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Optimization of Offshore Wind Turbine Support Structures Using an Analytical Gradient-Based Method

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Abstract

Design optimization of offshore wind turbine support structures is an expensive task; due to the highly-constrained, non-convex and non-linear nature of the design problem. This paper presents an analytical gradient-based method to solve this problem in an efficient and effective way. The design sensitivities of the objective and constraint functions are evaluated analytically while the optimization of the structure is performed, subject to sizing, eigenfrequency, extreme load and fatigue load constraints. A case study was carried out for the OC4 jacket substructure to evaluate the method. Results show that an optimal jacket design with 52 percent structural mass reduction was attained in 27 iterations, while satisfying all design constraints under the simplified load cases used. Besides, it is shown that the analytical sensitivity analysis was more accurate and efficient than the often used finite difference approximations. It could avoid numerical artifacts that typically occur in the analysis of extreme load constraint sensitivities.

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Keywords: Structural optimization; sensitivity analysis; gradient-based; offshore wind; support structures.

1. Introduction

A support structure system typically contributes around 17 percent of the total capital cost in an offshore wind project [1]. It is an area which attracts numerous studies in academia and industry, due to the potential for cost reduction. Despite that structural optimization is heavily used in the automotive and aerospace industries; its

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implementation in offshore wind turbine support structure design is comparably limited. Previous studies have suggested that the design of various types of offshore wind turbine support structures, e.g. monopile, jacket, full-height lattice tower and spar-type floater, etc., can attain cost savings by using different simulation-based optimization approaches [2,3,4,5]. However, the design process often involves a large number of iterations since the optimization problem is highly constrained and non-convex [6]. Furthermore, the dynamic analysis of an offshore wind system is prescribed to be carried out in time domain simulations in order to capture nonlinearities and time-history dependence, thus resulting in high computational demands [7]. As for the sensitivity analysis, a finite difference method is normally employed to handle the complicated gradient calculation. Although the method is easy to implement, it suffers from computational inefficiency and possible numerical errors [8]. As a result, these bring the focus to research on an optimization methodology which is effective and efficient in designing the offshore wind turbine support structures, particularly for complex structures that have many design variables, e.g. space frame structures.

2. Integrated Optimization Framework

The design of offshore wind turbine support structures is a non-linear dynamic response constrained structural optimization problem. An integrated dynamic simulation and optimization tool was developed in Matlab to solve the problem. It followed an iterative optimization procedure and the framework is as shown in Fig. 1.

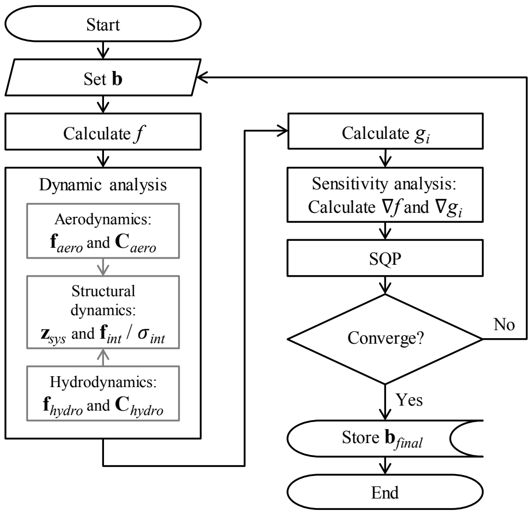

Fig. 1. Flowchart illustrating the integrated design optimization framework.

Generally, the objective function f to be minimized is the support structure mass. In the dynamic analysis, the finite element method was employed to solve the overall offshore wind turbine (OWT) system response. The rotor was subject to aerodynamic loads (i.e. aerodynamic damping $\mathbf{C}_{aero}$ and excitation $\mathbf{f}_{aero}$ forces) while the submerged parts were applied with hydrodynamic forces (i.e. hydrodynamic damping $\mathbf{C}_{hydro}$ and excitation $\mathbf{f}_{hydro}$ forces). The former was calculated using FEDEM WindPower Version R7.0.4 [†] while the latter was determined using the Morison formula. Subsequently, internal forces $\mathbf{f}_{int}$ or stresses σ_{int} were recovered from the support structure response $\mathbf{z}_{sys}$ and design constraints g_i were computed. The g_i were based on limit state functions prescribed by

[†] http://www.fedem.com/

the design standards and recommended practices used within offshore and wind industries [7,9,10]. They can be classified into:

- Sizing constraints g_1 and g_2 which define the lower and upper bounds of the design variables $\mathbf{b}$ as well as the geometrical relationships among the variables, respectively.
- Natural frequency constraint g_3 which ensures that the first mode natural frequencies are out of the wind (including the rotor rotational 1P and blade passing 3P) and wave excitation frequency zones.
- Extreme load constraints which are based on the ultimate limit state (ULS) analysis performed on tubular members (g_4 - g_9) and joints (g_{10}) to check if structural strength and stability requirements are satisfied.
- Fatigue load constraint g_{11} which is based on the fatigue limit state (FLS) analysis performed on tubular joints to confirm that a minimum survivability of 20 years design lifetime is attained under the design load case.

As for the design sensitivity analysis, gradient vectors of the objective function ∇f and the constraint functions ∇g_i were calculated using the analytical direct differentiation method (DDM) [11]. Detailed descriptions about the design constraints implemented and the methods to calculate the corresponding sensitivities are discussed in [12]. The gradients were then required by the Sequential Quadratic Programming (SQP) optimizer to determine the best direction for improvement in the successive iterations, until the final design $\mathbf{b}_{final}$ converged.

3. Case Study on the OC4 Offshore Wind Jacket Substructure

The optimization framework was evaluated in a case study performed on the offshore wind turbine jacket substructure used within the IEA Task 30 OC4 Project. The OWT system consists of the well-known 5 MW horizontal axis three-bladed baseline turbine developed by National Renewable Energy Laboratory (NREL) and the support structure system that includes a monopile tower, a concrete transition piece and a jacket substructure [13,14]. The overall wind turbine was assumed to be rigidly clamped onto the ground.

3.1. Design Variables

The jacket substructure is a symmetrical four-legged design which comprises four bays of X-braces and a bottom mudbrace at each side, see Fig. 2. The design variables selected for this study were the diameters and thicknesses of the jacket members. They were distinct for various bays and member types, either legs or braces. There were 22 design variables in total, i.e. $b_1 - b_{22}$, where the odd and even numbered design variables represented the member diameters and thicknesses, respectively. The initial values of the design variables were based on the OC4 jacket dimensions [14].

3.2. Design Load Cases

The OWT model was subject to combined wind and wave loads in the simulations. The wind and wave conditions were modeled as three-dimensional turbulent wind fields according to the von Kaimal spectral model, and as Wheeler stretched irregular waves according to the JONSWAP wave spectrum, respectively. The wind parameters, such as mean wind speed at hub height V_{hub}, turbulence intensity (ratio of standard deviation to mean wind speed) TI , wind gradient exponent α , as well as the wave parameters, such as significant wave height H_s, peak spectral period T_p and peak shape parameter γ are summarized in Table 1. The FLS load case modeled the operating power production condition under normal turbulent wind (NTM) and normal sea states (NSS); while the turbine was modeled in an idling condition under extreme wind (EWM) and extreme sea states (ESS) for the ULS load case.

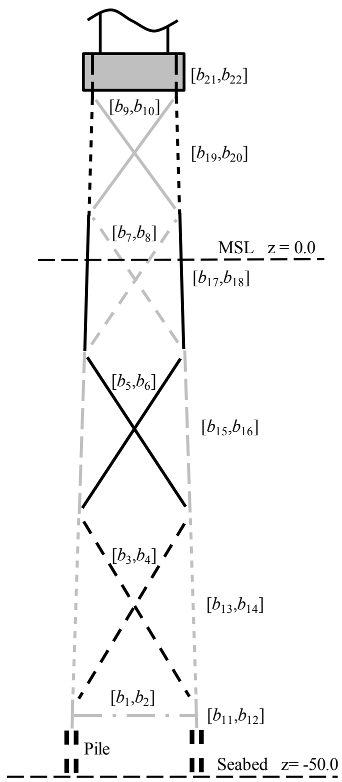

Fig. 2. The OC4 offshore jacket substructure (units are in m).

Table 1. Description of simplified load cases used in the case study.

Load Case	Wind Conditions	Wave Conditions
FLS	NTM (Kaimal spectrum)	NSS (JONSWAP spectrum)
	V_{hub} = 8.00 m/s	H_s = 1.31 m
	TI = 10.00 %	T_p = 5.67 s
	α = 0.14	γ = 1
ULS	EWM (Kaimal spectrum)	ESS (JONSWAP spectrum)
	V_{hub} = 42.73 m/s	H_s = 9.40 m
	TI = 10.00 %	T_p = 13.70 s
	α = 0.11	γ = 3.3

4. Results and Discussion

4.1. Accuracy of the Sensitivity Analysis

Table 2 summarizes the normalized root-mean-square deviations (NRMSD) of selected design sensitivities calculated using the DDM and finite difference methods (forward and central difference). The sensitivities were evaluated at the initial jacket dimensions against the brace diameter b_3, the brace thickness b_4, the chord diameter b_{15} and the chord thickness b_{16}, while numerical integrations were carried out at time steps of 0.01 s and 0.025 s, respectively.

The results in the first and second rows show that the central difference method attained good agreements with the DDM in estimating the displacement derivatives for both ULS and FLS load cases, which were generally less than 2.5 percent NRMSD; whereas the forward difference scheme yielded higher discrepancies of up to 6 percent NRMSD. The forward difference method generally experiences larger truncation errors in comparison with the central difference scheme, while using the same step size in the sensitivity analysis. The deviations have become more prominent when considering the nodal displacements for entire OWT system, reaching NRMSD of 12 percent. These large deviations occurred at the vertical displacement fields (z-axis) of the structure when varying the jacket leg dimensions. The truncation errors cascade upwards from the jacket to the other structural parts, e.g. transition piece, tower and nacelle, thereby causing large discrepancies. Similar trends could be observed in the eigenfrequency constraint derivatives when comparing between the finite difference methods. The central difference approximation generally performed better than the forward difference scheme, with much smaller NRMSD, except for $\mathrm{d}g_3/\mathrm{d}b_4$.

As for the sensitivities of the extreme load constraints in tubular beams, it can be seen from the data that the NRMSD were generally larger than the corresponding ULS displacement derivatives. The NRMSD could reach as high as 22.4 percent, when analyzing against the forward difference approximation with a 0.025 s time step. Upon investigation, it was found that these huge deviations took place at the beams which experienced alternating tension and compression modes in the sectional normal force or stress. The usage of different formulae between tension and compression modes has caused the ULS state functions to be discontinuous at these time steps. The discontinuities result in numerical errors when performing the finite difference calculations, due to the mismatches of ULS values at these affected time steps. In some cases, the discrepancies could be eliminated either when a smaller time step was utilized, or when the central difference method, with higher order of accuracy, was used, or both, since these methods might have removed the mismatches.

Regarding the extreme load constraints in tubular joints, the derivatives tallied well between the DDM and central difference method, giving NRMSD of below 2.5 percent. When employing the forward difference method in estimating the sensitivities against leg dimensions, the NRMSD were found to be relatively large and the results seemed to correlate with the displacement sensitivities under the ULS load case. Since the interaction equation applies the same formula for both axial tension and compression modes, it avoids the discontinuity problem. Finally, comparison between the analytical derivatives and the central difference approximations for fatigue load constraint yielded satisfying results that the NRMSD were well below 5 percent. However, no significant correlation was found between the results and the FLS displacement sensitivities, as the fatigue sensitivity analysis involves complicated calculation procedures and it also gets significant contributions from other derivatives, such as SCF derivatives [15].

In general, smaller time steps Δt used in the numerical integration are found to diminish the numerical errors in the sensitivity estimations for both finite difference schemes. Additionally, the accuracy of the finite difference method also depends on the step size Δb used in the sensitivity analysis. The two main sources of errors, i.e. truncation errors and condition errors, are positively and inversely proportional to Δb, respectively. The optimal step size can be determined from further analysis to minimize the total numerical error, yet this will introduce additional computational burdens [16].

In this study, the DDM and central difference method are shown to give comparable results in calculating the derivatives. The forward difference method, on the contrary is not recommended due to the large discrepancies in estimating the sensitivities. Furthermore, the DDM method is twice as efficient in the calculation process, since only N_{var} +1 number of system analyses are required instead of $2 * N_{var}$ +1 for the central difference method, for every function and gradient evaluation, where N_{var} is the number of design variables.

Table 2. Summary of NRMSD of the sensitivities computed using the DDM and finite difference methods.

Derivatives	Time step (method)	dg_i/db_3 [%]	dg_i/db_4 [%]	dg_i/db_{15} [%]	dg_i/db_{16} [%]
Displacement under ULS load case	Δt = 0.025 s Forward	2.6434 (2.4550)	2.8410 (2.3894)	5.9049 (7.4122)	5.7200 (6.7929)
	Δt = 0.025 s Central	2.1739 (1.8216)	2.4132 (2.0031)	1.0407 (1.1598)	0.9079 (0.9712)
	Δt = 0.010 s Forward	1.4582 (1.4695)	1.5102 (1.3090)	5.3705 (6.8260)	5.2418 (6.3035)
	Δt = 0.010 s Central	0.8757 (0.7349)	0.9719 (0.8085)	0.4860 (0.5387)	0.4042 (0.4340)
Displacement under FLS load case	Δt = 0.025 s Forward	3.6326 (4.3869)	2.9483 (2.7072)	4.3647 (11.918)	4.2949 (8.9243)
	Δt = 0.025 s Central	2.4404 (2.0997)	2.4661 (2.1671)	1.4902 (1.4805)	1.2174 (1.2375)
	Δt = 0.010 s Forward	2.4595 (3.2722)	1.5938 (1.5443)	3.8886 (11.258)	3.9032 (8.4451)
	Δt = 0.010 s Central	0.9634 (0.8360)	0.9721 (0.8638)	0.6113 (0.6695)	0.5030 (0.5441)
Eigenfrequency constraint	Δt = 0.025 s Forward	0.9439	1.0214	2.1064	2.0847
	Δt = 0.025 s Central	0.1463	2.4051	0.0258	0.0362
	Δt = 0.010 s Forward	0.9439	1.0214	2.1064	2.0847
	Δt = 0.010 s Central	0.1463	2.4051	0.0258	0.0362
Extreme load constraint (beam)	Δt = 0.025 s Forward	18.4280	22.3923	7.5369	8.4559
	Δt = 0.025 s Central	17.9255	1.5763	7.5181	8.4446
	Δt = 0.010 s Forward	18.2616	1.7916	7.4631	8.3772
	Δt = 0.010 s Central	0.7792	1.0068	7.3310	8.2352
Extreme load constraint (joint)	Δt = 0.025 s Forward	3.9071	2.3724	6.1640	7.3502
	Δt = 0.025 s Central	0.8636	0.9225	2.0554	2.1116
	Δt = 0.010 s Forward	3.5686	2.3008	4.7684	6.2169
	Δt = 0.010 s Central	0.4399	0.3299	2.2352	2.3758
Fatigue load constraint	Δt = 0.025 s Forward	6.9605	1.6801	2.7034	1.9704
	Δt = 0.025 s Central	4.5504	1.9618	1.8839	0.8800
	Δt = 0.010 s Forward	4.8361	2.0250	2.3996	1.8080
	Δt = 0.010 s Central	3.8099	1.4374	1.6962	0.5633

Note: Values in parentheses are the NRMSD of derivatives for overall OWT system.

4.2. Performance of the Integrated Optimization Method

This section discusses the performance of the integrated optimization framework. Fig. 2(a) depicts how the optimal support structure mass and the corresponding maximum constraint violations change during the optimization process. The active FLS constraint violations ascended sharply in the beginning of the process, which were in line with the significant mass reduction caused by the decreasing member dimensions in general; and progressively fell back to the limits in later iterations as the structural design approached the optimal design. The final jacket design attained a mass reduction of 52 percent as compared with the initial design.

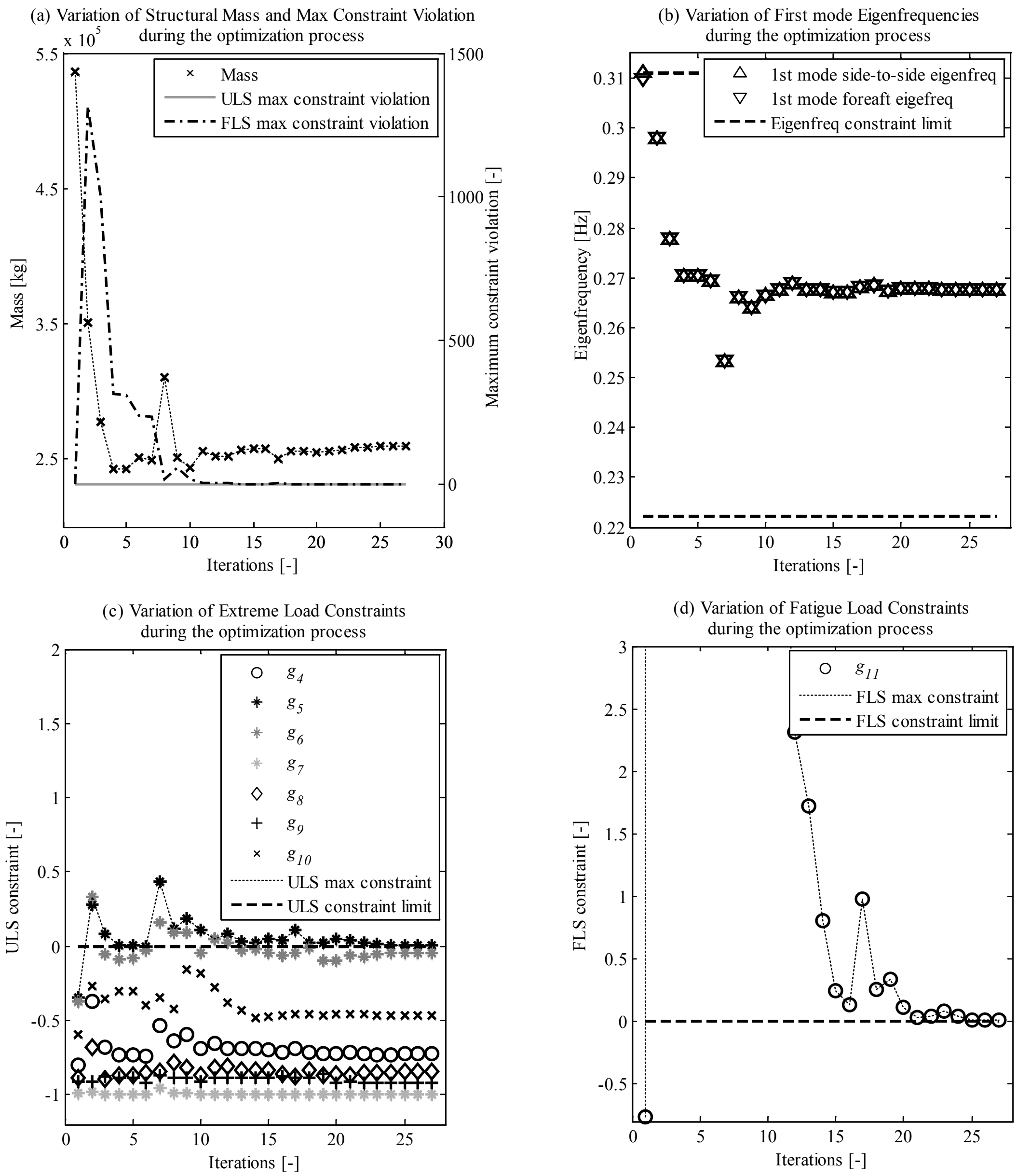

Fig. 3. Variation of design objective and constraint functions during the optimization iterations.

Details about the eigenfrequency, extreme load and fatigue load constraints are displayed in Figs. 2(b), (c) and (d), respectively. The first mode side-to-side and foreaft eigenfrequencies varied in similar patterns as the structural mass, indicating that a lighter support structure tends to be associated with a 'softer' design while maintaining the structural integrity against the limit states. Throughout the process, the eigenfrequencies fluctuated within the allowable limits, and finally converged to 0.27 Hz, which was at the center of the constraint limits.

Although the design of bottom fixed jacket substructures is generally fatigue driven, Fig. 2(c) shows that the ULS criteria are as important in governing the design process. In this optimization study, there were 1352 extreme load constraints in total, of which 1248 were contributed from the tubular beams (i.e. 6 constraints $g_4 - g_9$ for both ends of each beam) and 104 from the tubular joints (i.e. 2 constraints for each X- and K- joints and 1 constraint for each Y-joint). Among the list of ULS criteria implemented, buckling and compressive limit state functions, g_5 and g_6 were found to be active during the optimization process. The former remained as an active constraint along with the FLS design constraint at the final iterations, while the structure was gradually tuned to be less fatigue prone. The remaining extreme load constraints were found to be inactive throughout the optimization study. The tensile limit state function g_4 and the extreme load constraint for tubular joints g_{10} were relatively more 'active' in fluctuating within the allowable limit. On the contrary, the variation of the shear-bending-torsion interaction and hoop buckling constraints, g_8 and g_9 were very small at all iterations; due to the minimal shear force and torsional moment experienced in tubular members as well as the diameters of jacket members are relatively small for hoop buckling to occur. In addition, g_7 is a conditional design constraint, and it was not activated in most of the iterations.

Fig. 2(d) illustrates the fatigue load constraints change with respect to the optimization iterations. There were 208 constraints imposed in total, with 2 from each end of the members. In general, the g_{11} constraint enveloped the overall design constraints at all iterations. It was the final constraint to converge in the optimization procedure and it governed the feasibility of the optimal solution. Successful local minimal design solution was finally attained when both first order optimality measure and maximum constraint violation were less than the respective tolerance values.

The proposed gradient based optimization method has demonstrated that the non-linear programming design problem of offshore wind turbine support structure could be solved in 27 iterations, indicating a fast convergence in search for an improved design. Since the problem is non-convex, the global optimum of design solutions cannot be guaranteed. One way to carry out the global optimization is by performing multiple runs of a local optimization solver at different starting points. In addition, the OC4 jacket substructure was originally designed against the design load cases (DLC) formulated in the UpWind project [17]. The governing load cases DLC 6.1 for extreme conditions as well as the combined DLC1.2 and DLC 6.4 for fatigue conditions have determined the final jacket substructure design with thickening at some joint cans. By applying only two simplified load cases in the current study (i.e. modified DLC 6.1a and DLC 1.2c with a reduction on the *TI* and without consideration of the structure orientation and wind-wave misalignment), the initial jacket substructure can be well conservative and hence giving the result of a significant mass reduction. However, knowing that the jacket members and joints are not all fully utilized, the OC4 jacket model offers plenty of optimization potential. Detailed optimization subject to the comprehensive design load cases could be extended readily using the same framework. This will be addressed in the future work.

5. Conclusions

This paper presented an analytical gradient-based optimization framework to solve the dynamic constrained optimization problem of offshore wind turbine support structures. The framework was validated using the numerical OC4 offshore wind turbine jacket model. Several key findings include:

- The central difference approximation matched well with the analytical DDM in the design sensitivity analysis. The DDM was also capable of avoiding potential numerical errors occurring during the calculation of extreme load constraint gradients for tubular beams. The numerical errors are due to the discontinuities of ULS utilization factors when the beams are experiencing internal forces/ stresses switching between tension and compression modes.
- The DDM method is comparably more efficient than the finite difference methods since it requires less number of equations to be solved during the optimization process for a similar or better level of accuracy.
- The overall optimization framework has demonstrated a successful and efficient search for an improved design solution, while satisfying all design constraints.

- Both fatigue and extreme load constraints have exhibited important influences in determining the optimal design solution during the optimization process. The FLS constraint was the critical constraint which decided the final design. As for the ULS constraints, buckling and compressive load constraints were found active during the optimization process while the rest were well below the constraint limit.

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