CHALLENGES OF CREATING REALISTIC PERIODICAL UNSTEADY INFLOW CONDITIONS IN A LINEAR COMPRESSOR CASCADE

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ABSTRACT
The paper presents results of an experimental investigation into the effects of a periodic wake induced inlet disturbance on the tip leakage flow in a linear compressor cascade. A translatory wake generator, being equipped with circular cylinders, is used to produce the unsteady inflow. The setup resembles the kinematic conditions found in the compressor rotor tip region. A variation of the flow coefficient \( \varphi \) is considered to account for effects arising from inappropriately oriented wake trajectories. The evaluation of the cascade flow field mainly relies on five-hole probe and two-dimensional CTA probe data. Differences in the resulting inlet conditions are discussed in the context of the steady and time-resolved leakage flow characteristics being strongly dependent on \( \varphi \). The interaction between the moving bar wakes and the sidewall boundary layer is suggested to cause the absence of any periodic interaction for realistic values of \( \varphi \) in this configuration.

KEYWORDS
COMPRESSOR, CASCADE, TIP LEAKAGE VORTEX, PERIODIC WAKE INTERACTION, UNSTEADY

NOMENCLATURE

\begin{align*}
\begin{array}{ll}
c, w, u & \text{absolute, relative, bar velocity} \\
C & \text{chord length} \\
d & \text{diameter, blade thickness} \\
f & \text{frequency} \\
h & \text{blade height} \\
i & \text{incidence angle} \ (i = \alpha_{1m} - \alpha_{1, MS}) \\
k & \text{yaw coefficient} \\
r^* & \text{normalized span} \\
R \text{e} & \text{Reynolds number} \\
s & \text{clearance width} \\
t & \text{pitch} \\
T & \text{time, period} \\
tke & \text{turbulent kinetic energy (cf. Eqn. (2))} \\
Tu & \text{turbulence intensity} \\
x, y, z & \text{axial, pitchwise, height coordinate} \\
\alpha, \beta & \text{absolute, relative flow angle} \\
\varphi & \text{flow coefficient} \ (\varphi = c_x/u) \\
\lambda & \text{stagger angle} \\
\zeta & \text{loss coefficient (cf. Eqn. (1))} \\
\end{array}
\end{align*}

Subscripts

1 \quad \text{inlet} \\
2 \quad \text{outlet} \\
m \quad \text{metal} \\
MS \quad \text{midspan} \\
p \quad \text{profile} \\
sec \quad \text{secondary} \\
undist \quad \text{undisturbed (no bars)}

Mathematical symbols

\begin{align*}
\langle \ldots \rangle & \quad \text{ensemble average} \\
\prime & \quad \text{fluctuation} \\
\ldots & \quad \text{fluctuation} \\
\end{align*}

Abbreviations

2C \quad \text{two component procedure} \\
CTA \quad \text{constant temperature anemometry} \\
LSRC \quad \text{low-speed research compressor} \\
MP \quad \text{measurement plane} \\
SFP \quad \text{split-fibre probe} \\
SN \quad \text{single normal (wire)} \\
TLV \quad \text{tip leakage vortex}
INTRODUCTION

An important aspect of multistage axial turbomachinery aerodynamics is the periodic interaction between adjacent blade rows moving relatively to each other. Thereby, one significant source of aerodynamic excitation arises from the incoming wakes. To allow for detailed investigations on isolated aspects of this interaction, great effort was put into studies on enhanced wind tunnel setups employing wake generators that utilize circular cylinders. Over the past decades numerous researchers dedicated their work to the complex topic of boundary layer transition on flat plates and airfoils in the presence of moving wakes (e.g. Dong and Cumpsty (1990) and Curtis et al. (1997)). Besides "squirrel cage" type configurations (e.g. Pfeil et al. (1983)) or spoked wheels (e.g. Funazaki and Koyabu (1999)) translatory modules represent the most common design, although they suffer from limitations of the bar velocity due to mechanical constraints. Thus, their operation is usually restricted to low-speed applications to establish realistic inlet velocity triangles and wake passing frequencies (cf. Hilgenfeld and Pfitzner (2004)).

The periodic effects observed around midspan also occur in the flow field close to the end-walls. Therefore, more recently e.g. Murawski and Vafai (2000), Volino et al. (2013) and Ciorciari et al. (2015) expanded their cascade investigations to secondary flow effects in turbine passages without tip clearance. In the case of compressor rotors the main secondary flow phenomenon affected by the incoming wakes is the tip leakage vortex (TLV). As shown by Mailach et al. (2008), incoming stator wakes trigger a cyclic segmentation of the TLV structure by interacting with its outer shear layer. Besides continuative experimental studies (e.g. Smith et al. (2015)) it is the rising number of numerical investigations using higher order models such as large eddy simulations (e.g. Hah et al. (2015), Riéra et al. (2016)) that demonstrate the demand for an improved physical understanding of the TLV itself and the wake interaction process.

The present study intends to enable a more detailed examination of effects resulting from varying stage kinematics using a rotor tip section equivalent linear cascade. A wake generator was designed to permit a stable operation at rather high translatory speeds and thus allow for accurate loss measurements. Following the argumentation of Cumpsty (2004) on elementary differences of the inlet boundary layer profile in cascades, challenges of generating an appropriate inlet disturbance will be discussed. Hence, the paper may contribute to an improved design of similar experimental configurations.

EXPERIMENTAL SETUP

All experiments were carried out at the open-circuit cascade wind tunnel located at the Centre for Energy Technology of the Technische Universität Dresden. Figure 1 depicts the cross-sectional view of the components used for flow conditioning purposes including essential geometric parameters. In order to enhance the tunnel’s operational spectrum to investigations into effects of periodically unsteady inflow conditions, a translatory wake generator has been implemented. With its supporting frame being fixed to the structure of the test section’s rotary head, the module is applicable over the full range of geometric cascade angles $\gamma$. Based on their favourably unidirectional behaviour and the findings of Pfeil and Eifler (1976) the module is equipped with circular cylinders. The bars are made from carbon fibre and attached to the back of a pair of synchronous belts using specific, coaxial weld-on profiles. An 18 kW AC motor drives the rig and permits translatory bar velocities up to $u = 31.5 \text{ m/s}$. Either a Hall effect sensor applied to the belt or an infrared photoelectric sensor provide the reference trigger signal for time-resolved measurements. Thus, the phase-locked ensemble average of either each individual bar wake or the total number of recorded wakes may be calculated.
The investigated compressor cascade is derived from the build 2 of the Dresden 4-stage LSRC (cf. Boos et al. (1998)) and represents the tip section of a typical high-pressure compressor rotor. It consists of seven cantilevered aluminium blades and provides a variable tip clearance control mechanism. The table in Fig. 2 summarizes the main geometric properties of the blading. In addition, definitions of the main geometric and kinematic parameters are given in Fig. 2(b). All probe traverses were conducted in two y-z-planes up- and downstream of the blade row (MP1: $-0.45 \ C_x$, MP2: $1.27 \ C_x$), whereat the position of the latter is chosen to coincide with a measurement plane in the LSRC. The additional measurement plane MP1X ($-0.08 \ C_x$) is only accessible at a defined pitchwise location of $y/t = 0.48$. It has been applied for distinct single normal hot-wire traverses to evaluate the bar wake characteristics in close vicinity to the cascade inlet. The leakage flow through the continuous sidewall gap at $-0.72 \ C_x$, which is required for the bar motion, was minimized by applying brush seals to both facing sides of the gap. A bar pitch ratio of $t_{bar}/t = 0.76$ was selected to be consonant with the build 2 stage geometry. Based on the findings of Krug et al. (2015) on the impact of a stationary bar cascade using a bar diameter of $d_{bar} = 2 \ mm$ this geometry was tested for the unsteady configurations, too. As will be shown in the discussion of the flow field data, this diameter was doubled to a value of $d_{bar} = 4 \ mm$ in the course of this study. Detailed information on the purpose of the specific setup shown in Fig. 2(a) are given in the aforementioned reference.

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Figure 2: (a) Schematic diagram of the test section, (b) cascade parameter definitions and tabular summary of important cascade properties and inlet boundary layer parameters
MEASUREMENT TECHNIQUES

Pneumatic Probes

Steady state flow field measurements were carried out using a cranked five-hole probe with a head diameter of 2 mm. The probe was traversed in the measurement planes MP1 and MP2. It accesses the flow channel through the hub sidewall with its longitudinal axis aligned with the cascade’s z-axis. All measurements were conducted over at least 1.5 pitches and from midspan up to a distance of 1.5 mm to the tip sidewall (1 ≥ z/z_{MS} ≥ 0.017). Total pressure losses were measured relative to a fixed Prandtl-probe 6.4 C ahead of the cascade. The maximum error regarding the non-dimensional total pressure loss coefficient, defined according to Eqn. (1), was estimated to \( \Delta \zeta = 0.0078 \). Flow angles were determined with an accuracy of \( \Delta \alpha = 0.2^\circ \). The calculation of pitch- and spanwise averaged values uses an algorithm based on Scholz (1956) and includes the three-dimensional conservation of momentum and mass continuity.

\[
\zeta(y, z) = \frac{p_0 - p(y, z)}{\frac{1}{2} \rho u_1^2_{MS}}
\]

Constant Temperature Anemometry (CTA)

Time-resolved flow field data was collected with a Dantec 3-channel StreamLine CTA system. A 55P11 type, single normal hot-wire has been traversed in the two inlet planes MP1 and MP1X. A 2C procedure was used to determine the axial and tangential velocity component by subsequently orienting the wire normal to the x- and y-axis. As the time series of both components have not been recorded simultaneously, the resulting flow vector may only be estimated in terms of a time or ensemble averaged quantity. Moreover, calculated values of the turbulent kinetic energy (cf. Eqn. (2), for two-dimensional flows) are subject to a certain overestimation caused by a weighted double balancing of turbulent fluctuations parallel to the wire.

\[
\langle tke \rangle = \frac{1}{2} \left( \langle c_x'^2 \rangle + \langle c_y'^2 \rangle \right)
\]

The evaluation of the two data sets relies on the extended cosine law proposed by Hinze (1959) and given in Eqn. (3). The yaw coefficient \( k \) was determined by calibration for yaw angles of \( \alpha_{yaw} = \pm [20^\circ, 70^\circ] \) (flow vector normal to the wire at \( \alpha_{yaw} = 0^\circ \)) and velocities in the range of 15 m/s ≤ c ≤ 35 m/s. From investigations of e.g. Jørgensen (1971) it is known that \( k \) alters significantly with the yaw angle. Consequently, more precise results may be yielded by taking this dependence into account instead of relying on the assumption of an average value (cf. Jørgensen (2002)). As \( k \) was found to marginally vary with velocity two generalized functions \( k_{I,II} = f(\alpha_{yaw}) \) were derived using a second order polynomial fit over all calibrated data points for each the I (\( \alpha_{yaw} > 0^\circ \)) and II (\( \alpha_{yaw} < 0^\circ \)) Cartesian quadrant.

\[
c_{eff} = |c| \cdot \left( \cos^2(\alpha_{yaw}) + k^2(\alpha_{yaw}) \cdot \sin^2(\alpha_{yaw}) \right)^{0.5}
\]

Finally, the mean flow vector is determined iteratively by solving Eqn. (3) for both directional components \( c_{x,eff} = f(k_{II}(\alpha_{yaw})) \) and \( c_{y,eff} = f(k_I(90^\circ - |\alpha_{yaw}|)) \). The extended \( Nu(Re) \) correlation of Collis and Williams (1959) is applied to compensate for disturbing effects of varying fluid temperatures. All fluid properties are calculated using the film temperature.

A Dantec 55R75 split-fibre probe (SFP) was used for extensive flow field traverses in MP1 and MP2. The implemented calibration procedure is based on a fully two-dimensional \( E_{1,2} = f(c, \alpha_{yaw}) \) “look-up table” scheme for \( |\alpha_{yaw}| \leq 45^\circ \) and 3 m/s ≤ c ≤ 42 m/s. Following the above-mentioned Nusselt number approach two polynomial regression surfaces were
determined by a least square fit algorithm including mixed terms \( \text{Re}(Nu(E_{1,2})) = P^3 \) and \( \alpha_{yaw}(Nu(E_{1,2})) = P^6 \), cf. Richter (1985). Relying on a typical base of at least 250 sample points both models give a coefficient of determination of \( R^2 > 0.999 \) with a reasonably high sample point-coefficient-ratio of \( SCR_{Re} \approx 25 \) and \( SCR_{\alpha} \approx 9 \). The rather narrow angular evaluation rate of \( \Delta \alpha_{yaw} = \pm 45^\circ \) was chosen to ensure a maximum regression error of \( \Delta \alpha_{max} < 0.5^\circ \) (\( \sigma_\alpha \approx 0.2^\circ, \sigma_c \approx 0.2 \text{ m/s} \)). Moreover, both explicit formulations allow for a very fast evaluation. As with any polynomial model, the implemented evaluation algorithm assures a consequent elimination of any ensembles including a single outlier.

To prevent disturbances arising from the static cross-talk described by Ho (1982), both sensors were operated with identical over-temperatures. However, the increased heat capacity of the fibre body does affect the probe’s frequency response. In comparison with the SN and an x-wire probe (55P62) the SFP underestimated the turbulence intensity within the inlet boundary layer (MP1, not shown) by 21\% to 26\%, whereas the mean velocity profiles were identical for all probes. A result being in good agreement with the findings of Hartmann and Dengel (1989).

Both CTA probes were operated with an overheat ratio of 1.8. The signal input was 10 kHz low-pass filtered and a sampling rate of 40 kHz was used for all measurements. The overall number of samples was chosen to guarantee a total number of 2500 bar wakes to be recorded.

RESULTS

Definition of Operating Point

To allow for studies of the periodically disturbed TLV seen by Mailach et al. (2008) in the LSRC, the linear cascade setup was chosen to match the geometry and particularly reproduce the kinematics found in the rotating rig. Figure 3(a) shows the pitch averaged inlet conditions to the rotor blade row of the LSRC and the values adjusted in the wind tunnel. The declaration of the inlet angles is consonant with the nomenclature used in the linear cascade (see Fig. 2(b)). It is noted that the LSRC related data is plotted over the normalized spanwise coordinate \( r^* = 2 (r_{casing} - r) / (r_{casing} - r_{hub}) \), to obtain a data representation equivalent to the definition of \( z/z_{MS} \). Since the given cascade values are derived from the undisturbed inlet conditions they describe the bar’s inflow. As will be shown, the cylinder wakes do not substantially affect the pitch-averaged values in MP1 so that the general conclusions drawn remain unaffected by this slight inaccuracy. Despite great care was taken to reproduce adequate inflow conditions, deviations in the sidewall flow inevitably arise from the linear cascade’s collateral inflow showing an almost uniform \( \alpha_1 \) distribution. In contrast to that, the LSRC rotor tip region faces a severely skewed flow vector due to its motion relative to the casing boundary layer and the upstream stator’s secondary flow. However, constructive restrictions resulting from the...
wake generator module precluded a proper integration of swirl generators or blowing devices (cf. Williams et al. (2009)) to achieve an adequately conditioned sidewall boundary layer in the test section. As the peak tip incidences would not allow for a stable operation of the cascade (cf. Krug et al. (2015)) free stream values were finally adjusted to represent the conditions in the range of \(1 \geq r^* \geq 0.5\). Yet, a decent global agreement regarding the trend of the flow coefficient is achieved. With the relative frame of motion being swapped in the linear cascade, the bar is subject to an intensely skewed sidewall boundary layer profile (see \(\beta_1\) in Fig. 3(a)).

The inlet velocity triangle referring to the compressor representative operating point with \(\varphi = 0.489\) is given in Fig. 3(b). To account for effects caused by disregarding the real stage kinematic a second operating point was investigated using only half of the design bar speed \((\varphi = 0.977)\). The two main drawbacks of such a non-representative experiment are: i) a misalignment of the bar wake from the correct orientation being almost normal to the blade chord to a rather tangential one and ii) changes in the appearance of the wake in the blade’s reference frame. To illustrate this behaviour, a simplified bar wake, described by a distinct velocity defect of \(0.2 \, w_1\) is considered for both flow coefficients in Fig. 3(b), dashed vectors, too. With \(\varphi = 0.489\) this wake will mainly be registered by the blade – as well as any probe used – as a local incidence increase, whereas a coupled appearance of a velocity defect and a comparably lower incidence variation is generated with \(\varphi = 0.977\).

**Inlet Flow Characteristics**

Steady effects of the unsteady inflow are characterized using the pitch averaged five-hole probe data for a medium tip clearance of \(s/C = 0.03\) (cf. Fig. 4). Though slight variations of the inlet angle occur for different gap sizes, shown trends apply to all cases. It is furthermore noted, that the MP1 is positioned \(30.3(15.2) \, d_{\text{bar}}\) downstream of the moving bars and thus lies well within the intermediate range of the evolving cylinder wake (cf. Pfeil and Eifler (1975), \(x/d_{\text{bar}} \leq 100\)). Due to the lacking availability of an additional probe access closer to the cascade inlet no data could be determined describing the specific subsequent wake mixing loss trend for the staggered bar row. However, the data of the aforementioned authors on individual cylinders suggest the resulting errors regarding the inlet loss coefficient to be minor and thus no correction was applied.

Following the preceding statements on the cylinder wake’s appearance to the probe a reasonable elevation of the free stream loss can only be recognized for \(\varphi = 0.977\), whereupon the disturbance grows with the bar diameter but progressively weakens through the sidewall boundary layer. Flow angle variations remain generally small. The local peak for \(d_{\text{bar}} = 4 \, \text{mm}\) at \(0.167 \, z_{\text{MS}}\) – labelled (i) – relates to a stable region, connecting two ”sections” of the developing wake, which may be divided into a free stream/outer and an inner boundary layer structure. This interpretation is confirmed by the identically labelled region within the upper left plot in Fig. 7, showing the ensemble averaged incidence over one

![Figure 4: Inlet boundary layer profiles (MP1)](image-url)
Two further features of the incidence contour within the cylinder wake are revealed in this subfigure: firstly, a wavy pattern around midspan, and secondly, a local incidence peak close to the sidewall, labelled (ii). Particularly the first observation was not expected, as it represents the average over 2500 bar wakes. It is also identifiable for $\varphi = 0.489$, though less intense and in the velocity magnitude contour (not shown) only. While no traces of that wavy pattern were identified for both operating points with the $d_{\text{bar}} = 2 \text{ mm}$ bar, the near wall spot-like incidence peak (ii) appeared for $\varphi = 0.977$, too. Whereas hardly any influence on the time averaged inlet conditions could be noticed with the thin bar for $\varphi = 0.489$, implementing the 4 mm bar enhanced the steady incidence by approx. $+0.4^\circ$ (cf. Fig. 4). The time-resolved incidence signature (cf. Fig. 7, upper right subfigure) also denotes a considerable delay of the wake centre across the boundary layer.

**Cascade Flow Characteristics**

The evaluation of the cascade’s loss production in the presence of the periodic inlet disturbance relies on the steady five-hole probe data, summarized in Fig. 5. In order to account for the $\varphi$-variant inlet total pressure footprints of the bar wake, the value of $\Delta \zeta_{2-1}$ will be used for both pitch and globally averaged losses. While its calculation regarding the latter is trivial, a more sophisticated method is used for the pitch averaged data relying on a balancing of the normalized mass flow distribution for $0.0167 \leq z/z_{\text{MS}} \leq 1$. Thus, the trends derived from that procedure do not represent the real physical loss distribution. Nevertheless, it is the authors’ opinion, that the consideration of the actual inlet loss is essential for a sensible evaluation.

Looking at the pitch averaged data obtained for $s/C = 0.03$ with the $d_{\text{bar}} = 2 \text{ mm}$ bar in Fig. 5(a) we found the trends of both unsteady cases to be practically identical with those of the undisturbed inflow. The same behaviour was observed for $s/C = 0.01$, too. In addition to that, no evidences of a periodic, wake induced excitation of the TLV were detectable in the ensemble averaged SFP flow field data (not shown). Consequently, an intensification of the inlet disturbance by doubling the bar diameter was considered to reduce the relative flow path $x/d_{\text{bar}}$ and thus increase the wake depth. By doing so, the steady cascade flow field was noticeably altered. The profile loss was increased for both wake impingement scenarios. Compared with the undisturbed reference case the TLV loss core, which is equivalent to the $\Delta \zeta_{2-1}$ peak location, is slightly shifted towards the sidewall for $\varphi = 0.977$. In contrast to that, the wake induced incidence increase mainly contributes to an augmented TLV loss production for $\varphi = 0.489$. The global data shown in Fig. 5(b) confirm the latter to produce the highest overall losses for

![Figure 5: Total pressure loss and flow angle distribution in MP2 in terms of (a) pitch averaged data for $s/C = 0.03$ and (b) globally averaged data with $d_{\text{bar}} = 4 \text{ mm}$](image-url)
all investigated gap sizes and the former to yield losses similar to the undisturbed reference case. Concerning the secondary loss production, calculated by subtracting the profile loss, the \( \varphi = 0.977 \) case appears to be capable of reducing them. An effect, that is partly related to the strongest degradation of the profile loss. Referring to the pitch averaged outlet flow angle, variations are rather small with maximum differences in the order of \( \approx 1^\circ \) in the lower branch of the TLV for \( 0.07 \leq z_{MS} \leq 0.22 \). As a result of that, quite similar global outlet angles are calculated. Differences exceeding the measurement accuracy are only detected for \( \varphi = 0.489 \), supporting the previous discussion on the secondary loss reduction. Static sidewall pressure readings (not shown) support the findings of the steady flow data. The \( \varphi = 0.489 \) cases confirmed a slight intensification of the TLV in terms of the suction peak and thus its trajectory being slightly shifted towards the leading edge. However, for \( \varphi = 0.977 \) only a marginal elevation of the local pressure magnitudes is indicated while the flow topology remains unchanged.

The evaluation of the unsteady flow data gives insight to an unexpected phenomenology regarding the time-resolved flow structure. Figure 6 illustrates the corresponding ensemble and pitch averaged flow angle \( \Delta \langle \alpha_2 \rangle_{undist} \) \( \varphi = 0.489 \) and \( \varphi = 0.977 \). Opposed to the clearly periodic intensity of the leakage flow for \( \varphi = 0.977 \), reaching maximum deviations from the undisturbed case in the order of \( \pm 1.7^\circ \), the TLV lacks any signs of a wake induced excitation for \( \varphi = 0.489 \). Thus, the rise in loss production may not be explained by the time-resolved impact of the incoming wakes, but rather a quasi-steady loss augmentation. A common characteristic for both cases is the global \( \langle k e \rangle \) increase in the flow field beyond the TLV \( (\varphi > 0.5 z_{MS}) \). Peak values occur for the passing interaction zone of the bar wake and the profile’s suction side.

**DISCUSSION**

In order to answer the question of why no periodic disturbance of the leakage flow can be observed for the compressor representative operating point of \( \varphi = 0.489 \) a detailed evaluation of the particular inlet conditions is inevitable. Since the kinematics of the two studied flow coefficients are very different, additional measurements were conducted for five \( \varphi \)-values between 0.489 and 0.977 to get insight into the mechanics involved. Figure 7 summarizes the corresponding data, obtained by wall-normal traverses at selected pitchwise positions of \( (y/t)_1 = -0.94 \), \( (y/t)_{1X} = 0.48 \) and \( (y/t)_2 = 2.28 \). The MP1/1X midspan wake position of all configurations is synchronized to \( T/T_{bar} = 0.22 \) for reasons of comparability. Consequently, one may notice the marginal whitespace between the first \( (T/T_{bar} = 0) \) and the last \( (T/T_{bar} \approx 0.99) \) point of the time series to be shifted along the abscissa for each \( \varphi \)-configuration. Fundamental properties of the respective inlet characteristics are illustrated in Fig. 9.

The first point to be addressed is the wavy structure found in the ensemble averaged velocity vector around midspan, indicating a coherent vortical structure. In order to generate a phase-locked registration of the shed vortices a distinct trigger mechanism is needed. A similar wake appearance was monitored by Holland and Evans (1996) and Wysocki et al. (1996), who used
Figure 7: Effect of different wake kinematics on bar wake development and periodical excitation of the leakage vortex for $s/C = 0.05$ and $d_{bar} = 4\,\text{mm}$

translatory wake generators in flat plate and turbine cascade configurations, too. With the bars moving $3.1 \, d_{bar}$ and $5.9 \, d_{bar}$ upstream the tested geometry the former authors suggested the potential field of the flat plate to be the source. However, no effect on the ordered vortex shedding could be achieved by a one-off removal of the upper headboard (see Fig. 2(a), $\Delta x = 2.8 \, d_{bar}$) in our study. Interestingly, Bogusławski and Elsner (1994), utilizing a spoked wheel rotating through a free jet, found the bar wake signal to be dependent on the probe’s circular position relative to the wheels rotational axis in absence of any downstream body. In the context of our observations one may constitute the bar’s passing of a sheared velocity profile when entering the test section through the upper endwall’s boundary layer or the free jet’s turbulent outer layers to be a common property. From investigations as the one of Cao et al. (2010) it is known that linear shear rates induce a strong asymmetry in the vorticity shed from both sides of the cylinder. With the bars moving across a wall-bounded or – in the case of Bogusławski’s free jet – free shear layer of a certain thickness, shear rate and turbulence intensity it seems reasonable that the ordered shedding is first triggered at the higher velocity side of the circular cylinder (cf. Fig. 8). Yet, a sensible discussion on whether missing reporting on phase-locking in various other studies originate from the specific shear layer constitution or e.g. the utilized probe configuration remains unfeasible due to the absence of corresponding boundary layer data.
The phase-locked character of the $d_{bar} = 4\text{ mm}$ data enabled a complementary investigation of the spanwise wake structure by a detailed field traverse in MP1. Through this, a chevron-shaped oblique vortex shedding pattern was verified for $\varphi = 0.977$ ($Re_{bar} = 4800$). It occurs in the free stream beyond a wall distance of about $1.25 \delta (= 0.54 z_{MS})$ and is symmetric about midspan. These structures are usually discussed in the range of laminar vortex shedding ($47 \leq Re_{bar} \leq 180$, e.g. Williamson (1989)) and known to be an end effect of the finite cylinder. In contrast to that, the shedding pattern for $\varphi = 0.489$ was found to be rather parallel. The shedding frequencies of both cases were determined to be in good agreement with the correlation given in Fey et al. (1998). Thus, the vortex street density $f_{vortex}/f_{bar}$ decreases from 5.02 to 2.05 with increasing bar speed from $\varphi = 0.977$ to $\varphi = 0.489$. Measurements in the wake of a stationary bar also revealed the dominant shedding frequency to become blurred at the above-mentioned height and to fade away at $0.7 \delta (= 0.3 z_{MS})$ in any case. Hence, the less regular shedding constitutes the expected mean wake pattern for $z \leq 1.25 \delta$, that is apparent for all $\varphi$-cases in the MP1 data in Fig 7. The data measured in MP1X proves the vortices to exist up to the cascade inlet (cf. Fig. 7, second row). Thus, the blade is expected to be rather affected by means of a discrete structure with an operating point dependent phase and rotating direction than by a mean wake profile outside the sidewall boundary layer.

The two near wall features of the wake found for $\varphi = 0.977$ (a local incidence (i) minimum at $0.167 z_{MS}$ and (ii) peak close to sidewall, see upper left plot in Fig. 7) exist consistently up to an operating point of $\varphi = 0.698$. For lower flow coefficients, the boundary layer related wake profile exhibits a continuous shape. The reason for this may be found in the skewed inlet angle profile shown in Fig. 9(c). When entering the boundary layer largest $\beta_1$-gradients appear for $\varphi = 0.489$. They remain fairly constant over most of the boundary layer, causing the temporal shift of the wake and the impingement of this disturbance on the blade, respectively. Referring to the MP1X data in Fig. 7, this delay already amounts to $\approx 0.8t_{bar}$ between midspan and the last measuring point above the sidewall. Thus, the blade is rather subject to a continuous than an instant periodical disturbance along its height. In contrast to that, only a moderate gradient emerges over main portions of the boundary layer for $\varphi = 0.977$, but as $\beta_1$ ultimately has to become zero at the sidewall large shear angles appear for $z/z_{MS} \leq 0.02$. Noticing the $\beta_1$ distributions to resemble this character down to at least $\varphi = 0.782$, it supposedly causes the aforementioned near wall incidence peak. Comparing the MP1X data (cf. Fig. 7, second row) one may also recognize the position of the highest incidence within the wake (iii) – and thus the maximum load enhancement – to progressively drift towards the edge of the boundary layer as the flow coefficient increases.

Taking into account the ensemble averaged flow angle measured in MP2 (cf. Fig. 7, lowermost row) an almost exponentially decaying periodic feedback of the leakage flow is found. For $\varphi \leq 0.611$ no variation of either $\langle \alpha_2 \rangle$ or $\langle tke \rangle$ is observable any more. The contour plots for both upstream measurement planes and the derived time averaged $\langle tke \rangle$-dissipation trends (not shown) indicate a similar wake mixing for all $\varphi$-cases. Evaluating the normalized relative velocity distributions in Fig. 9(c), with $\Delta w_{1,MS} = w_1 - w_{1,MS}$, the characteristic tends to become more and more rectangular for $\varphi < 0.698$. As the velocity gradient for the higher $\varphi$-values resemble each other very well one may not identify this feature to be the fundamental factor.

![Figure 8: Sketch of the endwall boundary layer velocity profile triggering the ordered vortex shedding](image-url)
Figure 9: Effects of a ϕ-variation on the midspan inlet (a) velocity triangles, (b) normalized velocity magnitude and (c) inlet boundary layer profiles in the bar reference frame causing the TLV’s rapidly fading periodicity. However, the inflow velocity magnitude changes with the flow coefficient as shown in Fig. 9(b). The absolute free stream wake intensity may thus be described as a parabola, with its apex at ϕ ≈ 0.556 (β₁ = 90°). With the spanwise velocity profiles for ϕ ≥ 0.698 being similar, it follows that the mean wake depth and turbulence level globally decrease with ϕ. At the same time one may also find w₁,ϕ=0.489 > w₁,ϕ=0.977 to be valid for < 0.14 z_MS. Yet, the maximum difference of w₁ only amounts to 0.13 c₁ for all studied cases, giving no clear evidence of its relevance to the occurrence of a periodic TLV excitation.

CONCLUSIONS

Measurements of the tip leakage vortex in a low-speed linear compressor cascade that is subject to a periodic inlet disturbance were presented in this study. A translatory wake generator using circular cylinders was designed to correctly represent the stage kinematics with a flow coefficient of ϕ = 0.489. With the bar traversing speed being the limiting factor for this kind of test rigs, a second operating point at only half the design speed was chosen to allow for the evaluation of misinterpretations arising from disregarding the impact of this parameter. First studies with a bar diameter of d_bar = 2 mm, which was already used to assess the influence of a stationary bar row prior to this investigation, indicated neither a steady nor an unsteady impact. Consequently, a doubling of the diameter was considered to intensify the disturbance by proportionally reducing the relative flow path x/d_bar and thus compensating for the lower relative inlet velocity (w₁ < c₁). Although steady effects remained still small for both ϕ-configurations, the realistic operating point was found to elevate the loss production, whereas the secondary losses were reduced for all investigated gap sizes with ϕ = 0.977. Time-resolved flow measurements revealed that only the non-representative case with ϕ = 0.977 caused a periodic excitation of the TLV. Enhanced tests with numerous intermediate ϕ-values indicated the interaction between the incoming wakes and the natural sidewall boundary layer to be the origin. Besides differences in the wake intensity and the wall-normal location of the maximum wake induced incidence it is the delayed impingement of the wake throughout the boundary layer that is proposed to cause the absence of any periodicity for the compressor equivalent operating point. Hence, the authors intend to validate these findings using a modified setup with a significantly reduced sidewall boundary layer thickness in a subsequent study.

Moreover, the effect of a phase-locked vortex shedding, which is assumed to be triggered by the upper endwall’s shear layer, could be identified and used to validate an oblique vortex shedding mode for Re_bar = 4800.
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