ABSTRACT
In modern turbomachinery design, one of the main objectives of aviation industry is the continuous research for higher performance with lighter engines. This trend leads to a reduction in the number of blades, which become increasingly thin and loaded, with a consequent increase in the occurrence of aeroelastic phenomena, compromising the structural integrity. This paper aims to present a numerical flutter assessment of two different types of blade assembly: a turbine cluster system typical of stator segments and an intentionally mistuned row representing an up-to-date low pressure turbine rotor. The numerical results obtained by a time-accurate CFD solver with vibrating blades will be compared with experimental data measured in the context of the EU project FUTURE. The first part of the paper will describe the study of a stator turbine cascade assembly, whose blades are mounted in packets and vibrate as a cluster mode. The comparison between numerical and experimental data showed an excellent agreement and further validated the aeroelastic solver. Then, the attention will be focused on the flutter analysis of an intentionally mistuned turbine rotor bladerow in comparison with a traditional row consisting of identical blades: this highlights how this type of assembly may stabilize the bladerow. The results of the numerical blade stability analysis show a flutter instability for the first bending mode which becomes stable, once the modal mistuning is introduced by adding masses at the tip of alternate blades. This numerically predicted flutter stabilization was confirmed by the experimental campaigns.

NOMENCLATURE
\[ L \] aerodynamic work
\[ \vec{c}_{\text{blade}} \] blade velocity
\[ \vec{n} \] surface normal unit vector
\[ a \] vibration amplitude
\[ E \] kinetic energy
\[ J \] number of packets
\[ m \] modal mass
\[ p \] static pressure
\[ T \] vibration period

Subscripts:
\[ \text{in} \] inlet quantity
\[ \text{out} \] outlet quantity
\[ s \] static quantity
\[ t \] total quantity

Greek:
\[ \omega \] angular frequency
\[ \Sigma \] blade surface
\[ \Xi \] aerodynamic damping coefficient
\[ \xi \] critical damping ratio

Acronyms:
\[ BCs \] Boundary Conditions
\[ CFD \] Computational Fluid Dynamics
\[ CSD \] Computational Solid Dynamics
\[ DFT \] Discrete Fourier Transform
\[ IPPA \] Inter-Packet Phase Angle
\[ nd \] nodal diameter
\[ PS \] Pressure Side
\[ SS \] Suction Side
INTRODUCTION

New concepts of blisk design are trending toward the idea of mounting the blades in sectors on the bladerow, rather than individually, in order to reduce the manufacturing and assembling costs and decrease the number of components. This type of assembly is called “blade cluster” and it has the advantage that it is more convenient and easy to be mounted. At the same time aeroengine manufacturers tend to design lightweight and highly-loaded turbine blades more and more prone to aeroelastic instabilities. For this reason, a further important objective for the turbomachinery industry is to avoid fatigue failure due to aeroelastic phenomena such as flutter and forced vibration. It is therefore essential to numerically study the flutter response of turbine bladerows with CFD methods. In recent years thanks to the growth in computer technology, numerical methods have become more and more accurate and faster and non-linear methods are becoming a viable alternative to linearized techniques, especially when strong non-linear effects (shock waves, flow separation, etc...) are present. Moreover, wider tangential domain including entire cluster packets can be now easily simulated with full-annulus or phase-lagged strategy. The first flutter analyses of grouped turbine blades were performed by Whitehead and Evans (1992) and showed computational results in agreement with the experimental data. Whitehead and Evans (1992) firstly proved the beneficial effect of grouping blades in packets, while Chernysheva et al. (2003) studied the aerodynamic stability of a 6-blade low pressure turbine packet, highlighting an important stabilizing effect. Corral et al. (2007) have later shown the stabilizing mechanism of welding the rotor blades in packets and have also demonstrated the stability increase of flutter unstable rotors using intentional mistuning (Martel et al. (2008)). In this research context, the 3D Traf code, already able to perform flutter computations (Pinelli et al. (2009, 2015)), was further extended to deal with packets of vibrating blades in order to assess the flutter stability of turbine assemblies.

First numerical investigations of a blade cluster presented in the paper were focused on a turbine cluster system tested at the École Polytechnique Fédérale de Lausanne (EPFL) (Zanker et al. (2013)). The analyzed bladerow consists of 20 blades, each one equipped with its own independent suspension system for controlled vibration able to create “single blade” or “cluster system” traveling waves. Special attention will be paid to the 4-blade cluster system oscillating in torsion mode. All the details of these experimental campaigns, carried out in the context of EU project FUTURE, can be found in Zanker et al. (2013). In the second part of the paper, the attention will be turned to mistuned rows which are introduced to mitigate the risk of flutter vibrations. This design strategy consists of mounting adjacent blades with different modal and/or geometrical characteristics (Martel et al. (2008)). After validating the solver with the EPFL cluster mode system, the method has been applied to the LPT rotor tested at Centro de Tecnologías Aeronáuticas (CTA). This blisk has been experimentally studied both in tuned and in mistuned configurations within the FUTURE project, in order to highlight the stabilization effect due to mistuning. The tuned configuration showed high flutter instability for a wide range of negative nodal diameters whereas the mistuned blisk was stable as also shown by Korte and Peitsch (2013). The results of the numerical simulations reported hereafter confirmed the experimental evidence measured by a tip timing technique and unsteady pressure probes. In this paper, numerical flutter results obtained by the 3D Traf code (non-linear uncoupled flutter method) with a phase-lagged approach are compared with experimental measurements. Both EPFL cluster mode and CTA mistuned bladerow required blade packet simulations and the results showed a good agreement with experimental data. This validates the numerical method for cluster system and confirms the beneficial effect of blade assembly on flutter stability.
AEROELASTIC TEST CASES

Various blade assembly configurations were experimentally investigated during the FUTURE project for the first time. Dedicated experimental campaigns were focused on blade cluster system and intentionally mistuned blisks and provided very high-quality data to the aeroelastic community. These measurements are extremely useful to improve flutter physical understanding and for code validation.

EPFL cluster system

Different cluster system testcases were simulated in the non-rotating annular test facility at EPFL (Bölcs (1983)). This facility can be used to reproduce steady and unsteady (with vibrating blades) testing of turbine and compressor cascades. Each blade is equipped with its own controlled vibration system which ensures independent blade oscillation. Blades can be controlled in frequency, amplitude and phase in order to reproduce “single-blade” and “cluster” bladerow deformations (Zanker et al. (2013)).

![EPFL cluster system setup (left), artificial torsional mode shape (right)](image)

Figure 1: EPFL cluster system setup (left), artificial torsional mode shape (right)

The numerical analyses presented in the following will be focused on the “single blade” and 4-blade “cluster system” torsion deformation in subsonic conditions. As shown in the left side of Fig. 1, the cluster system setup is composed by 4 adjacent blades which are moved in an axial bending direction with different amplitudes and phases in order to reproduce the cluster system torsion (see right side of Fig. 1). The cluster outer blades ("1st" and "4th") have a nominal vibration displacement amplitude \( h \) in opposite directions, while the two inner blades ("2nd" and "3rd") still oscillate in opposite directions, but with amplitude \( h/3 \). The amplitude \( h \) was chosen to produce a 0.04° torsion deformation of the 4-blade cluster. Since the whole bladerow includes 5 cluster packets, 5 traveling waves are possible for this cluster system configuration. Each traveling wave is associated to a single IPPA (Inter Packet Phase Angle):

\[
IPP A = \frac{2\pi}{J} j \quad \text{with} \quad j \in \mathbb{Z} : -\frac{J}{2} \leq j < \frac{J}{2}
\]  

CTA mistuned row

The second testcase represents an up-to-date low pressure turbine rotor composed by 146 blades. This row was designed to operate in subsonic conditions and, more importantly, to experience flutter instability at the first bending mode. The blisk was tested in different configurations at CTA rig in Bilbao. The design of the blisk allowed the insertion of additional masses with a specific pattern at the blade tip (see Fig. 2) thus introducing modal mistuning. Potential blade oscillations due to flutter occurrence were measured both by means of tip timing techniques at blade tip and microphones installed at the rotor exhaust. A numerical analysis similar to the one presented hereafter can be found in Korte and Peitsch (2013).
NUMERICAL METHOD

The 3D aerodynamic solver Traf was extended to analyze unsteady flows around vibrating rows, thus implementing an aeroelastic non-linear method able to deal with tuned rows, cluster systems and mistuned disks. The method description and the validation in subsonic and transonic tuned turbine environments can be found in Pinelli et al. (2009, 2015). To perform a flutter analysis, time-sinusoidal blade vibrations coming from modal analysis of single blades or blade packets are assigned to the unsteady calculations as input data. A mesh perturbation technique is used to actually deform the grid at each time step. The non-linear unsteady flow equations are integrated on the deforming domain by applying the same dual time-stepping algorithm used for aerodynamic unsteady simulations. Traveling waves are solved on a single-angular-pitch domain by applying phase-lagged conditions to periodicity boundaries. Fourier transforms are used to assign quantities on periodicity boundaries with appropriate time shifts, limiting the memory storage requirements. Two tangential blocks are simulated in order to speed up the convergence (Giovannini et al. (2014)). To analyze blade cluster systems and mistuned rows, the phase-lagged approach was further extended to deal with tangential multi-block configurations allowing the analysis of row sectors composed by different blades with a given IPPA.

Mesh deformation strategy

In order to deform the computational domain according to a single blade oscillation or to an assembly mode, a mode shape transfer technique is used to interpolate a real or a complex mode from the modal analysis results to the blade surfaces within the multi-block CFD mesh. When referring to classical flutter problems, the row oscillation is the only source of unsteadiness and, obviously, the whole fluid domain (where Navier-Stokes equations are numerically integrated by the solvers) has to be deformed following the bladerow vibration. The non-linear method employs a grid deformation strategy to actually rebuild the computational domain at different equally-spaced instants over the oscillation period. It is essential that the volume of each cell must be positive during the whole deformation in order to avoid cell intertwining. For this reason, the grid deformation is built by using an algebraic method which distributes the largest deformations where the biggest mesh elements are located, thus maintaining the shape of the smallest elements (located, for instance, within the boundary layer or near the blade clearance). The amplitude distribution of the mesh displacement in a blade to blade section can be seen in Fig. 3 and in Fig. 4. Anyway, as the blade oscillation is imposed, the grid deformation can be checked in advance before running the non-linear flutter simulations. The aforementioned pro-
Procedure was employed to build the mesh deformation for both testcases. For the EPFL cascade, as the blade system deformation is restricted to the bending spring (see left side of Fig. 3) and there is no coupling between blades, rigid oscillations are imposed to the blade surfaces. For each blade, frequency and amplitude of the oscillation are taken from experimental measurements and composed together to reproduce the cluster torsion. More in detail, 4 artificial real mode shapes were created and used to deform the upper and lower blade surfaces within each of the 4 H-type grids composing the cluster. In the first 3 channels within the cluster (B, C, and D in Fig. 3) the deformation phase between tangential boundaries is set to zero, whereas the upper and lower blade surface within the fourth channel (E) were shifted by the possible IPPAs (see Fig. 3 which depicts the mesh displacement amplitude in a blade to blade surface near the blade tip). In order to create the deformation for the CTA mistuned row, the same procedure for a 2-blade sector was employed. This time the two bending mode shapes (at lower and higher frequency) are extracted from an actual modal analysis of a 2-blade mistuned sector (see left side of Fig. 4). Again the blade oscillation is imposed to upper and lower blade surface of the H-type meshes which are smoothly deformed to follow the mode shape with a given IPPA (see right side of Fig. 4). The oscillation amplitude was chosen to reproduce blade displacements typical of the flutter onset.

Figure 3: EPFL rig: instrumented blade (left), amplitude of grid displacement near tip (right)

Figure 4: CTA rig: mistuned mode shapes (left), amplitude of grid displacement (right)
Flutter assessment

Since in uncoupled methods blade vibrations are input data, rather than a result of the computation, flutter stability is estimated by checking the sign of the aerodynamic work done by the fluid onto the blade during one vibration cycle. This quantity is integrated over the vibration period on the whole blade surface as follows:

\[ L = \int_{t}^{t+T} \int_{\Sigma} (-p) \hat{n} \cdot \vec{v}_{\text{blade}} d\Sigma dt \]  

(2)

where \( \Sigma \) is the blade surface, \( T \) is the vibration period, \( p \) the unsteady pressure, \( \hat{n} \) is \( \Sigma \) outgoing normal unit vector and \( \vec{v}_{\text{blade}} \) is the blade velocity. As usual the global aerodynamic work may be normalized to obtain damping coefficients (Pinelli et al. (2015)): for the EPFL testcase comparisons the aerodynamic damping coefficient \( \Xi \) for each blade is used, while for the CTA testcase the energetic damping coefficient (also known as critical damping ratio) \( \xi \) is computed.

\[ \Xi = \frac{-L}{\pi (p_{t_{\text{in}}} - p_{s_{\text{in}}}) \ell^2 b A^2} \quad \xi = \frac{-L}{8\pi E} = \frac{-L}{2\pi m\omega^2 a^2} = \frac{-L}{8\pi^3 m \nu^2 a^2} \]  

(3)

where \( p_{t_{\text{in}}} \) is the total inlet pressure, \( p_{s_{\text{in}}} \) is the static inlet pressure, \( \ell \) and \( b \) are the reference midspan axial chord and the reference span, \( A \) a defined blade displacement, \( E \) is the blade average kinetic energy, \( m \) is the blade modal mass, \( a \) is the modal amplitude, \( \omega \) is the angular frequency and \( \nu \) is the frequency of the vibration. To further compare the numerical results with the experimental measurements for the EPFL testcase, the unsteady pressure fluctuations over the blade surface were decomposed with a DFT algorithm for each harmonic \( (n) \) and the unsteady pressure perturbation coefficient \( C_{p}^{(n)} \) was computed as follows:

\[ p_{k}^{(n)} = \frac{1}{N_{\text{div}}} \sum_{k=0}^{N_{\text{div}}} p_{k} e^{-i2\pi nk/N_{\text{div}}} \quad C_{p}^{(n)} = \frac{p_{k}^{(n)}}{(p_{t_{\text{in}}} - p_{s_{\text{in}}}) A} \]  

(4)

The analyzed pressure time history \( p_{k} \) is equally spaced, and it was noticed that around 80 instants during the oscillation period are enough to accurately compute the aerodynamic work and to extract the first pressure harmonic. Once \( C_{p}^{(1)} \) has been calculated with a DFT algorithm, its amplitude and phase can be plotted at a required span and compared with experimental data.

NUMERICAL RESULTS

EPFL testcases

Among the different aeroelastic configurations tested at the EPFL test rig, a single blade torsion mode and a 4-blade cluster system with packet torsion oscillation were selected for the numerical analysis. The two flutter campaigns share an identical steady flow which is a subsonic condition for the row. The unsteady tests with vibrating airfoils were constructed at a given frequency and included all the possible IBPAs/IPPAs: blade oscillation amplitude and phase and steady/unsteady pressure measurements are available for each tested configuration.

Steady Results

Before starting with unsteady simulations, it is essential to reproduce the steady condition as accurately as possible. This flow field will be used to initialize the computation with moving blades. Steady state simulations were performed with a \( k - \omega \) turbulence model in high-Reynolds formulation (Wilcox (1998)) and the BCs were taken from spanwise measurements at
<table>
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<th></th>
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<tr>
<td>$M$</td>
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<td>0.68</td>
</tr>
<tr>
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<td>-58.0</td>
</tr>
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<td>135.0</td>
<td>130.6</td>
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<tr>
<td>$P_s$ kPa</td>
<td>124.3</td>
<td>95.6</td>
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Figure 5: EPFL: aerodynamic conditions (left), isentropic Mach number at midspan (right)

row inlet/outlet. Fig. 5 reports the aerodynamic conditions and compares the isentropic Mach number distribution at midspan. Pressure spanwise distributions and the Mach number contours at blade midspan are shown in Fig. 6.

**Flutter Results**

First flutter simulations of the EPFL test rig concerned the analysis of the “single blade” torsion mode. The computations for each possible IBPA where performed with phase-lagged approach until the solution reached periodicity. Fig. 7 shows that the computed aerodamping curve is in good agreement with experimental measures, although the aerodamping from measurements was computed only from the unsteady pressure values acquired at midspan (where the unsteady probes are located), whereas the numerical aerodamping was calculated over all the blade surface. In addition to the aerodamping curve the experimental campaigns provided the amplitude and phase of the unsteady pressure coefficient at the blade midspan for each IBPA. This complex quantity can be obtained from the numerical results by performing the DFT of the pressure blade response and by computing the unsteady pressure coefficient of the 1$^{st}$ harmonic as described in the right side of equation (4).

To have an overall view of blade midspan response for all the different IBPAs, the unsteady
pressure coefficient distributions can be gathered together in a 2D contour map where the amplitude and phase are plotted as a function of the normalized blade curvilinear coordinate and of the IBPA. An excellent signal-to-noise ratio for the unsteady pressure measurements confirms the accuracy of the experimental data. Fig. 8 compares the numerical and experimental 2D contour plots for the amplitude and phase of the unsteady pressure coefficient: a very satisfactory agreement was found. It is worth nothing that the maximum of the pressure response is located on the first part of the blade suction side where the mean velocity gradient is higher. As far as the phase shift (between unsteady pressure response and surface motion) is concerned, it defines

Figure 7: EPFL single blade: aerodynamic damping (left), local aerodynamic damping (right)

Figure 8: EPFL single blade, unsteady pressure coefficient amplitude (top) and phase (bottom) maps, computed (left), measured (right)
the transfer of energy from blade to fluid (or vice-versa), so it is essential that numerical simulations are able to properly compute this aspect for the flutter assessment. As shown in Fig. 8, the phase shift is correctly captured by the simulations. Afterwards the four-blade cluster was computed still using the phase-lagged approach based on cluster IPPA. In this configuration, it is useful to distinguish between outer and inner blades. The two outer blades (1\textsuperscript{st} and 4\textsuperscript{th}) have a higher oscillation amplitude and one of the blade surfaces delimits one of the two channels (A and E) where the IPPA shift is imposed. On the other hand, the inner blades (2\textsuperscript{nd} and 3\textsuperscript{rd}) vibrate less and are included in the 3 inner channels (B, C, and D) where blade surfaces are always in phase (B and D) or anti-phase (C). This entails that the outer blades have a higher aerodynamic response with high variation over the different IPPAs. This aspect can be clearly seen in Fig. 9 which shows the aerodamping for each of the 4 blades composing the cluster. Numerical results (dashed red lines) are compared with measurements (dashed blue line). The dotted blue lines define the confidence interval. The agreement is very good, especially for the outer blades. The unsteady pressure coefficient 2D contour maps at midspan of the outer blades are reported in Fig. 10 and Fig. 11. It is easy to identify the two blade surfaces located between different 4-blade clusters (that is to say, the SS of 1\textsuperscript{st} blade and the PS of the 4\textsuperscript{th} blade), which experience the highest channel deformation and consequently a higher pressure response. Again, a satisfactory agreement between numerical and experimental data was found. This numerical evidence is in agreement with the discussion included in Zanker et al. (2013).

![Figure 9: EPFL cluster system: aerodynamic damping for each blade](image-url)
Figure 10: EPFL cluster system: 1st blade $C_p^{(1)}$ amplitude (top) and phase (bottom), computed (left) and measured (right)

Figure 11: EPFL cluster system: 4th blade $C_p^{(1)}$ amplitude (top) and phase (bottom), computed (left) and measured (right)
CTA testcases

The solver extension for the analysis of blade clusters and its validation through experimental data provided by FUTURE project was a prerequisite to study the testcase with intentional mistuning, as the bladerow deformation of a cluster or mistuned packet rests on the same concepts. Before studying the mistuned configuration, once again the “single blade” (tuned) configuration was analyzed. This tuned configuration was designed to be aerodynamically unstable to the flutter vibration and it was numerically studied here as a reference case in order to assess the beneficial effect of mistuning. This bladerow was designed to be representative of low pressure turbine rotors ($M_{in} = 0.69$ and $M_{out} = 0.40$).

Tuned system

The “single blade” (tuned) flutter analysis was focused on the first bending mode of the cantilever configuration. The critical damping ratio curve, computed for selected nodal diameters (see Fig. 12) shows a sinusoidal trend and highlights a flutter instability for a large range of negative nodal diameters. The experiments proved the high instability of the bladerow, and the signal recorded by the tip timing system and unsteady pressure probes revealed the presence of several unstable nodal diameters. From numerical results, the most unstable nodal diameter corresponds to $nd = -24$ and this is in agreement with the expectation for this type of rotor row and with the experimental evidence.

Mistuned system

When modal mistuning was introduced into the blade row by placing an additional mass on the tip of alternate blades, the measurements showed no bladerow vibrations, confirming the stabilizing effect of mistuning. For the numerical study of the mistuned configuration the two first mode shape families of the 2-blade packet are considered: the mode shape family with lower frequency where only the blade with mass at the tip vibrates within the packet and the mode shape with higher frequency where the blade without mass oscillates the most. No coupling between modes at close frequencies was considered in the following analysis, although introducing the coupling effect may alter the aerodamping curves which however remain stable, as shown in Korte and Peitsch (2013). Mistuned configurations were computed with phase-lagged conditions applied at the circumferential boundaries of the 2-blade sector, and the quantities

![Figure 12: CTA testcases: single blade (left) and mistuned row (right) critical damping ratio](image-url)
used to assess flutter stability are the same as for the single blade case. Critical damping ratio curves for the two families are presented in Fig. 12 as a function of nodal diameters of the 2-blade packet (“Mode Shape H” indicates the mode shape with higher frequency and consequently “Mode Shape L” indicates the mode shape with lower frequency). The stabilizing effect of mistuning for each mode shape family and the lower response of the bladerow are clearly visible. This latter aspect is due to the fact that in each mistuned packet deformation only one blade vibrates and this causes a lower response of the entire bladerow in comparison to the tuned “single blade” deformation.

CONCLUSIONS

The flutter assessment of two different turbine blade assemblies (cluster system and mistuned row) was numerically performed with a non-linear uncoupled method based on the 3D Traf code. For these assembly analyses the code was extended in order to deal with circumferential packets of blades and the result comparisons with experimental data (provided in the context of the FUTURE project) reported in this paper validate the method.

The first analyzed assembly is a 4-blade turbine cluster system typical of stator rows. The torsional behavior of this stator segment was reproduced at the EPFL test rig and unsteady pressure response and aerodamping values were measured for all the possible IPPAs. Comparisons between numerical and experimental values show a satisfactory agreement and validate the method.

Afterward the method was also applied to an intentionally mistuned row in order to highlight the beneficial effect of mistuning on flutter stability. The aerodamping results of the mistuned row were compared with the respective tuned configuration (which showed a high instability for negative nodal diameters) highlighting the bladerow stabilization due to modal mistuning. This numerical evidence was experimentally confirmed by means of tip timing and unsteady pressure probe measurements.

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