Comparison of the Sound Power Levels of an Aerodynamically Designed EGV and a State-of-the-Art EGV

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Abstract: Within previous EU projects, possible modifications to the engine components have been investigated, that would allow for an optimised aerodynamic or acoustic design of the EGV (exit guide vanes) of the TEC (turbine exit casing). However, the engine weight should not be increased and the aerodynamic performance must be at least the same. This paper compares the sound power level of a state-of-the-art TEC (reference TEC) with typical EGVs with an aerodynamically optimised TEC configuration for the engine operating point approach. It is shown that a significant weight reduction (only bladings considered) and reduction in engine length can be achieved but the sound power level for the fundamental tone (1st blade passing frequency) for this acoustically important operating point is increased. It is also shown that the losses of the aerodynamical optimised EGVs are higher for this off design point but significantly lower at the aero design point. Measurements were conducted in the STTF (subsonic test turbine facility) at the Institute for Thermal Turbo machinery and Machine Dynamics, Graz University of Technology. The inlet guide vanes, the LPT (low pressure turbine) stage, and the EGVs have been designed by MTU Aero Engines.

Key words: Low pressure turbine, exit guide vane, turbine exit casing, acoustic, sound power level, mode analysis.

Nomenclature

Abbreviations

BPF	Blade passing frequency
CS	Corner separation
EGV	Exit guide vanes
HSL	High-stage-loading
H-TEC	High lift design TEC
IGV	Inlet guide vanes
LE	Leading edge
LPT	Low pressure turbine
LPV	Lower passage vortex
L-SST	Low shear stress
MTF	Mid turbine frame
PS	Pressure side
SS	Suction side

S1, S2	Separation lines
STTF	Subsonic test turbine facility
PWL	Sound power level
TE	Trailing edge
TEC	Turbine exit casing
TSV	Trailing edge shed vortex
UPV	Upper passage vortex

Symbols

α	deg	Yaw angle
Δ		Difference
ζ		Total pressure loss coefficient
σ_{mn}		Eigenvalue of besseI diff. Equ.
φ	rad	Circumferential coordinate
ω	rad/s	Angular frequency
A		Complex amplitudes
B		Number of blades
C_p		Static pressure coefficient
c	m/s	Speed of sound
f_c	Hz	Cut-off frequency
f		Modal shape factor

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i		$\sqrt{(-1)}$
k		Integer number; $k = -1, 0, 1, \dots$
k		Axial wave number
Ma		Mach number
m		Circumferential mode order
m_s		Scattered circumf. mode
n		Radial mode order
p	bar	Pressure
R	m	Outer TEC radius
SPL	dB	Sound power level
r	m	Local radius
t	sec	Time
V		Number of vanes
x	m	Axial coordinate

Subscripts

0, C, D, D0	Measurement plane 0, C, D, D0
c	Cut-off
ex	Exhaust casing
i	Local position
m	Mean value
m, n	Circumf., radial mode order
p	Pressure
ref	Reference
s	Scattered
t	Total value

Superscripts

$\bar{\cdot}$	Area averaged
$\tilde{\cdot}$	Mass averaged
$\cdot_{\pm}$	Propagation in (+) and against (–) flow direction

1. Introduction

For state-of-the-art design of EGVs (exit guide vanes) structural requirements are more important than the aerodynamical and/or the acoustical point of view. Today the EGVs have to support the rear bearing and the engine mount and therefore they are relatively thick and rigid. However, the EGVs also have to fulfill aerodynamic aspects. The main aerodynamic purpose is to reduce swirl in the flow. The goal is to generate homogeneous LPT (low pressure turbine) exit flow by transforming swirl into thrust for highest propulsive efficiencies [1]. If structural requirements are of minor interest, for

example if the rear bearing and the engine mount is moving forward near the MTF (mid turbine frame) supported by the MTF struts, an optimisation of the EGVs may lead to a highly efficient low Reynolds number EGV [2]. However, the EGVs downstream of a last stage LPT have to have a large operating range in order to provide a near axial outflow for all operating points of the turbine. Therefore its operating range is significantly larger than that of a compressor vane.

The number of investigations on modern low Reynolds number turbine exit guide vanes is limited e.g. Sonoda et al. [3], Sonoda and Schreiber [4] and Hjärne [5]. Koch et al. [1] investigated a cross section at 33% blade height of the acoustically optimised EGVs used in this investigation in a linear cascade as a baseline design. They experimentally and numerically showed that an aerodynamically optimised EGV meets all requirements for an EGV. The turning was similar to the baseline design and for the entire operating range the outlet flow angle was nearly axial. Further, Koch et al. [1] achieved a significant loss reduction for the optimised airfoil and a slightly higher pressure rise due to lower losses at reduced wetted surface. Marn et al. [6] showed that a reduction of 5 dB in sound power level is possible by applying an inverse cut-off design, however the losses increased significantly for the off design point approach. Broszat et al. [7] verified this effect numerically and compared it with experimental results.

In this study two configurations are compared to each other at a typical off design point (approach), relevant for acoustics. The design intent was to achieve lowest losses and to show the change in sound power level for the aerodynamically optimised TEC (turbine exit casing) in comparison to a state-of-the-art TEC.

Changes in pressure loss and sound power levels are shown as well as a rough estimation of the weight of the bladings is given.

2. Experimental Facility and Instrumentation

2.1 Test Facility

The Institute for Thermal Turbomachinery and Machine Dynamics at Graz University of Technology operates a 3 MW compressor station in order to supply a couple of test facilities continuously with pressurized air. In the described subsonic turbine test facility the maximum pressure ratio is limited to 2 due to the inlet spiral casing. The maximum mass flow rate is 15 kg/s at a temperature at stage inlet of 100 °C. This inlet temperature can be adjusted by coolers within a wide range. The pressurized air enters the facility through a spiral inlet casing where the flow turns into axial direction. Within this spiral inlet casing the front bearing of the overhung-type turbine shaft is mounted. The shaft is coupled to a water brake, whose cooling water cycle is connected to the re-cooling plant of the institute.

In order to provide well defined and uniform inflow

conditions a de-swirler together with a perforated plate is located upstream of the stage inlet. Further, upstream of the stage (and downstream of the perforated plate) inlet guide vanes can be found that should simulate additional wakes of other upstream low pressure turbine stages. The air leaves the rig through an acoustic measurement section, supporting struts, exhaust casing, and the exhaust stack to ambient.

A detailed description of the STTF (subsonic test turbine facility) is given in Ref. [8].

2.2 Turbine Stage and EGVs

The aerodynamic design of the low pressure turbine stage, IGVs as well as the EGVs was performed by MTU Aero Engines. Considerable effort was put into the adjustment of relevant model parameters to reproduce the full scale LPT configuration. The turbine diameter is approximately half of that of a commercial aero engine LPT and therefore the rig is operated at higher rotational speeds. A meridional

Fig. 1 Meridional section of the STTF: (a) Reference TEC; (b) H-TEC.

Table 1 Geometry details and operating conditions.

Geometry details	
Number of blades/vanes	
IGV/Stator/Rotor/	83/96/72
EGV state-of-the-art (reference TEC)	15
EGV H-TEC	< 35
Tip gap to blade height ratio	1.0%
Hub to tip radius ratio	$\approx 2/3$
Operating conditions	
EGV Reynolds number ratio Re_{ref}/Re_{H-TEC}	~ 3
Diffusion factor $ref./H-TEC$	$\sim 0.5/\sim 0.6$
Reduced mass flow	6.94 kg/s
Reduced rotational speed	3,653 rpm
Stage pressure ratio	1.16
Stage total inlet temperature	100 °C

section of the rig is shown in Fig. 1. The state-of-the-art (reference) TEC is shown in the sketch at the top (a) and the high lift design (H-TEC) can be seen at the bottom (b). The bladings are not drawn to scale. The rig is characterised by a high aspect ratio unshrouded rotor followed by the EGVs of the TEC. Relevant geometry parameters can be seen in the upper half of Table 1.

For this investigation two different set-ups have been tested. A state-of-the-art TEC that is referred to as the reference TEC and an aerodynamically optimised TEC with a higher number of vanes and therefore shorter chord length. The leading edge is at the same axial position for both configurations. One significant difference is that the reference TEC is manufactured without fillets while the H-TEC has fillets at hub and tip due to manufacturing and assembly requirements.

2.3 Operating Conditions

Based on the intended use of the LPT rig for both acoustic and aerodynamic investigations, the main operating points are selected according to the typical noise certification points. They have been defined using an aero design point of the last stage LPT, derived from current LPT design practice using scaling along reduced speed, reduced mass flow (both referred to 288.15 K and 1,013.25 mbar) and pressure

ratio. For this investigation the operating point approach was chosen. The Reynolds number of the EGVs is defined using the midspan conditions at rotor exit as well as the axial chord of the vanes. The lower half of Table 1 shows the operating conditions.

2.4 Instrumentation

Five-hole-probes (from Institute of Jet Propulsion and Turbomachinery, RWTH Aachen University) with a probe head of 2.5 mm diameter were applied in measurement plane C and D (see Fig. 1b). The probes are calibrated for Mach numbers between 0.1 and 0.8, yaw angles between -20 deg and $+20$ deg, pitch angles between -16 deg and $+20$ deg. Negative values of the yaw angle indicate a counter-rotating flow and negative values of the pitch angle indicate the flow direction towards the hub.

The correlation between the calibration characteristics and the value to be measured is given by a multi-parameter approximation.

The axial positions of measurement planes can be seen in Fig. 1. Plane C is located downstream of the rotor TE in a distance of 60% of the axial rotor blade chord length. Plane D0 can be found 55% of the axial chord length of the H-TEC EGV downstream of its TE and plane D is also located downstream of the TE (= 130% axial chord length of the reference TEC).

The measurement grid in each plane consists of 21 points in radial direction and 33 points (24 deg sector for reference TEC) and 19 points (for H-TEC) in circumferential direction. The grid covers one EGV pitch and about 95% passage height. Traversing was done along radial lines. In each measurement point the probe was turned into the flow to reach the highest accuracy and to ensure to be always within the calibration range of the probe (with this probe it would not have been necessary, if one ensures to be always within the calibration range).

Static pressure taps are located at several axial positions along the suction and pressure surface at 20%, 30%, 50%, and 80% channel height.

In order to estimate the sound power propagating downstream 12 flush mounted condenser microphones (1/4") at the hub and 12 at the casing are located in the 360 deg rotatable acoustic measurement section. In addition to these microphones 1 microphone was mounted at a fixed position downstream of the EGV's TE as a reference. The complete sound field was detected at the hub and the casing by traversing the section 360 deg in steps of 2 deg. Some more detailed information about the acoustic measurement section is given in Ref. [9].

2.5 Measurement Uncertainty

Instrumentation has to meet not only high demands on accuracy and repeatability but requires higher numbers of channels, too. Therefore, the measurement system is made up by eleven multi channel pressure transducers PSI 9016 with a total amount of 176 channels and an accuracy of 0.05% full scale and four National Instruments Field Point FP-TC-120 eight-channel thermocouple input modules and one FP-RTD-122 resistance thermometer input module.

Table 2 shows the measurement uncertainties (within a 95% confidence interval) of the five-hole-probe measurements. These values are positive and negative deviations and contain the error due to the approximation, random error and the systematic error of the PSI modules. The difference between the positive and the negative direction is a result of the multi-parameter approximation.

The measurement uncertainties of the static pressure and the total pressure at rig and stage inlet are ± 1 mbar. Total pressure measurements up and downstream of the EGVs have an uncertainty of ± 1 mbar. An estimation of the overall uncertainty of the total pressure loss coefficient ζ gives a value of ± 0.0014 .

The variation of speed is below 0.2% of the current operating speed and the measurement uncertainty of the temperature measurement is about ± 0.5 K.

The variation of the operating parameters (pressure ratio, corrected speed, speed, total pressure and

Table 2 Measurement uncertainties of the 5HP.

Ma	+ 0.006	− 0.003	[/]
α	+ 0.5	− 0.08	[deg]
p_t	+ 3.3	− 3.0	[mbar]
p	+ 5.3	− 5.2	[mbar]

temperature at rig inlet) between different measurement days has been below 0.5%.

2.6 Acoustic Measurements and Mode Analysis

A decomposition of a variable in time and space was provided by Tyler and Sofrin [10]. The circumferential (azimuthal) mode orders excited by the relevant airfoil interactions can be calculated as follows:

$$m = n \cdot B + k \cdot V; k = -\infty, \dots, -1, 0, 1, \dots + \infty \quad (1)$$

With the vane and blade counts B and V , respectively, the harmonic order n , and the integer index k as well as an additional similar equation describing the scattering of acoustic modes when interacting with a following vane.

$$m_s = m + k \cdot V; k = -\infty, \dots, -1, 0, 1, \dots + \infty \quad (2)$$

In order to describe the sound generating mechanisms, Tyler and Sofrin wrote the pressure fluctuations at any circumferential positions downstream of the compressor stage as a sum of harmonics. The theoretical model they proposed has been verified by numerous aeroacoustic experimental investigations and it is also valid for turbines (e.g. Moser et al. [9]). The theory of the acoustic data analysis used in this work is well described in Refs. [11, 12]. In cylindrical coordinates and for a single frequency component ω the solution of the wave equation is given by a linear superposition of modal terms as can be seen in the equation below.

$$p(x, r, \varphi, t) = \sum_{m=-\infty}^{\infty} \sum_{n=0}^{\infty} \left(A_{mn}^{\pm} e^{ik_{mn}^{\pm} x} \right) f_{mn} e^{im\varphi} e^{-i\omega t} \quad (3)$$

For hard-walled acoustic boundary conditions the modal shape factor is reported in Ref. [13]. They also describe the calculation of complex mode amplitudes

$A_{mn}^{\pm}$ as an inverse problem of the equation above, which is also applied in this work.

2.7 Cut-Off Modes

If $k_{mn}^{\pm}$ in Eq. (3) is real the (m, n) mode propagates energy. The associated frequency is given by:

$$f_c = \frac{\sigma_{mn}}{2\pi R} c \sqrt{1 - Ma^2} \quad (4)$$

For a certain Mach number and frequency only a limited number of azimuthal modes can propagate. The highest mode order m at operating point approach is ± 17 . All other modes are predicted to be cut-off.

3. Results and Discussion

3.1 Sound Power Level

Figs. 2 and 3 show the sound power level per azimuthal mode order m of the two TEC configurations evaluated for the first BPF of the operating point approach. The blue bars indicate the sound power level of sound waves propagating in flow direction PWL^+ and the green bars the propagating waves against flow direction PWL^- . The dotted red line indicates the noise floor of the measurements. The microphones are mounted on a vibrating machine which causes some systematic error resulting in that noise floor.

The difference in overall sound power level of the first BPF tone $\Delta PWL = PWL - PWL_{ref}$ in and against flow direction as well as the sum of both is given in Table 3. It can be clearly seen that the reference TEC has slightly higher sound power levels. Taking a closer look to specific airfoil interactions shows a different trend as it is depicted in Table 3. Table 4 shows the azimuthal interaction mode orders (cut-on modes) and scattered modes calculated according to the equation of Tyler and Sofrin.

Higher modes than ± 17 are cut-off. The main interaction mode of the rotor with the EGV of the H-TEC ($m = -8$) is cut-on and clearly visible in Fig. 3. Fig. 4 shows the sound power level of the main interaction modes for the reference TEC in blue and

for the H-TEC in red. It can be seen that the mode -8 of the H-TEC has again the highest amplitude.

Table 5 and Fig. 4 show the change in sound power level for the main interaction modes given in Table 4. The blue bars represent the amplitudes for the reference TEC and the red bars that for the H-TEC.

Fig. 2 Sound power level (1 BPF) of the reference TEC (blue: PWL^+ , green: PWL^-).

Fig. 3 Sound power level (1 BPF) of H-TEC (blue: PWL^+ , green: PWL^-).

Table 3 Overall sound power level change for 1 BPF.

	$\sum PWL^+$	$\sum PWL^-$	$\sum PWL^{\pm}$
Reference TEC	0.0 dB	0.0 dB	0.0 dB
H-TEC	-0.6 dB	-8.6 dB	-1.0 dB

Table 4 Airfoil interaction modes m at 1 BPF.

Configuration	IGV-Rotor	Vane-Rotor	Rotor-TEC
Reference TEC	-4, +11	-6	-12, +3
H-TEC	+11	Cut-Off	-8

Table 5 Sound power level change for 1 BPF.

	$\sum PWL^+$	$\sum PWL^-$	$\sum PWL^\pm$
Reference TEC	0.0 dB	0.0 dB	0.0 dB
H-TEC	+ 2.4 dB	− 11.5 dB	+ 2.0 dB

Fig. 4 Sound power level (1BPF) of the main interaction modes (blue: Reference TEC; red: H-TEC).

Although the reduction for $\sum PWL^-$ is larger, it has no influence on the total reduction $\sum PWL^\pm$ due to the much lower amplitudes.

In addition to the acoustic measurements five-hole-probe measurements were taken in order to compare the aerodynamics of the H-TEC with that of the reference TEC. These results are presented in the following section.

3.2 EGV Inlet Flow-Plane C

The inlet condition shows only minor differences due to the potential effect of the EGVs. The mass averaged Mach number varies slightly (Fig. 6) between the setups. Also the mass averaged yaw angle is the same for both configurations as can be seen in Fig. 8. Figs. 5 and 7 show the circumferential distribution of the Mach number and the yaw angle, respectively. Both figures are viewed from downstream and are representative for both configurations.

Structures belonging to the rotor appear as circumferentially uniform bands that are modulated by the structures from the upstream vane row. In the Mach number plot the wakes of the upstream vanes can be clearly seen as well as the influence of the upper passage vortex together with the tip leakage vortex at 90% channel height. The lower passage

Fig. 5 Mach number distribution at the inlet to the EGV for the reference TEC measured at plane C. **Fig. 6** Mach number distribution at TEC inlet. **Fig. 7** Yaw angle distribution at the inlet to the EGV for the reference TEC measured at plane C.

Fig. 8 Yaw angle distribution at TEC inlet.

vortex can be seen at 10% channel height. Both effects can be clearly identified in the mass averaged results in Figs. 6 and 8.

3.3 EGV Outlet Flow-Plane D

Fig. 9 through Fig. 13 shows the Mach number and yaw angle distribution downstream of the EGV trailing edge at the same absolute position (measurement plane D) which corresponds to 130% axial chord length of the reference TEC. This plane represents the acoustic rig inlet position. Mach number and yaw angle are plotted with the same scale. However, the absolute value is not shown.

Fig. 9 Mach number distribution at the outlet of the EGV for the reference TEC measured at plane D.

Fig. 10 Mach distribution at the outlet of the EGV for the H-TEC measured at plane D.

Fig. 11 Mach number distribution at the outlet of the EGV for the H-TEC measured at plane D0 (closer to the vane TE).

Fig. 12 Yaw angle distribution at the outlet of the EGV for the reference TEC measured at plane D.

Fig. 13 Yaw angle distribution at the outlet of the EGV for the Inverse cut-off TEC measured at plane D.

Fig. 14 Yaw angle distribution at the outlet of the EGV for the H-TEC measured at plane D0 (closer to the vane TE).

An additional measurement plane D0 closer to the trailing edge of the EGV (55% axial chord length downstream of TE) was realised for the H-TEC. Figs. 11 and 14 show the Mach number and the yaw angle distribution, respectively. All plots are viewed from downstream.

For the reference TEC the wake (dashed line) is bowed in plane D. Further a region of low Mach number at the shroud can be seen at the suction side of the wake which is mainly due to a CS (corner separation) (see also Fig. 20) close to the trailing edge that rolls up into a vortex driven by the large UPV (upper passage vortex). The region marked with LPV represents the lower passage vortex which seems to be

quite large in plane D for the reference TEC. However the LPV seems to be smaller and less pronounced than the UPV.

In plane D0 (H-TEC) a thin wake but with a large velocity deficit can be seen. However, from plane D0 to D the wake is going to be bowed and mixed out. In D0 the formation of a trailing edge shed vortex due to the radial flow on suction and pressure surface can be seen. This radial migration of fluid can be observed in the oil flow visualisation (Figs. 18 and 21). In plane D this vortex can be nicely seen and is responsible for the bowing of the wake. The formation of this vortex is reported in Ref. [14].

The low momentum fluid at the hub and the casing (tends to be the upper passage vortex) is being mixed out and increases in size producing some blockage causing the flow to concentrate slightly below midspan (20-50% passage height), which can be seen in Fig. 15 as well as in Figs. 10 and 11. However, the upper passage vortex is much smaller for the H-TEC than for the reference TEC. Looking at the mass averaged results in plane D and D0 it can be seen that the Mach number (see Fig. 15) is higher for the H-TEC. Due to the fact, that the H-TEC is aerodynamically optimised but the area is not changed it was not accounted for the additional blockage a much higher vane count would produce. This possibly leads to the slightly higher Mach number. In the yaw angle plot (see Fig. 16) a much more uniform distribution is

Fig. 15 Mach number distribution TEC exit.

Fig. 16 Yaw angle distribution TEC exit.

evident for the H-TEC due to the smaller secondary vortices.

3.4 Oil Flow Visualisation

A mixture of synthetic motor oil and titanium oxide is used to cover the endwalls as well as the suction and pressure side. Attention was paid on reaching the operation point as fast as possible to avoid run up effects. The operating point was held for about 2 hours. The shut down procedure was again as fast as possible in order to preserve the flow structures made visible by the oil at the operating point. Figs. 17-21 show the oil flow visualisation on the suction and pressure side as well as on the hub and casing endwalls of both configurations.

A region of L-SST (low shear stress) on the suction side is visible for both configurations up to about 80% channel height. Above 80% this region cannot be seen anymore. Both configurations show radial inward flow on the suction side at the casing and a radial outward flow at the hub. This is due to the cross passage flow rolling up into the upper and lower passage vortex. These vortices seem to be stronger for the reference TEC. At about 30% channel height the flow is nearly axial on the suction side. For the H-TEC on the pressure side a strong radial outward flow above 30% channel height can also be seen in Fig. 18. Again at 30% channel height the flow is nearly axial.

Another region of low shear stress (oil accumulation) can be seen on the pressure side of the H-TEC.

At the endwalls the cross passage flow is clearly visible for both configurations. The oil flow visualisation of the reference TEC at the hub and casing (Figs. 19 and 22) clearly show two separation lines, S_1 and S_2 as reported by Sieverding [15]. Sieverding stated that S_2 corresponds to the lift off line of the horse shoe vortex, while the separation line S_1 is due to the boundary layer separation ahead of the horse shoe vortex. Sieverding also mentioned that S_2 is easier to detect than S_1 . Between these two lines a region of low momentum fluid exists. For the H-TEC only one separation line could be clearly identified.

Fig. 17 Pressure side reference TEC.

Fig. 18 Pressure side H-TEC.

Fig. 19 Hub endwall reference TEC.

Fig. 22 Casing end wall reference TEC.

Fig. 20 Suction side reference TEC.

Fig. 23 Hub endwall H-TEC.

Fig. 21 Suction side H-TEC.

Fig. 24 Distribution of pressure coefficient at 30% span.

3.5 Suction and Pressure Side C_p Distribution

The static pressure coefficient is calculated according Eq. (5) using the static wall pressure taps and mass averaged five-hole probe results.

$$C_{pi} = \frac{p_i - \tilde{p}_c}{\tilde{p}_{t,c} - \tilde{p}_c} \quad (5)$$

The subscript i in Eq. (5) relates to the local position and C refers to the measurement plane C at exit guide vanes inlet. Fig. 24 shows the static pressure

Fig. 25 Casing endwall H-TEC.

coefficient distribution at 30% span. For both setups the chord length is given in percent. Considering that fact it is clear that the fluid undergoes a steeper adverse pressure gradient (H-TEC) from peak suction to the trailing edge. The same pressure rise has to be achieved in a shorter vane.

3.6 Loss Estimation

Eq. (6) gives a rough estimation of the total pressure loss coefficient from plane C upstream of the EGVs to plane D downstream of the EGVs. The total pressure has been mass averaged by means of the five-hole-probe data.

$$\zeta = \frac{\bar{p}_{t,C} - \bar{p}_{t,D}}{\bar{p}_{t,C} - p_{ex}} \quad (6)$$

Table 6 compares the total pressure loss between both configurations. The values have been normalised with the total pressure loss coefficient ζ_{ref} of the reference configuration. Inverse cut-off TEC shows larger loss coefficients than the reference TEC. Also the H-TEC produces higher losses. From plane D0 to D the expected loss increase could only be measured for the H-TEC because of the missing plane D0 for the reference TEC.

The increased losses for the Inverse cut-off TEC and the H-TEC may originate in the higher sensitivity to wrong incidence, due to the much smaller leading edge radius. However at the aero design point the

Table 6 Total pressure loss for operating point approach.

Configuration	Total pressure loss $\frac{\zeta}{\zeta_{ref}}$	
	C-D	C-D0
Reference TEC	1.0	/
H-TEC	~ 1.3	~ 0.9

losses of the H-TEC are significantly ($\sim 50\%$ from C to D) lower than that of the reference TEC. The overall sound power levels for the main interaction modes at the aero design point are 2 dB higher for the H-TEC.

3.7 Comparison of the Masses of EGVs

Table 7 shows a summary of the masses of the EGVs in kg for the reference TEC and as a fraction regarding to the reference TEC for the H-TEC. Additionally it gives the values for a single EGV and for all EGVs in the TEC. Further, in order to have an upper limit for the design space a solid configuration and to set a lower limit a hollow configuration is given, where the wall thickness was chosen (by the first author) with 1 mm for all EGVs.

Comparing the solid configurations the EGVs of the reference TEC are significantly heavier than the H-TEC EGVs. However, when considering the hollow configuration the H-TEC EGVs are lighter than the EGVs of the reference TEC.

But it is very important to consider that the entire engine (nacelle and casing parts) may be shorter due to the reduced chord length (approx 1/3) of the EGV. That means that an additional weight reduction of the engine is expected compared to an engine with a state-of-the-art TEC configuration.

In Fig. 26 the weight of the EGVs is depicted. The inclined lines show the linear increase of the weight due to increased EGV counts. The red (hollow) and

Table 7 Weight of the EGVs.

Configuration	Mass of EGVs			
	Solid		Hollow	
	single	all	single	all
Reference TEC [kg]	0.44	6.54	0.12	1.76
H-TEC	$\sim 1/10$	$\sim 1/4$	$\sim 1/3$	~ 0.9

Fig. 26 Weight of the EGVs.

blue (solid) line belong to the reference TEC, the purple (hollow) and the green (solid) line belong to the H-TEC. Between the lines representing the solid and the hollow EGVs different EGV counts are possible (dependent on the wall thickness) to reach the same limit.

The black horizontal line depicts the mass of the solid EGVs of the reference TEC (for sure every other value can be chosen) and is used as a limit that should not be exceeded. The crossings with the four inclined lines give some certain EGV counts to keep the weight of the EGVs of the H-TEC below that limit of the reference TEC.

4. Conclusions

It is shown that an aerodynamically optimised TEC reduces losses at the aero design point, but it can increase losses at off design conditions as well as the overall sound power level of the first BPF tone is increased by about 2 dB at the aero design point and approach. However, the increase in sound power level reveals that an optimisation must not be done only considering aerodynamics but also acoustics has to be taken into account. The optimisation process should be in any case a combined process of aeroacoustics and aerodynamics. Considering results from Marn et al. [6] and from the present work a reduction (or at least keeping loss level) of loss and a reduction of sound power level seems to be possible all together keeping at least the weight of the engine. A combined aeroacoustic and aerodynamic optimisation is therefore a powerful tool for a holistic TEC optimisation.

Results from the five-hole-probe measurements showed that a much more uniform yaw angle distribution can be achieved for the H-TEC due to the larger EGV count.

The next step in this study will be the comparison of an aerodynamically optimised TEC and an acoustically optimised TEC.

Further, the weight of the EGVs is compared between the reference TEC and the H-TEC. A solid (as an upper limit) and a hollow (lower limit) version were considered. It is shown that it is possible to design an aerodynamically optimised TEC reducing the weight of the EGVs.

Additional weight reduction can be achieved if the struts in the s-duct are designed as an integrated concept (turning mid turbine frame) where the function of a first LP vane is integrated in the strut. Marn et al. [16] showed that a maximum weight reduction of the blading of 39% can be realised.

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