flow near the endwalls. This three-dimensional flow is often referred to as secondary flow

Secondary flow is a complex three-dimensional flow pattern near the endwalls of a turbomachinery blade passage. In this system, each vortical flow strongly interacts with the others. The losses produced by the secondary flow can account for as much as 30-50% of the total loss in a blade or stator row (Sharma and Butler, 1987).

The physics of the secondary flow has been studied since the 1940s and several physical secondary flow models have been published to describe this complicated flow pattern. A comprehensive review of the secondary flow structures and associated experimental information has been presented by Sieverding (1985). The secondary flow patterns proposed by Sharma and Butler (1987) are illustrated in Figure 2.3. The flow field near the endwall of the turbine airfoil passage is composed of a system of vortical flows that are caused by the inlet boundary layer separation and cross-passage flow within the blade passage. Physically, as the flow approaches the turbine airfoil passage, some of the inlet endwall boundary layer separates in the leading edge region of the blade due to the local adverse pressure gradient. The separated flow rolls up into a horseshoe vortex that wraps around the

leading edge with suction and pressure side legs. The pressure gradient between the pressure surface and suction surface causes the pressure side leg to migrate across the airfoil passage along the endwall towards the suction surface of the adjacent airfoil. This pressure difference also drives the endwall boundary layer with the blade passage towards the suction surface and together they constitute the passage vortex. The various vortices in the blade passage cause a significant total pressure loss. As implied by Figure 2.3, the secondary flow extends much farther across the blade passages than the thickness of the inlet boundary layer flow. In cascade experiments intended to investigate the two-dimensional blade flow behavior, it is therefore necessary to determine the spanwise extent of the secondary flow.

Figure 2.3: Schematic of secondary flow (Sharma and Bulter, 1987).

2.3 Overview of Previous Studies on Highly-Loaded Low-Pressure Turbine Airfoils

The desire to further improve the low-pressure turbine efficiency by optimizing the stage performance and decreasing the weight of LPTs has led to the design of high-lift, or high-loading, airfoils. The high loading of the blades results in stronger adverse pressure gradients on the suction side of the airfoils especially and increases the risk of boundary layer separation and stall, especially at low Reynolds number conditions. In this section, recent experimental investigations of the aerodynamic performance of low-pressure turbines are reviewed. The most relevant investigations are summarized and presented in Table 2.1. The abbreviations used in Table 2.1 are listed below:

Type of experiment:

FP	Flat plate experiment with imposed pressure gradient
LC	Linear cascade
SPC	Single passage cascade

Measurement locations:

BL	Boundary layer measurements
DS	Downstream measurements

Type of measurements:

3HP	Three-hole pressure probe
HFP	Hot-film probe
HW	Hot-wire probe
TPP	Total pressure probe
SFV	Smoke flow visualization
SHF	Surface-mounted hot-film
SPT	Surface pressure taps

Parameters investigated:

I	Incidence variation
LD	Loading distribution
Re	Reynolds number
Tu	Turbulence intensity
Z	Zweifel coefficient (See Section 5.1)

Table 2.1: Summary of experimental low-pressure turbine studies.

Reference	Type	Measurement Details							Conditions		Parameters Investigated					Comments	
		Location BL	3HP	HFP	HW	TPP	SFV	SHF	SPT	Re ($\times 10^3$)	Tu (%)	I	LD	Re	Tu		Z
Volino and Hultgren (2001)	FP	*							*	50-300 ³	0.2-7			*			-PAK-B profile -Suction surface pressure distribution -Investigation of separation and transition
					*												
Qui and Simon (1997)	SPC	*				*			*	50-300 ³	0.5-11			*	*		-PAK-B profile -Surface pressure distribution -Investigation of separation and transition
Volino (2002)	SPC	*				*			*	25-300 ³	0.5-9			*	*		-PAK-B profile -Surface pressure distribution -Investigation of separation and transition
Lake <i>et al.</i> , (1999)	LC	*	*		*					50-200 ¹	1.0-4.0			*	*		-PAK-B profile -Investigation of separation and transition
Murawski and Vafai (1999)	LC	*	*	*	*				*	50-300 ³	1			*			-PAK-B profile -Loading effects on separation bubble
Dorney <i>et al.</i> (2000)	LC	*	*	*	*				*	43-172 ¹	1			*	*		-PAK-B profile -Investigation of separation and transition
Mahallati (2003)	LC	*	*	*	*				*	25-150 ¹	0.4-4.5		*	*	*	1.076	-PAK-B profile -Surface pressure distribution/profile loss -Investigation of separation and transition
Schulte <i>et al.</i> (1998)	LC	*	*	*	*	*		*	*	130-210 ²	0.5-10		*	*	*	1.006	-A High-lift blade (Blade H) -Suction surface separation and transition
					*	*		*	*								
Howell <i>et al.</i> (2001)	LC	*	*	*	*	*		*	*	90-220 ²	0.5		*	*	*		-Blade/H/A/B/C/U ₁ /U ₂ (aft) -Surface pressure -Suction surface profile loss
Hodson <i>et al.</i> (2002)	LC	*	*	*	*	*		*	*	80-320 ²	0.5		*	*	*		-Blade/F/G/G2 -Surface pressure/profile loss
Brear <i>et al.</i> (2002)	LC	*	*	*	*	*		*	*	100-300 ²	0.6-7.4	$\pm 10^\circ$	*	*	*		-Blade A/B/C/D -Pressure surface separation -Iteration of pressure surface separation and secondary flow

1: Re based on inlet velocity (V_i) and axial blade chord (B_x)2: Re based on outlet velocity (V_o) and true blade chord (B)3: Re based on outlet velocity (V_o) and suction surface length (SSL)

Eleven experimental investigations are summarized in Table 2.1. Among these investigations, seven experiments have focused on the PAK-B airfoil, which is also the baseline airfoil in the present investigation.

Volino and Hultgren (2001) have performed detailed velocity measurements along a flat plate subject to the same pressure distribution as the suction side of the PAK-B profile. In their measurements, the boundary layer separation and transition characteristics with varying Reynolds number and freestream turbulence intensity were studied. Although the boundary layer development can be measured in detail in the flat plate facility, the flow periodicity and profile losses cannot be obtained.

Qui and Simon (1997) and Volino (2002) have conducted their measurements with the PAK-B geometry in a “cascade simulator”. This cascade simulator is a single-passage cascade test section which is composed of two large-scale PAK-B airfoils. Although the larger size and simpler geometry result in better probe access, the main disadvantage of the single passage test section cannot be avoided. In this type of arrangement, the flow periodicity is impossible and the downstream losses cannot be measured directly.

Lake *et al.* (1999), Murawski and Vafai (1999, 2000), Dorney *et al.* (2000), and Mahallati (2003) have performed PAK-B measurements in linear cascades test section. Lake *et al.* (1999) and Dorney *et al.* (2000) have performed measurements in the boundary layer and downstream of the cascade exit. The flow separation and transition characteristics on the suction surface of the PAK-B profile were presented in their work. It was found that flow separation and transition characteristics were

affected by the Reynolds number and freestream turbulence intensity. Increasing Reynolds number and freestream turbulence intensity resulted in quicker reattachment of the separated flow and a relative smaller separation bubble. Murawski and Vafai (1999, 2000) conducted measurements to examine the loading effects on the flow separation and transition and downstream turbulence levels. Mahallati (2003) performed experimental measurements of both steady and unsteady flows for the PAK-B blades. His experimental results for separation and transition characteristics agreed largely with those from Qui and Simon (1997), Volino and Hultgren (2001), and Volino (2002). However, Mahallati's measurements were much more extensive than the earlier studies. For a review of unsteady effects on PAK-B, see Mahallati (2003).

In addition, a number of significant studies on high-lift low-pressure turbines have been conducted in the low-speed cascade wind tunnel in the Whittle Laboratory, University of Cambridge. The Whittle Laboratory group, led by Professor Howard Hodson, has conducted both steady and unsteady aerodynamic measurements on a series of high-lift LPT blades.

Schulte *et al.* (1998) have investigated the development of the suction surface boundary layer of a high-lift low-pressure turbine blade (Blade H) in a linear cascade test section. It was found that the suction surface boundary layer featured a large separation bubble and high profile losses were obtained at lower Reynolds numbers. It was also found that a passing wake can significantly reduce the bubble size and the profile losses. Howell *et al.* (2001) examined the behavior of the

suction surface boundary layer on a related profile. Both steady and unsteady measurements were obtained with the results compared for Blade H. Howell *et al.* (2001) found that for the aft-loaded blade, the separated flow on the suction surface reattached closer to the trailing edge, which resulted in only a small amount of the blade surface being covered in turbulent boundary layer. This decreased the losses generated.

Breear *et al.* (2002) have investigated the behaviour of a thin, low-pressure turbine blade in a linear cascade, focusing on the pressure surface separation at midspan and the interaction between the pressure surface separation and the secondary flow. It was found that the pressure surface separation at the midspan depends on the incidence and can significantly contribute to the profile loss. It has been found that suppression of the pressure surface separation by thickening the profile can reduce profile loss by approximately 10%. The interaction of the pressure surface separation with the secondary flow can affect the development of the secondary flow and its loss.

Chapter 3

Experimental Apparatus

3.0 Introduction

The present experiments were conducted in the Low-Speed Turbomachinery Laboratory (LSTL) at Carleton University. All measurements were made in an open-circuit wind tunnel with a variable-incidence linear cascade test section. The cascade used in the current measurements consists of seven Pratt & Whitney PAKD-A airfoils. The measurements are compared with results for the baseline PAK-B airfoil obtained by Mahallati (2003) and McAuliffe (2003).

In this chapter the experimental apparatus used throughout the present research project is described. The first sections describe the wind tunnel, test section and cascade configuration. This is followed by an outline of the traverse gear used for the current measurements. The uncertainty analysis for the measurement is described in Section 3.3. The instrumentations of the experiment are presented in Section 3.4. The probe calibration rig and pressure probes are described in Section 3.5 and 3.6. The instrumentation and geometry of the loading blade and Fluid Wafer Switch system are described in Section 3.7 and 3.8. The data acquisition system is described briefly in Section 3.9.

3.1 Low Speed Wind Tunnel

3.1.1 Wind Tunnel

The open-circuit, low-speed wind tunnel used in the current project was commissioned by Roger (1992) and is composed of a radial blower, AC motor, honeycomb, diffuser, settling chamber and contraction, as shown in Figure 3.1.

Figure 3.1: Schematic drawing of low-speed wind tunnel (McAuliffe, 2003).

Through the bell-mouth, the inlet airflow is drawn into the wind tunnel by a radial blower, which is driven by an AC motor. The maximum air mass flow rate is about 5kg/s at a total pressure rise of about 120 mm of water. The AC power motor has 4 poles and its maximum output power is 40 hp. The rotating speed of AC motor can be controlled and adjusted by a remote digital-input frequency controller to satisfy the various velocity requirements in the cascade test section. The control input frequency is related to the motor rotating speed (RPM) by a factor of 30.

The swirl in the fan outlet flow is removed by a hexagonal honeycomb with 9.5-mm-wide and 250-mm-deep cells located downstream of the fan. Then the air is diffused by a rectangular diffuser with an outlet-to-inlet area ratio of 7 and an equivalent cone angle of 19°. The diffuser has five fine single-layered and evenly spaced stainless steel screens so the flow uniformity is improved. In the settling chamber, four fine steel screens are employed to improve the flow quality by reducing the turbulence length scale of the flow. The flow leaves the square settling chamber through a contraction with an inlet-to-outlet area ratio of 14. In the present experiments the velocity varies up to 30m/s at the inlet of the test section, and thus the flow through the test section can be regarded as incompressible.

Two static pressure taps (labeled P_{C1} and P_{C2} in Figure 3.1) are located at the inlet and outlet of the contraction respectively. They are used to monitor the wind tunnel operation point and provide the reference pressures for the differential pressure transducers during the experimental measurements.

3.1.2 Test Section and Cascade Geometry

Figure 3.2 shows a schematic drawing of the variable-incidence linear cascade test section which was designed and manufactured by Goobie (1989).

Figure 3.2: Schematic drawing of test section (modified from McAuliffe, 2003).

After leaving the contraction, the flow enters the test section which is rectangular with a height of 67.2 cm and a width of 20.3 cm. Downstream of the test section inlet, two adjustable slots are used to remove fluid and thus reduce the boundary layer thickness on the endwalls of test section. The linear cascade and its mounting plate are installed on an aluminum turntable. This turntable can be rotated

manually up to 60° to supply variable incidences for the linear cascade. For the present study, the turntable was set at the design incidence for the PAKD-A cascade.

The mounting plate of the cascade is flush with the turntable and it is a very convenient way to install or remove different cascades without changing the turntable settings. The turntable and mounting plate form the hub endwall of the cascade test section. The tip endwall, which is not shown in Figure 3.2, is a plexiglass window that is mounted on a wheeled steel frame. This plexiglass assembly gives an easy and convenient access to the cascade test section and allows visual inspection inside of the test section.

Inlet flow uniformity and outlet flow periodicity can be achieved by adjusting the control surfaces. The control surfaces consist of the bottom tailboard, top tailboard, bottom flap, top flap, blockage mechanism, bypass board and floating wall. The top flap (TF) and bottom flap (BF) are mainly used to control the flow uniformity at the entrance of the cascade. The top tailboard (TTB) and bottom tailboard (BTB) are primarily used to control the flow periodicity at the outlet of the cascade. The adjustment of the blockage mechanism can improve the inlet flow quality at the upper part of the cascade. The bypass flaps are used to control the mass flow rate through the cascade. The floating wall is used to adjust the height of the test section as the turntable is rotated to change the cascade incidence. All the experimental measurements in the present research project were conducted at the design incidence.

3.1.3 Linear Cascade

The cascade is composed of seven aft-loaded, high-lift PAKD-A blades and the detailed geometry is presented in Figure 3.3.

Axial Chord Length	$B_x = 76.2 \text{ mm}$	Inlet Metal Angle	$\beta_i = 35^\circ$
True Chord Length	$B = 84.8 \text{ mm}$	Outlet Metal Angle	$\beta_e = 60^\circ$
Blade Spacing	$S = 84.2 \text{ mm}$	Inlet Flow Angle	α_i
Blade Span	$H = 203.2 \text{ mm}$	Outlet Flow Angle	α_e
Trailing Edge Thickness	$t_{et} = 1.1 \text{ mm}$	Stagger Angle	$\gamma = 26^\circ$
Maximum Thickness	$t_{max} = 16.4 \text{ mm}$		

Figure 3.3: Geometry of the linear cascade and PAKD-A blade.

The aspect ratio (H/B) for the PAKD-A cascade is 2.4 with a solidity (B_x/S) of 1.1. Figure 3.3 also shows the locations of the measurement planes. All inlet measurements in the current study were made at 1.2 axial chord lengths (B_x) upstream of the leading edge and the outlet measurements were conducted at 0.4 axial chord lengths (B_x) downstream of the trailing edge.

Figure 3.4 presents the comparison between the PAKD-A and PAK-B cascades. Both have the same inlet and outlet metal angle, stagger angle and blade span. The significant difference is the blade spacing. PAKD-A has a 25% higher space-to-chord ratio than PAK-B and is therefore much more highly loaded. PAKD-A has 84.2 mm blade spacing and a Zweifel coefficient (Z) of 1.37 while PAK-B has 66.8 mm blade spacing and a Zweifel coefficient (Z) of 1.07. The Zweifel coefficient is a tangential force coefficient and is a commonly-used measure of blade loading in axial turbines. The definition of Zweifel coefficient is given by:

$$Z = 2 \left(\frac{S}{B_x} \right) \cos^2 \alpha_e \left(\tan \alpha_e + \frac{V_{xi}}{V_{xe}} \tan \alpha_i \right) \quad (3.1)$$

where V_{xi}/V_{xe} is the inlet-to-outlet axial velocity ratio. A more detailed discussion of Zweifel coefficient is given in Section 5.1.

	PAKD-A	PAK-B
Axial Chord Length (B_x)	76.2 mm	75.4mm
True Chord Length (B)	84.8 mm	83.3 mm
Blade Span (H)	203.2 mm	203.2 mm
Maximum Thickness (t_{max})	16.4 mm	10.7 mm
Trailing Edge Thickness (t_{et})	1.1 mm	1.1 mm
Stagger Angle (γ)	26°	26°
Inlet Metal Angle (β_i)	35°	35°
Outlet Metal Angle (β_e)	60°	60°
Blade Spacing (S)	84.2 mm	66.8 mm
Zweifel coefficient (Z)	1.36	1.07

Figure 3.4: Comparison of geometry for PAK-B and PAKD-A cascades.

3.1.4 Turbulence Grids

As described in Section 3.1.1, the turbulence level in the mean flow is determined by the screens in the settling chamber and the contraction ratio. In the settling chamber, the four fine steel screens are employed to improve the flow quality by reducing the turbulence length scale of the flow and the level of freestream turbulence intensity at the entrance of the test section is approximately 0.4%. But in the real engine environment, the freestream turbulence level in the low-pressure turbine is elevated due to the complex flow produced by combustion processes and blade row motion. Typically, the turbulence intensity in the low-pressure turbine is about 3% to 7% and the length scale is smaller than 10% of blade axial chord (McAuliffe, 2003). To simulate the flow in the real gas turbine more closely, turbulence grids have to be used to elevate the freestream turbulence intensity and control the turbulent length scale at the entrance to the cascade.

The freestream turbulence intensity (FSTI) is used to specify the random fluctuations in the magnitude of the velocity field. The instantaneous velocity in the main flow direction at a spatial location in a turbulent flow can be written as:

$$v(t) = V + v'(t) \quad (3.2)$$

where V is the mean velocity and $v'(t)$ is the time fluctuating velocity component in the flow direction. The freestream turbulence intensity (FSTI) is defined as:

$$FSTI = \frac{v'}{V} \times 100\% \quad (3.3)$$

where v' is the root-mean-square of the fluctuating component of the velocity and is given as:

$$v' = \sqrt{v'(t)^2} = \sqrt{\frac{1}{T} \int_{t_0}^{t_0+T} (v(t) - V)^2 dt} \quad (3.4)$$

and the mean velocity (V) is given by:

$$V = \frac{1}{T} \int_{t_0}^{t_0+T} v(t) dt \quad (3.5)$$

The integral length scale (Λ) is used to specify the dominant eddy size in a turbulent flow. Hinze (1975) discussed that the size of a typical large eddy can be determined from the spatial correlation between the velocity fluctuations at two points in the flow field. Assuming a homogeneous and isotropic turbulence, the integral length scale can be estimated from the correlation of fluctuations at two points in space simultaneously:

$$\Lambda = \int_0^\infty f_s(\Delta x) d\Delta x \quad (3.6)$$

where $f_s(\Delta x)$ is the spatial correlation coefficient function and is expressed as:

$$f_s(\Delta x) = \frac{\overline{v'(x)v'(x + \Delta x)}}{\overline{v'^2}} \quad (3.7)$$

The spatial correlation coefficient $f_s(\Delta x)$ can be calculated if the velocity fluctuation measurements are performed at two spatial points simultaneously in the flow field, which is not very practical because of the interference between the hotwire probes. However, Taylor frozen-turbulence-pattern hypothesis assumes that

the eddies vary little in the time in which the eddies convect the distance Λ at the mean flow velocity (V). Using the Taylor frozen-turbulence-pattern hypothesis, the spatial correlation coefficient function $f_s(\Delta x)$ can be estimated from the autocorrelation coefficient function $f_t(\Delta t)$, which is given by:

$$f_t(\Delta t) = \frac{\overline{v'(t)v'(t + \Delta t)}}{v'^2} \quad (3.8)$$

The spatial integral length is then given by:

$$\Lambda = V \int_0^\infty f_t(\Delta t) d\Delta t \quad (3.9)$$

In the current study, two turbulence generating grids have been used. The 1.5% turbulence grid was designed by Mahallati (2003) and is located $18.5 B_x$ upstream of the cascade middle blade. This grid generates a turbulent flow with a turbulence length scale of approximately 40% of the PAKD-A axial chord at the leading edge of the cascade middle blade. The 4.0% turbulence grid was designed by McAuliffe (2003) and is located $4.55 B_x$ upstream of the cascade middle blade, and generates a turbulent flow with a turbulence length scale of approximately 10% of the PAKD-A axial chord at the entrance of the cascade middle blade. For more details of these two grids, refer to Mahallati (2003) and McAuliffe (2003).

3.2 Probe Traverse Gear

The probe traversing mechanisms are mounted to the backside of the turntable and are used to move probes in blade spanwise and pitchwise directions at the inlet and outlet measurement planes. A traverse gear is composed of a motor controller, stepping motors and threaded driving rods. Through the computer parallel port, the Data Acquisition System sends the control signals to the motor controller. The four-phase stepping motor receives the signals from the motor controller and drives the threaded rod (1/4-20). The resulting minimum stepping size for the traverse mechanism is 0.00635 mm.

3.3 Uncertainty Analysis

Although experimental errors exist in all measurements and cannot be eliminated completely, we can estimate the uncertainty for the experimental results. The uncertainty defines an interval having a level of confidence of approximately 95%. There are several error analysis methods to estimate the uncertainty associated with experimental data. In this section, the single-sample uncertainties analysis (Moffat, 1982) has been used to estimate the uncertainties for the present experimental results.

The uncertainty of any experimental quantity can be estimated from the instrument calibration data. It is expressed as:

$$\partial f = \pm 2\sigma \quad (3.10)$$

where f is any independent variable and σ is the standard deviation of the calibration data. ∂f represents the uncertainty with 95% confidence if the scatter in the calibration data is normally distributed. If a dependent parameter F is a function of various independent parameters f_i , $F=F(f_1, f_2, f_3, \dots, f_n)$, the total uncertainty of this dependent variable (F) is given by:

$$\delta F = \sqrt{\sum_{i=1}^n \left(\frac{\delta F}{\delta f_i} \delta f_i \right)^2} \quad (3.11)$$

where n is the total number of fundamental parameters.

The uncertainties for the pressure probes and associated with any calculated results are quoted in their respective sections.

3.4 Instrumentation

3.4.1 Pressure Transducers

Pressure transducers are used to convert the pressure signals to electrical signals. Depending on the range of the pressure measurements, three types of Data Instrument differential pressure transducer were used: DRAL501 DN (± 250 Pa), DRAL505 DN (± 1250 Pa) and DRAL520 DN (± 5000 Pa). All of these pressure transducers use silicon micro-mechanical sensing element to sense the pressure differences and provide an electrical signal to a Wheatstone bridge and a differential

amplifier. The calibration of the pressure transducers were conducted by using a water micro-manometer (Benner, 2003) with full scale range of 0-30 cm water. For the transducer calibration, 50 to 150 points were usually used to perform the full range pressure calibration. The transducer calibration procedure and resultant data are presented in Appendix A.

Depending on the calibration of these pressure transducers performed roughly every two weeks over a period of 8-months-measurement, the uncertainty was $\pm 0.5\text{Pa}$, $\pm 2\text{Pa}$ and $\pm 10\text{ Pa}$ for DRAL 501 DN, DRAL 505 DN and DRAL 520 DN respectively.

3.4.2 Temperature Monitor

An Omega I-series Dpi8-C24 temperature monitor receives signals from a T-type thermocouple (Copper-Constantan) and measures the temperature inside the bypass duct of the test section. The measured output temperature signal can be displayed and was used to set the cascade Reynolds number. The accuracy of temperature monitor is about $\pm 0.5^\circ\text{C}$ based on manufacturers data.

3.4.3 Digital Barometer

An Omega DPI740 model digital barometer was used to measure the ambient pressure, which is used to set the wind tunnel operating point, along with the test section temperature and wind tunnel contraction pressure. The accuracy of this pressure measurement is $\pm 7.6\text{Pa}$ based on manufacturers data.

3.5 Calibration Rig

Figure 3.5: Schematic drawing of calibration rig (modified from McAuliffe, 2003).

Figure 3.5 shows the schematic drawing of the in-house calibration rig which is used to calibrate the pressure probes used for the current project. A suction-type blower draws air through the outlet bellmouth and a throttle valve located upstream of the inlet of the blower is used to control the air flow rate. Air passes through the inlet filter panel and enters the nozzle which creates a jet in which the probes are calibrated. The maximum velocity at the exit of the nozzle is

about 45m/s. The pressure probes are mounted on the probe extension arm and aligned in both pitch and yaw aerodynamically. Two turntables are driven by stepper motors and they allow the probe to be rotated in both pitch and yaw up to $\pm 90^\circ$.

In this apparatus, the total reference pressure (P_{C1}) is measured within the inlet filter panels and the static reference pressures (P_{C2}) are obtained at a static tap at the exit of the nozzle. Detailed descriptions of the calibration rig and nozzle flow quality are presented in Benner (2003) and Mahallati (2003).

3.6 Pressure Probes

3.6.1 Pitot Tube

A pitot tube is employed to measure the inlet flow velocity profile and the endwall boundary layer development at the inlet measurement plane, which is located in 1.2 Bx upstream of the cascade. A schematic drawing of the Figure 3.6 shows a schematic drawing of the pitot tube used in present study. The probe consists of a 340 mm long hollow brass stem and a 63.5 mm long stainless steel tube. For the stainless tube, the inter and outer diameters are 0.33 mm and 0.60 mm respectively. The stainless tube is bent to about 91° with respect to the stem in order to ensure the tip of the probe contacts the endwall first.

According to repeatability tests, the uncertainty in total pressure is about 0.5% of the reference dynamic pressure.

Figure 3.6: Schematic drawing of Pitot tube.

3.6.2 Three-Hole Pressure Probe

A three-hole pressure probe is used to measure the flow angle, static pressure and flow velocity in the essentially two-dimensional flow at the midspan of the cascades at both inlet and outlet measuring planes. Figure 3.7 shows the three-hole probe used in the present study. The probe consists of a 72 mm long brass square body section supporting three 36 mm long stainless steel tubes. The tube section has a width of 2.0 mm and a thickness of 0.64 mm. For each stainless steel tube, the inner and outer diameters are 0.33 mm and 0.64 mm respectively. The two

side tube tips are beveled at an angle of 45° as shown in the drawing. The center tube tip was machined at a 90° angle to make sure the surface of the center tube is perpendicular to the centerline.

Figure 3.7: Schematic drawing of three-hole probe.

The probe was calibrated in 0.5° steps over a range of $\pm 20^\circ$ in the yaw direction using the calibration rig described in Section 3.5. The Lewis method (1966) was used to express the calibration data as pressure coefficients that can be used to extract the flow angle, static pressure, and dynamic pressure for an unknown

flow. The Lewis calibration procedure and the resultant calibration data for this probe are presented in Appendix B.

In the present experiments, the mean downstream velocities varied from approximately 8m/s to 48m/s. Measurements with the three-hole pressure probe are subject to a number of sources of error. In a sheared flow that is aligned with the axis of the probe, the side tubes of the probe will sense different values of pressure. From the calibration data, this difference will be incorrectly interpreted as flow angularity. Angle errors due to shear flow will be largest for measurements in the blade wakes. Uncertainty in both the probe calibration and the experimental measurements also exist, which is another error source in an unknown flow field.

The mean flow velocities at inlet measuring plane varied from approximately 5m/s to 30m/s, corresponding to the variation from 8 m/s to 48m/s at outlet. Due to these variations in flow velocity, the pressure probes experienced a fairly wide range of Reynolds numbers. Calibration coefficients for the three-hole probe were found to be sensitive to the probe Reynolds number (Re_w), especially for the low Reynolds number range. The probe Reynolds number (Re_w) is based on the velocity and the three-hole probe width (2 mm). In the range of high Reynolds numbers ($Re_w=2600, 3900$ and 5400 , corresponding to 20m/s, 30m/s, 42m/s of flow velocity), flow angle, dynamic pressure and total pressure were not strongly affected by the Reynolds number. At low Reynolds number ($Re_w=670, 1300$, corresponding to 5m/s, 10m/s of flow velocity), the calibration coefficients depend on the variation of Reynolds number. Since the probe Reynolds number is not known initially for

measurements in the test section, it must be determined by iteration. The data are initially reduced assuming the probe is at high Reynolds number. The resulting flow velocity is then used to estimate the actual Reynolds number. The data reduction is then repeated using the correct coefficients for the new Reynolds number, if necessary. The process is repeated until the assumed Reynolds number is consistent with the iterated Reynolds number. Linear interpolation is used when the probe Reynolds number lies between two values for which calibration curves were obtained.

Through the present experiment, probe calibration was repeated about once per month. The estimated uncertainty is $\pm 0.5^\circ$ for flow angle, $\pm 0.5\%$ of reference dynamic pressure for dynamic pressure and $\pm 0.25\%$ of reference dynamic pressure for static pressure respectively (Mahallati, 2003).

3.6.3 Seven-Hole Pressure Probe

A seven-hole probe is used for the three-dimensional flow measurements. In the current study, the seven-hole probe was used to measure the cascade downstream secondary flow. Figure 3.8 shows a schematic drawing of the seven-hole probe with its dimensions. The probe consists of a 94.5 mm long brass square body section (3×3 mm) and a 15 mm long section enclosing seven stainless steel tubes. Each stainless tube has a 0.33 mm inner diameter and a 0.71 mm outer diameter. The six outer tubes formed a bundle around the single center one. The conical tip is machined at 30° with respect to the centerline of the probe. The

Gallington (1980) method seven-hole probe calibration data reduction is used to extract the pressure coefficients and flow angles. The Gallington calibration procedure is described in Appendix C.

In the three-dimensional flow measurements, the probe Reynolds number varied from 3900 to 6400 (about from 30m/s to 48m/s flow velocity). The probe was calibrated in the middle of this range. The uncertainty is estimated to be $\pm 0.5^\circ$ for flow angle, $\pm 1.2\%$ of reference dynamic pressure for dynamic pressure and $\pm 0.7\%$ of reference dynamic pressure for total pressure respectively (Benner, 2003).

Figure 3.8: Schematic drawing of seven-hole probe.

3.7 Blade Loading

The static pressure distribution around the blade surface, which is referred to as the blade loading, strongly affects the boundary layer development and determines the tangential force acting on the blade. To conduct measurements of static pressure distributions in the present study, static pressure taps were designed and machined into the PAKD-A blades. Two adjacent PAKD-A blades in the middle of the cascade were instrumented with a total of 49 static pressure taps. The tap locations are illustrated and tabulated in Figure 3.9 and Figure 3.10. There is a total 17 taps on the pressure surface and 32 taps on the suction surface. In the cascade, the blade with the suction side static taps is arranged at the center of the seven-blade cascade while the blade with the pressure static taps is installed below this center blade. The static pressure taps located on the pressure and suction surfaces are thus in the same flow passage.

For the earlier PAK-B loading measurements (Section 5.2.3), the cascade middle blade was replaced with an instrumented blade with a total of 40 static pressure taps around the suction and pressure surfaces. The tap locations are illustrated and tabulated in Figure 3.11. There is a total 15 taps on the pressure surface and 24 taps on the suction surface. One tap was included on the blade trailing edge.

Taps	x/B_x	y/B_x	S/PSL	Taps	x/B_x	y/B_x	S/PSL
1	0.071	-0.496	0.064	10	0.427	-0.545	0.355
2	0.106	-0.514	0.095	11	0.498	-0.516	0.416
3	0.142	-0.530	0.126	12	0.569	-0.473	0.482
4	0.178	-0.542	0.156	13	0.640	-0.417	0.553
5	0.213	-0.551	0.185	14	0.711	-0.349	0.631
6	0.249	-0.558	0.214	15	0.794	-0.257	0.738
7	0.285	-0.562	0.241	16	0.877	-0.151	0.851
8	0.320	-0.563	0.270	17	0.960	-0.035	0.931
9	0.356	-0.560	0.298				

Figure 3.9: Locations of PAKD-A pressure surface static taps.

Taps	x/B_x	y/B_x	S/SSL	Taps	x/B_x	y/B_x	S/SSL
1	0.019	-0.524	0.034	17	0.673	-0.634	0.528
2	0.042	-0.553	0.058	18	0.694	-0.607	0.549
3	0.084	-0.599	0.098	19	0.715	-0.578	0.571
4	0.126	-0.638	0.135	20	0.737	-0.545	0.595
5	0.168	-0.671	0.169	21	0.758	-0.510	0.621
6	0.210	-0.699	0.201	22	0.779	-0.473	0.648
7	0.253	-0.721	0.231	23	0.821	-0.394	0.676
8	0.295	-0.739	0.261	24	0.842	-0.351	0.705
9	0.337	-0.752	0.289	25	0.863	-0.308	0.735
10	0.379	-0.759	0.316	26	0.884	-0.263	0.765
11	0.421	-0.762	0.343	27	0.905	-0.217	0.796
12	0.463	-0.759	0.369	28	0.912	-0.203	0.822
13	0.505	-0.749	0.397	29	0.918	-0.188	0.849
14	0.547	-0.733	0.426	30	0.935	-0.152	0.872
15	0.589	-0.710	0.456	31	0.951	-0.115	0.899
16	0.631	-0.677	0.490	32	0.968	-0.077	0.925

Figure 3.10: Locations of PAKD-A suction surface static taps.

Taps	x/B_x	y/B_x	Taps	x/B_x	y/B_x
1	0.008	-0.505	21	0.885	-0.236
2	0.072	-0.581	22	0.913	-0.181
3	0.155	-0.646	23	0.938	-0.130
4	0.248	-0.689	24	0.964	-0.077
5	0.348	-0.706	25	0.995	-0.012
6	0.435	-0.700	26	0.941	-0.061
7	0.492	-0.685	27	0.872	-0.156
8	0.546	-0.663	28	0.827	-0.215
9	0.596	-0.632	29	0.809	-0.238
10	0.620	-0.616	30	0.753	-0.307
11	0.643	-0.596	31	0.731	-0.332
12	0.664	-0.577	32	0.626	-0.439
13	0.685	-0.556	33	0.537	-0.508
14	0.704	-0.534	34	0.424	-0.561
15	0.731	-0.500	35	0.362	-0.572
16	0.756	-0.464	36	0.288	-0.570
17	0.813	-0.371	37	0.213	-0.555
18	0.825	-0.350	38	0.159	-0.537
19	0.845	-0.313	39	0.108	-0.513
20	0.866	-0.273	40	0.058	-0.487

Figure 3.11: Locations of PAK-B static taps (Mahallati, 2003).

For the loading measurements, the coefficient of static pressure is normalized by the inlet stagnation pressure (P_{oi}) minus the inlet static pressure (P_{si}) and defined as:

$$C_{pl} = \frac{(P_{oi} - P_{sl})}{(P_{oi} - P_{si})} \quad (3.12)$$

where P_{sl} is the surface static pressure. A more detailed discussion of the data reduction of the static pressures coefficient and a discussion of the loading measurements are given in Section 5.2.

3.8 Fluid Wafer Switch System

For the PAKD-A blade loading investigation, a 12-port fluid switch wafer system from Scanivalve Corp was used to measure the pressures. The fluid wafer switch is driven by a solenoid to apply each of the twelve pressures in turn to a single pressure transducer. A balance pressure must be applied to prevent the rotating rotor separating from the stator, on which the pressure ports are installed. The balance pressure should be the average of the pressure ports or above, but not exceed 20 Psi above the average pressure. The solenoid controller is provided to control the stepping speed of solenoid driven. The stepping speed can be adjusted from 30 steps/sec to 1 step/ 2 second.

From the present experiment the balance pressure was obtained from the upstream contraction pressure (P_{C1}). For the loading measurements, each port was sampled at a frequency of 1000 Hz for 20 seconds for a total of 20000 samples.

3.9 Data Acquisition System

The data acquisition system used for the current measurements was developed by Mahallati (2003). This data acquisition system is composed of a Power DAQ II data acquisition integrated circuit board (United Electronic Industries Inc) and Window-based object-oriented software, referred to as TurboDAQ. The TurboDAQ software was written by Mahallati (2003).

Power DAQ II data acquisition integrated circuit board is a sample-and-hold PCI card that can be used to acquire the experimental data with a rate of up to 800,000 samples per second. The board has eight 14-bit analog inputs, two 12-bit analog outputs and 16-bit counters. The analog input and output can be set either unipolar with a range of 5 volts or bipolar with a range of 10 volts depending on the requirements of the experiment.

The TurboDAQ software provides the user with a Windows-based interface to perform device control, instrument set up, wind tunnel operating point monitoring, data acquisition, probe calibration, and data reduction.

Chapter 4

Flow Conditions in the Wind Tunnel

4.0 Introduction

To realize a simulation as close as possible in the real environment of low-pressure turbine blades, flow conditions in the wind tunnel have to be set and adjusted correctly. The wind tunnel operation point is set based on the required Reynolds number. Using the control surfaces of the test section, a linear cascade can be used to approximate the flow conditions in an infinite row of blades. The control surfaces are used to set the inlet flow uniformity, outlet flow periodicity. The axial velocity ratio is used to determine the two-dimensionality of the flow at midspan.

In this chapter, the flow conditions in the wind tunnel were examined at a wide range of Reynolds at two freestream turbulence intensity levels and the associated test matrix is tabulated in Table 4.1.

Table 4.1: PAKD-A flow conditions test matrix.

	Reynolds Number								
	30000	35000	40000	50000	65000	75000	100000	125000	150000
FSTI=1.5%				U/P/A	U/P/A	U/P/A	U/P/A	U/P/A	U/P/A
FSTI=4.0%	U/P/A	U/P/A	U/P/A	U/P/A		U/P/A	U/P/A	U/P/A	U/P/A

U: Flow Uniformity P: Flow Periodicity A: Axial Velocity Ratio

4.1 Wind Tunnel Operation Point

The cascade inlet velocity is determined by the required Reynolds number, which is given by:

$$\text{Re} = \frac{\rho V_i B_x}{\mu} \quad (4.1)$$

where B_x is the blade axial chord, V_i is the inlet midspan velocity, ρ and μ are the density and viscosity of the air respectively.

The operating point of the wind tunnel is monitored by measuring the contraction pressure difference, $P_{C1}-P_{C2}$. This pressure difference is close to the dynamic pressure at the test section inlet and therefore the cascade inlet dynamic pressure. For each operating condition, the cascade inlet dynamic pressure is calibrated against the $P_{C1}-P_{C2}$ (see Section 4.2.1). The fan speed is then adjusted to obtain the cascade inlet velocity required to give a desired Reynolds number.

4.2 Cascade Flow Quality

Theoretically, an infinite number of blades are required in the linear cascade to simulate the annular blade row in an axial turbomachine. This is because the blades are installed circumferentially in the engine. Using the control surfaces (Section 3.1.2) in the test section, a finite number of blades can achieve the simulation. In the linear cascade, the inlet flow uniformity can be achieved with the assistance of the adjustable side flaps while the outlet flow periodicity can be controlled by

adjusting the tail boards. The settings of the side flaps and the tail boards for each operating condition are adjusted carefully.

For the current measurements, seven PAKD-A blades were used in the linear cascade. To check the cascade flow quality for each operating condition, the inlet flow uniformity and the outlet flow periodicity were measured by a calibrated three-hole probe traversed at $1.2 B_x$ upstream and $0.4 B_x$ downstream of the leading edge and trailing edge of the cascade. All pitchwise probe traverses were performed at the blade midspan.

4.2.1 Flow Uniformity and Periodicity Data Reduction

Three-hole probe measurements in flow uniformity and outlet periodicity were reduced to three independent parameters: flow angle (α), dynamic pressure ratio (C_q) and static pressure coefficient (C_{ps}). Other parameters, such as velocity ratio (C_v) and total pressure coefficient (C_{p0}) can be obtained from these three independent parameters. All of the parameters are defined below.

During the uniformity and periodicity measurements, three ports of the three-hole pressure probe were connected to three differential pressure transducers. The reference pressures of these three differential pressure transducers were connected to the wind tunnel contraction static pressure (P_{C2}). For three-hole probe, each port pressure coefficient (C_{Pn} , $n=1, 2, 3$) relative to reference pressure is given by:

$$\begin{aligned}
C_{Pn} &= \frac{P_n - P_{sref}}{P_{oref} - P_{sref}} \quad (4.2) \\
&= \frac{\frac{P_n - P_{C2}}{P_{C1} - P_{C2}} - \frac{P_{sref} - P_{C2}}{P_{C1} - P_{C2}}}{\frac{P_{oref} - P_{sref}}{P_{C1} - P_{C2}}} \\
&= \frac{\frac{P_n - P_{C2}}{P_{C1} - P_{C2}} - C_{psref}}{C_{qref}}
\end{aligned}$$

where subscripts o, s, q and ref refer to the total pressure, static pressure, dynamic pressure and reference. The subscript n refers to the port number of the three-hole probe. The reference dynamic pressure coefficient (C_{qref}) and reference static pressure coefficient (C_{psref}) in Equation (4.2) are defined as:

$$C_{qref} = \frac{P_{oref} - P_{sref}}{P_{C1} - P_{C2}} \quad (4.3)$$

$$C_{psref} = \frac{P_{sref} - P_{C2}}{P_{C1} - P_{C2}} \quad (4.4)$$

For inlet uniformity measurements, the reference pressure was taken at the wind tunnel contraction. For outlet periodicity measurements, the reference pressure was obtained at the inlet measurement plane.

The dynamic pressure coefficient (C_q) reduced from three-hole probe measurements in flow uniformity and periodicity is given as:

$$\begin{aligned}
C_q &= \frac{P_o - P_s}{P_{oref} - P_{sref}} \quad (4.5) \\
&= \frac{(P_1 - P_{sr}) - (P_2 - P_{sr})}{P_{oref} - P_{sref}} \\
&= \frac{P_1 - P_2}{P_o - P_s} \\
&= \frac{C_{P1} - C_{P2}}{K_{12}}
\end{aligned}$$

Similarly, the dynamic pressure coefficient (C_q) can be expressed as:

$$\begin{aligned}
C_q &= \frac{P_o - P_s}{P_{oref} - P_{sref}} \quad (4.6) \\
&= \frac{C_{P1} - C_{P3}}{K_{13}}
\end{aligned}$$

where C_{P1} , C_{P2} and C_{P3} are the pressure coefficients of three-hole probe port 1, port 2 and port 3 respectively, as defined in Equation 4.2. The calibration coefficients K_{12} , K_{13} can be extracted from the Lewis three-hole probe calibration procedure (Lewis, 1966) described in Appendix B, defined as

$$K_{12} = \frac{P_1 - P_2}{P_o - P_s} = C_{P1} - C_{P2} \quad (B.3)$$

$$K_{13} = \frac{P_1 - P_3}{P_o - P_s} = C_{P1} - C_{P3} \quad (B.4)$$

The velocity ratio (C_v) in the flow uniformity and periodicity measurements can be calculated from the dynamic pressure coefficient, as follow:

$$C_v = \sqrt{C_q} \quad (4.7)$$

The static pressure coefficient (C_{ps}) reduced from three-hole probe measurements for flow uniformity and periodicity is given as:

$$\begin{aligned} C_{ps} &= \frac{P_s - P_{sref}}{P_{oref} - P_{sref}} \\ &= \frac{P_1 - P_{sref}}{P_{oref} - P_{sref}} - \frac{P_1 - P_s}{P_o - P_s} \cdot \frac{P_o - P_s}{P_{oref} - P_{sref}} \\ &= C_{p1} - K_1 C_q \end{aligned} \quad (4.8)$$

where C_{p1} is the pressure coefficient of three-hole probe port 1. Calibration coefficient K_1 is also defined in the Lewis three-hole probe calibration procedure (Lewis, 1966) described in Appendix B and given as

$$K_1 = \frac{P_1 - P_s}{P_o - P_s} = C_{p1} \quad (B.2)$$

The total pressure coefficient (C_{po}) reduced from three-hole probe measurements in flow uniformity and periodicity is expressed as:

$$\begin{aligned} C_{po} &= \frac{P_{oref} - P_o}{P_{oref} - P_{sref}} \\ &= 1 - \frac{P_s - P_{sref}}{P_{oref} - P_{sref}} - \frac{P_o - P_{sref}}{P_{oref} - P_{sref}} \\ &= 1 - C_{ps} - C_q \end{aligned} \quad (4.9)$$

4.2.2 Flow Uniformity and Periodicity

Figure 4.1 and Figure 4.2 present two typical uniformity and periodicity measurement results at Reynolds number of 150000 with FSTI=1.5% and FSTI=4.0% respectively for the middle three cascade passages. The inlet uniformity is shown in the left-hand plots and the outlet periodicity is presented in

the right-hand plots of Figure 4.1 and Figure 4.2. The three-hole probe traverses in the pitchwise direction from the upper end to the bottom of the cascade. The pitchwise distance (y) is normalized by the blade pitch (S). The measured data are expressed in terms of the parameters defined in the previous section. Additional plots showing the uniformity and periodicity for a wider pitchwise extent are included in Appendix D.

As seen in Figure 4.1, good flow uniformity and periodicity are achieved for the middle three blades in the cascade. Due to the three-hole probe traversing at $1.2 B_x$ upstream of the leading edges of the blades, there is a small influence of the potential effect of the blades and thus results in the small periodical variation during the in the flow angle. The periodic variation in the dynamic and total pressure coefficients, C_q and C_{po} , at the inlet is due to the wakes from the turbulence grid not having fully mixed out.

As seen in Figure 4.2, for the freestream turbulence intensity level of 4.0%, the inlet flow at low values of y/S in particular was spatially non-uniform. This is because the 4.0% turbulence grid is located $4.55 B_x$ upstream of the middle blade leading edge of the cascade and the measurements at low values of y/S are closest to the grid. The wakes generated by the grid bars have not completely mixed out. The flow non-uniformity close to the grid at $FSTI=4.0\%$ was also reported in the PAK-B inlet flow measurements by McAuliffe (2003). Samples of such results can be seen in Appendix D. As seen in Figure 4.2, the flow periodicity downstream of the cascade is good.

Figure 4.1: Flow uniformity and periodicity for PAKD-A at $Re=150000$, $FSTI=1.5\%$.

Figure 4.2: Flow uniformity and periodicity for PAKD-A at $Re=150000$, $FSTI=4.0\%$.

For the present study, the flow uniformity and periodicity in the wind tunnel were examined for a large range of Reynolds number at both levels of freestream turbulence intensity, as shown as the test matrix in Table 4.1. Laminar boundary layer separation commonly occurs in low-pressure turbines. If the separated flow reattaches, then the wakes downstream of the cascade are relatively narrow and clearly separated, as shown by the downstream total pressure and dynamic pressure distributions in Figure 4.1 and Figure 4.2. For Reynolds number lower than 75000 at $FSTI=1.5\%$ and lower than 40000 at $FSTI=4.0\%$, the separated flow did not reattach and the cascade blades were stalled. The downstream data for two such cases are shown in Figure 4.3 and Figure 4.4. The periodicity in the cascade flow is seen to be not quite good as for the reattaching cases shown in the earlier figures, but is still acceptable.

Figure 4.3: Outlet periodicity for PAKD-A at $Re=50000$, $FSTI=1.5\%$.

Figure 4.4: Outlet periodicity for PAKD-A at $Re=30000$, $FSTI=4.0\%$.

4.2.3 Axial Velocity Ratio

Axial velocity ratio (AVR) is used as a measure of the two-dimensionality of the flow at midspan. AVR is defined as the ratio of outlet-to-inlet, unit-span mass flow rates at midspan and expressed as:

$$\begin{aligned}
 AVR &= \frac{\int_0^s (\rho_e V_{xe})_{ms} dy}{\int_0^s (\rho_i V_{xi})_{ms} dy} \\
 &= \frac{\int_0^1 (\rho_e V_{xe})_{ms} d\left(\frac{y}{s}\right)}{\int_0^1 (\rho_i V_{xi})_{ms} d\left(\frac{y}{s}\right)}
 \end{aligned} \tag{4.10}$$

For incompressible flow, the density is assumed to be constant, $\rho_i = \rho_e$. The axial velocities at inlet and outlet of the cascade are expressed:

$$V_{xi} = V_i \cos \alpha_i \tag{4.11}$$

$$V_{xe} = V_e \cos \alpha_e \tag{4.12}$$

where a_i , a_e , V_e and V_i are obtained from three-hole probe uniformity and periodicity measurements. Substituting equation (4.11) and (4.12) into (4.10).

AVR is written as:

$$AVR = \frac{\int_0^1 (V_e \cos \alpha_e)_{ms} d\left(\frac{y}{s}\right)}{\int_0^1 (V_i \cos \alpha_i)_{ms} d\left(\frac{y}{s}\right)} \tag{4.13}$$

For a value of AVR less than 1.0, the outlet mass flow rate per unit area is lower than the inlet mass flow rate, and the flow must be passing through a divergent streamtube. Conversely, an AVR greater than 1.0 indicates a convergent streamtube. Thus, a requirement for two-dimensionality of the midspan flow is an AVR of 1.0. But note that $AVR=1.0$ does not necessarily mean that the flow is two-dimensional. $AVR=1.0$ is a necessary but not sufficient condition.

Figure 4.5 shows the experimental results of AVR for all operating conditions examined. The value of AVR varies from 0.95 to 1.03. For $FSTI=1.5\%$ conditions, the value of AVR increases from 0.984 to 1.018 as the Reynolds number decreases from 150000 to 75000. Further decreasing the Reynolds number to 65000 and 50000, the flow in the cascade passage is fully separated and the AVR dropped to 0.998. For $FSTI=4.0\%$ operating conditions, the value of the AVR slightly decreased from 0.995 to 0.970 as the Reynolds number decreased from 150000 to 75000. As Reynolds number decreased from 75000 to 40000, the AVR increased from 0.970 to 1.030. For Reynolds numbers lower than 40000, the flow stalled in the cascade and the AVR dropped as was also the case for stalled flow at $FSTI=1.5\%$.

The difference in the behaviour of the AVR for the two values of $FSTI$ is thought to be due to differences in the locations of the two turbulence grids. As mentioned, the 4.0% grid is much closer to the cascade than the 1.5% grid. This is expected to influence the blockage to the endwall boundary layers and therefore the flow convergence or divergence through the cascade.

Figure 4.5: Axial velocity ratio for PAKD-A.

Table 4.2: PAKD-A axial velocity ratio.

Reynolds Number	Axial Velocity Ratio (AVR)	
	FSTI=1.5%	FSTI=4.0%
150000	0.984	0.995
125000	1.002	0.982
100000	1.008	0.980
750000	1.018	0.970
65000	0.996	
50000	0.988	0.989
40000		1.030
35000		0.971
30000		0.954

It is worth pointing out that the accuracy of the inlet and outlet flow angles have a strong influence on the values of AVR, shown by Equation (4.13). Especially, AVR is sensitive to the outlet flow angle measurement.

Chapter 5

Results and Discussions of Measurements

5.0 Introduction

This chapter presents the two-dimensional measurements for the PAKD-A cascade performance. These consist of the blade loading and the profile loss measurements at different Reynolds numbers with two levels of free stream turbulence intensity (FSTI). The test matrix is shown in Table 5.1. The Reynolds number is based on the velocity at the inlet measuring plane and the blade axial chord. The downstream measurements were conducted at the midspan of the PAKD-A cascade.

Table 5.1: PAKD-A test matrix.

	Reynolds Number								
	30000	35000	40000	50000	65000	75000	100000	125000	150000
FSTI=1.5%				L/P	L/P	L/P	L/P	L/P	L/P
FSTI=4.0%	L/P	L/P	L/P	L/P		L/P	L/P	L/P	L/P

L: Loading measurement P: Profile loss

In addition to presenting the measurements for PAKD-A, the comparison with the corresponding measurements for the baseline PAK-B cascade is given in this chapter. As described in Section 3.1.3, PAKD-A is one of two more highly-loaded cascades that are intended for the same service as the baseline PAK-B.

PAKD-A is intended to investigate the influence of aft loading on the aerodynamic performance of a very highly-loaded LP turbine blade row, particularly at very low Reynolds numbers. The second cascade, with the same spacing but forward loaded, is being investigated separately in the Low-Speed Turbomachinery Laboratory (LSTL) at Carleton University.

5.1 Zweifel Coefficient

In the design of axial-flow turbines, the selection of the solidity ($\sigma=B/S$) is important to achieve an optimum aerodynamic performance. For low-solidity high-lift blades, the stronger adverse pressure gradients on the suction surface increase the risk of boundary layer separation, especially at low Reynolds number conditions. For high-solidity low-lift blades, the increased wetted-surface area leads to a high profile loss as the flow passes through the blade passages. Zweifel (1945) found that a loading coefficient based on the tangential force on the blade was approximately constant for optimum blade solidity. This loading coefficient is referred to as the Zweifel coefficient, Z , and expressed as:

$$Z = 2 \left(\frac{S}{B_x} \right) \cos^2 \alpha_e \left(\tan \alpha_e + \frac{V_{xi}}{V_{xe}} \tan \alpha_i \right) \quad (5.1)$$

where S is the blade spacing and B_x is the axial chord length. V_{xi} and V_{xe} are the axial inlet and outlet velocities. α_i and α_e are the inlet and outlet flow angles.

Zweifel found that a value of $Z=0.8$ gave approximately the minimum profile losses for the turbine blade designs. For a certain geometry cascade and axial velocity ratio, the value of Zweifel coefficient can be interpreted as tangential blade force coefficient.

With the improvement of turbine profile design, the Zweifel coefficients are much higher today than the value that was recommended by Zweifel. A value of $Z=1.0-1.05$ is common today. PAK-B has a Zweifel coefficient of approximately 1.07. For the current study, the Zweifel coefficient of PAKD-A is about 1.36 and thus it is regarded as very highly-loaded.

5.2 Blade Loading Measurements

5.2.1 Blade Loading Definition and Data Reduction

In turbomachinery, the blade loading refers to the static pressure distribution along the blade surfaces which reflects the lifting force acting on the blade. The static pressure distribution acting on the airfoil surfaces influences the boundary layer development, including boundary layer separation, transition, and reattachment. To investigate the static pressure distribution and the boundary layer developments on the PAKD-A blades, the blade loading measurements were performed over a wide range of Reynolds numbers at two different levels of free stream turbulence intensity.

A static pressure distribution coefficient, C_{pl} , is normally used to present the pressure distribution on the surfaces of airfoils. There are different definitions in use for C_{pl} . In the current study, the value of the static pressure coefficient C_{pl} is obtained by measuring the local static pressures (P_{sl}) on the blade surfaces with respect to the inlet stagnation pressure (P_{oi}) and normalized by the inlet stagnation pressure (P_{oi}) minus the inlet static pressure (P_{si}), which is expressed as follows:

$$C_{pl} = \frac{(P_{oi} - P_{sl})}{(P_{oi} - P_{si})} \quad (5.2)$$

In practice, both the blade surface pressures and the pressure probe pressures are measured relative to the pressure at the outlet of the wind tunnel contraction, P_{C2} . The pressure coefficients for the blade loading are then obtained from:

$$\begin{aligned} C_{pl} &= \frac{(P_{oi} - P_{sl})}{(P_{oi} - P_{si})} \\ &= \frac{(P_{oi} - P_{C2}) - (P_{sl} - P_{C2})}{Q_i} \\ &= \frac{(P_{oi} - P_{si}) + (P_{si} - P_{C2}) - (P_{sl} - P_{C2})}{Q_i} \\ &= 1 + \frac{P_{si} - P_{C2}}{Q_i} - \frac{P_{sl} - P_{C2}}{Q_i} \end{aligned} \quad (5.3)$$

where $Q_i = P_{oi} - P_{si}$. Recall that in section 4.2.1, the inlet static pressure coefficient (C_{psi}) and dynamic pressure coefficient (C_{qi}) were respectively defined as:

$$C_{psi} = \frac{P_{si} - P_{C2}}{P_{C1} - P_{C2}} \quad (4.8)$$

and

$$C_{qi} = \frac{P_{oi} - P_{si}}{P_{C1} - P_{C2}} \quad (4.5)$$

The values of C_{psi} and C_{qi} are determined from the results of the inlet flow uniformity measurements. Substituting 4.5 and 4.8 into 5.2, the blade static pressure coefficient can be calculated by the following expression:

$$C_{pl} = 1 + \frac{C_{psi}}{C_{qi}} - \frac{P_{sl} - P_{c2}}{C_{qi}(P_{C1} - P_{C2})} \quad (5.4)$$

The maximum uncertainty is estimated to be $\pm 0.04\%$ of C_{plmax} for FSTI=1.5% and $\pm 0.07\%$ of C_{plmax} for FSTI=4.0% respectively.

5.2.2 Blade Loading Experimental Results

5.2.2.1 Results for Freestream Turbulence Intensity of 1.5%

Figure 5.1 presents the distributions of PAK-D static pressure coefficient (C_{pl}) at freestream turbulence intensity (FSTI) of 1.5%, with varying Reynolds numbers. C_{pl} is plotted against the axial distance normalized by the axial chord length of the airfoil.

Figure 5.1: Static pressure distribution for PAKD-A at FSTI=1.5%.

For the high Reynolds numbers (75000, 100000, 125000 and 150000), the flow accelerates on the forward portion of the suction surface until the maximum velocity (suction peak) is reached. The extended region of flow acceleration along the suction surface would slow the boundary layer growth and it is expected that it remains laminar. The locations of the suction peaks vary slightly. This is probably due to the influence of the separation bubble downstream of the suction peak. With the adverse pressure gradient occurring downstream of the suction peak, the boundary layer separates at about $0.67 B_x$ for all cases, as evident from the plateau

in the pressure distribution. However, the length of the bubble is seen to increase as Reynolds number decreases, and this also seems to influence the pressure distribution upstream at least as far as the suction peak. As noted, the location of separation point varies very little with the Reynolds number. This suggests that the boundary layer upstream of the separation point is laminar.

At separation, the boundary layer is displaced away from the blade surface. The separated shear layer will have an inflection point and is therefore sensitive to disturbances. Such disturbances are present in the freestream and they stimulate the onset of transition to turbulence in the shear layer. The turbulent flow entrains high momentum fluid from the freestream and the shear layer is therefore able to reattach to the suction surface. Thus a separation bubble is formed on the suction surface of the blade. As noted, the location of the reattachment point varies with Reynolds number. For the Reynolds number of 150000, 125000, 100000 and 75000, the reattachment point is located at about 0.74, 0.76, 0.77 and 0.79 B_x respectively. The high momentum contained in the reattached turbulent boundary layer can overcome the adverse pressure gradient downstream and the boundary layers remains attached until the flow leaves the trailing edge of the blade.

Figure 5.2 shows the variation of the separation bubbles with the Reynolds number in more detail. The separated shear layer is normally assumed to be laminar until just before the end of the constant pressure region, where the transition occurs (Mayle, 1991). In the afterward part of the separation bubble, the static pressure increases and dynamic pressure decreases. With increasing Reynolds number, the

separation bubble becomes shorter, indicating that transition is occurring earlier in the free shear layer.

Figure 5.2: Separation bubble of non-full separation flow for PAKD-A at FSTI=1.5%.

For the lower Reynolds numbers (50000 and 65000), the flow fields on the suction surface are significantly different compared with those at higher Reynolds numbers, as shown in Figure 5.1. The flow accelerates on the forward part of the suction surface as before, but reaches the maximum velocity at about $0.45 B_x$. Due to the adverse pressure gradient occurring downstream of the suction peak, the flow

then separates at about $0.55 B_x$, but the separated shear layer does not reattach and airfoil stalls, as shown in Figure 5.3.

Figure 5.3: Separation bubble of full separation flow for PAKD-A at FSTI=1.5%.

It is noticeable that as the Reynolds number decreases towards 75000, the size of the bubble increases only gradually and even at $Re=75000$ there is still a long length of reattached flow downstream of the reattachment point. In fact, the reattachment point of about $0.79 B_x$ represents an even smaller fraction of the suction surface length (SSL): the reattachment for $Re=75000$ occurs at about 0.65 SSL. Thus, there was significant surface length for the separation bubble to become

larger with further decreases in the Reynolds number. However, it is seen that this does not occur. With a relatively small further reduction in Reynolds number to 65000, the separation bubble abruptly becomes so large that it is unable to reattach at all. It appears that the bubble “bursts” in this Reynolds number range, perhaps due to some kind of instability. As will be shown later, for the PAK-B airfoil an equivalent bubble continued to increase in length with decreasing Reynolds number until it was simply too long to reattach before the trailing edge. This is one of the respects that the aerodynamic behavior of the more highly-loaded PAKD-A is different from that of the baseline PAK-B.

A separation bubble is often present on the suction surface of LP turbine airfoils and can be tolerated if the separated flow reattaches on the surface before the blade trailing edge. For low Reynolds number conditions, if the separated flow cannot reattach on the blade surface, the massive flow separation on the blade surface causes very high profile losses (see Section 5.2.2.1).

The static pressure distribution on the pressure surface of PAKD-A is illustrated in Figure 5.4. On the pressure side of the blade, the flow moves forward with a slight deceleration up to about $0.36 B_x$. After reaching $0.36 B_x$, the flow begins to accelerate until leaving at the trailing edge of the blade. There is no evidence of the flow separation occurring on the pressure surface.

Figure 5.4: Static pressure distribution on pressure surface for PAKD-A at FSTI=1.5%.

5.2.2.2 Results for Freestream Turbulence Intensity of 4.0%

Figure 5.5 presents the static pressure distribution (C_{pi}) on the blade surfaces varying with the Reynolds number at a freestream turbulence intensity (FSTI) of 4.0%. The static pressure coefficient plots are very similar to the cases for FSTI=1.5%.

For the non-fully separated conditions ($Re=40000, 50000, 75000, 100000, 125000, 150000$), the flow accelerates on the forward part of the suction surface and reaches a maximum velocity. The locations of the maximum velocity vary slightly, as also seen for the cases at FSTI=1.5% in Figure 5.1. With the adverse pressure gradient occurring downstream of the suction peak, the boundary layer separates at about $0.67 B_x$ for all cases. The length of the bubbles formed on the suction surface increases as Reynolds number decreases. The flow reattachment points are at about

0.73, 0.77 and 0.82 B_x at Reynolds number of 150000, 75000 and 40000 respectively, as shown in Figure 5.6.

Figure 5.5: Static pressure distribution for PAKD-A at FSTI=4.0%.

With a relative small further reduction in Reynolds number from 40000 to 35000, the separated flow is unable to reattach on the suction surface and the bubble “bursts” abruptly, as shown in Figure 5.7. It is noted that the critical Reynolds number for stall is different for FSTI=1.5% and FSTI=4.0%. For FSTI=1.5% the airfoil stalls as the Reynolds number becomes lower than 70000, while stall happens for Reynolds number less than 40000 for FSTI=4.0%. As would be expected, the

higher disturbances in the freestream for the higher turbulence flow cause earlier transition to turbulence in the free shear layer. The turbulent shear layer entrains higher momentum fluid from the freestream and it allows the free shear layers to reattach down to lower values of Reynolds number.

On the pressure side of the blade, the flow reaches the minimum velocity at about $0.37 B_x$. As with $FSTI=1.5\%$, there is no evidence of boundary layer separation occurring on the pressure surface for $FSTI=4.0\%$, as shown in Figure 5.8.

Figure 5.6: Separation bubble of non-full separation flow for PAKD-A at $FSTI=4.0\%$.

Figure 5.7: Separation bubble of full separation flow for PAKD-A at FSTI=4.0%.

Figure 5.8: Static pressure distribution on pressure surface for PAKD-A at FSTI=4.0%.

5.2.3 Comparison of Static Pressure Distributions with PAK-B Profile

Figure 5.9 presents the static pressure distributions for PAKD-A and PAK-B (Mahallati, 2003) plotted against the fraction of axial chord length (B_x) for Reynolds number of 150000 at the free stream turbulence intensity (FSTI) level of 1.5%.

Figure 5.9: Comparison of blade loading for PAK-B (Mahallati, 2003) and PAKD-A at $Re=150000$, $FSTI=1.5\%$.

As seen from Figure 5.9, the maximum velocities on the suction side occur at about $0.59 B_x$ for PAKD-A and at $0.62 B_x$ for PAK-B. The maximum velocity of PAKD-A is higher than that of PAK-B, as a result of the higher spacing. Downstream of the maximum velocity, the stronger adverse pressure gradient on

PAKD-A leads to earlier laminar flow separation on the suction surface. The separation points are located at about $0.67 B_x$ for PAKD-A and at $0.76 B_x$ for PAK-B. Comparison of pressure distributions between PAKD-A and PAK-B airfoils indicates that PAKD-A has higher loading than PAK-B due to the low solidity for PAKD-A, as evidenced by the greater area between suction and pressure side loading curves in Figure 5.9.

Figure 5.10 shows the whole results of static distributions for PAK-B (Mahallati, 2003) (Figure 5.10 (a)) and PAKD-A (Figure 5.10 (b)) at FSTI=1.5% for a wide range of Reynolds numbers. The separation locations are largely independent of the influence of Reynolds number for both PAKD-A and PAK-B measurements. This suggests that in both cases, the boundary layer is always laminar at the separation points. But the lengths of the separation bubbles are dependent on the Reynolds number for both PAKD-A and PAK-B. The size of the separation bubble decreases with the increase of the Reynolds number in both cases. The flow behaviors on the pressure surfaces of these two blades are slightly different, as seen in Figure 5.10 (a) and (b). There is no evidence of pressure side flow separation for PAKD-A at 1.5% freestream turbulence intensity. But for the pressure surface of the PAK-B, the flow appears to separate at about $0.2 B_x$ and reattach at approximately $0.55 B_x$ for $Re=25000$ and at $0.45 B_x$ for $Re=50000$. As the Reynolds number increases to 100000 or above, there does not appear to be boundary layer separation on the PAK-B pressure side.

(a) PAK-B (Mahallati, 2003)

(b) PAKD-A

Figure 5.10: Comparison of blade loading for PAK-B (Mahallati, 2003) and PAKD-A at FSTI=1.5%.

The significant difference between these two blades is the behavior of separation bubbles on the suction surfaces. With decreasing Reynolds number, the length of the PAK-B separation bubble continues to increase and reattach closer and closer to the trailing edge of the blade. For the lowest Reynolds number of 25000, the bubble reattaches right at the trailing edge. Mahallati (2003) found that for this Reynolds number and $FSTI=0.4\%$, the bubble did not reattach and PAK-B was fully stalled. The separation bubble on PAK-B appears to develop from a short bubble to a long bubble with decreasing Reynolds number, as shown in Figure 5.11(a). For PAKD-A, the separation bubble develops as a short bubble and the separation bubble length increases only slightly with decreasing Reynolds number. Unlike for the PAK-B blade, the separation bubble does not increase continuously and become a long bubble as the Reynolds number decreases. Instead, the bubble increases only slightly with decreasing Reynolds number. Finally, at a Reynolds number of about 70000, the bubble very abruptly increases in size and fails to reattach at all, as shown in Figure 5.11(b). According to the location of the onset of the transition, the Reynolds number, and adverse pressure gradient, Hatman and Wang (1998) distinguished at least three primary separated-flow transition modes that they defined as transitional separation mode, laminar separation-short bubble mode and laminar separation-long bubble mode, as discussed in Section 2.2.3. The separation bubble behavior for PAK-B airfoil is consistent with the separation bubble development from laminar separation-short bubble to laminar separation-long bubble modes, as described by Hatman and Wang (1998). However, the

development of the separation bubble for the more highly-loaded PAKD-A airfoil does not appear to be consistent with the results of Hatman and Wang (1998).

To the author's knowledge, the physical mechanism for separation bubble bursting is not fully understood. It seems likely that some kind of instability is involved, one that is triggered for PAKD-A but not for PAK-B. The main difference between PAK-B and PAKD-A is the stronger adverse pressure gradient that is present in the freestream in the area of the bubble for PAKD-A. Therefore, it seems reasonable to assume that instability mode that occurs is a function of pressure gradient.

(a) PAK-B separation bubbles (Mahallati, 2003)

(b) PAKD-A separation bubbles

Figure 5.11: Comparison of separation bubble for PAK-B (Mahallati, 2003) and PAKD-A at FSTI=1.5%.

5.3 Profile Loss Measurements

Aerodynamic loss is an important issue for turbine design and application. In low turbines, the loss occurs in an irreversible and nearly adiabatic process in which the mechanical energy converts into internal energy and entropy is generated. The entropy production would be a direct measurement to evaluate the loss. Unfortunately, the entropy production cannot be measured directly by experimental methods. As an alternative method of determining entropy production, the change of the total pressure is employed as an indirect measurement to determine the loss production that occurs in turbomachines. Historically, the total pressure loss in turbines is divided into four components: profile loss, secondary loss, tip leakage loss and trailing-edge loss. Although the different loss components are not independent, they are treated as such and the total loss is normally calculated by a linear superimposing of the above four components. In this study, the loss of PAKD-A is being examined and the experimental results are presented in this section.

5.3.1 Profile Loss Definition and Data Reduction

Profile loss is generated mainly by the friction in boundary layers on the pressure and suction surfaces of the blade. The additional losses due to the sudden area increase at trailing edge of the airfoil and the mixing out of the wakes downstream of the blade are usually included in the profile losses. From the measurements made at the outlet measurement plane, there are two calculations to

evaluate the profile loss: one is the measured profile loss and the other is the mixed-out profile loss.

The measured profile loss refers to the losses that have happened between the inlet and the outlet measurement planes and the loss coefficient is defined as the mass-averaged total pressure difference ($P_{oi}-P_{oe}$) between the inlet and the outlet measurement planes, normalized by the mass-averaged dynamic pressure ($P_{oe}-P_{se}$) at outlet measurement plane. The mass-averaged profile losses are used to be consistent with the use of mass-averaged loss coefficient in meanline analyses. It is noted that in the present study the outlet measurement plane is located at $0.4 B_x$ downstream of the trailing edge of the blade. The total pressure loss coefficient is then defined as

$$Y = \frac{\overline{P_{oi}} - \overline{P_{oe}}}{\overline{P_{oe}} - \overline{P_{se}}} \quad (5.4)$$

where

$$\overline{P_o} = \frac{1}{\dot{m}} \int P_o d\dot{m} \quad (5.5)$$

is the mass-averaged total pressure. The measured profile loss in the cascade is mainly two-dimensional and the measurements are conducted at the midspan measurement plane in the current study.

The results of the periodicity measurements in Figures 4.3 and 4.4 show that at the outlet measurement plane the flow is not spatially uniform. The mixing of wakes and free stream will generate an additional loss further downstream of the

measurement plane. In this irreversible mixing process, extra entropy is generated.

The total pressure loss coefficient at completely mixed-out condition is given by

$$Y_m = \frac{\overline{P_{oi}} - \overline{P_{om}}}{\overline{P_{om}} - \overline{P_{sm}}} \quad (5.6)$$

where P_{om} is the mixed-out total pressure and P_{sm} is the mixed-out static pressure.

It should be noted that in the real engine, the fully mixed-out losses will not normally be seen since a following blade row is usually reached before the mixing is completed. However, mixed-out losses are commonly used in cascade research because they are independent of the measurement location downstream of the cascade outlet. Harrison (1992) derived the calculation for the mixed-out loss by applying a control volume extending from the outlet measurement plane to the mixed-out plane and from the endwall to the middle span plane. He assumed that the mixing occurs in constant area and the flow is under incompressible conditions. The flow is taken as uniform at the mixed-out plane. According to his assumptions, the mixed-out total pressure is given as follows:

$$\overline{P}_{om} = \frac{\rho}{2} \left\{ \left(\frac{\iint V_{xe} V_{ye} dydz}{\iint V_{xe} dydz} \right)^2 - \frac{1}{A_m^2} \left(\iint V_{xe} dydz \right)^2 \right\} + \frac{1}{A_m} \left\{ \iint \left(P_{oe} + \frac{1}{2} \rho V_{xe}^2 \right) dydz \right\} \quad (5.7)$$

where A_m is the area of control volume exit plane normal to the axial direction.

In order to combine the resultant equation derived by Harrison and the results from the probe measurements, Benner (2003) re-expressed the quantities of equation 5.7 and derived the mixed-out profile loss coefficient, Y_m . The resultant expression for the mixed-out loss coefficient is:

$$Y_m = \frac{\bar{P}_{oi} - \bar{P}_{om}}{\bar{P}_{om} - \bar{P}_{sm}} = \frac{\bar{P}_{oi} - \bar{P}_{om}}{\bar{q}_m} = \frac{1 - \int_0^l C_{pse} d(y/s) + 2(I_1^2 - I_2)}{\left(\frac{V_m^2}{V_i^2}\right)} - 1 \quad (5.8)$$

where C_{pse} (section 4.2.2) is the static pressure coefficient measured at the exit of the cascade, defined as

$$C_{pse} = \frac{P_{se} - P_{si}}{P_{oi} - P_{si}} \quad (4.17)$$

I_1 and I_2 are defined as:

$$I_1 = \int_0^l \left(\frac{V_e}{V_i}\right) d\left(\frac{y}{s}\right) \quad (5.9)$$

$$I_2 = \int_0^l \left(\frac{V_e}{V_i}\right)^2 d\left(\frac{y}{s}\right) \quad (5.10)$$

where V_i and V_e are the velocities at the inlet and outlet measurement planes.

5.3.2 Profile Loss Experimental Results and Analysis

5.3.2.1 Losses for PAKD-A at Freestream Turbulence Intensity of 1.5%

Figure 5.12 shows the experimental results for mixed-out profile loss coefficient with varying Reynolds number at a freestream turbulence intensity FSTI=1.5%. The values of the profile loss are also listed in Table 5.2.

Figure 5.12: Mixed-out profile losses for PAKD-A at FSTI=1.5%.

Table 5.2: Mixed-out profile losses for PAKD-A at FSTI=1.5%.

Reynolds Number	Profile Loss
50000	0.630
65000	0.615
75000	0.068
100000	0.041
125000	0.033
150000	0.031

At Reynolds numbers of 50000 and 65000 with FSTI of 1.5%, the mixed profile losses are extremely high. This is because the flow is fully separated on the

suction surface (Figure 5.3). This causes a relatively wide and deep wake at the trailing edge of the blade, as shown in Figure 5.13. The wake for $Re=50000$ is wider and deeper than that for $Re=65000$, and the loss is seen to be larger. Due to the full separation for $Re=65000$ and 50000 , the wakes from adjacent blades are overlapping each other at the downstream measurement plane. It is also noticeable that for $Re=65000$ the mixing process seems to be faster than that for $Re=50000$. This is possibly because the transition occurs earlier in the free shear layer of the separated flow than that for $Re=50000$.

Figure 5.13: Wakes of full separation flow for PAKD-A at FSTI=1.5%.

With Reynolds number increased to 75000, the value of the profile loss is reduced to 0.068 as the separated bubble begins to reattach. Further increasing the Reynolds number (100000, 125000, 150000) causes the profile losses to reduce gradually to a value of 0.031 at $Re=150000$. Above a Reynolds number of about 125000, the profile losses vary only slightly as the size of the separation bubble changes little. The wakes observed at the downstream measurement plane are shown in Figure 5.14. It is evident that the change in losses with Reynolds number is due to changes in the losses produced in the boundary layer on the suction surface of the airfoil.

Figure 5.14: Wakes of non-full separation flow for PAKD-A at FSTI=1.5%.

5.3.2.2 Losses for PAKD-A at Free Stream Turbulence Intensity of 4.0%

Figure 5.15 shows profile loss coefficients as a function of Reynolds number at free stream turbulence intensity of 4.0%. The values are also listed in Table 5.3.

The variation of the profile losses with the Reynolds number is similar to the results for $FSTI=1.5\%$, except the full separation is delayed until a Reynolds number of about 35000. At the lower Reynolds numbers of 30000 and 35000, the flow is fully separated on the suction surface (as shown in Figure 5.7) and as a result the losses are very high. For the fully separated flow, the wakes at the downstream measurement plane are relative wide and deep due to the wake's overlapping from adjacent blades, as shown in Figure 5.16. Unlike the mixing of the fully separated wakes presented in Figure 5.13, the mixing for $Re=35000$ and 30000 are fairly similar. This indicates probably fairly similar transition locations for $Re=35000$ and 30000.

Figure 5.15: Mixed-out profile losses for PAKD-A at $FSTI=4.0\%$.

Table 5.3: Mixed-out profile losses for PAKD-A at FSTI=4.0%.

Reynolds Number	Profile Loss
30000	0.607
35000	0.590
40000	0.084
50000	0.067
75000	0.040
100000	0.029
125000	0.028
150000	0.026

Figure 5.16: Wakes of full separation flow for PAKD-A at FSTI=4.0%.

For Reynolds number above $Re=40000$, the flow is separated but reattaches on the suction surface. The size of the separation bubble decreases as the Reynolds

number increases, as shown in Figure 5.6. At the downstream measurement plane, the wakes also become narrower and smaller as the Reynolds number increases, as shown in Figure 5.17.

Figure 5.17: Wakes of non-full separation flow for PAKD-A at FSTI=4.0%.

5.3.2.3 Loss Comparison between FSTI=1.5% and FSTI=4.0%

The comparisons between the results for mixed-out loss coefficients at FSTI=1.5% and 4.0% are shown in Figure 5.18. These results show that the turbulence intensity influences both the mixed-out profile losses at a given Reynolds number and the Reynolds number at which the losses rise due to full separation. For the same Reynolds number, the profile loss with the high turbulence intensity (FSTI=4.0%) is less than that with lower turbulence intensity (FSTI=1.5%). This is because the separation bubble is smaller at a given Reynolds number for the high FSTI flow than for the low FSTI flow. Figure 5.19 presents separation bubbles at Reynolds number of 75000 with FSTI=1.5% and 4.0%. For the high turbulence

intensity, the large disturbances cause the transition to occur earlier, and thus allow the separated flow to reattach to the suction surface earlier. The separation bubble for the high FSTI flow is therefore smaller. The smaller the separation bubble, the lower is the profile loss.

Figure 5.18: Mixed-out profile losses for PAKD-A at FSTI=1.5% and 4.0%.

Figure 5.19: Comparison of separation bubbles between FSTI=1.5% and 4.0% for PAKD-A at $Re=150000$.

Figure 5.20 shows the comparison of the wakes at Reynolds number of 75000 with FSTI=1.5% and 4.0%. For the high FSTI flow, the wake is narrower and more shallow than that of the low FSTI flow, and thus the profile loss is lower. For FSTI=4.0%, the critical Reynolds number for full separation is 35000 to 40000 approximately. For the low turbulence intensity flow, the critical Reynolds number is 70000 to 75000. This difference is due to the earlier transition in the separation bubble for the higher FSTI.

Figure 5.20: Comparison of wakes between FSTI=1.5% and 4.0% for PAKD-A at Re=75000.

5.3.3 Comparison with Profile Losses for the PAK-B Profile

Figure 5.21 summarizes the profile losses for FSTI=1.5%, obtained for PAKD-A and PAK-B (Mahallati, 2003), as a function of Reynolds number. The data are also listed in Table 5.4. For the Reynolds number range from 100000 to 170000, the profile losses for both of them decrease only slightly. The PAKD-A profile loss is slightly higher than that of PAK-B as would be expected from its higher loading. As the Reynolds number decreases, the profile losses for both of these two profiles increase. For PAKD-A, the bursting of the separation bubble leads to the full separation on the suction side when the Reynolds number is below 70000. By comparison, the separation bubble of PAK-B becomes gradually larger without “bursting” and the full separation is delayed to a much lower Reynolds

number. As a result, the profile loss of PAKD-A is substantially higher at low Reynolds numbers.

Figure 5.21: Comparison of profile losses for PAK-B (Mahallati, 2003) and PAKD-A at FSTI=1.5%.

Table 5.4: Comparison of profile losses for PAK-B (Mahallati, 2003) and PAKD-A at FSTI=1.5%.

Reynolds Number	Profile Loss	
	PAKD-A	PAK-B
25000		0.080
50000	0.630	0.044
65000	0.615	
75000	0.068	
100000	0.041	0.030
125000	0.033	
150000	0.031	0.025
170000		0.021

As mentioned, the profile loss is related to the loading produced by the blades. For a more highly-loaded blade, the profile loss is generally expected to be higher. Denton (1993) argued that the profile loss generated in an attached boundary layer was proportional to the cube of the boundary layer edge velocity and a dimensionless dissipation coefficient, C_d , defined as

$$C_d = \frac{T\dot{S}_a}{\rho V_e^3} \quad (5.11)$$

where $\dot{S}_a$ is the rate of entropy production per unit surface area. T is the static temperature. V_e is the blade surface velocity at the edge of the boundary layer. The exact magnitude of the dissipation coefficient cannot be calculated without knowing the full details of the state of the boundary layer. However, correlation of experimental data has led to some approximate results. Denton and Cumpsty (1987) suggest that a reasonable approximation to the dimensionless dissipation coefficient is 0.002, for turbulent boundary layers. The dissipation in a laminar boundary is much less than that in the turbulent boundary layer, by a factor of about 2 to 5. The simplification of the dissipation coefficient implies that the dissipation could be regarded as independent of, or at least relatively insensitive to the details of the boundary layer development beyond whether it is laminar or turbulent. The regions of high surface velocity contribute proportionally much higher amounts of loss and the suction surface is dominant in producing loss. As shown from the pressure distributions in Figure 5.22, the velocities on the PAK-B and PAKD-A pressure surfaces are relatively lower and differ only slightly. However, in the laminar flow

region on the suction surfaces, the velocity for PAKD-A is significantly higher than that for PAK-B, especially around the suction peak. The maximum PAKD-A to PAK-B velocity ratio is about 1.14, corresponding to the associated maximum pressure coefficients. Hence, the profile loss generated in the PAKD-A attached laminar boundary layer is proportionally about 50% higher than that for PAK-B. The attached turbulent boundary generates relatively higher entropy and losses than the laminar (by a factor of about 2 and 5). In the turbulent flow region of Figure 5.22, for PAKD-A the velocity in the forward part of turbulent boundary is slightly higher than that for PAK-B while the velocity in the rear part of turbulent boundary is slightly lower than that for PAK-B. The profile losses generated due to the velocity difference obviously is not as great as that in the laminar region. But the lengths covered by the turbulent boundary layer are different for these two airfoils. Figure 5.22 shows that the PAKD-A separated flow reattaches on the suction surface at about $0.76 B_x$, while the PAK-B separated flow reattaches at approximately $0.83 B_x$. The length of the PAKD-A suction surface that is covered by the turbulent flow is about 1/3 longer than for PAK-B. Brear *et al.* (2002) found that the profile loss is significantly influenced by the length of the turbulent flow on the suction surface. The longer turbulent flow, the more profile loss is generated. Finally, the separation bubble is also one of the sources that contribute the profile losses on the suction surface. As shown in Figure 5.22, the sizes of the bubbles are not significantly different for PAKD-A and PAK-B.

Figure 5.22: Comparison of blade loading for PAK-B (Mahallati, 2003) and PAKD-A at $Re=150000$, $FSTI=1.5\%$.

According to the previous arguments, it is reasonable to expect that PAKD-A should have higher profile loss than PAK-B, based on the higher velocities and longer length of the turbulent boundary layer. Comparing the measured profile losses listed in Table 5.4, the profile loss generated by PAKD-A is in fact higher than that of PAK-B for the reattached flow cases. At $Re=100000$, the profile loss for PAKD-A is 0.041 and the loss for PAK-B is 0.030. The predicted PAKD-A loss is about 40% higher than that of PAK-B. At $Re=150000$, the profile loss for PAKD-A is 0.031 and the loss for PAKD-B is 0.025. The predicted PAKD-A loss is about 30% higher than that of PAK-B.

5.3.4 Comparison with Loss Correlation System

To predict the axial turbine profile losses in meanline analysis, the Kacker & Okapuu (1982) loss correlation is widely used. Kacker & Okapuu presented a modified version of the Ainley/Mathieson/Dunham/Came (AMDC) loss system for axial turbines. The Kacker & Okapuu loss system presents a basis for predicting the complete losses of an axial turbine at the design point. A more detailed description of the Kacker & Okapuu loss system is given in their paper. In the current study the measured loss includes both the profile loss and the trailing edge loss (Section 5.2.1). Here both the profile loss and the trailing edge loss predictions defined in the Kacker & Okapuu loss system are compared with the present measurements.

The comparison between measured and the predicted profile losses for PAKD-A and PAK-B are listed in Table 5.5, and shown in Figure 5.23. The predicted profile losses for PAKD-A are significantly higher than those for PAK-B. At $Re=100000$, the predicted profile loss for PAKD-A is 0.0384 and the predicted loss for PAK-B is 0.0263. At $Re=150000$, the corresponding values are 0.0366 and 0.0249. These predicted results demonstrate that the PAKD-A losses are higher than those for PAK-B as expected and are consistent with the discussions presented earlier. The comparison between the correlated and measured profile loss shows that the Kacker & Okapuu loss correlation system predicts well the profile loss for PAK-B. However, the comparisons also show that the Kacker & Okapuu loss correlation system over-estimates the profile losses for PAKD-A at Reynolds number above 100000. It is noteworthy that the Kacker & Okapuu loss correlation

system is based on cascade and other data for turbine blades for which flow separation was not present. As a result the predicted profile losses for both PAK-B and PAKD-A based on the Kacker & Okapuu loss correlations would be expected to be lower than the actual measurements since both airfoils have separation bubbles at Reynolds number above 100000. This makes it even more surprising that the Kacker & Okapuu loss correlation system still over-estimates the losses for PAKD-A. It seems that some improvements for the Kacker & Okapuu loss correlation system may be needed to predict the performance of the current very highly-loaded lower turbine airfoil.

Figure 5.23: Comparison between measured and correlated profile losses for PAK-B (Mahallati, 2003) and PAKD-A at $Re=150000$, $FSTI=1.5\%$.

Table 5.5: Comparison between measured and correlated profile losses for PAK-B (Mahallati, 2003) and PAKD-A at FSTI=1.5%.

	Reynolds Number	Experimental Cases							Profile Loss	
		β_i	β_e	α_i	α_e	S/B	$t_{\max}/B$	t_{et}/O	Measured	Kacker & Okpau Correlation (Y_p)
PAKD-A	50000	35	60	35.0	52.2	0.993	0.194	0.027	0.630	0.0499
	65000	35	60	35.0	53.4	0.993	0.194	0.027	0.615	0.0453
	75000	35	60	35.0	57.2	0.993	0.194	0.027	0.068	0.0425
	100000	35	60	35.0	57.7	0.993	0.194	0.027	0.041	0.0384
	125000	35	60	35.0	57.5	0.993	0.194	0.027	0.033	0.0366
	150000	35	60	35.0	58.3	0.993	0.194	0.027	0.031	0.0362
PAK-B	25000	35	60	35.0	58.0	0.802	0.128	0.034	0.080	0.0446
	50000	35	60	35.0	58.2	0.802	0.128	0.034	0.044	0.0343
	100000	35	60	35.0	58.3	0.802	0.128	0.034	0.030	0.0268
	150000	35	60	35.0	58.3	0.802	0.128	0.034	0.025	0.0249
	170000	35	60	35.0	58.2	0.802	0.128	0.034	0.021	0.0245

5.4 Deviation Angle

At the trailing edge of the blade the static pressure difference caused by the flows on the suction and pressure surfaces force the streamlines exiting the blade passage towards the suction side (Islam and Sjolander, 1998). The difference between the outlet flow angle and the blade metal angle is defined as deviation angle, δ .

Figure 5.24 compares the deviation angles of PAKD-A and PAK-B (McAuliffe, 2003) at freestream turbulence intensities of 1.5% and 4.0%. The higher pressure difference towards the trailing edge of PAKD-A causes the deviation angle of that blade to be somewhat larger than that for PAK-B at the same Reynolds number (Section 5.2.3, Figure 5.9 and 5.10). At low Reynolds numbers

($Re \leq 65000$ at $FSTI=1.5\%$ and $Re \leq 35000$ at $FSTI=4.0\%$), the flow stalls on the PAKD-A suction surface and leads to the measured deviation angles being much larger.

Figure 5.24: Comparison of deviation angles for PAK-B (Mahallati, 2003) and PAKD-A at $FSTI=1.5\%$ and 4.0% .

The accurate prediction of the flow deviation is important for the estimation of the blade outlet metal angle in the preliminary design phase of a turbine. One empirical deviation correlation was established by Carter and Hughes (1950). This correlation was originally developed for compressors, but the correlation for compressor inlet guide vanes has also been used in turbine analysis. The most widely used empirical correlation was provided by Ainley & Mathieson (1951) for predicting a flow outlet angle rather than the deviation angle. After studying several

published turbine blade deviation correlations, Riess *et al.* (1986) concluded that most of the correlations were only valid for particular airfoils under certain specific operation conditions.

In view of the shortcomings of the existing correlations, Islam and Sjolander (1998) re-examined the factors that influence deviation and developed an improved correlation for axial turbine deviation. The authors assumed that the deviation angle is largely determined by the blade pressure difference towards the blade trailing edge. Examining the factors that affect the deviation, they concluded that additional parameters have significant effects on deviation compared with those used in the traditional correlations. They concluded that the geometric and flow parameters that influence deviation most are the total flow turning angle, the stagger angle, the solidity, the thickness-to-chord ratio and the axial velocity density ratio (AVDR). Islam and Sjolander's correlation seems somewhat better than the traditional ones. The detailed discussions of the factors that influence the deviation and the correlation development procedure are presented in Islam and Sjolander (1998). The final form of the new deviation correlation proposed was expressed as:

$$\delta = \frac{(AVDR)^3 \left(\frac{S}{B}\right)^{1.1} (\alpha_i + \beta_e)^{2.25}}{\gamma^{1.45} \left(\frac{t_{\max}}{B}\right)^{0.3} (22 + 0.22\beta_i^{1.64})} \quad (5.12)$$

$$\text{where AVDR} = \text{axial velocity density ratio} = \frac{\int_0^1 (\rho_e V_e \cos \alpha_e)_{ms} d\left(\frac{y}{s}\right)}{\int_0^1 (\rho_i V_i \cos \alpha_i)_{ms} d\left(\frac{y}{s}\right)}$$

- α_i = inlet flow angle (in degrees)
 β_i = inlet metal angle (in degrees)
 β_e = outlet metal angle (in degrees)
 γ = stagger angle (in degrees)
 S/B = blade spacing-to-true chord ratio
 $t_{\max}/B$ = blade maximum thickness-to-true chord ratio

Based on the deviation correlation equation (5.12) proposed by Islam and Sjolander (1998) and the geometric and flow parameters of PAKD-A and PAK-B, the predicted deviation angles for PAKD-A and PAK-B are presented in Table 5.6 and compared with the measured values.

Table 5.6: Comparison between measured and correlated deviation angles for PAK-B (Mahallati, 2003) and PAKD-A at FSTI= 4.0% with measured AVDR.

	Reynolds Number	Experimental Cases								Deviation	
		Incidence (i)	AVDR	α_i	β_i	β_e	γ	S/B	$t_{\max}/B$	Measured	Islam & Sjolander Correlation
PAKD-A	35000	0.0	0.971	35.0	35	60	26	0.993	0.194	8.9	4.31
	40000	0.0	1.030	35.0	35	60	26	0.993	0.194	3.7	4.58
	50000	0.0	0.989	35.0	35	60	26	0.993	0.194	2.9	4.05
	100000	0.0	0.980	35.0	35	60	26	0.993	0.194	2.0	3.94
	150000	0.0	0.995	35.0	35	60	26	0.993	0.194	2.0	4.13
PAK-B	25000	0.0	0.970	35.0	35	60	26	0.802	0.128	2.1	3.41
	50000	0.0	0.973	35.0	35	60	26	0.802	0.128	1.8	3.44
	100000	0.0	0.972	35.0	35	60	26	0.802	0.128	1.6	3.43
	150000	0.0	0.987	35.0	35	60	26	0.802	0.128	1.6	3.59

For the reattached flow cases for both PAKD-A and PAK-B, the Islam and Sjolander deviation correlation over-estimates the deviation for both PAKD-A and PAK-B. Considering about the maximum uncertainty of 0.5° in angular

measurements, the correlation deviation angles are still larger than the measured deviation angles. However, the Islam and Sjolander correlation predicts that the PAKD-A should have higher deviation than that of PAK-B and this was also found experimentally. For the flow stall condition for PAKD-A, the deviation predicted by the correlation is lower than the measured deviation angle. This is because the Islam and Sjolander deviation correlation is only valid for attached as reattached flow conditions.

As discussed by Islam and Sjolander, the axial velocity density ratio (AVDR) has very strong influence on the deviation correlation due to the high power to which AVDR is raised. The authors also point out that this power was determined from a very small number of cases and therefore should be treated with caution. In order to reduce the influence of AVDR on the correlations, the AVDR for both PAKD-A and PAK-B correlation was set to 1.0 in the Equation 5.13. The comparison between the measured and resulting correlated deviation angles of the PAKD-A and PAK-B airfoils are presented in Figure 5.25 (a) and (b) and listed in Table 5.7. The correlated deviation angles are still over-estimated. Obviously the deviation correlation does not work well for very highly loaded blades and should be improved.

(a) PAK-B measured and correlated deviation angle

(b) PAKD-A measured and correlated deviation angle

Figure 5.25: Measured and correlated deviation angles for PAK-B (Mahallati, 2003) and PAKD-A at FSTI= 4.0%.

Table 5.7: Comparison between measured and correlated deviation angles for PAK-B (Mahallati, 2003) and PAKD-A at FSTI= 4.0% with AVDR=1.0.

	Reynolds Number	Experimental Cases								Deviation	
		Incidence (i)	AVDR	α_i	β_i	β_e	γ	S/B	$t_{\max}/B$	Measured	Islam & Sjolander Correlation
PAKD-A	35000	0.0	1.0	35.0	35	60	26	0.993	0.194	8.9	4.2
	40000	0.0	1.0	35.0	35	60	26	0.993	0.194	3.7	4.2
	50000	0.0	1.0	35.0	35	60	26	0.993	0.194	2.9	4.2
	100000	0.0	1.0	35.0	35	60	26	0.993	0.194	2.0	4.2
	150000	0.0	1.0	35.0	35	60	26	0.993	0.194	2.0	4.2
PAK-B	25000	0.0	1.0	35.0	35	60	26	0.802	0.128	2.1	3.7
	50000	0.0	1.0	35.0	35	60	26	0.802	0.128	1.8	3.7
	100000	0.0	1.0	35.0	35	60	26	0.802	0.128	1.6	3.7
	150000	0.0	1.0	35.0	35	60	26	0.802	0.128	1.6	3.7

Chapter 6

Three-Dimensional Investigations

6.0 Introduction

In the previous chapters, all the results presented were for the flow at the midspan of the cascade. It was assumed that the flow at midspan was essentially two-dimensional. However, there will be three-dimensional flow in the form of secondary flow near the endwalls of the cascade. Furthermore, for high-lift turbine blades, the high pressure difference between the pressure surface and suction surface of each passage in the cascade has the potential to produce strong secondary flows. Therefore, it is necessary to investigate the extent to which the flow was essentially two-dimensional at the midspan of the cascade.

Measurements of boundary layer at the inlet traverse plane ($1.2 B_x$ upstream of the leading edge) were conducted and the results are presented in Section 6.1. For the secondary flow investigation, the total pressure was measured at the outlet traverse plane ($0.4 B_x$ downstream of the trailing edge) and the results are given in Section 6.2. The comparison of the secondary flows of PAKD-A and PAK-B are shown in Section 6.3.

6.1 Inlet Flow Investigation

For the inlet flow investigation, the velocity profiles at the inlet of the cascade across the full spanwise extent were measured. Before the discussion of the experimental results, the definitions of boundary layer integral parameters are given.

The displacement thickness, δ^* , which represents the deficit in mass flow rate in the boundary layer, is defined as

$$\delta^* = \int_0^\delta 1 - \left(\frac{V}{V_e} \right) dy \quad (6.1)$$

where δ is the boundary layer thickness, V is the local mean velocity, and V_e is the local freestream velocity. The boundary layer thickness δ is defined arbitrarily as the point at which the velocity reaches some fraction of the “undisturbed” freestream velocity: usually $V/V_e=0.995$, but sometimes $V/V_e=0.99$ is used. In this study, the standard convention of the boundary layer thickness δ is defined as V/V_e reaching 0.995.

The momentum thickness, θ , which represents the deficit in momentum flow rate in the boundary layer, is defined as

$$\theta = \int_0^\delta \frac{V}{V_e} \left(1 - \frac{V}{V_e} \right) dy \quad (6.2)$$

These two integral length scales, δ^* and θ , are important parameters of the boundary layer and are also often expressed in terms of Reynolds numbers based on the local freestream velocity, V_e . That is

$$\text{Re}_{\delta^*} = \frac{\rho V_e \delta^*}{\mu} \quad (6.3)$$

and

$$\text{Re}_{\theta} = \frac{\rho V_e \theta}{\mu} \quad (6.4)$$

The shape factor, H , measures the fullness of the boundary layer velocity profile, and is defined as

$$H = \frac{\delta^*}{\theta} \quad (6.5)$$

The measured inlet velocity profiles and their associated integral parameters are discussed in the next section for the two FSTI cases (1.5% and 4.0%) at a Reynolds number of 150000.

At inlet, the boundary layer has developed on the surfaces of the hub and tip walls of the test section. The hub endwall is taken as the aluminum turntable and tip endwall as the plexiglass window (Section 3.1.2). The inlet boundary layer thickness has been examined by traversing a total pressure probe (Section 3.6.1) at the inlet measurement plane that is located at $1.2 B_x$ upstream of the leading edge. The velocity profile is obtained from the total pressure and the static pressure measured at the tip wall at the same location. The total pressure probe was traversed at three locations corresponding to the middle of the blade passages for the three passages at the centre of the cascade. Figure 6.1 shows the results of the normalized velocity profiles (V/V_{CL}) against the relative spanwise position (y/H) for the centre passage at a typical Reynolds number of 150000 with FSTI levels of 1.5

and 4.0%, respectively. V_{CL} is the inlet velocity at midspan of the blade. The velocity profiles in Figure 6.1 show the boundary layers on the hub endwall surface. The boundary layer thicknesses are estimated as $0.106H$ (2.1 cm) for $FSTI=1.5\%$ and $0.110H$ (2.2 cm) for $FSTI=4.0\%$ based on the velocity ratio (V/V_{CL}) reaching 0.995. The boundary layer thickness at the inlet measurement plane is thus about ten percent of the blade span. Boundary layer integral parameters calculated by numerical integration are given in Table 6.1 for the two values of $FSTI$.

Table 6.1: PAKD-A inlet boundary layer integral parameters.

	Displacement Thickness (δ^*) (mm)	Momentum Thickness (θ) (mm)	Reynolds Number (Re_{δ^*}) ($\times 10^3$)	Reynolds Number (Re_{θ}) ($\times 10^3$)	Shape Factor (H)
$FSTI=1.5\%$	1.26	0.895	2.52	1.79	1.41
$FSTI=4.0\%$	1.45	1.102	2.90	2.20	1.33

The shape factor for $FSTI=1.5\%$ (1.41) is close to 1.4, the value of that is expected for a turbulent boundary layer in zero pressure gradient, while the shape factor for $FSTI=4.0\%$ is a little lower than would be expected. The lower value of shape factor at $FSTI=4.0\%$ is possibly the result of wakes from the bars that make up the 4% turbulent grid. The $FSTI=1.5\%$ turbulence grid is installed far upstream from the inlet measurement plane of the cascade and its wakes are nearly fully mixed out by the time they reach the cascade. However, the 4% is located much closer to the cascade. The influence of the wakes of the grid bars is still present at the traverse plane, including inside the boundary layer on the hub endwall. This may explain why the shape factor is lower than expected at $FSTI=4.0\%$. The flow characteristics of the various grids that are used in the cascade test section are

documented in detail by McAuliffe (2003). Beyond the hub wall region, the velocity profiles are relatively uniform out to the middle span of the blade. The flow beyond the endwall boundary layer is thus essentially two-dimensional. The variations of velocity profile beyond the endwall boundary layer at the FSTI level of 4.0% are larger than those at FSTI=1.5% and again the result of the wakes from the bars that make up the 4% turbulence grid.

Figure 6.1: Hub endwall inlet velocity profiles for PAKD-A at $Re=150000$, FSTI=1.5% and 4.0%.

Figure 6.2 and 6.3 show the velocity distributions across the full blade span and for the three centre passages with $Re=150000$ at $FSTI=1.5\%$ and 4.0% respectively.

Figure 6.2: Full spanwise inlet velocity profiles for PAKD-A at $Re=150000$, $FSTI=1.5\%$.

The full-span results indicate that the boundary layer thicknesses are fairly similar at the hub and tip endwalls although the tip wall boundary layers appears to be slightly thicker than those at the hub wall. There are some differences in the number and height of small steps that exist on each endwall between the inlet of the test section and the cascade leading edge. In addition, the amount of mass removal

at the two boundary layer bleed slots (Section 3.1.2) may be slightly different. The variations of the velocity profile at FSTI level of 4.0% are larger than for FSTI=1.5%, as also seen in Figure 6.1. The largest variations are seen for the upper blade passage since it is the one located closest to the grid.

Figure 6.3: Full spanwise inlet velocity profiles for PAKD-A at $Re=150000$, $FSTI=4.0\%$.

6.2 Outlet Flow Investigation

In a turbine airfoil passage, the flow field near the endwalls is composed of complex three-dimensional flow structures. These reduce the extent of the two-dimensional flow near the midspan. To determine the spanwise extent of the two-dimensional flow, the secondary flow has been measured downstream of the cascade using a seven-hole pressure probe (Section 3.6.3). The measurements were made at the standard outlet measurement plane, which is located at $0.4 B_x$ downstream of the trailing edge.

Figure 6.4 shows the measurement grid overlaid on the contours of normalized total pressure at FSTI of 1.5% and a Reynolds number of 150000. The value of the normalized total pressure is obtained by taking the difference between the outlet total pressures (P_{oe}) and the inlet total pressure (P_{oi}) obtained on the centerline and normalizing it by the difference between the inlet total pressure (P_{oi}) and inlet static pressure (P_{si}), given by

$$C_{poe} = \frac{P_{oe} - P_{oi}}{P_{oi} - P_{si}} \quad (6.6)$$

As seen, the traverse grid was fairly coarse since it was only intended to identify the extent of two-dimensional flow at midspan, not to provide detailed results for the secondary flow. The two black lines indicate the blade pitch. As shown in Figure 6.4, the flow field near the hub endwall of the turbine airfoil passage is composed of vortical flow structures that are caused by the inlet boundary layer separation and

cross-passage flow within the blade passage. Physically, as the flow approaches the turbine airfoil passage, some of the inlet endwall boundary layer separates in the leading edge region of the blade due to the local adverse pressure gradient. The separated flow rolls up into a horseshoe vortex that wraps around the leading edge with suction and pressure side legs. The pressure gradient between the pressure surface and suction surface causes the pressure side leg to migrate across the airfoil passage along the endwall towards the suction surface of the adjacent airfoil. This pressure difference also drives the endwall boundary layer within the blade passage towards the suction surface and together they constitute the passage vortex. The various vortices in the blade passage cause a significant total pressure loss, as evident from the contour plots. The range influenced by the secondary flow extends about 60-70 mm from the hub endwall. From about 70-100 mm (midspan), the flow appears to be composed of blade wake and therefore the blade flow appears to be essentially two-dimensional at midspan. The axial velocity ratio (AVR) (Section 4.2.3) is one of the important parameters used to investigate the flow two-dimensionality. The value of AVR obtained varies from 0.984 to 1.018 for FSTI=1.5% (Section 4.2.3), which are close to 1.0 but do not exactly equal to 1.0 at midspan. This shows that the flow at midspan is not exactly two-dimensional.

Figure 6.4: Hub endwall normalized total pressure contour for PAKD-A at $Re=150000$, $FSTI=1.5\%$.

Figure 6.5 presents the normalized total pressure contours for the same Reynolds number of 150000 but with an FSTI of 4.0%. The traverse grid was the same as that used for FSTI of 1.5%. Comparing with the case at $FSTI=1.5\%$, the secondary flow appears to occupy a slightly smaller spanwise extent. The range influenced by the secondary flow extends approximately 50-60 mm from the hub endwall. From about 60-100 mm (midspan), the flow appears to be composed of blade wake. The reason for the differences between the two cases may be that the

high turbulent flow has more resistance for the boundary layer separation and the rolled-up pressure leg of the horseshoe vortex may be smaller.

Figure 6.5: Hub endwall normalized total pressure contour for PAKD-A at $Re=150000$, $FSTI=4.0\%$.

To investigate the spanwise symmetry of the flow, the total pressure was measured for the full spanwise extent for $Re=150000$ and $FSTI=4.0\%$, as shown in Figure 6.6. From Figure 6.6, the extent of secondary flow is seen to be somewhat larger at the tip endwall compared with that appearing at the hub endwall. This is consistent with the somewhat thicker inlet endwall boundary layer observed at the

tip endwall (see Figure 6.3). Nevertheless, the essentially two-dimensional flow seems to extend over at least 40% of the blade span (80 mm).

Figure 6.6: Full spanwise normalized total pressure contour for PAKD-A at $Re=150000$, $FSTI=4.0\%$.

6.3 Comparison with PAK-B Measurements

6.3.1 Inlet Three-Dimensional Comparison

Since the midspan results were compared for the PAKD-A and PAK-B airfoils, it was also necessary to verify the extent of the essentially two-dimensional flow for the latter airfoil. For the PAK-B blades, the inlet velocity profiles (V/V_{CL}) were measured at the standard inlet traverse plane ($1.2 B_x$) from the hub endwall to

the tip endwall in exactly the same way as for PAKD-A. The total pressure probe was again traversed at three locations corresponding to the middle of the blade passages for the three passages at the centre of the cascade.

Figure 6.7 shows the comparison of the normalized PAKD-A and PAK-B inlet velocity profiles (V/V_{CL}) against the relative spanwise position (y/H) for the centre passage at a typical Reynolds number of 150000 with $FSTI = 1.5\%$. V_{CL} is the inlet velocity at midspan of the blade.

Figure 6.7: Hub endwall inlet velocity profiles for PAK-B and PAKD-A at $Re=150000$, $FSTI=1.5\%$.

As shown in Figure 6.7, the velocity profiles at the hub endwall for PAK-B and PAKD-A are very similar, as expected. The boundary layer thickness (δ) is about $0.106H$ (2.1 cm) for PAKD-A and PAK-B as the velocity ratio (V/V_{CL}) reaches 0.995. The shape factor (H) is about 1.41 for PAKD-A and 1.40 for PAK-B, which is close to a typical shape factor of turbulent flow (1.4). The associated boundary layer integral parameters obtained by numerical integration for PAKD-A and PAK-B in the region of hub endwall are presented in Table 6.2.

Table 6.2: PAK-B and PAKD-A inlet boundary layer integral parameters.

	Displacement Thickness (δ^+) (mm)	Momentum Thickness (θ) (mm)	Reynolds Number (Re_{δ^+}) ($\times 10^3$)	Reynolds Number (Re_{θ}) ($\times 10^3$)	Shape Factor (H)
PAK-B	1.29	0.921	2.57	1.84	1.40
PAKD-A	1.26	0.896	2.52	1.79	1.41

Beyond the hub wall region, the velocity profiles are relatively uniform out to the middle span of the blade, as shown in Figure 6.8. For PAK-B, the flow beyond the boundary layer is thus essentially two-dimensional. The boundary layer developments are also asymmetrical for both PAKD-A and PAK-B. The tip wall boundary layers are thicker than those at hub wall. This is because of the different small steps and blade slots on each endwall, as discussed in Section 6.1.2.

Figure 6.8: Full spanwise inlet velocity profiles for PAK-B and PAKD-A at $Re=15000$, $FSTI=1.5\%$.

6.3.2 Outlet Three-Dimensional Comparison

To determine the spanwise extent of the two-dimensional flow and symmetry of flow in PAK-B cascade, the normalized total pressure was measured for the full spanwise extent for $Re=150000$ and $FSTI=1.5\%$, as shown in Figure 6.9(a). As for PAKD-A, the total pressure was measured using the seven-hole pressure probe at the standard outlet measurement plane, which is located at $0.4 B_x$

downstream of the trailing edge. The total pressure coefficient (C_{poe}) is defined by Equation 6.6 in Section 6.2. The two black lines indicate the PAK-B blade pitch. For the purpose of comparison, the corresponding results for PAKD-A are presented again in Figure 6.9(b).

As shown in Figures 6.9(a) and (b), the range influenced by the secondary flow for PAK-B extends approximately 75-80 mm from the hub endwall, while the secondary flow for PAKD-A extends to approximately 50-70mm from the hub endwall. At the region of the hub endwall, the secondary flow generated in PAKD-A cascade is stronger than for PAK-B, as evidenced by high total pressure loss in Figure 6.9. This is because that PAKD-A has more high lift than PAK-B and causes stronger secondary flow. From about 80-120 mm (midspan), the flow appears to be composed of blade wake and therefore the blade flow appears to be essentially two-dimensional at midspan. The two-dimensional flow seems extend to over at least 20% of the blade span (40 mm). The blade wake of PAK-B is smaller and thinner than that of PAKD-A. This is because of the thinner boundary layer development on the surfaces of PAK-B airfoils. At the region of tip endwall (from 120-200mm), the extent of secondary flow of PAK-B is also somewhat larger at the tip endwall compared with that appearing at hub endwall. This is again consistent with the somewhat thicker inlet endwall boundary layer observed at the tip endwall (see Figure 6.8).

The values of AVR vary from 0.99 to 1.015 for PAK-B (Mahallati, 2003) and from 0.984 to 1.018 for PAKD-A at FSTI=1.5%. Both of them are close to 1.0.

This implies that all the measurements conducted in the current PAKD-A and previous PAK-B at the midspan are not exactly two-dimensional. However, the measurements performed at midspan of cascade are essentially two-dimensional.

(a) PAK-B

(b) PAKD-A

Figure 6.9: Full spanwise normalized total pressure contour for PAK-B and PAKD-A at $Re=150000$, $FSTI=4.0\%$.

Chapter 7

Conclusions and Recommendations

7.1 Conclusions

The performance of a very high-lift low-pressure turbine blade has been investigated for a wide range of Reynolds number with two different levels of freestream turbulence intensity in the Low-Speed Turbomachinery Laboratory at Carleton University. The profile used in the present study is the Pratt & Whitney PAKD-A airfoil. The measurement results were compared with the results for a more lightly-loaded reference blade, PAK-B, designed for the same inlet and outlet flow conditions.

For each measurement in the present PAKD-A study, the uniformity at the inlet and periodicity at the outlet have been obtained by adjusting the control surface positions. The value of the axial velocity ratio (AVR) shows that nearly two-dimensional flow was achieved for each measurement case.

The static pressure distributions on the suction and pressure surfaces have been measured by static taps at the midspan of the blade. The PAKD-A blade was found to have a separation bubble on the suction side for all Reynolds numbers and levels of freestream turbulence intensity examined. The location of the separation

point varied very little with the Reynolds number suggesting the flow upstream of the separation point is laminar. With the decreasing of Reynolds number, the separated flow reattaches on the suction surface slightly further downstream and the separation bubble becomes larger. However, at a Reynolds number less than about 75000 at FSTI=1.5% and 40000 at FSTI=4.0%, the separation bubble appears to “burst” and fails to reattach. For a fixed Reynolds number condition, the separation bubble is influenced by the freestream turbulence intensity (FSTI). With the increment of the level of FSTI, the separation bubble becomes smaller. No separation has been observed on the pressure surface for all the static pressure measurements in the present study.

Comparison of static pressure distributions with the PAK-B profile indicates that PAKD-A has higher loading than PAK-B as a result of the higher spacing. The maximum velocity of PAKD-A is higher than that of PAK-B. The stronger adverse pressure gradient on PAKD-A leads to the laminar flow separation on the suction surface. The significant difference between these two blades is the behavior of separation bubbles on the suction surfaces. Unlike for PAKD-A blade, the length of the PAK-B separation bubble continues to increase and it reattaches closer and closer to the trailing edge of the blade with decreasing Reynolds number. The separation bubble on PAK-B appears to develop from a short bubble to a long bubble with decreasing the Reynolds number.

Profile loss measurement has been performed at the midspan of the cascade. The profile loss is influenced by the Reynolds number and freestream turbulence

intensity. For a given level of freestream turbulence intensity, the profile loss increases with decreasing Reynolds number. If the flow stalls on the suction surface, the profile loss becomes massively larger than those of the reattached flows. These measured profile losses are consistent with the blade static pressure distribution measurements. For a certain Reynolds number condition, the loss is smaller at the higher level of FSTI.

Comparison with profile losses for the PAK-B profile shows that the measured PAKD-A profile loss is somewhat higher than that of PAK-B as expected. The comparison between the correlated and measured profile loss shows that the Kacker & Okapuu loss correlation system predicts well the profile loss for PAK-B but over-estimates the profile losses for PAKD-A. The improvement of profile loss correlation for very highly-loaded low-pressure turbine blade seems to be needed. PAKD-A demonstrates that it is possible to design very highly-loaded turbine blades with loss penalties as earlier design experience predicted.

The Islam and Sjolander deviation correlation over-estimates the deviation for both PAKD-A and PAK-B. The deviation correlation does not work well for very highly loaded blades and should be modified.

In the three-dimensional investigations, for both PAKD-A and PAK-B cascades, the midspan flow fields are not affected by the flow at endwall regions. All the measurements performed at midspan of cascade in the current study are essentially two-dimensional.

7.2 Recommendations for Future Research

From the present experimental investigations, several directions of future research have been identified.

During the course of the present experimental measurements, it was realized that hot wire and hot film measurements for the aft suction surface of PAKD-A blades could provide useful information on the boundary layer development and the laminar separation bubble. Especially in a range of critical Reynolds number, the short-laminar-separation bubble “bursts” abruptly. The separated flow could not reattach on the suction surface and profile losses increased suddenly. This was unlike the cases in the PAK-B measurements. At present, very little is known about the mechanism of separation bubble “bursting”. With more detailed blade surface boundary layer information, the investigation of the loss components is a direction that maybe leads to the explanation of the low profile loss for the highly-loaded turbine blades.

The performance of the high-lift PAKD-A blade at off-design conditions is an interesting area for future research. Low-pressure turbine blades are generally designed with optimum performance at design conditions. During different operational phases from taking-off to landing, the operating point for airborne engines will vary from the design value. Information about the off-design performance with varying inlet flow angles would be valuable for the future research and design practice.

The research of unsteady performance of the high-lift PAKD-A blade is also an interesting area for future research. In practice, the blade-rows in the LP turbine produces periodical wakes that pass through the downstream passages, which makes the flow inherently unsteady. With the assistance of the wake generator rig, a realistic simulation of the rotor-stator interaction could be achieved.

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Appendix A

Calibration of Pressure Transducers

This appendix presents the calibrations of the differential pressure transducers used for the current study. The differential pressure transducers are used to convert the measured differential pressures to electrical voltage signals. The calibration of the pressure transducers were performed by using a water micro-manometer (Benner, 2003) with full scale range of 0-30 cm water. Table A.1 lists three types of differential pressure transducers, including the respective manufacturer, pressure ranges and the number for each type of the transducers used.

Table A.1: Differential pressure transducers.

Transducer	Manufacturer	Quantity	Pressure Range
DRAL501DN	Data Instruments	4	± 250 Pa
DRAL505DN	Data Instruments	4	± 1250 Pa
DRAL520DN	Data Instruments	2	± 5000 Pa

All the differential pressure transducers listed in the Table A.1 have a linear relationship between the pressure difference and the output voltage, as expressed by:

$$\Delta P = C_1 E + C_2 \quad (\text{A.1})$$

where ΔP is the measured difference pressure and E is the output voltage. C_1 and C_2 are the linear calibration coefficients. The zero-offset voltage has been found to

drift for different measurements. The zero-offset voltage is the voltage when the pressure difference is zero. The C_2 coefficient was used to correct for the zero-offset voltage drift during the measurements. Table A.2 lists the calibration coefficients for two individual calibrations performed during the current experiments. The calibration data for the first calibration are plotted in Figure A.1, A.2 and A.3 for the DRAL 501, DRAL 505 and DRAL 520 respectively. DRAL 501DN and DRAL 505DN transducers have been calibrated over their full ranges (see Table A.1), whereas DRAL 520DN transducers have been calibrated over the required range for the experiments (± 4000 Pa).

Table A.2: Differential pressure transducer calibration coefficients.

Pressure Transducer	Calibration 1		Calibration 2	
	C1	C2	C1	C2
DRAL501DN-1	96.15	-333.81	96.09	-333.45
DRAL501DN-2	96.43	-334.39	96.31	-334.17
DRAL501DN-3	96.59	-334.58	96.51	-334.44
DRAL501DN-4	96.34	-335.21	96.28	-334.87
DRAL505DN-1	493.41	-1711.6	492.82	-1710.3
DRAL505DN-2	491.92	-1716.2	490.45	-1715.9
DRAL505DN-3	493.24	-1713.8	492.62	-1712.4
DRAL505DN-4	492.23	-1716.2	492.02	-1715.5
DRAL520DN-1	2027.9	-7084.4	2023.8	-7082.7
DRAL520DN-2	2027.4	-7093.1	2023.2	-7091.4

Figure A.1: DRAL501DN calibration curves.

Figure A.2: DRAL505DN calibration curves.

Figure A.3: DRAL520DN calibration curves.

Appendix B

Calibration of Three-Hole Pressure Probe

This appendix presents the calibration procedure and results for the three-hole pressure probe used in the current study. Figure B.1 shows the geometry and port numbering of the three-hole pressure probe. The calibrations were performed in the calibration rig as described in Section 3.5. Before each calibration the three-hole pressure probe was aligned in the calibration rig with nozzle flow. The probe was calibrated in yaw over a range from -20° to 20° with an increment of 0.5° .

The Lewis method (1966) was used to express the calibration data as pressure coefficients that can be used to extract the flow angle, static pressure, and dynamic pressure for an unknown flow. For three-hole probe, each port pressure coefficient ($C_{P,n}$) is given by:

$$C_{P,n} = \frac{P_n - P_s}{P_o - P_s} \quad (\text{B.1})$$

where $n=1, 2, 3$. The port pressure coefficients have been used to calculate the 6 calibration coefficients presented in Lewis (1966), which are defined as:

Figure B.1 Geometry and Port Numbering of Three-Hole Pressure Probe

$$K_1 = \frac{P_1 - P_s}{P_o - P_s} = C_{P1} \quad (\text{B.2})$$

$$K_{12} = \frac{P_1 - P_2}{P_o - P_s} = C_{P1} - C_{P2} \quad (\text{B.3})$$

$$K_{13} = \frac{P_1 - P_3}{P_o - P_s} = C_{P1} - C_{P3} \quad (\text{B.4})$$

$$K_{23} = \frac{P_1 - P_2}{P_1 - P_3} = C_{P2} - C_{P3} \quad (\text{B.5})$$

$$K_{\phi 2} = \frac{P_1 - P_2}{P_1 - P_3} = \frac{K_{12}}{K_{13}} \quad (\text{B.6})$$

$$K_{\phi 3} = \frac{P_1 - P_3}{P_1 - P_2} = \frac{K_{13}}{K_{12}} \quad (\text{B.7})$$

For the current study, calibrations have been performed at 17 different probe Reynolds numbers, defined as:

$$\text{Re}_w = \frac{Uw}{\nu} \quad (\text{B.8})$$

where w is the probe width, 2.0 mm. Table B.1 lists the calibrated three-hole pressure probe Reynolds numbers and the corresponding cascade Reynolds numbers at inlet and outlet measurement planes, corresponding to the associated flow velocities. The calibration coefficients for $\text{Re}_w=650, 1300, 2600, 3900$ and 5200 are presented in Figure B.2, B.3, B.4 and B.5.

Table B.1: Reynolds numbers of three-hole probe calibration.

Flow Velocity (m/s)	Probe Reynolds Number	Cascade Reynolds Number	Measurement Plane
5	650	25000	Inlet
8	1040	25000	Outlet
6	810	30000	Inlet
10	1300	30000	Outlet
7	910	35000	Inlet
11	1430	35000	Outlet
8	1040	40000	Inlet
13	1690	40000	Outlet
10	1300	50000	Inlet
16	2080	50000	Outlet
13	1690	65000	Inlet
21	2730	65000	Outlet
15	1950	75000	Inlet
24	3240	75000	Outlet
20	2600	100000	Inlet
32	4160	100000	Outlet
25	3250	125000	Inlet
40	5200	125000	Outlet
30	3900	150000	Inlet
48	5400	150000	Outlet

Figure B.1: Variation of K_1 with pitch angle. Figure B.2: Variation of K_{12} , K_{13} with pitch angle.

Figure B.3: Variation of K_{23} with pitch angle. Figure B.4: Variation of $K_{\phi 1}$, $K_{\phi 2}$ with pitch angle.

Appendix C

Calibration of Seven-Hole Pressure Probe

This appendix presents the calibration procedure and results for the seven-hole pressure probe. DeCecco (1995) and Benner (2003) have given detailed descriptions of the seven-hole probe calibration procedure and most of the description provided here was drawn from their work. The seven-hole probe calibration procedure used in the current study was presented by Gallington (1981) and Gerner and Maurer (1981). The calibrations were performed in the calibration apparatus described in Section 3.5. Before each calibration the seven-hole pressure probe was aligned with the nozzle flow. The probe was calibrated in the pitch and yaw directions over a range from -40° to 40° in an increment of 2.0° . Figure C.1 shows the geometry and the port numbering of the seven-hole pressure probe.

Low-Angle Regime

For the small misalignment angles, which are typically less than 26° relative to the probe axis, the flow is expected to remain attached over the entire surface of the probe tip. As seen from Figure C.1, the central port (Port 7) normally senses the highest pressure. Pressures sensed by the seven ports are used to determine the follow non-dimensional parameters:

Figure C.1: Geometry and port numbering of seven-hole pressure probe.

$$C_{\alpha 1} = \frac{P_4 - P_1}{P_7 - \overline{P_{1-6}}} \quad (\text{C.1})$$

$$C_{\alpha 2} = \frac{P_3 - P_6}{P_7 - \overline{P_{1-6}}} \quad (\text{C.2})$$

$$C_{\alpha 3} = \frac{P_2 - P_5}{P_7 - \overline{P_{1-6}}} \quad (\text{C.3})$$

where P_1 to P_7 is the port pressure sensed by the seven-hole pressure probe. $\overline{P_{1-6}}$ is the average of the six surrounding peripheral port pressure. It is approximated as the local static pressure. P_7 is approximated to be the local total pressure. The denominator in the above expressions is referred to as the “pseudo-dynamic”

pressure. The numerator in each non-dimensional expression is based on the difference between diametrically opposed port pressure readings. This implies that each coefficient is most sensitive to flow angularity in three separate planes, each 60 degrees apart, passing through the probe axis.

Figure C.2 shows the seven-hole probe in the tangential coordinate axes. Of the above three non-dimensional expressions, only two are required to uniquely define the flow directions. By equally weighting each coefficient, the following angle coefficients are defined as:

Figure C.2: Tangential coordinate system for low-angle regime (from Benner, 2003).

$$C_\alpha = \frac{2C_{\alpha 1} + C_{\alpha 2} - C_{\alpha 3}}{3} \quad (\text{C.4})$$

$$C_\beta = \frac{C_{\alpha 2} + C_{\alpha 3}}{\sqrt{3}} \quad (\text{C.5})$$

By calibrating the probe in a known flow, the local pitch angle, α , yaw angle, β , total pressure coefficient C_o , and dynamic pressure coefficient C_q can all be related to be the these two non-dimensional coefficients.

$$\alpha = f_1(C_\alpha, C_\beta) \quad (\text{C.6})$$

$$\beta = f_2(C_\alpha, C_\beta) \quad (\text{C.7})$$

$$C_o = f_3(C_\alpha, C_\beta) \quad (\text{C.8})$$

$$C_q = f_4(C_\alpha, C_\beta) \quad (\text{C.9})$$

where

$$C_o = \frac{P_7 - P_o}{P_7 - P_{1-6}} \quad (\text{C.10})$$

$$C_q = \frac{P_7 - \overline{P_{1-6}}}{P_o - P_s} \quad (\text{C.11})$$

The seven-hole pressure probe calibration results with a flow velocity of 42 m/s for the low-angle-regime are presented through Figure C.3 to C.6. This purpose of this calibration was to justify by the fact that only approximate three-dimensional measurements were needed to determine extent of two-dimensional flow at midspan, rather than the calibration for the Reynolds number effects. As outlined above, the low-angle-regime calibration procedure is used as long as pressure of the central port (Port 7) is the highest and the flow misalignment angles are less than 25°.

Figure C.3: C_α calibration data, sector 7.

Figure C.4: C_p calibration data, sector 7.

Figure C.5: C_o calibration data, sector 7.

Figure C.6: C_q calibration data, sector 7.

High-Angle Regime

At large flow misalignment angles the flow will tend to separate over the downstream portions of the probe. The pressure ports in the separated region are insensitive to change in flow angularity. Therefore, it is necessary to redefine calibration coefficients so they contain only the port pressures in the attached flow region. This is done by grouping the pressure ports into six 60 degree wedge-shaped sectors each containing the central port and three adjacent peripheral ports. Figure C.7 shows the resultant division of angular space.

Figure C.7: Division of angular space (from Benner, 2003).

In the high-angle regime, Gallington (1980) found that it was more appropriate to use a polar coordinate system, shown in Figure C.8, to define the calibration coefficients:

Figure C.8: Polar coordinate system for high-angle regime (from Benner, 2003).

$$C_{\theta n} = \frac{P_n - P_7}{P_n - \left(\frac{P_{n-} + P_{n+}}{2} \right)} \quad (\text{C.12})$$

$$C_{\phi n} = \frac{P_{n-} - P_{n+}}{P_n - \left(\frac{P_{n-} + P_{n+}}{2} \right)} \quad (\text{C.13})$$

where the subscripts n , $n+$, and $n-$ represent the peripheral-pressure-port number, and the adjacent ports clockwise and counterclockwise to port n , respectively. These coefficients can be related to the pitch angle, roll angle, total pressure coefficient, C_o and dynamic pressure coefficient, C_q , as follows:

$$\theta_n = f_1(C_{\theta n}, C_{\phi n}) \quad (C.14)$$

$$\phi_n = f_2(C_{\theta n}, C_{\phi n}) \quad (C.15)$$

$$C_{on} = f_3(C_{\theta n}, C_{\phi n}) \quad (C.16)$$

$$C_{qn} = f_4(C_{\theta n}, C_{\phi n}) \quad (C.17)$$

$$C_o = \frac{P_n - P_o}{P_n - \left(\frac{P_{n-} + P_{n+}}{2} \right)} \quad (C.18)$$

$$C_q = \frac{P_n - \left(\frac{P_{n-} + P_{n+}}{2} \right)}{P_o - P} \quad (C.19)$$

Similar to the low-angle regime, the functions in Equation C.14 to C.17 are determined by calibrating the probe in a known flow. Figure C.9 to C.12 present the calibration data for Sector 4 of the seven-hole probe.

Figure C.9: C_ϕ calibration data, sector 4.

Figure C.10: C_θ calibration data, sector 4.

Figure C.11: C_o calibration data, sector 4.

Figure C.12: C_q calibration data, sector 4.

Appendix D

Four-Passages Uniformity and Periodicity Results

In this section, uniformity and periodicity measurements for four passages are presented for maximum and minimum Re conditions for each FSTI

The uniformity measurements were conducted at $1.2B_x$ upstream of the leading edge of the cascade while the periodicity was measured at $0.4B_x$ downstream of the trailing edge of the cascade (see Section 3.1.3). The measured data are expressed in terms of the non-dimensional parameters defined in Section 4.2.1. In each of the following plots, the settings of the control surfaces in the test section (see Section 3.1.2) are also included for each measurement.

Figure D.2: PAKD-A Outlet flow Periodicity at $Re=50000$, $FSTI=1.5\%$.

Figure D.3: PAKD-A inlet flow uniformity at Re=150000, FSTI=1.5%.

Figure D.4: PAKD-A Outlet flow Periodicity at Re=150000, FSTI=1.5%.

Figure D.5: PAKD-A inlet flow uniformity at Re=30000, FSTI=4.0%.

Figure D.6: PAKD-A Outlet flow Periodicity at Re=30000, FSTI=4.0%.

Figure D.7: PAKD-A inlet flow uniformity at Re=150000, FSTI=4.0%.

Figure D.8: PAKD-A Outlet flow Periodicity at Re=150000, FSTI=4.0%.